

proceedings of the



international meeting on

FAST REACTOR SAFETY AND RELATED PHYSICS

Chicago, Illinois, October 5-8, 1976

Volume IV

PHENOMENOLOGY

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FAST REACTOR SAFETY AND RELATED PHYSICS

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Volume IV: PHENOMENOLOGY

Sponsored by
American Nuclear Society
European Nuclear Society

In Cooperation with
Energy Research and Development Administration

Honorary Chairman: Robert G. Sachs
General Chairman: Donald T. Eggen (ANS)
General Cochairman: Michel Banal (ENS)

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FOREWORD BY THE GENERAL CHAIRMAN

The International Meeting on Fast Reactor Safety and Related Physics was jointly sponsored by the American Nuclear Society (Nuclear Reactor Safety Division, Reactor Physics Division, Chicago Section), and the European Nuclear Society. It provided a forum for exchange of information concerning all aspects of fast reactor safety. The timeliness and international character of the meeting was apparent from the large attendance (580 engineers and scientists), coming from ten different countries.

The European Nuclear Society, organized shortly before detailed planning for the meeting was started, joined in co-sponsorship through the good offices of Alain Colomb and Pierre Zaleski, respectively, President and Vice-President of ENS. There is no doubt that the large attendance from outside the US was due to a considerable extent to the organizational support received from Europe and Japan as well.

Special gratitude is expressed to Michel Banal, General Co-Chairman of the meeting representing ENS, Member of the Board of Directors of Electricité de France and President of the Board of Directors of NERSA, and to Robert G. Sachs, Honorary Chairman of the meeting, Director of Argonne National Laboratory and Professor at the University of Chicago, for their support and contributions. The presence of Laura Fermi at the banquet was greatly appreciated and added historical perspective to the significance of the meeting.

The luncheon address by Jean Mégy, Deputy President of Novatome, the dinner address by Hans A. Bethe, Professor at Cornell University, and the closing address by Michel Banal, formed some of the highlights of the meeting and added considerably to its significance.

The Steering Committee acknowledges its indebtedness to many persons for their support and contributions, including the chairmen and members of the various committees, the conference staff, the student projectionists, etc. While it is not possible to thank everybody individually, it is appropriate that the names of some of the key persons are given in the committee listings included in these volumes. Special thanks for outstanding services rendered are due to the Technical Program Committee, the International Advisory Committee, and the Special Consultant; their efforts resulted in a most valuable technical program. The difficult and lengthy tasks of transcribing and editing the recorded discussions pertaining to all the special sessions was performed under the able and careful direction of William J. Sturm, Chairman Publication Committee, assisted by Jan B. van Erp, Associate Technical Program Chairman.

Finally, sincere thanks are due to all attendees for their many contributions, either through participation in the discussions or otherwise. It is hoped that this set of Proceedings will provide an accurate record of the state-of-the-art and the spirit of international cooperation prevalent during the meeting.

Donald T. Eggen
Evanston, Illinois

The safety of nuclear power reactors is, by its nature, a subject that is best studied and dealt with on an international basis. The co-sponsorship, on an equal basis, of the International Meeting on Fast Reactor Safety and Related Physics held in Chicago, Illinois, by the American Nuclear Society and the European Nuclear Society, is in keeping with a long-standing tradition of intensive and open international dialogue on all matters concerning nuclear reactor safety.

The usefulness, and thus the success, of any scientific and technical meeting depends to a very large extent on the resourcefulness and willingness to contribute of the members of the Technical Program Committee, who are charged with the planning and organization of the technical content of the meeting. Therefore if the International Meeting on Fast Reactor Safety and Related Physics is judged to have been successful it is to a very large extent attributable to the Technical Program Committee, which consisted primarily of members of the Nuclear Reactor Safety Division and the Reactor Physics Division, the two sponsoring ANS professional divisions.

Consistent with the international character of the meeting, a particularly large contribution in the planning and organization of the meeting was also made by the non-US members of the Technical Program Committee, namely the Special Consultant, the ENS-appointed Associate Technical Program Chairman, and the Members of the International Advisory Committee. No doubt the large participation and the high quality of the contributions from outside the US are to a large extent due to their considerable efforts.

The apparent timeliness of the meeting resulted in a large number of contributed papers, necessitating an increase in the number of parallel sessions beyond that originally intended and beyond what is usually considered optimum. The alternative, a large rejection rate, was considered but found not to be acceptable in view of the high quality of the papers.

Nuclear reactor safety, like most aspects of any technology, has evolutionary characteristics, and can only develop hand-in-hand with the development of the technology itself. Though perhaps some safety issues pertaining to postulated low-probability events have not been answered in all detail, there is no doubt that - and this appears to have been the consensus at the meeting - the large body of safety-related knowledge amply justifies proceeding prudently with the construction and operation of demonstration fast breeder power plants at this time. It is therefore also not too soon to initiate the development of internationally accepted safety-related design criteria and standards. This point was forcefully and eloquently brought home by, among others, M. Jean Mégy in his interesting luncheon speech. This is one of many areas for profitable international cooperation.

It would be an omission not to mention the important contributions to the meeting made by Professor Hans A. Bethe, truly the internationally accepted mentor of fast reactor safety, which were not limited to his interesting and informative dinner speech but included many penetrating remarks and questions made during the sessions as well. We wish to thank him most cordially for his willingness to participate. Finally thanks are due to all attendees - authors, session chairmen, and others - for their contributions to, and participation in, this meeting which is hoped to have promoted continued and intensified international cooperation in the development of fast breeder reactors.

R. Avery, Chairman
Technical Program Committee

PREFACE

As presented in these volumes, the Proceedings of the International Meeting on Fast Reactor Safety and Related Physics incorporate a number of departures from straightforward replication of the technical program. First, the textual matter was regrouped into four somewhat topical or functional categories as follows:

- Volume I - Invited Papers; Panels; Summary
- Volume II - Licensing; Safety-Related Design
- Volume III - Accident Analysis; Structural Dynamics
- Volume IV - Phenomenology.

With this separation of papers it is possible for the reader to purchase or consult volumes more specific to his individual needs or interests. Volume I should have the broadest appeal to lay and professional readers; subsequent volumes are topically grouped to be more conveniently useful to the technical generalist and specialist alike.

The Proceedings also include the floor discussion associated with the invited papers of the five Special Sessions in Volume I, as well as a complete transcription of the panel discussions following these sessions. This material was compiled from tape recordings made during the meeting and, although they considerably increased the editorial production problems and delayed printing somewhat, the discussions are expected to increase the usefulness of the Proceedings. The audience questions provide a valuable complement to the technical papers and summary discussions, revealing much about the current status of the technology and something of the differences in opinion and attitude toward the remaining problems in the field.

In a similar vein, the banquet address of Hans Bethe, the luncheon address of Jean Mégy and the closing remarks of Michel Banal have been transcribed from recordings and manuscript. Appearing at the end of Volume I, these talks provide international and historical perspective as well as sage practical admonitions both technical and philosophical.

Although taking moderate editorial privilege in the interest of readability, the Publication Chairman has throughout exercised his responsibility to retain the absolute integrity of the speaker's message (even including an occasional apparent lapsus linguae) in the transcription of the tape-recorded material. As is clear from some of the interchanges however, what is said in the discussions represents the opinion of the speaker and not necessarily that of the sponsoring organizations nor the consensus of the attendees.

It is a pleasure to acknowledge my indebtedness to the professional advice and productive competence of Ms. Joyce A. Kopta, Assistant Publication Chairman, who reduced an overwhelming task to tractability and contributed many of her own ideas to the usefulness of the publication.

William J. Sturm
Publication Chairman

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ABBREVIATIONS OF ORGANIZATIONS

AI	Atoms International
ANC	Aerojet Nuclear Corporation
ANL	Argonne National Laboratory
ANS	American Nuclear Society
BN	Belgonucléaire
BNL	Brookhaven National Laboratory
CE	Combustion Engineering
CEA	Commissariat à l'Energie Atomique
CEC	Commission of the European Communities
CEGB	Central Electricity Generating Board
CEN	Centre d'Etudes Nucléaires (Cadarache, Grenoble, Mol, Saclay)
CIRNA	Compagnie d'Ingénierie pour les Réacteurs à Sodium
CNEN	Comitato Nazionale Energia Nucleare (Bologna, Casaccia)
ECN	Energieonderzoek Centrum Nederland (Petten)
EdF	Electricité de France
EIR	Eidgenössisches Institut für Reaktorforschung (Wuerenlingen)
ENS	European Nuclear Society
ENEL	Ente Nazionale per l'Energia Elettrica
EPRI	Electric Power Research Institute
ETA	Environmental Technology Assessment, Inc.
GA	General Atomic
GBRA	Gas Breeder Reactor Associates
GE	General Electric
GfK	Gesellschaft für Kernforschung
HEDL	Hanford Engineering Development Laboratory
INB	Internationale Natriumbrüter Baugesellschaft GmbH
JEN	Junta Energia Nuclear (Spain)
JRC	Joint Research Centre (Ispra)
KEMA	N.V. tot Keuring van Elektrotechnische Materialen
KWU	Kraftwerk Union
LASL	Los Alamos Scientific Laboratory
MAGS	Ministerium für Arbeit, Gesundheit und Soziales des Landes Nordrhein-Westfalen
MIT	Massachusetts Institute of Technology
NERSA	Centrale Nucléaire Européenne à Neutrons Rapide, SA
NIRA	Nucleare Italiana Reattori Avanzati
NPC	Nuclear Power Company (Risley) Ltd.
NWU	Northwestern University
ORNL	Oak Ridge National Laboratory
PNC	Power Reactor and Nuclear Fuel Development Corporation (Japan)
PNL	Pacific Northwest Laboratories (Battelle)
RPI	Rensselaer Polytechnic Institute
SAI	Science Applications, Incorporated
Sandia	Sandia Laboratories
S&L	Sargent & Lundy Company
SRI	Stanford Research Institute
Technicatome	Société Technique pour l'Energie Atomique
TU-Hannover	Technische Universität Hannover
UCCND	Union Carbide Corp. Nuclear Division
UC-Berkeley	University of California at Berkeley
UCLA	University of California at Los Angeles

UKAEA	United Kingdom Atomic Energy Authority (Risley, Winfrith)
UKAWRE	United Kingdom Weapons Research Establishment (Aldermaston, Foulness)
UK-NII	United Kingdom Nuclear Installations Inspectorate
USERDA	United States Energy Research and Development Administration
USNRC	United States Nuclear Regulatory Commission
W-ARD	Westinghouse Advanced Reactors Division

VOLUME I

INVITED PAPERS; PANELS; SUMMARY

SPECIAL SESSION I

Key Safety Issues, Selection of Safety Design Bases, and Licensing Considerations

Cochairmen: E. C. Cobb (*NPC-Risley*)
W. Kerr (*The University of Michigan*)

	Page
Introductory Remarks	
E. C. Cobb	1
Key Issues on Safety Design Basis Selection and Safety Assessment	
S. An and Y. Togo (<i>University of Tokyo</i>)	2
Licensing Aspects in the Verification of the SNR 300 Design Concept Against Hypothetical Accidents	
E. Kugler and S. Wiesner (<i>MAGS</i>)	14
Key Safety Issues for Fast Breeder Reactors	
P. Tanguy, J. Petit, and F. Justin (<i>CEA</i>)	24
Safety Issues in Licensing Commercial LMFBR's	
J. Hannaford and D. R. H. Fryer (<i>UK-NII</i>)	33
Some Thoughts on Fast Reactor Safety	
D. Okrent (<i>UCLA</i>)	44
Panel Discussion and Floor Discussion	52
Summary Remarks	
W. Kerr and E. C. Cobb	58

SPECIAL SESSION II

Safety-Related Operation and Design Experience Pertaining to Demonstration Plants

Cochairmen: M. Israël (*EdF*)
W. Loewenstein (*EPRI*)

Safety Conclusions from Start-Up Tests and from the Analysis of the Core Behaviour of PHENIX	
P. P. Clauzon (<i>CEN-Cadarache</i>), P. Coulon (<i>Centrale Phénix</i>), A. Meyer-Heine, and F. Pénét (<i>CEN-Cadarache</i>)	61
Operating Experience with the PHENIX Nuclear Power Station from the Point of View of Safety	
J. Goddet (<i>EdF/CEA</i>)	76
Safety Design of SNR-300	
K. Traube (<i>Interatom and INB</i>)	84
Safety-Related Design of MONJU	
K. Kawashima and Y. Suzuki (<i>PNC</i>)	99
Safety-Related Design Considerations for the Clinch River Breeder Reactor Plant	
R. J. Slember (<i>W-ARD</i>)	112
Safety Considerations in the Design of Gas-Cooled Fast Breeder Reactors	
D. R. Buttemer (<i>GA</i>), C. A. Goetzmann (<i>KWU</i>), and J. A. Larrimore (<i>GA</i>)	129
Summary Remarks	
W. B. Loewenstein	144

SPECIAL SESSION III

Safety Aspects of Large Fast Breeder Nuclear Power Plants

Cochairmen: E. S. Beckjord (*USERDA*)
D. Smidt (*GfK*)

	Page
Introductory Remarks	
D. Smidt	147
A European Assessment of Gas-Cooled Breeder Reactor Safety	
J. Chermann (<i>GBRA</i>) and H. J. Allgeier (<i>CEC</i>)	148
Main Safety Features of the SUPER-PHENIX Project	
E. Robert, G. Lucenet (<i>EdF/NERSA</i>), A. Chalot, and J. Leduc (<i>CIRNA</i>)	164
Seismic Analysis for the SUPER-PHENIX Reactor	
J. C. Lefèvre (<i>CIRNA</i>), M. Livolant (<i>CEA-DEMT</i>), and G. Lucenet (<i>EdF/NERSA</i>)	180
Safety Analysis Aspects of a 2000 MWe LMFBR Core	
D. Struwe, W. Maschek, G. Heusener, E. Kiefhaber, K. Kleefeldt (<i>GfK</i>), and H. Hübel (<i>Interatom</i>)	193
Safety Related Design Considerations for Large Fast Breeder Plants	
J. D. Griffith (<i>USERDA</i>), J. Graham (<i>W-ARD</i>), P. Greebler (<i>GE</i>), and R. T. Lancet (<i>AI</i>)	209
Panel Discussion	224
Summary Remarks	
E. S. Beckjord	228

SPECIAL SESSION IV

Additional Safety Test Facilities—Needs and Concepts

Cochairmen: H. A. Bethe (*Cornell University*)
H. H. Hennies (*GfK*)

Introductory Remarks	
H. A. Bethe	231
Considerations on a PAHR Test Facility	
G. Bönisch, G. Grötzbach, V. Heinzl, K. Kleefeld, M. Küchle, R. A. Müller, P. Roysl, K. Schramm, D. Smidt, and H. Werle (<i>GfK</i>)	232
The SAREF Program	
R. Avery, M. A. Grolmes, D. Rose (<i>ANL</i>), and A. C. Millunzi (<i>USERDA</i>)	246
Safety Test Facilities, Needs, and Concepts—A French Evaluation	
O. Tretiakoff and J. Bailly (<i>CEA</i>)	262
Evaluation of Safety Test Needs for the Gas Cooled Breeder Reactors	
D. E. Emon (<i>USERDA</i>), D. R. Buttemer (<i>GA</i>), and R. H. Sevy (<i>ANL</i>)	270
Panel Discussion	286
Summary Remarks	
H. H. Hennies	296

SPECIAL SESSION V

Summaries and Conclusions

Cochairmen: D. Okrent (*UCLA*)
C. P. Zaleski (*EdF-Technicatome*)

Introductory Remarks	
C. P. Zaleski	301

	Page
Summary on Licensing Considerations and Selection of Safety Design Bases; Summary on Safety-Related Operation/Design Experience Pertaining to Demonstration and Large-Fast-Breeder Plants J. F. Petit (<i>CEA</i>)	302
Summary of Accident Analysis—Methodology and Results H. J. Teague (<i>UKAEA</i>)	308
Summary on Accident Energetics, Including Coolant Dynamics, Cladding and Fuel Relocation, and Molten-Fuel-Coolant Interaction H. K. Fauske (<i>ANL</i>)	316
Summary on Core-Debris Fluidization and Post-Accident Heat Removal D. Smidt (<i>GfK</i>)	325
Panel Discussion	334
Summary Remarks C. P. Zaleski	340
—————	
ANS/ENS Banquet Address H. A. Bethe (<i>Cornell University</i>)	341
ANS/ENS Luncheon Address—For Fast Reactor Development in the World: Safety and International Cooperation J. Mégy (<i>Directeur Général Adjoint de Novatome</i>)	347
Concluding Remarks for the Meeting M. Banal (<i>ENS</i>)	355
—————	
List of Registrants	357

* * * * *

VOLUME II

LICENSING; SAFETY-RELATED DESIGN

Licensing Issues; Safety-Related Core and Plant Design

Cochairmen: A. Meyer-Heine (*CEN-Cadarache*)
J. Wett (*W-ARD*)

LMFBR Accident Energetics and Their Role in Licensing T. P. Speis, R. P. Denise, R. W. Starostecki, L. Lois, J. F. Meyer (<i>USNRC</i>), and T. G. Theofanous (<i>Purdue University</i>)	369
Safety Related Design Philosophy and Experience of the Experimental Fast Reactor "JOYO" Y. Matsuno, K. Maeda, T. Inoue, and K. Kodaira (<i>PNC</i>)	388
Resolution of Key Safety-Related Issues in FFTF Regulatory Review D. E. Simpson (<i>HEDL</i>)	400
Development of Technical Specifications for EBR-II: Some Considerations for Application to Commercial LMFBR's J. I. Sackett and N. L. Gale (<i>ANL</i>)	411

	Page
Designing for Safety in Fast Reactors in the Presence of Uncertainties L. Cave, D. Ilberg, and D. Okrent (<i>UCLA</i>)	420
Design-Related Inherent Safety Characteristics in Large LMFBR Power Plants C. P. Tzanos, W. P. Barthold, C. H. Bowers, D. R. Ferguson, F. G. Prohammer, and J. B. van Erp (<i>ANL</i>)	431
The Decay Heat Removal Plan of the SNR-300—A Licensed Concept F. H. Morgenstern, W. Gyr, D. Stötzel, and H. Vossebrecker (<i>Interatom</i>)	442
Design Requirements for the SNR-300 Containment System H. Oeynhausen, F. Morgenstern, U. Scholle, L. Lange, and G. Waldhör (<i>Interatom</i>)	452

Safety-Related Reactor Physics I

Cochairmen: P. Clauzon (*CEN-Cadarache*)
P. Greebler (*GE*)

Comparison of Neutronics Calculations in a Loss-of-Flow Accident R. W. Schaefer (<i>ANL</i>)	463
An Assessment of Transport Effects in LMFBR Safety Neutronics J. E. Cahalan, K. O. Ott (<i>Purdue University</i>), and D. R. Ferguson (<i>ANL</i>)	473
Neutronic Coupling of Feedback Effects D. J. Malloy and K. O. Ott (<i>Purdue University</i>)	482
Recent Improvements in the Prediction of Reactivity Coefficients Relevant to LMFBR Accident Analysis E. A. Fischer and F. Helm (<i>GfK</i>)	492
Status of Safety-Related FFTF Neutronics Parameters R. A. Bennett, J. W. Daughtry, R. A. Harris, W. L. Bunch, D. L. Johnson, and R. E. Peterson (<i>HEDL</i>)	502
On the Extrapolation of ZPR Sodium Void Measurements to the Power Reactor C. L. Beck, P. J. Collins, M. J. Lineberry, and G. L. Grasseschi (<i>ANL</i>)	512
Experimental Studies of GCFR Safety Physics Parameters in the ZPR-9 Critical Assemblies S. K. Bhattacharyya, E. M. Bohn, L. G. LeSage, and R. B. Pond (<i>ANL</i>)	521
Analysis of Safety-Related Physics Measurements in the Initial GCFR Critical Experiments R. A. Moore and A. L. Hess (<i>GA</i>)	531
Effect of Steam Entry in a GCFR Core C. McCombie, G. Brunson, W. Heer, M. Jermann, R. Richmond, and S. Seth (<i>EIR-Würenlingen</i>)	541

Safety-Related Reactor Physics II

Cochairmen: G. I. Bell (*LASL*)
K. Wirtz (*GfK*)

Safety Aspects in Nuclear Core Design of LMFBRs U. Wenmann (<i>Interatom</i>)	555
Low Sodium Void Coefficient LMFBR Cores B. R. Sehgal, C. Lin, J. Naser, and W. B. Loewenstein (<i>EPRI</i>)	565
Safety and Breeding—Related Aspects of Fast Reactor Cores Having Internal Blankets M. J. Driscoll, G. A. Ducat, R. A. Pinnock, and D. C. Aldrich (<i>MIT</i>)	575
Simulation of an HCDA Sequence on the ZPPR Critical Facility R. E. Kaiser, C. L. Beck, and M. J. Lineberry (<i>ANL</i>)	584
Analysis of Simulated HCDA Experiments in ZPPR S. L. Stewart, B. W. Lee, J. T. Hitchcock, and S. C. Crick (<i>GE</i>)	594

LMFBR Instrument and Protection System Development

Cochairmen: S. H. Hanauer (*USNRC*)
W. Peppler (*GfK*)

Safety Philosophy in Upgrading the EBR-II Plant Protection System J. I. Sackett (<i>ANL</i>)	605
Safety Related Considerations for Operation with Defected Elements in EBR-II R. M. Fryer, J. I. Sackett, and J. D. B. Lambert (<i>ANL</i>)	615
The Detection of Coolant Blockages—Problems and Possibilities C. P. Greef, A. J. Clare, S. J. Board, and R. S. Hall (<i>CEGB</i>)	625
Designs for In-Reactor Sodium Boiling Detection and Generation S. A. Wright (<i>Sandia</i>), R. W. Albrecht, S. C. Artus, R. D. Crowe, and L. R. Dodd (<i>University of Washington</i>).	635
Temperature Noise Analysis and Sodium Boiling Detection in the Fuel Failure Mockup W. H. Sides, Jr., D. N. Fry, W. H. Leavell, M. V. Mathis (<i>ORNL</i>), and R. F. Saxe (<i>North Carolina State University</i>)	646
LMFBR-Subassembly Fault Detection and Identification by Temperature Noise Analysis E. Ohlmer and D. Schwalm (<i>JRC-Ispra</i>)	658
Indication of a Coolant Blockage in a Fuel Element of a Sodium-Cooled Reactor by Temperature Fluctuation Measurements with Steel-Sodium Thermocouples L. Krebs and G. Weinkötz (<i>GfK</i>)	665
LMFBR Self-Actuated Shutdown Systems E. S. Sowa, W. P. Barthold (<i>ANL</i>), D. T. Eggen (<i>Northwestern University</i>), P. R. Huebotter, J. Josephson, P. A. Pizzica, R. B. Turski, and J. B. van Erp (<i>ANL</i>)	673
Hydraulically Supported Absorber Balls Shutdown System for Inherently Safe LMFBR's E. R. Specht, R. K. Paschall, M. Marquette, and A. Jackola (<i>AI</i>)	683

Component Development

Cochairmen: R. Keaten (*AI*)
G. C. Schileo (*CNEN-Casaccia*)

Manufacturing and Testing Experience for FFTF Major Safety Related Components C. L. Peckinpaugh (<i>Westinghouse-Hanford</i>)	699
Safety Evaluation of the MONJU Steam Generator M. Hori, K. Kawashima, M. Sato, T. Saito, N. Kanegae, and H. Hiroi (<i>PNC</i>)	709

Reliability and Probabilistic Risk Analysis

Cochairmen: B. Gachot (*EdF*)
V. Joksimovic (*GA*)

Reliability Models for Plant Protection Systems R. W. Hockenbury and M. L. Yeater (<i>RPI</i>)	721
Applicability of the Reactor Safety Study (WASH-1400) to LMFBR Risk Assessments K. A. El-Sheikh, K. G. Feller, L. Fleischer, P. Greebler, A. McDonald, P. Sultan, M. I. Temme (<i>GE</i>), and R. R. Fullwood (<i>SAI</i>)	728
Safety Assurance Logic Techniques for Evaluation of Accident Prevention and Mitigation L. M. McWethy and J. W. Hagan (<i>HEDL</i>)	738
Accident Trees—A Framework for Accident Evaluation J. O. Bradfute, A. R. Gilchrist, and J. E. Mott (<i>GE</i>)	748

A Comprehensive Method of Common-Mode Failure Analysis for LMFBR Safety Systems A. J. Unione, R. L. Ritzman, and R. C. Erdmann (<i>SAI</i>)	757
Probabilistic Safety Evaluation of the Gas-Cooled Breeder Reactor J. J. Dekais (<i>GBRA-Brussels</i>)	765
Reliability Evaluations of GCFR Residual Heat Removal Systems A. P. Kelley, Jr. (<i>GA</i>) and P. DeLaquil III (<i>Sandia</i>)	775

Safety Test Facilities; Fuel and Cladding Motion Detection Techniques

Cochairmen: J. D. Griffith (*USERDA*)
P. Tanguy (*CEA*)

In-Pile Experiments and Test Facilities Proposed for Fast Reactor Safety M. A. Grolmes, R. Avery, A. J. Goldman, H. K. Fauske, J. F. Marchaterre, D. Rose, and A. E. Wright (<i>ANL</i>).	787
LMFBR Safety Experiment Facility Planning and Analysis M. G. Stevenson and J. H. Scott (<i>LASL</i>)	797
STF Design Features and Estimated Performance R. A. Lewis, D. C. Rardin, A. Travelli, J. L. Snelgrove, R. G. Matlock, B. C. Cerutti, K. P. Coover, R. L. Knawa, J. E. Matos, F. D. McCuaig, D. Ramaswami, F. A. Rough, and R. C. Watson (<i>ANL</i>)	807
Physics Design of Fast Reactor Safety Test Facilities for In-Pile Experiments A. Travelli, J. E. Matos, J. L. Snelgrove, D. H. Shaftman, C. P. Tzanos, S. K. Lam, E. M. Pennington, and W. L. Woodruff (<i>ANL</i>)	817
Capabilities of the Annular Core Pulse Reactor for Reactor Safety Experiments J. A. Reuscher, P. S. Pickard, R. H. Marion, C. H. Karnes, and F. M. Morris (<i>Sandia</i>).	827
Development of LMFBR Safety Testing in FFTF G. E. Russcher and J. W. Hagan (<i>HEDL</i>)	836
Fuel and Cladding Motion Surveillance A. De Volpi (<i>ANL</i>)	846
X-Ray Monitoring of Fuel Motion W. E. Stein, V. Starkovich, and J. D. Orndoff (<i>LASL</i>)	856
Coded Aperture Imaging Methods for Fuel Motion Measurement J. G. Kelly and K. T. Stalker (<i>Sandia</i>)	864

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VOLUME III ACCIDENT ANALYSIS; STRUCTURAL DYNAMICS

Accident Analysis I

Cochairmen: D. E. Simpson (*HEDL*)
S. Wiesner (*MAGS*)

CEC Activities in the Field of LMFBR Safety W. Balz, S. Finzi, and R. Klersy (<i>JRC-Ispra</i>)	877
A Method for Assessing Core-Wide Incoherency Effects on LMFBR Whole-Core Accident Evolution J. V. Massey (<i>Purdue University</i>), L. Lois, T. P. Speis (<i>USNRC</i>), and T. G. Theofanous (<i>Purdue University</i>)	885

The Transition Phase in LMFBR Hypothetical Accidents	
R. W. Ostensen, R. J. Henninger, and J. F. Jackson (<i>ANL</i>)	895
An Assessment of Fuel Failure Propagation in LMFBRs	
H. K. Fauske, M. A. Grolmes (<i>ANL</i>), and S. H. Chan (<i>University of Wisconsin-Milwaukee</i>).	905
Depressurization Accident Studies for GCFR's	
D. R. Buttemer and H. S. Chung (<i>GA</i>)	915
Computational Analysis of Fuel-Sodium Interactions with an Improved Method	
H. Jacobs (<i>GfK</i>)	926
Probabilistic Analysis of Accidental Transients in an LMFBR	
J. Amesz, G. Volta (<i>JRC-Ispra</i>), F. Cesari, and R. Righini (<i>CNEN-Bologna</i>)	936

Accident Analysis II: Transient Overpower and Fuel Pin Failure Modeling

Cochairmen: R. Casini (*ENEL-NERSA*)
W. Kastenberg (*UCLA*)

Effect of Failure Incoherence in a LMFBR Hypothetical Overpower Accident	
P. Royl and B. Kuczera (<i>GfK</i>)	947
Analysis of LMFBR Overpower Accident, Including Fission Gas Effects in the Predisassembly and Disassembly Phase	
H. G. Bogensberger, E. A. Fischer, P. Royl, and G. Arnecke (<i>GfK</i>)	958
Comparison of SAS3A and MELT-III Predictions for a Transient Overpower Hypothetical Accident	
N. P. Wilburn (<i>HEDL</i>)	969
EPIC: A Computer Program for Fuel-Coolant Interactions	
P. A. Pizzica and P. B. Abramson (<i>ANL</i>)	979
Sensitivity of LMFBR Hypothetical Accident Analysis to Variations in an Empirical Fuel Pin Failure Criterion	
N. P. Wilburn, L. D. O'Dell, and A. E. Waltar (<i>HEDL</i>)	988
Fuel Cladding Mechanical Properties for Transient Analysis	
G. D. Johnson, C. W. Hunter, and J. E. Hanson (<i>HEDL</i>)	998
Analytic Models for Fuel Pin Transient Performance	
F. E. Bard, G. L. Fox, D. F. Washburn, and J. E. Hanson (<i>HEDL</i>)	1007
Fuel Pin Failure Models and Fuel-Failure Thresholds for Core Disruptive Accident Analysis	
P. K. Mast and J. H. Scott (<i>LASL</i>)	1015

Accident Analysis III: Advanced Fuels

Cochairmen: G. I. Bell (*LASL*)
K. Wirtz (*GfK*)

Safety Issues for Advanced-Fueled LMFBRs	
D. Rose, L. W. Deitrich, M. A. Grolmes, and D. R. Ferguson (<i>ANL</i>)	1027
Behavior of Carbide Fuel in Local and Whole Core Accidents	
E. R. Siegmann, R. J. Bergeron, and G. M. Nickerson (<i>CE</i>)	1037
Safety Problems Related to Microheterogeneities in Physically Mixed Oxide Fuels	
A. Renard, G. Evrard, and G. Vanhellemont (<i>Belgonucléaire</i>)	1047
Central Computerized Data Base for Liquid Metal Fast Breeder Reactor Safety Codes	
H. Alter (<i>USERDA</i>), G. F. Flanagan (<i>ORNL</i>), and N. M. Greene (<i>UCCND</i>)	1057

Accident Analysis IV: Unprotected Pump Coastdown and Loss of Piping Integrity

Cochairmen: H. Hübel (*Interatom*)
C. N. Kelber (*USNRC*)

Influence of Fuel-Coolant-Interactions on Loss of Flow Type Core Disruptive Accidents for the SNR-300 C. Essig (<i>Interatom</i>), R. Froehlich, and P. Schmuck (<i>GfK</i>)	1069
Unprotected Loss-of-Flow Transients in Large LMFBRs C. P. Tzanos, W. P. Barthold, and J. B. van Erp (<i>ANL</i>)	1081
Treatment of Incoherencies and Uncertainties in LMFBR LOF Accident Analysis with PARSEC R. D. Burns III and K. O. Ott (<i>Purdue University</i>)	1091
Pipe Rupture Studies for the CRBR Using DEMO and SAS H. H. Hummel and Kalimullah (<i>ANL</i>)	1101
Analysis of Postulated FFTF Pipe Ruptures F. J. Markowski, T. B. McCall, and L. E. Strawbridge (<i>W-ARD</i>)	1111

Accident Analysis V: Core Disassembly and Recriticality

Cochairmen: G. Kessler (*GfK*)
K. Ott (*Purdue University*)

Work-Energy Characterization for Core-Disruptive Accidents J. Marchaterre, T. Marciniak, J. Bratis, H. Fauske (<i>ANL</i>), and J. Jackson (<i>ANL</i>)	1121
Experimental Comparison: Studies with the VENUS-II Disassembly Code T. F. Bott (<i>Brigham Young University</i>) and J. F. Jackson (<i>LASL</i>)	1134
A Computational Model for Analyzing Postulated LMFBR Accident Fuel Boil-Up Conditions A. E. Waltar, J. Muraoka, and F. J. Martin (<i>HEDL</i>)	1142
Critical Configurations and Secondary Excursions for High Temperature Fuel/Steel Mixtures J. Pena (<i>JEN-Spain</i>), R. Froehlich, and P. Schmuck (<i>GfK</i>)	1152
LMFBR Disassembly Analyses T. P. McLaughlin, J. F. Jackson, H. M. Forehand, Jr., J. J. Koelling, and W. R. Stratton (<i>LASL</i>)	1162
Role of Wall Heat Transfer and Other System Variables on Fuel Compaction and Recriticality V. K. Dhir, J. N. Castle, I. Catton, W. E. Kastenber, and J. B. Doshi (<i>UCLA</i>)	1172
Accident Termination by Element Dropout in the GCFR A. Torri and J. L. Tomkins (<i>GA</i>)	1183

Accident Analysis VI: HCDA Codes and Computational Methods

Cochairmen: R. B. Nicholson (*Exxon Nuclear*)
A. Siebertz (*CEN-Mol*)

SIMMER-I, an LMFBR Disrupted Core Analysis Code L. L. Smith, J. E. Boudreau, C. R. Bell, P. B. Bleiweis, J. F. Barnes, and J. R. Travis (<i>LASL</i>)	1195
Analyses of LMFBR Core Disruption and Accident Phenomena Using the SIMMER-I Code C. R. Bell, P. B. Bleiweis, and J. E. Boudreau (<i>LASL</i>)	1203
Large-Scale Deterministic Computer Code Systems and LMFBR Safety: A Perspective H. U. Wider, W. R. Bohl, F. E. Dunn, and D. R. Ferguson (<i>ANL</i>)	1216

The SAS4A LMFBR Accident Analysis Code System: A Progress Report

D. R. Ferguson, W. R. Bohl, C. H. Bowers, J. E. Cahalan, F. E. Dunn, T. J. Heames, J. M. Kyser, W. L. Wang, and H. U. Wider (<i>ANL</i>)	1225

Molten Fuel Motion During a Fast-Reactor Overpower Transient

D. C. Kolesar, A. Padilla, Jr., C. H. Lewis, and A. E. Waltar (<i>HEDL</i>)	1236

Multi-Field Hydrodynamic Disassembly Calculations with Space-Time Kinetics: FX2/VENUS-III

D. P. Weber and B. D. Ganapol (<i>ANL</i>)	1244

Core Disruptive Accident and Recriticality Analysis with FX2-POOL

P. B. Abramson (<i>ANL</i>)	1253

An Eulerian Finite Element Method for Unsteady Compressible Flow Problems

G. Van Goethem (<i>JRC-Ispra</i>)	1261

A Probabilistic/Deterministic Procedure for Analyzing LMFBR Core Disruptive Accidents

J. K. Vaurio and C. Mueller (<i>ANL</i>)	1269

Structural Dynamics I: Containment Response

Cochairmen: M. Bender (*ORNL*)

S. Finzi (*JRC-Ispra*)

Introductory Remarks

M. Bender	1283

A Generalized Eulerian Method in Reactor Containment Analysis and Its Comparison with Other Numerical Methods

H. Y. Chu, C. Y. Wang, J. Gvildys, and Y. W. Chang (<i>ANL</i>)	1285

Evaluation of Explicit Finite-Difference Techniques for LMFBR Safety Analysis

D. Bernstein, R. D. Goluen, M. B. Gross, and R. Hofmann (<i>SAI</i>)	1295

An Experimental Programme to Validate Wave Propagation and Fluid Flow Codes for Explosion Containment Analysis

H. Holtbecker (<i>JRC-Ispra</i>), N. E. Hoskin (<i>AWRE-Aldermaston</i>), N. J. M. Rees (<i>AWRE-Fculness</i>), R. B. Tattersall (<i>UKAEA-Winfrith</i>), and G. Verzeletti (<i>JRC-Ispra</i>)	1304

Simulation of Hypothetical Core Disruptive Accidents in Vessel Models

M. Eglème, N. Brahy, J. P. Fabry, H. Lamotte, M. Stiévenart (<i>Beigonucléaire</i>), H. Holtbecker, and P. Actis-Dato (<i>JRC-Ispra</i>)	1314

A Comparison of ANL Containment Codes with SNR-300 Simulation Experiments

A. H. Marchertas, C. Y. Wang, and S. H. Fistedis (<i>ANL</i>)	1324

Safety Analysis of the Primary Heat Transport Systems of LMFBRs

M. T. A-Moneim and Y-W. Chang (<i>ANL</i>)	1334

Some Aspects of the Structural Response of Pool-Type LMFBR Systems to HCDA and Seismic Events

T. J. Marciniak, R. W. Seidensticker, R. F. Kulak, J. M. Kennedy, and S. H. Fistedis (<i>ANL</i>)	1344

The Mitigating Effect of Coolant Cavitation in the Response of LMFBR Containment to a Whole-Core Accident

A. V. Jones (<i>JRC-Ispra</i>)	1354

**Structural Dynamics II:
Cladding and Core Component Response**

Cochairmen: A. Brandstetter (*Interatom*)

M. Reich (*BNL*)

On the Fuel-Clad Stress-Strain State at the Beginning of a Transient

Y-H. Sun and D. Okrent (<i>UCLA</i>)	1365

Prediction of High Strain-Rate Deformation of LMFBR Cladding during Reactor Transients L. S. H. Chow and W. J. Shack (<i>ANL</i>)	1376
The Response of Single Subassembly Hexcan Wrappers to Pressure Loads J. E. Ash, T. J. Marciniak (<i>ANL</i>), C. M. Romander, and D. J. Cagliostro (<i>SRI</i>)	1385
Development of Models for Dynamics of Subassembly Can Walls during Core Disruption P. J. Blewett and G. P. DeVault (<i>LASL</i>)	1393
Analytic Studies of Subassembly Failure Propagation T. Belytschko (<i>University of Illinois-Chicago</i>) and J. M. Kennedy (<i>ANL</i>)	1399
Stress Intensity Factors for Corner-Cracked Subassembly Ducts H. J. Petroski (<i>ANL</i>) and J. D. Achenbach (<i>Northwestern University</i>)	1409
Explosion Tests on LMFBR-Core Models; An Evaluation for Reactor Safety H. Will (<i>GfK</i>)	1419
Finite Element Solution of Transient Fluid-Structure Problems in Lagrangian Coordinates J. Donea, P. Fasoli-Stella, and S. Giuliani (<i>JRC-Ispra</i>)	1427

* * * * *

VOLUME IV PHENOMENOLOGY

Coolant Dynamics I: Single-Phase Flow and Sodium Boiling

Cochairmen: P. Lecocq (*EdF*)
N. Todreas (*MIT*)

A Multichannel Analysis for Predicting Temperatures and Boiling Initiation in LMFBR Subassemblies during a Flow Transient R. Nijssing and W. Eifler (<i>JRC-Ispra</i>)	1439
An Analytical Model for Transient Fluid Mixing in Upper Outlet Plenum of an LMFBR J. W. Yang and A. K. Agrawal (<i>BNL</i>)	1448
Integral Boiling Phenomena under Radial Temperature Gradients in a Twelve Rod Bundle of LMFBR Geometry H. H. Boswinkel, J. R. C. Maarleveld, and E. Türkçan (<i>ECN-Petten</i>)	1457
A Numerical Simulation of the Two-Dimensional Boiling (Voiding) in LMFBR Subassemblies C. Miao and T. G. Theofanous (<i>Purdue University</i>)	1467
An Investigation of Forced Convection Boiling of Sodium at Low Heat Fluxes in Electrically Heated Seven Pin Bundles S. W. Eisenhower and R. W. Albrecht (<i>University of Washington</i>)	1477
Transient Sodium Boiling Characteristics at Pump Run Down H. M. Kottowski, C. Savatteri, and M. Mol (<i>JRC-Ispra</i>)	1488
Steady State Liquid Metal Boiling Pressure Drop Characteristics H. M. Kottowski, M. Mol, C. Savatteri (<i>JRC-Ispra</i>), and J. Costa (<i>CEN-Grenoble</i>)	1494
Influence of the Axial Distribution of Heat Flux on Sodium Boiling in Forced Convection S. Meneghello, M. Pezzilli, and G. Scarano (<i>CNEN-Casaccia</i>)	1502
Multiphase Fluid Dynamics with Applications in LMFBR Safety Analysis J. R. Travis and W. C. Rivard (<i>LASL</i>)	1511

Coolant Dynamics II: Sodium Boiling and Subassembly Flow Blockages

Cochairmen: J. A. Goedkoop (*ECN-Petten*)
V. E. Schrock (*UC-Berkley*)

Sodium Boiling Experiments in a 19 Pin Bundle: Two Phase Coolant Dynamics J. Costa and B. Menant (<i>CEN-Grenoble</i>)	1523
Temperature Distribution and Local Boiling Behind a Central Blockage in a Simulated FBR Subassembly A. J. Brook (<i>UKAEA-Risley</i>), F. Huber, and W. Pepler (<i>GfK</i>)	1532
Thermal-Hydraulic Correlations of a Six-Channel Blockage in a Sodium-Cooled Simulated LMFBR Fuel Assembly J. T. Han, M. H. Fontana, J. L. Wantland, P. A. Gnadt, and C. M. Smith (<i>ORNL</i>)	1541
Quasi-Steady State Boiling Downstream of a Six-Channel Central Blockage in a 19-Rod Simulated LMFBR Subassembly N. Hanus, M. H. Fontana, P. A. Gnadt, R. E. MacPherson, C. M. Smith, and J. L. Wantland (<i>ORNL</i>)	1550
Recent Results of a Local Blockage Experiment in a Sodium Cooled Electrically Heated Bundle J. E. de Vries, J. C. Hoebe (<i>ECN-Petten</i>), and B. Dorr (<i>GfK</i>)	1560
Local Boiling of Sodium in Downstream of Local Flow Blockage in a Simulated LMFBR Fuel Subassembly Y. Kikuchi, Y. Daigo, and A. Ohtsubo (<i>PNC</i>)	1568
Decay Heat Removal from a Pin Bundle A. Kaiser, W. Pepler, and M. Straka (<i>GfK</i>)	1578
A Model for Predicting Temperature Fluctuation Characteristics Beyond the Exit of LMFBR Subassemblies with Faults W. Eifler and R. Nijssing (<i>JRC-Ispra</i>)	1587

Transient-Overpower and Loss-of-Flow Experiments

Cochairmen: S. An (*University of Tokyo*)
J. E. Hanson (*HEDL*)

HEDL Experimental Transient Overpower Program T. Hikido, G. E. Culley, and J. E. Hanson (<i>HEDL</i>)	1599
LMFBR Prompt Burst Excursion (PBE) Experiments in the Annular Core Pulse Reactor (ACPR) T. R. Schmidt (<i>Sandia</i>)	1605
Loss-of-Cooling Experiments with Single Fuel Pins H. Kwast (<i>ECN-Petten</i>)	1615
Results of Recent TOP and LOF Experiments in TREAT A. B. Rothman, C. E. Dickerman, R. K. Lo, R. G. Palm, L. A. Semenza, R. Simms, and H. U. Wider (<i>ANL</i>)	1625
Summary and Evaluation of R-Series Loss-of-Flow Safety Tests in TREAT B. W. Spencer, M. A. Grolmes, R. E. Holtz, F. J. Testa, and N. A. Kramer (<i>ANL</i>)	1647
On the Simulation of LOF Accidents with Directly Electrical Heated UO₂ Pins A. Alexas (<i>GfK</i>)	1658
Out-of-Reactor Experimental Study of Fuel-Pin Failure Phenomena B. J. Wrona, T. M. Galvin, and D. Stahl (<i>ANL</i>)	1663
HEDL Empirical Correlation of Fuel Pin TOP Failure Thresholds—Status 1976 R. E. Baars, T. Hikido, and J. E. Hanson (<i>HEDL</i>)	1673
Cladding Failure in TREAT Overpower Experiments: A Mechanistic Interpretation and Its Implications for LMFBR Safety Analysis D. B. Atcheson, R. R. Sherry, and K. J. Shimane (<i>GE</i>)	1679

Fuel/Cladding Behavior and Relocation

Cochairmen: H. Alter (*USERDA*)
J. P. van Dievoet (*Belgonucléaire*)

Cladding Relocation Experiments	
R. E. Henry, W. C. Jeans, D. J. Quinn, and E. A. Spleha (<i>ANL</i>)	1691
Clad Relocation Dynamics—The Physics and Accident Evolution Implications	
T. G. Theofanous, W. Prather, M. Chen (<i>Purdue University</i>), T. P. Speis, and L. Lois (<i>USNRC</i>)	1697
Fuel Dispersal Experiments with Simulant Fluids	
M. Farahat, R. E. Henry, and J. Santori (<i>ANL</i>)	1707
Modeling the Response of Fast Reactor Fuel to Accident Transients	
L. W. Deitrich, E. E. Gruber, T. H. Hughes, J. R. Hofmann, J. M. Kramer, C. C. Meek, R. W. Ostensen, M. C. Billone, W. J. Shack, and C. I. Yang (<i>ANL</i>)	1715
Transient Undercooling Analysis of the Dispersal Mechanism Test	
L. Mync, C. H. Bowers, and J. B. Heineman (<i>ANL</i>)	1725
Experiments on Pressure-Driven Fuel Compaction with Reactor Materials	
R. E. Henry, E. G. Erickson, G. T. Goldfuss, J. J. Heiberger, D. J. Quinn, R. L. Roth, E. A. Spleha, and J. O. Winsch (<i>ANL</i>)	1735
Equations of State for LMFBR Materials	
J. F. Barnes and G. P. DeVault (<i>LASL</i>)	1744

Fuel and Cladding Freezing

Cochairmen: R. Keaten (*AI*)
G. C. Schileo (*CNEN-Casaccia*)

A Study of Fuel Freezing and Channel Plugging During Hypothetical Overpower Transients	
K. W. Wong, V. K. Dhir, and W. E. Kastenberg (<i>UCLA</i>)	1757
A Hydrodynamic Model for Treating Transient Freezing in Pin Bundles	
S. J. Hakim and R. J. Henninger (<i>ANL</i>)	1767
A Computer Program for Transient Freezing Flow into a Channel and Its Application to LMFBR Safety Analysis	
D. Majumdar and C. J. Hsu (<i>ANL</i>)	1778
Analytical and Experimental Studies of Transient Fuel Freezing	
M. Epstein, R. E. Henry, M. A. Grolmes, H. K. Fauske, G. T. Goldfuss, D. J. Quinn, and R. L. Roth (<i>ANL</i>)	1788
Simulation of Transient Freezing of Cladding in GCFR Blanket Channels	
N. N. Y. Chu, D. T. Eggen, and E. Khan (<i>Northwestern University</i>)	1799
Dynamics of Solidification of Flowing Fluids with Applications to LMFBR Post-Accident Fuel Relocation	
M. H. Chun, R. D. Gasser, M. S. Kazimi, T. Ginsberg, and O. C. Jones, Jr. (<i>BNL</i>)	1808

Molten-Fuel-Coolant Interaction

Cochairmen: R. Semeria (*CEN-Grenoble*)
R. W. Wright (*USNRC*)

A Critique of the Board-Hall Model for Thermal Detonations in UO_2-Na Systems	
D. C. Williams (<i>Sandia</i>)	1821
Mechanisms of Vapor Explosions	
S. G. Bankoff, J. H. Jo, and A. Ganguli (<i>Northwestern University</i>)	1833
Liquid-Liquid Contact in Vapor Explosions	
M. Ochiai and S. G. Bankoff (<i>Northwestern University</i>)	1843

Some Aspects of Mixing in Large-Mass, Energetic Fuel-Coolant Interactions D. H. Cho, H. K. Fauske, and M. A. Grolmes (<i>ANL</i>)	1852
Vapor Explosion Experiments with Simulant Fluids R. E. Henry, H. K. Fauske, and L. M. McUmber (<i>ANL</i>)	1862
Heat Transfer and Pressure Generation due to a Temperature Discontinuity at a Liquid-Liquid Interface F. M. Cooper (<i>LASL</i>)	1870
A Model for Fuel Fragmentation during Molten Fuel/Coolant Thermal Interactions G. J. Vaughan (<i>UKAEA-Culham</i>), L. Caldarola (<i>CEC-Karlsruhe</i>), and N. E. Todreas (<i>MIT</i>)	1879
Theoretical and Experimental Investigation of Fuel-Coolant Interactions in a Shock Tube Configuration H. Goldammer and H. M. Kottowski (<i>JRC-Ispra</i>)	1889
Effects of Fuel-Sodium Interaction in TOP Tests with Electrically Heated Fuel Pins E. Bojarsky, H. Deckers, H. Drexler, S. Gross, H. Lehning, D. Piel, and H. Reiser (<i>GfK</i>)	1896

Aerosols and Fission Product Release

Cochairmen: P. Mostert (*KEMA*),
M. Silberberg (*USNRC*)

On the State of the Art in Aerosol Modelling for LMFBR Safety Analysis W. O. Schikarski (<i>GfK</i>)	1907
Deposition of Aerosols Formed by HCDA due to Decay Heat Transport in LMFBR Inner Containment Atmospheres J. F. van de Vate, A. Plomp, C. de Jong, and H. C. D. Smit (<i>ECN-Petten</i>)	1915
Fuel Particle and Fission Product Release from LMFBR-Core Catcher S. Jordan and Y. Ozawa (<i>GfK</i>)	1924
Development and Application of Capacitor Discharge Vaporization Technique for Fuel Aerosol Studies M. J. Kelly, T. S. Kress, G. W. Parker, J. M. Rochelle, and M. H. Fontana (<i>ORNL</i>)	1930
Some Potential Reductions in the Release of Radioactivity under LMFBR Accident Conditions C. T. Nelson, R. P. Johnson, E. U. Vaughan, C. A. Guderjahn, and H. A. Morewitz (<i>AI</i>)	1937
Sodium Fire Studies in France: Safety Tests and Applications on an LMFBR Y. Fruchard, J. Colome, J. C. Malet, M. Berlin, G. Duvergier de Cuy, J. Justin (<i>CEN-Cadarache</i>), J. Duco (<i>CEA</i>), and B. Fourest (<i>CIRNA</i>)	1945
Release of Fission Products from Contaminated Sodium Fires S. Jordan (<i>GfK</i>)	1955
SPOOL-FIRE—An IBM 370/195 Code for the Analysis of Combined Spray and Pool Burning of Sodium I. Charak and L. W. Person (<i>ANL</i>)	1962
Sodium Spray Release Accident Analysis for Fast Reactor Safety Studies P. R. Shire (<i>HEDL</i>)	1972

Post-Accident Heat Removal I

Cochairmen: E. C. Cobb (*NPC-Risley*)
J. F. Marchaterre (*ANL*)

A Model for Evaluating Probabilities of Primary System Retention of Core Debris and Time of Release LMFBR Risk Assessments L. S. Fleischer, K. G. Feller, K. A. El-Sheikh, and J. R. Medley (<i>GE</i>)	1985
Considerations of the Third Line of Assurance—Post-Accident Heat Removal and Core Retention in Containment E. L. Glueckler and A. Dayan (<i>GE</i>)	1995

	Page
Post-Accident Fuel Containment in the Gas-Cooled Fast Breeder Reactor	
C. S. Kang and A. Torri (<i>GA</i>)	2005
Post-Accident Heat Removal for Gas Cooled Fast Reactors	
M. Dalle Donne (<i>CEC-Karlsruhe</i>), S. Dorner, G. Schumacher (<i>GfK</i>), J. Artnik, C. A. Goetzmann, and P. Rau (<i>KWU</i>)	2016
Prediction of Dryout Heat Fluxes in Beds of Volumetrically Heated Particles	
V. K. Dhir and I. Catton (<i>UCLA</i>)	2026
Studies of the Formation and Cooling of Particulate Fuel Debris Beds in Sodium	
E. S. Sowa, J. D. Gabor, L. Baker, Jr., J. R. Pavlik, J. C. Cassulo, and W. Holloway (<i>ANL</i>)	2036

Post-Accident Heat Removal II

Cochairmen: F. Justin (*CEA*)
R. Lancet (*AI*)

Experimental Investigations of Heat Transfer Characteristics in Liquid Layers with Internal Heat Sources	
G. Fieg (<i>GfK</i>)	2047
Heat Removal from Molten Fuel Pools	
L. Baker, Jr., M. G. Chasanov, J. D. Gabor, W. H. Gunther, J. C. Cassulo, J. D. Bingle, and G. A. Mansoori (<i>ANL</i>)	2056
Heat Transfer and Fluid Dynamics in a Volume-Heated Boiling Pool	
W. R. Gustavson, M. S. Kazimi, and J. C. Chen (<i>BNL</i>)	2066
Experimental Investigations on the Compatibility of an SNR-Type Corium with Graphite	
M. Peehs, G. Hofer (<i>KWU</i>), H. J. Friedrich, and H. J. Heuvel (<i>Interatom</i>)	2077
Thermohydraulics of the Interactions between Molten Core Material and Reactor Concrete	
R. Schramm and H. H. Reineke (<i>TU-Hannover</i>)	2087
Studies on Molten Fuel-Concrete Interactions	
J. F. Muir, D. A. Powers, and D. A. Dahlgren (<i>Sandia</i>)	2095
Interactions of LMFBR Core Debris with Concrete	
L. Baker, Jr., F. B. Cheung, M. G. Chasanov, E. S. Sowa, J. D. Bingle, G. Staahl, J. R. Pavlik, and W. Holloway (<i>ANL</i>)	2105
Acoustic Diagnostic Techniques for Post-Accident Heat Removal Experiments	
H. J. Sutherland, G. A. Carlson, and L. A. Kent (<i>Sandia</i>)	2115

**Coolant Dynamics I:
Single-Phase Flow
and Sodium Boiling**

Cochairmen: P. Lecocq (*EdF*)
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A MULTICHANNEL ANALYSIS FOR PREDICTING TEMPERATURES AND BOILING INITIATION IN LMFBR SUBASSEMBLIES DURING A FLOW TRANSIENT

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ABSTRACT

This paper deals with the transient thermo-hydraulic subchannel behaviour of LMFBR subassemblies subjected to loss-of-coolant-flow conditions. It focusses on the temperature behaviour in the stage prior to boiling inception. The model applied is based on the description of a significant number of subchannels and associated fuel rods and takes account of transverse mixing between adjacent subchannels. An analytical approach is used for solving the energy equations pertaining to both the coolant and the solid material region. Illustrative results are given for various rod bundle configurations subjected to a prescribed flow decay.

INTRODUCTION

Loss of coolant flow accidents in LMFBR reactors - initiated by highly unlikely events as: pump coast down without scram, pipe rupture, sudden channel inlet blockage - may lead to coolant boiling and partial voiding of the core, followed by melting of fuel rod material and reassembly of large amounts of fuel into a more reactive configuration. An accurate description of void propagation within the core is of key importance for evaluating reactivity insertion rates and for predicting the course of the accident. In currently applied accident-analysis code systems [1,2,3] it is assumed that boiling inception and voiding occur simultaneously across large sections of the core and across single subassembly sections. This assumption is excessively pessimistic and in recent years it has been pointed out that allowing for delays between boiling inception at various radial positions of a subassembly may result in a considerable reduction of the accident consequences [4,5,6]. The incoherency effects referred to here originate from radial variations in the coolant temperature prior to boiling. As reported in previous publications [7,8] under normal reactor operating conditions radial coolant temperature differences develop in the flow direction due to radial power gradients and/or geometry effects. In first approximation these differences are proportional to the ratio power/flow, consequently they increase at reduced flows.

It is important to bear in mind that during normal reactor operation the coolant is highly subcooled. In fact, the axial coolant temperature rise has to be increased by about 4 times its nominal steady state value before boiling can occur. Therefore, when a subassembly is subjected to a flow transient, during a significant part of the transient single phase conditions will prevail. During this time the coolant temperature varies due to transient convection, mixing between subchannels and transient conduction effects in fuel rods and structure materials. The single-phase-flow part of the transient merits detailed analysis for the following reasons:

- a) it allows determination of position and time of boiling inception
- b) the spatial temperature distribution, at boiling inception, of both the coolant and fuel rod materials may have a significant effect on the subsequent transient boiling process
- c) the physical situation in the preboiling stage is well defined and is thus amenable to reliable analysis

The results presented in this paper chiefly regard the single phase stage prior to inception of boiling. The computational approach adopted is briefly described below.

COMPUTATIONAL MODEL

The central problem is to find a solution for combined transient coolant heat transfer and transient fuel rod conduction in a bundle with a significant number of fuel rods. The essential features of the analysis adopted by us for this purpose are:

- subdivision of the flow area into KT subchannels in which the hydrodynamic and thermal coolant conditions are characterized by bulk average values (Fig. 1).

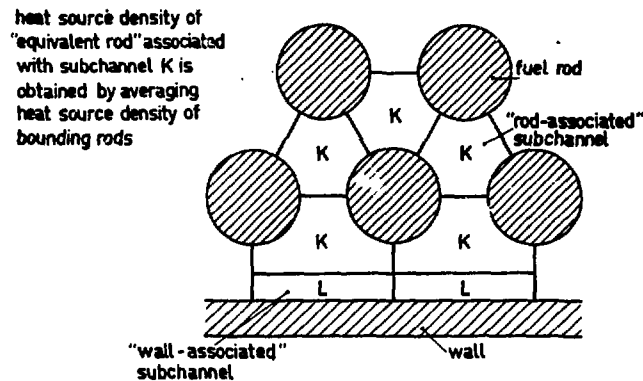


Fig. 1 : Subdivision of bundle flow area

- description of thermal coolant interactions between adjacent subchannels in terms of a mixing coefficient
- association of a single equivalent fuel rod or channel wall segment with each subchannel (Fig. 1); the former being radially discretized for a specified number (NAX) of axial positions (Fig. 2)

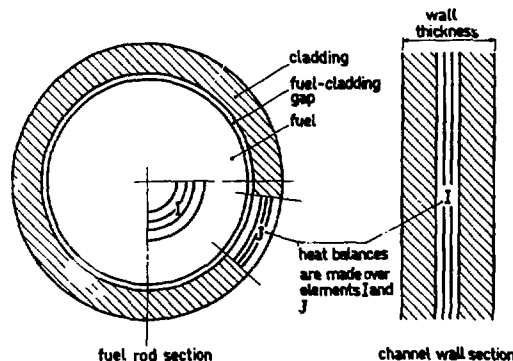


Fig. 2 : Radial discretisation of fuel rod and channel wall segment

- application of energy balances to the fuel rod (wall) elements, yielding for each axial position $X(NA)$ a system of differential equations with the average element temperature $T(I)$ as dependent variable and time as independent variable
- application of energy balances to subchannel coolant elements in motion yielding a differential equation system with subchannel coolant enthalpy $H(K)$ as dependent variable and time as independent variable

- formulation of separate analytical solutions for the coolant - and solid material - regions by satisfying the continuity requirements for heat flux and temperature at the solid-coolant boundaries for each subchannel and for each of the axial positions considered.

Intersubchannel mixing

The models applied for evaluating intersubchannel mixing coefficients, take account of conduction, turbulent diffusion and/or the action of helical spacing wires and have been described by us in a previous publication [7].

Fuel rod (wall segment) heat conduction aspects

Neglect of axial conduction reduces the fuel rod (wall) problem to one of one-dimensional transient conduction. For a given subchannel K and axial position X(NA) the radial temperature distribution in a rod (wall segment) is governed by:

$$\left\{ \frac{dT}{dt} \right\} = [DS]^{-1} [AS] \{T\} + \{Q(t)\} \quad (1)$$

where [DS] is a diagonal matrix of rod (wall) - element heat capacities and [AS] a symmetrical tridiagonal matrix of coefficients governing conductive heat transport between adjacent elements, whereas

$\left\{ \frac{dT}{dt} \right\}$, $\{T\}$ and $\{Q(t)\}$ represent column vectors of temperature and heat source terms. Eq.(1) is

solved analytically, as described by us, in ref. [9], via the solution of an algebraic eigenvalue problem. Important is that all the different fuel rod positions require a single solution of the algebraic eigenvalue problem and storage of a single set of eigenvalues and associated eigenvectors.

Coolant heat transfer

The bundle is subdivided into axial segments of equal length Δx (Fig. 3). For subchannel coolant elements travelling during a time interval $\Delta t(N)$ from initial axial positions X(NA) [10] to arrival positions X(NA+1) the time dependence of subchannel coolant enthalpies is governed by:

$$\left\{ \frac{dH}{dt} \right\} = [DC]^{-1} [AC] \{H\} + \{QH(t)\} \quad (2)$$

where [DC] is a diagonal matrix of subchannel coolant heat capacities and [AC] a symmetrical matrix of intersubchannel heat transport coefficient whereas

$\left\{ \frac{dH}{dt} \right\}$, $\{H\}$ and $\{QH(t)\}$ represent column vectors of coolant enthalpy change rates, coolant enthalpies and time-dependent subchannel heat input terms. The procedure adopted for solving eq (2) is again based on an "eigenvalue approach" and is similar to that described by us in ref. [7].

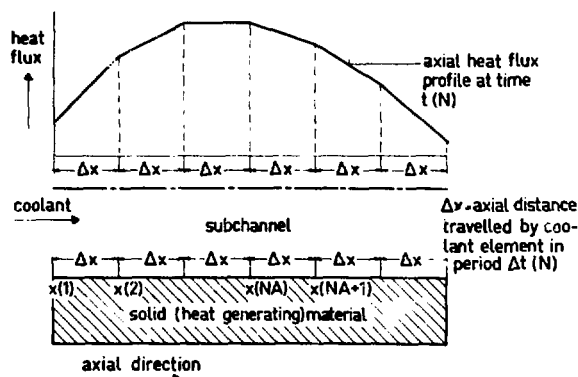


Fig. 3: Schematic representation of heat input into subchannel coolant elements

Coupling between coolant- and solid material-regions

The solution of eq.(1) for a given position in the bundle depends on the time variation of thermal coolant conditions, whereas the solution of eq.(2) depends on the rod (wall) heat flux experienced by the coolant elements in their axial motion through the bundle. For a sufficiently large number of axial positions and (consequently) sufficiently small time intervals $\Delta t(N)$ it is justified to make the following assumptions:

- the local heat flux at the solid-coolant boundary varies in a linear fashion with time during a time interval $\Delta t(N)$
- the heat flux, at a given time, varies linearly over the axial distance interval Δx .

The combined solution of eqs.(1) and (2) is now straightforward and yields temperature and heat flux results at $t(N) = t(N-1) + \Delta t(N)$ representing initial conditions for a subsequent computation step.

APPLICATIONS

The illustrative results presented in this section regard the effect of prescribed flow decays on the thermal behaviour of bundles of different sizes and with different spatial distributions of heat generation. The most important parameters underlying the computations have been collected in table I.

Table I : Input parameters for illustrative calculations (kg-m-sec units)

pitch-to-diameter ratio	1.2	fuel cladding gap heat transfer coefficient	10000
distance rod centre-channel wall		radial discretisation of fuel, cladding,	15,5,10
relative to rod radius	1.4	channel wall	elements
outer cladding diameter	0.006	average linear power (W/m) in	
inner cladding diameter	0.0052	fuel region	30000
outer fuel diameter	0.005	axial power distribution	
axial pitch for wire spacers	0.15	217-pin subassembly	see Fig.10
fuel properties		19-rod, 37-rod and	
thermal conductivity	2.5	61-rod bundles	uniform
specific heat	400	lateral power distribution	
density	10000	217-pin subassembly	40%
cladding properties			variation
thermal conductivity	20	19-rod, 37-rod and	
specific heat	500	61-rod bundles	uniform
density	8000	coolant	sodium
channel wall properties		initial bulk coolant velocity	7
thermal conductivity	20	inlet coolant temperature	400°C
specific heat	500	coolant saturation temperature	1000°C
density	8000	boiling superheat	20°C

For all cases considered the inlet bulk coolant velocity undergoes a rapid exponential decay according to

$$u_b = 7 \exp(-2t) \quad (3)$$

from an initial value $u_b = 7$ m/sec until $u_b = 1$ m/sec at $t = 0.973$ sec, after which it remains constant.

Hexagonal wire-spaced bundles containing 19, 37 and 61 fuel rods

The attention given to small bundles is justified by their frequent use in experimental out-of-pile and in-pile investigations. The results presented here regard wire-spaced bundles with 19, 37 and 61 rods respectively and with a radially and axially uniform heat source distribution. Figs. 4 to 7 deal with transient temperatures and heat fluxes in a 19-rod bundle.

In transient experiments with small bundles central subchannel temperatures tend to be higher than the bulk coolant temperature due to the important fraction of coolant flowing in the relatively large peripheral subchannels and due to a transient heat sink effect of the channel wall. Fig. 4 provides information on this aspect. It shows axial profiles of bulk coolant temperature and central subchannel coolant temperature for the initial steady state and two times after start of the transient. It is ob-

served that both the bulk coolant temperature and the subchannel coolant temperature increase during the flow decay and continue to increase after the time $t = 0.973$ sec at which the coolant velocity has assumed the new steady value of 1 m/sec. The difference in temperatures of subchannel coolant and bulk coolant appears to be less important than that expected from considerations based on the power to flow ratio. The effects referred to here can be explained by the thermal inertia of the fuel rod- and channel wall-materials which alter the spatial distribution of heat input into the coolant. Information on this is provided by Fig. 5 which shows the axial distribution of heat flux associated with the central subchannel of the 19-rod bundle at various times.

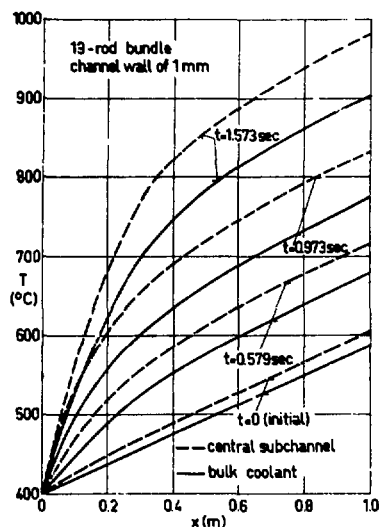


Fig. 4 : Axial profiles of bulk coolant temperature and central subchannel coolant temperature in 19-rod bundle

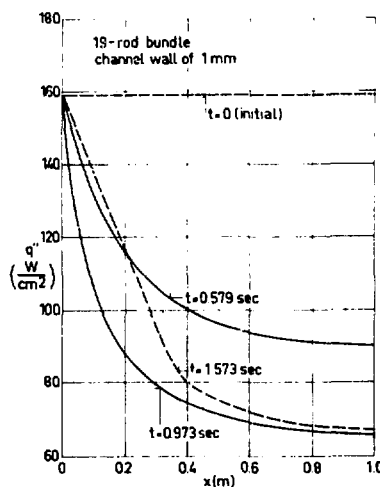


Fig. 5 : Axial distribution of heat flux for central subchannel of 19-rod bundle at various times

It is observed that during the flow transient the heat flux diminishes sharply from the initial axially uniform value of 159 W/cm^2 to less than 70 W/cm^2 , the axial heat flux variation being steepest near the bundle inlet. For times beyond $t = 0.973$ sec characterized by a constant coolant velocity of 1 m/sec the thermal situation tends newly towards a steady state. This explains the observed increase of heat flux at $t = 1.573$ sec. In small bundles the thermal inertia of the channel wall may have an important effect on the transient thermal behaviour and hence on the time and position of boiling inception. The effect of channel wall thickness on the outward heat flux into the channel wall is shown in Fig. 6 for the axial position at the bundle outlet. It appears that the heat sink effect exerted by the channel wall is very important and corresponds to outward heat fluxes beyond 100 W/cm^2 . In line with expectations the effect of wall thickness is relatively more important for larger times. The time history of central subchannel coolant temperatures at the bundle outlet is shown in Fig. 7 for various thicknesses of the channel wall. It may be observed that the effect of channel wall thickness on the central subchannel coolant temperature starts to be significant for times exceeding 1 sec. Fig. 8 provides a comparison of transient coolant temperatures in central subchannels of wire-spaced hexagonal fuel rod bundles with a channel wall thickness of 2 mm containing 19, 37 and 61 rods respectively.

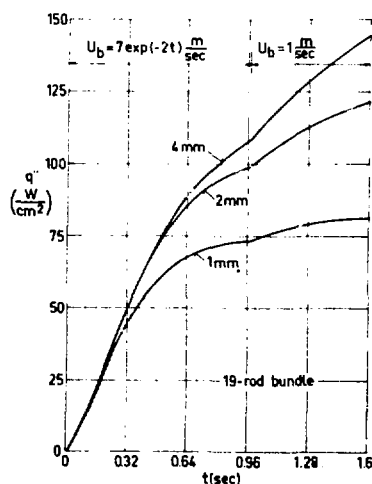


Fig. 6 : Time-dependent outward heat flux into channel wall at outlet of 19-rod bundle for various wall thicknesses

As to be expected an increase in bundle size entails an increase in coolant temperature for the central subchannels. The temperature difference ΔT between central subchannels and bulk coolant is a measure for the radial temperature incoherency in the bundle. Fig. 8 shows that ΔT is largest for the 61-rod bundle and may amount to more than 100°C. This at first sight surprising behaviour is associated with intersubchannel mixing.

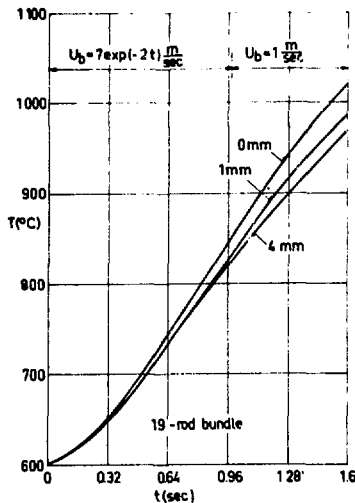


Fig. 7 : Time history of central subchannel coolant temperatures in 19-rod bundle for various wall thicknesses

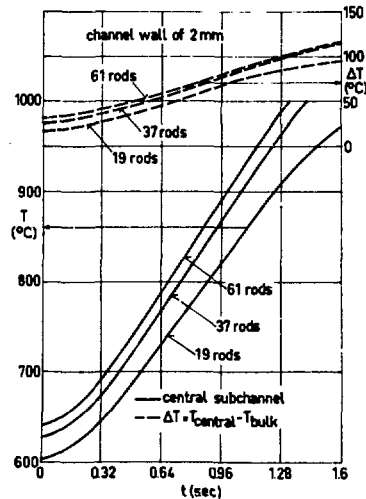


Fig. 8 : Transient coolant temperatures in bundles containing 19, 37 and 61 rods

Hexagonal 217-pin subassembly with 40% lateral power variation

The case considered here is that of a hexagonal 217-pin subassembly including axial blanket with a lateral power variation of 40% along the diagonal of the hexagonon. The subchannels considered are those located along the diagonal (Fig. 9). Channel wall heat capacity effects are supposed to be of negligible importance here.

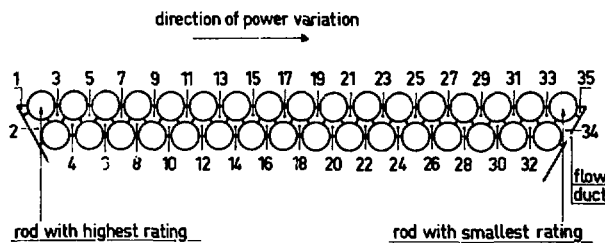


Fig. 9 : Numbering of subchannels along diagonal in hexagonal 217-rod assembly

Fig. 10 deals with the steady state situation of a bare rod system. It shows the axial distribution of heat source density in fuel (0 - 1.0 m) and axial blanket (1.0 - 1.4 m) and additionally the initial axial coolant temperature profiles for subchannels 2, 4, 32 and 34. Noteworthy is that the difference in temperature between subchannels 4 - 2 and 32 - 34 respectively represents the maximal peripheral cladding temperature difference for a "high power" rod and "low power" rod respectively.

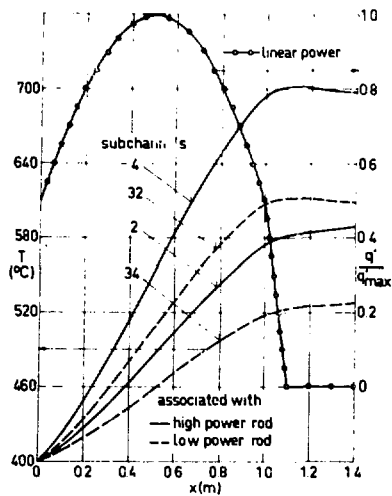


Fig. 10: Axial distribution of power and of initial subchannel coolant temperatures in sub-assembly with bare rods

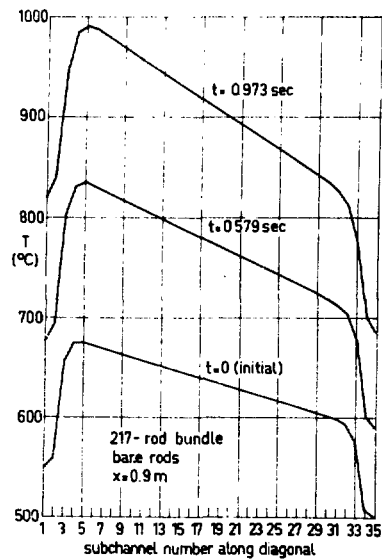


Fig. 11: Time history of lateral subchannel coolant temperature profiles for subassembly with bare rods

Figs. 11 and 12 show how at $x = 0.9$ m lateral temperature profiles develop with time for bare rod systems and wire-spaced systems respectively. Both figures show that during the transient the temperature incoherency increases with respect to that pertaining to the initial steady state. This increase is however much smaller than that expected from considerations based on the power to flow ratio. Axial profiles of coolant temperature and heat flux associated with the "hot" subchannel no.5, for the case of bare rods, are given in Figs. 13 and 14 respectively.

Fig. 13 shows that in the fuel region (0 - 1.0 m) both the subchannel coolant temperature and the bulk coolant temperature increase with time. The difference between the latter is again much

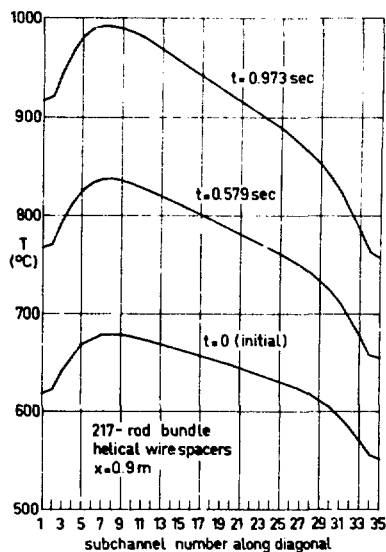


Fig. 12: Time history of lateral subchannel coolant temperature profiles for subassembly with helical wire spacers

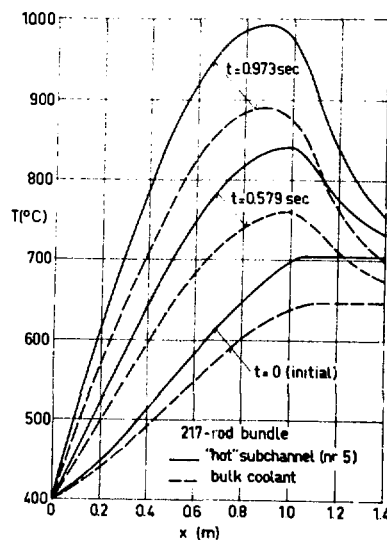


Fig. 13: Time history of axial profiles of hot sub-channel coolant temperature and bulk coolant temperature for subassembly with bare rods

smaller as expected from power to flow considerations. The heat sink effect exerted by the axial breeder is remarkable. At $x = 0.9$ m the hot subchannel increases in temperature from 675°C to 992°C in 0.973 sec whereas the corresponding increase at the blanket outlet is less than 60°C . The importance of heat capacity effects associated with pins in both the fuel and blanket regions is supplemented by the results of Fig. 14. The heat flux decreases rapidly during the transient assuming negative values in the blanket region beyond -60 W/cm^2 .

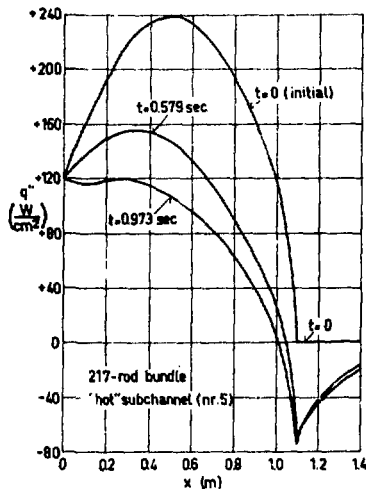


Fig. 14 : Time history of heat flux pertaining to hot subchannel in subassembly with bare rods

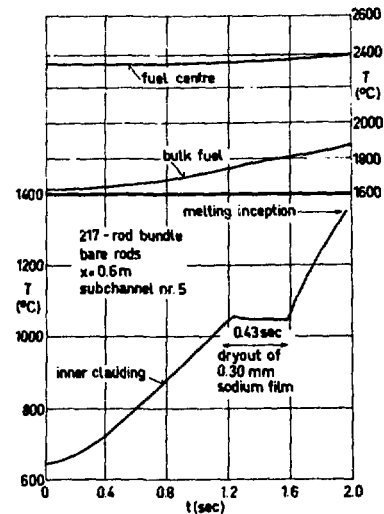


Fig. 15 : Transient cladding and fuel temperatures at axial position $x = 0.6$ m and for subchannel No. 5

Figs. 15 and 16 deal with boiling inception in the 217-pin subassembly. The data were computed for conditions where sodium boiling initiates at 20°C superheat and where at boiling initiation a liquid coolant film of 3 mm is formed at the outer cladding surface. It is assumed that this film is not affected by the boiling-flow process and that it may disappear only due to evaporation. Further assumptions underlying the present computations are that (a) upon start of boiling in the subassembly all coolant at temperatures above the saturation temperature is involved in the boiling process, (b) the boiling process does not affect the inlet flow conditions and does not give rise to internal subchannel flow-redistribution phenomena upstream of the position of boiling inception. Fig. 15 presents temperature data pertaining to inner cladding, bulk fuel and fuel centre for the "hot" subchannel No. 5 at axial position, $x = 0.6$ m where the linear power $q' = 380\text{ W/cm}$. During the flow transient the inner cladding is rising in temperature up to a fraction of a second after local inception of boiling. The inner cladding temperature then slightly decreases until occurrence of dry-out, after which it rises rapidly to reach at about 2 sec after start of the transient the value of the melting point. Fig. 15 shows that, as to be expected, bulk fuel temperature and fuel centre temperature change much less drastically, the respective increases at $t = 2$ sec being less than 260°C and 80°C .

Fig. 16 shows how boiling boundaries propagate upstream for 3 characteristic subchannels of the 217-pin bundle. Boiling in the subassembly starts in subchannel 5 at $x = 0.9$ m at 1.045 sec after start of the flow transient. It appears from Fig. 16 that at boiling initiation relatively large axial portions of the coolant are involved in the boiling process. It further may be observed that the lateral power gradient entails a significant incoherency of boiling inception, the delay of subchannel No. 31 with respect to subchannel No. 5 amounting to more than 0.5 sec.

FINAL REMARKS

The computational approach put forward in this paper provides the possibility to determine accurately the transient temperature field during the single-phase flow part of accidental fuel bundle transients. Although no special efforts were made to minimize computation times, the latter are quite satisfactory. The 217-pin subassembly case dealt with in this paper required, for the IBM 370/165

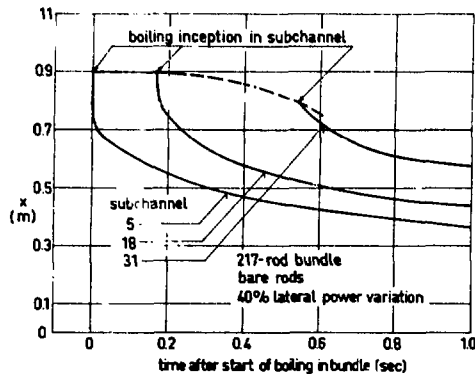


Fig. 16 : Upstream propagation of boiling boundary in subchannels 5, 18 and 31

computer, 3.45 minutes. This feature makes the method an efficient tool for performing parametric studies for flow/power transients in large fuel rod bundles.

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AN ANALYTICAL MODEL FOR TRANSIENT FLUID MIXING
IN UPPER OUTLET PLENUM OF AN LMFBR*

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ABSTRACT

A two-zone mixing model based on the lumped-parameter approach was developed for the analysis of transient thermal response in the outlet plenum of an LMFBR. The maximum penetration of core flow is used as the criterion for dividing the sodium region into two mixing zones. The model considers the transient sodium temperature affected by the thermal expansion of sodium, heat transfer with cover gas, heat capacity of different sections of metal and the addition of bypass flow into the plenum. The results of numerical calculations indicate that effects of flow stratification, chimney height, metal heat capacity and bypass flow are important for transient sodium temperature calculation. Thermal expansion of sodium and heat transfer with the cover gas do not play any significant role on sodium temperature.

INTRODUCTION

The current design of loop-type Liquid Metal Cooled Fast Breeder Reactor (LMFBR) plant contains a large volume of sodium stored in the upper outlet plenum. This large fluid volume can significantly attenuate the magnitude of transient temperature change issuing from the core prior to its impacting on the primary loop heat transport system. Thus, predicting the thermal response of the fluid in the upper plenum under various off-normal or accident conditions is important for the overall safety evaluation of the reactor system.

In the simplest mixing (perfect or complete mixing) model, the mixing process is characterized as that occurring in a well-stirred chamber. This model is satisfactory if the temperature of coolant entering the plenum is higher than the average temperature of the fluid in the plenum. It is believed that the momentum of the core flow aided by the positive buoyancy force is sufficient to provide a full penetration and results in complete mixing in the plenum. However, recent experiments have shown that this simplified model is inadequate under certain off-normal or accident conditions. The flow stratification within the plenum can occur if (a) the fluid entering the plenum is colder than that in

* This work was done under the auspices of the U.S. Nuclear Regulatory Commission.

the plenum and (b) the fluid experiences a large decrease in momentum. Consequently, recent analytical models^{1,2} have attempted to include the stratification effect by confining the mixing of fluids within the penetrating region only. The penetration distance is calculated from Bernoulli's equation.

In addition to the two aforementioned mixing models, the mixing process is further complicated by the interactions among the various components in the upper plenum. These interactions involve the metal heat capacity, bypass flow, thermal expansion of sodium and interface heat transfer between sodium and the cover gas, etc. A review of various analytical models shows that systematic study of the effects of these interactions on the two basic mixing processes has not been reported in literature.

This paper presents a detailed study of the mixing process in a general form. The maximum penetration distance of the core flow is determined from the analysis of one-dimensional turbulent jet equations.³ The penetration distance is used to develop the two-zone mixing model. The model includes the interactions among the various components in the plenum. The purpose of this study is to determine the relative importance of different effects for the safety analysis of an LMFBR plant.

ANALYSIS

The physical model considered in this study is shown in Fig. 1. The upper

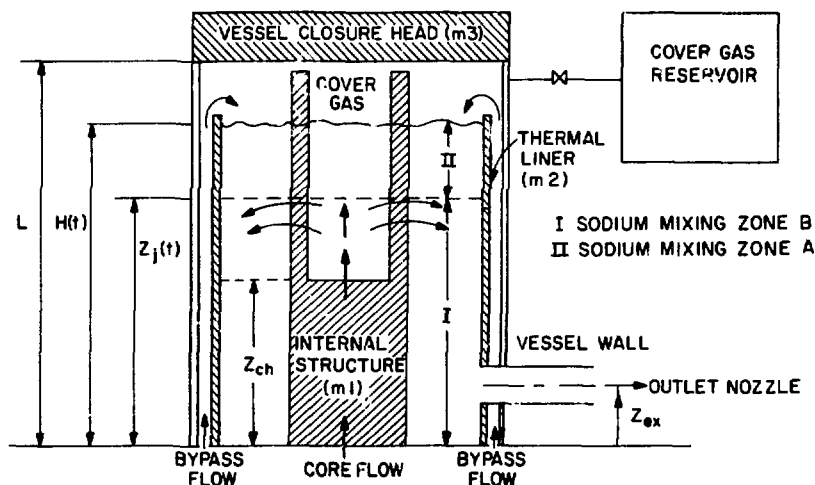


Figure 1. Schematic diagram for fluid mixing in the upper outlet plenum.

plenum contains a large volume of sodium, an annular bypass channel, a small region occupied by the cover gas and three sections of metal. Fluid leaving the reactor core enters the plenum from the bottom section, while a small percentage of cold bypass flow enters the plenum through the annular space formed by the thin thermal liner and the vessel wall. The outlet flow is represented by an exit nozzle. The support columns, chimney of the outlet module, control rod drive mechanism, vertex suppressor plate, control assembly, cellular flow collector, baffle, and all other structures are lumped together and represented by a section of mass (m1) immersed in fluids. The cylindrical thermal liner is indicated as m2. The vessel closure head and other metals above the cover gas region are considered as mass m3. The cover gas region is connected to a large

reservoir which represents all its connections, such as the overflow tank, equalization line header and gas region of the loop pumps. The sodium region is divided into two zones according to the maximum penetration distance of the core flow. The upper mixing zone is denoted as zone A and the lower zone as zone B.

The basic assumptions are:

- 1) Core flows from different channels into the upper plenum are represented by a single equivalent flow. This flow is associated with the mass-average enthalpy of the different channel flows.
- 2) The maximum penetration distance, which is related to the initial Froude number, divides the upper plenum into two mixing zones. Full penetration is assumed for flow with positive buoyancy.
- 3) The mixing process in both zones is described by the lumped-parameter approach, i.e., perfect mixing in each zone is assumed.
- 4) The cover gas obeys perfect gas law, and it is initially in equilibrium with the gas in reservoir.

The governing equations which determine the instantaneous sodium level and various temperatures are given below:

Sodium level:

$$\frac{dH}{dt} = \frac{W_i - W_{ex}}{\rho_A A_{gl}} + H \left[(1-f) \alpha(T_B) \frac{dT_B}{dt} + f \alpha(T_A) \frac{dT_A}{dt} \right] \quad (1)$$

Sodium in upper mixing zone A:

$$\begin{aligned} \frac{dE_A}{dt} = \frac{1}{\rho_A A_{gl} H f} \left\{ W_{BP}(E_{BPE} - E_A) + \beta_1 W_C(E_B - E_A) + h A_{gl}(T_B - T_A) \right. \\ \left. + U_g A_{gl}(T_g - T_A) + U_l f \left[A_{lm1}(T_{m1} - T_A) + A_{lm2}(T_{m2} - T_A) \right] \right\} \quad (2) \end{aligned}$$

Sodium in lower mixing zone B:

$$\begin{aligned} \frac{dE_B}{dt} = \frac{1}{\rho_B A_{gl} H(1-f)} \left\{ W_C(E_C - E_B) + \beta_2 W_{BP}(E_A - E_B) + h A_{gl}(T_A - T_B) \right. \\ \left. + U_l(1-f) \left[A_{lm1}(T_{m1} - T_B) + A_{lm2}(T_{m2} - T_B) \right] \right\} \quad (3) \end{aligned}$$

Cover gas:

$$\frac{dT_g}{dt} = \frac{U_g}{(mC)_g} \left[A_{gl}(T_A - T_g) + A_{gm1}(T_{m1} - T_g) + A_{gm2}(T_{m3} - T_g) \right] \quad (4)$$

Metal m1 (Internal structure):

$$\frac{dT_{m1}}{dt} = \frac{1}{(mC)_{m1}} \left\{ U_{\ell} A_{\ell m1} \left[f T_A + (1 - f) T_B - T_{m1} \right] + U_g A_{gm1} (T_g - T_{m1}) \right\} \quad (5)$$

Metal m2 (Thermal liner):

$$\begin{aligned} \frac{dT_{m2}}{dt} = \frac{1}{(mC)_{m2}} \left\{ U_{\ell} A_{\ell m2} \left[f (T_A - T_{m2}) + (1 - f) (T_B - T_{m2}) \right] \right. \\ \left. + U_g A_{gm2} (T_g - T_{m2}) + (UA)_{BP} (T_{BPM} - T_{m2}) \right\} \end{aligned} \quad (6)$$

Metal m3 (Vessel Closure head):

$$\frac{dT_{m3}}{dt} = \frac{U_g A_{gm3}}{(mC)_{m3}} (T_g - T_{m3}) \quad (7)$$

The auxiliary equations required by the above governing equations are

$$T_{BPE} = T_{m2} + (T_{BPI} - T_{m2}) \exp \left(- \frac{UA}{WC} \right)_{BP} \quad (8)$$

$$T_{BPM} = T_{m2} + (T_{BPI} - T_{BPE}) \left(\frac{WC}{UA} \right)_{BP} \quad (9)$$

$$W_i = W_C + W_{BP} \quad (10)$$

$$f = 1 - z_j(t) / H(t) \quad (11)$$

$$\bar{\rho} = (1 - f) \rho_B + f \rho_A \quad (12)$$

and the liquid sodium densities, ρ_A and ρ_B , are obtained from its constitutive relationships. The contact areas between the cover gas and liquid or metals ($A_{g\ell}$, A_{gm1} , A_{gm2} , A_{gm3}) and between liquid and metals ($A_{\ell m1}$, $A_{\ell m2}$) are obtained by assuming that the cross-sectional areas in direction perpendicular to the jet are constant during transients.

In the above equations, there are two control indices β_1 and β_2 which take values of either 0 or 1 depending upon the relative location of the outlet nozzle and the maximum jet penetration height, z_j . Their values are noted below:

$$\beta_1 = 0 \text{ and } \beta_2 = 1 \quad \text{for } z_j \geq (z_{ex} + \frac{1}{2} D)$$

$$\beta_1 = 0 \text{ and } \beta_2 = 0 \quad \text{for } (z_{ex} - \frac{1}{2} D) < z_j < (z_{ex} + \frac{1}{2} D)$$

and

$$\beta_1 = 1 \text{ and } \beta_2 = 0 \quad \text{for } z_j \leq (z_{ex} - \frac{1}{2} D)$$

The maximum penetration height is taken from a semi-empirical correlation.³ It is given as

$$z_j = (1.0484 Fr_o^{0.785}) r_o + z_{ch} \quad (13)$$

where Fr_o is the local Froude number and is defined as

$$Fr_o = \left(\frac{W_C}{\pi r_o^2 \rho_C} \right)^2 \left[\frac{\rho_B}{gr_o (\rho_C - \rho_B)} \right] \quad (14)$$

For the case of full penetration, i.e., for $z_j(t) = H(t)$, f becomes zero. Equations (2) and (3) are then replaced by the following equations:

$$\frac{dE_B}{dt} = \frac{1}{\rho_R A_{g\ell} H} \left\{ W_C (E_C - E_B) + W_{BP} (E_{BPE} - T_B) + U_g A_{g\ell} (T_g - T_B) \right. \\ \left. + U_\ell \left[A_{\ell m1} (T_{m1} - T_B) + A_{\ell m2} (T_{m2} - T_B) \right] \right\} \quad (15)$$

$$E_A = E_B \quad (16)$$

The cover gas mass is determined by assuming that the temperature of the gas in the reservoir remains constant and its pressure equals that of the cover gas in the vessel at any instant. The cover gas mass is then given by

$$M_g(t) = \frac{M_t V_g(t) T_{res}}{V_{res} T_g(t) + V_g(t) T_{res}} \quad (17)$$

where

$$V_g(t) = [L - H(t)] A_{g\ell}$$

These equations when coupled with the boundary conditions ($W_C(t)$, $W_{BP}(t)$, $E_C(t)$, $T_{BPI}(t)$ and $W_{ex}(t)$) provide for a complete solution to the problem, i.e., the height of sodium, and coolant and other temperatures are obtained.⁴ Using prototypical CRBR parameters actual computations were performed for two cases. Results are discussed in the following.

RESULTS AND CONCLUSIONS

A simplified boundary condition, which corresponds approximately to the case of normal scram and flow coastdown, was used so that our results can be compared with that obtained from the MIX code.⁵ The average core exit temperature was assumed to decrease linearly from 834 K to 693 K in ten seconds and to 667 K in 110 seconds. The core flow rate was dropped exponentially to about 10% in 35 seconds and was assumed to remain constant beyond this time. The computed sodium temperature at outlet nozzle and mixing zone heights are shown in Fig. 2. Our computations were made by using perfect mixing (1-zone) as well as two-zone models. The flow stratification is illustrated by the instantaneous height of the two mixing zones. Initially, the core flow yields a full penetration and

the entire plenum can be considered as a single mixing zone. As the momentum and temperature of the core flow are continuously decreasing, the penetration of the core flow is reduced and develops into a two-zone mixing.

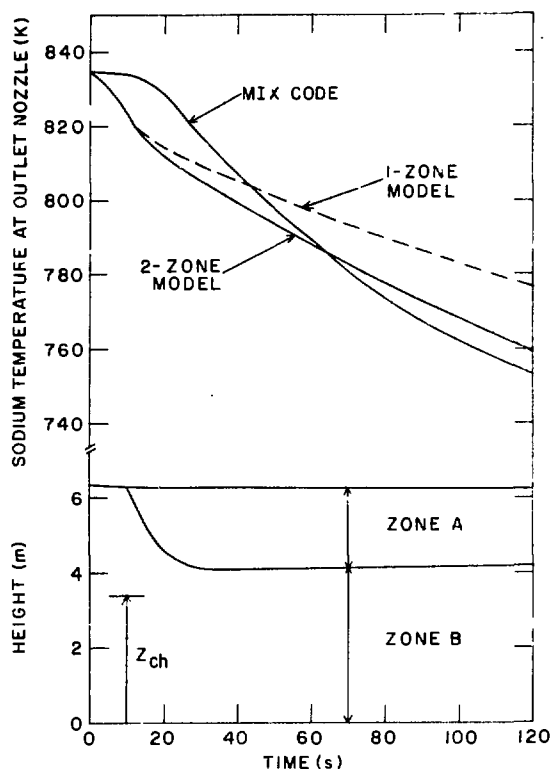


Figure 2. Comparison of the present mixing models with MIX code.

The present results were also compared with that obtained from a more detailed two-dimensional MIX code. As it can be seen from Fig. 2, MIX predicts a much slower change in sodium temperature during first twenty seconds. During this time, the MIX code prediction differs significantly (maximum difference of about 15 K) from either single-zone or two-zone mixing models of the current analysis. An inspection of comparison between MIX predictions and ANL experimental data^{2,5} reveals that for most of the cases, MIX predicts a very slow response during the first 10 or 20 seconds. As the transient continues, the 2-zone mixing model (one-dimensional in space) exhibits a reasonably good agreement with the 2-D MIX code and is clearly better than the single-zone model.

An analysis of sodium mixing in the outlet plenum was also performed for the case of a double-ended pipe rupture. The boundary conditions (shown in Fig. 3) such as the core flowrate, upper plenum exit flow rate and core flow temperature were obtained from the preliminary computations with the DEMO code. The predicted sodium temperature in the lower mixing zone (zone B) is also included in this figure. It is seen that the core flow is characterized by negative buoyancy during the period between 11 to 52 seconds. Thus, it is expected to have one-zone and two-zone mixing occurring alternately during the transient.

Computed values for various temperatures are plotted in Fig. 4. The curves of two sodium temperatures reveal the short period (27 to 52 seconds) of two-zone mixing. Due to the large time constant associated with metals, the metal temperatures (T_{m1} , T_{m2} and T_{m3}) remain practically constant. The cover gas

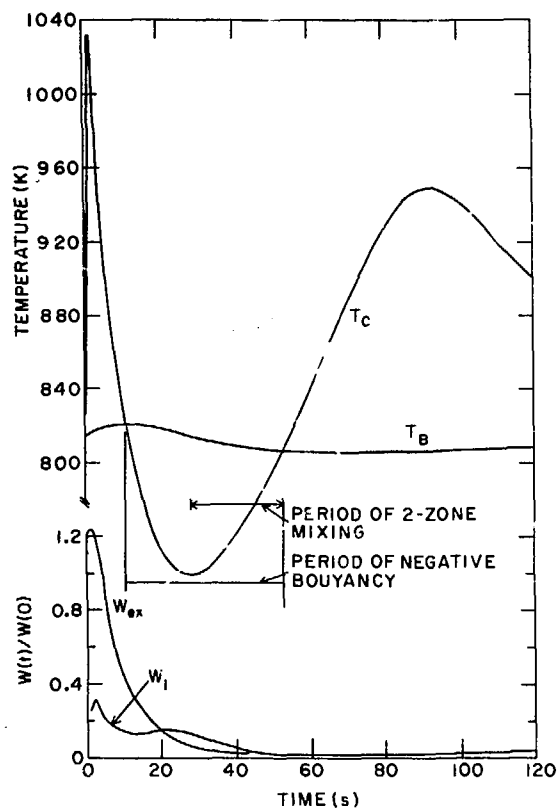


Figure 3. Transient boundary condition used for the case of a double-ended pipe rupture in the primary heat transport system.

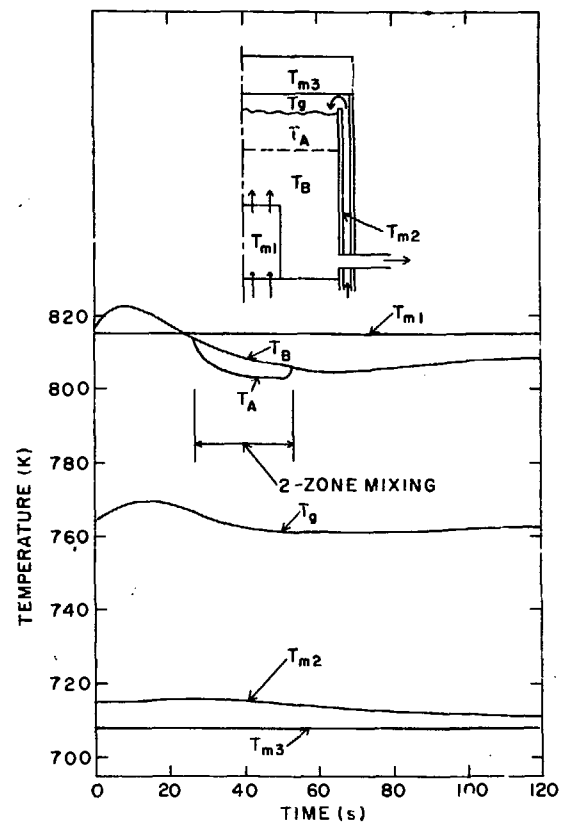


Figure 4. Computed transient temperatures for the double-ended pipe rupture case.

temperature, on the other hand, follows closely that of sodium in the upper zone since the cover gas was assumed to be isolated from the reservoir tank. The small discontinuity (~ 1 K) in sodium temperature at 52 is caused by the sudden change from the two-zone mixing into single mixing as the buoyancy force of the core flow changes from negative to positive.

Figure 5 shows (a) sodium temperature at the outlet nozzle and (b) the maximum jet penetration distance for three different chimney heights. This figure illustrates the importance of chimney on the coolant mixing in the plenum. For Curves 1 and 2, the top of the chimney is higher than the height of the outlet nozzle and the nozzle is completely located within the lower mixing zone. Hence, the sodium temperature at the outlet nozzle does not differ much for the two cases. In contrast to Curves 1 and 2, a large variation of the temperature is predicted (see Curve 3) when chimney height is reduced below the outlet nozzle. In this case, the nozzle is exposed to both upper and lower mixing zones. A large temperature variation occurs when the interface of the two zones passes through the outlet nozzle. Further calculations show, as expected, that the temperature variation is even more pronounced when the chimney is completely removed.

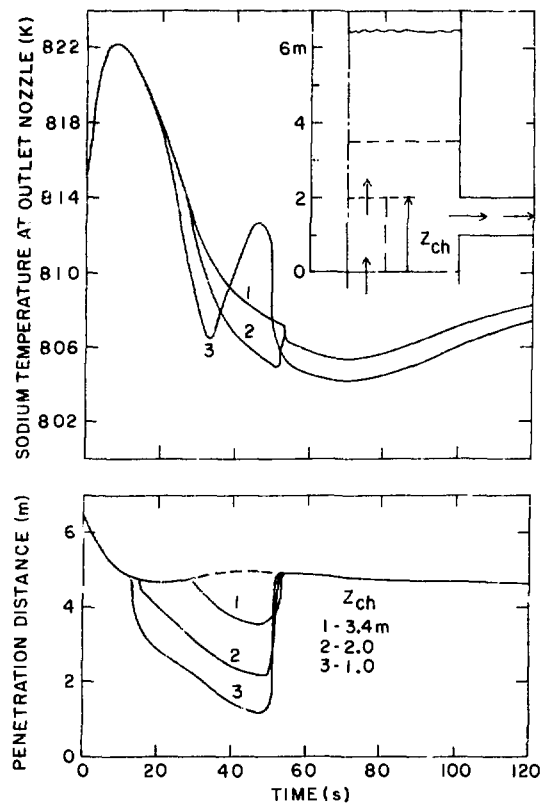


Figure 5. Effect of chimney height in sodium temperature at outlet nozzle.

The effects of metal heat capacity, bypass flow, the thermal expansion of sodium and heat transfer with the cover gas on the upper plenum were also investigated. The details are given in Ref. [4].

Based on these studies the following conclusions have been arrived at:

1. The transient mixing process in the upper plenum can be characterized by the present 2-zone, one-dimension in space mixing model.
2. Stratification effect is important when the exiting core flow is characterized by negative buoyancy. Perfect mixing model is inadequate for this situation.
3. Metal heat capacity and bypass flow are not negligible for calculation of transient sodium temperature.
4. Thermal expansion of sodium and heat transfer to cover gas have no significant effect on transient sodium temperature.
5. Chimney structure attached to the core is important in promoting complete mixing. Height of the chimney has an important effect on sodium temperature at the outlet nozzle.

NOMENCLATURE

Symbol	Description	Subscript	Description
C	Heat Capacity	BP	Bypass flow
D	Diameter of outlet nozzle	BPE	Exit of bypass flow
E	Enthalpy	BPI	Inlet of bypass flow
h	Heat transfer coefficient	BPM	Mean value of bypass flow
t	Time	C	Core flow
T	Temperature	ch	Chimney
U	Overall heat transfer coefficient	ex	Exit of outlet nozzle
V	Volume	g	Cover gas
z_j	Maximum penetration distance of jet flow	i	Inlet end of upper plenum
α	Thermal expansion coefficient	l	Liquid sodium
		rew	Cover gas reservoir
		t	Total

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INTEGRAL BOILING PHENOMENA UNDER RADIAL TEMPERATURE GRADIENTS IN A TWELVE ROD BUNDLE OF LMFBR GEOMETRY

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ABSTRACT

In this paper steady boiling patterns obtained from a partly heated twelve rod bundle are discussed. A comparison with existing boiling models gives preference to the two-phase flow patterns which are usually applied in calculations for water cooled reactors. An attempt is made to apply the results obtained to the core of a fast breeder reactor under loss of flow conditions.

INTRODUCTION

The aim of this presentation is to contribute to the understanding of integral sodium boiling in a core which is a requirement for the loss of flow analysis of future commercial fast breeders. The title of this contribution refers to a bundle experiment carried out in 1974 originally with twelve heated rods. However, during introductory heat transfer measurements the inner three rods failed. Therefore the entire boiling programme had to be performed with radial temperature differences ranging from 20°C, as present in full size fuel elements under nominal conditions, up to 50°C, a value which is reached in a fuel element at the onset of boiling during a pump run down accident.

Although integral boiling could also result from an inlet blockage of a fuel element, or from a steadily increasing local blockage in the core region, the main part of this paper is devoted to integral boiling during a pump coast down.

The interpretation of the experimental results and other considerations in this paper apply to the SNR 300 |1, 2, 3, 4|.

1. REDISTRIBUTION OF THE FLOW IN A CORE PARTLY VOIDED DUE TO A PUMP COAST DOWN ACCIDENT

During a hypothetical accident in which the primary pumps fail simultaneously and in which at the same time a failure occurs of the shut down system, voiding of the core is inevitable. Voiding starts with boiling in the central fuel element when its residual flow is about 25% of the nominal value. The calculated voiding rate is highly dependent on the boiling model assumed. Especially the multi-bubble slug models as for instance applied in the Capri-2/Kadis whole core accident code forecasts a quick voiding which prohibits a net posi-

tive flow through the boiling elements. (Fig. 1)

Furthermore the present WCAC do not account for the flow redistribution which occurs due to the increase in hydraulic resistance resulting from these boiling models.

If it is possible to improve the boiling model the present codes could better describe the pre-disassembly phase without the complication of a refined flow redistribution calculation.

Nevertheless it seems worthwhile at least for safety evaluations of future larger cores to include in the whole core accident codes a module which handles flow redistributions during voiding.

2. EXPERIMENTAL RESULTS OF THE TWELVE ROD BUNDLE

2.1. Description of the bundle and test section

In Fig. 2 a drawing of the bundle housed in a test section is shown. The main sodium flow from the EM induction pump of the loop is split into two flows. After passing a regenerative heat exchanger one of these supplies the bundle. The second flow serves as a controlled bypass flow stabilizing the pressure drop over the bundle and lowering the mixed outlet temperature of the upper plenum below the maximum loop operating temperature of 625°C. The sodium samples taken showed that the impurity level of the loop was low, an oxygen content < 10 ppm was achieved while the concentration of carbon and metals stayed below 1 - 2 ppm.

TABLE I
Loop characteristics

Cooling capacity 500 kW	* cold trap
Pump: 16 m ³ /hr at 6 kg/cm ²	* sodium sampling system
Maximum loop temperature 625°C	* argon cover gas
Main piping 2"	

The twelve rods are surrounded by a profiled shroud adjusting the flow in the edge channels further by a helium filled double walled tube which prevents heat losses to the bypass flow. The measured radial temperature gradients resulting from the non-heated central rods compared well with Hectic (Sceptic) [5] calculations.

The thermocouples used for these single phase measurements and also the instruments used for two-phase measurements are indicated in the same Fig. 2. Two important void detectors from which most of the experimental results are deduced are described in more detail in Fig. 3.

The first is an adaption of a proposal made by Chen [6]. An alternating current is led through one of the wires of a normal thermocouple with a grounded junction. The change in resistivity between the thermocouple tip and mass due to a passing vapour bubble results in a voltage change between mass and the second thermocouple wire.

The void network on the right hand side detects vapour in a cross section. The sodium in the subchannels and the heater cladding may be represented by a resistive network. If sodium vapour changes the local resistance a change in the voltage distribution along the boundary of the network occurs [7].

The signals of the instruments are transmitted to a data registration system (Fig. 4). Signals with high frequencies are stored on analogue recorders

and afterwards digitized on a small computer. Later on the signals are recovered on our main CDC 6600 computer with the aid of a data evaluation programme [8]. Low frequency signals are treated separately and stored on a data-logger.

2.2. Description of the boiling experiments

The boiling behaviour on the time scale of interest for a pump run down accident, approximately 1 s, can be treated quasi stationary (Fig. 1). During this second in a reactor fuel element the level at which the sodium reaches the saturation temperature moves towards the inlet. In our experiments the flow was kept constant and the distinct stages in the boiling progression were obtained from different bulk superheats at boiling inception. Due to a careful degasing procedure superheats of 70°C were reached, covering more than one second in the Loss of Flow accident.

Almost each boiling experiment was initiated by a reduction of the inlet flow until the start of boiling. The superheat is responsible for an initial flow reduction superseding a flow decrease due to steady two-phase flow. Within one boiling run, which itself lasted several minutes, stable boiling could be interrupted many times after which boiling restarted with or without superheat.

Our boiling runs showed about 50 times bulk superheat at the onset of boiling. No statistically significant correlation has been found between the amount of superheat on the one hand and system pressure, sodium velocity and heat flux on the other. An influence on the superheat of small entrained gas bubbles cannot be excluded [9].

In several boiling runs subcooled boiling has been indicated by the Chen detectors and by the acoustical detector prior to stable saturated boiling.

Details of three different runs are given in Figs 5, 6 and 7. In each figure from the top downwards the amplitudes of various signals as a function of time are shown in a sequence corresponding with the axial location of the instruments.

Figure 5 contains a part of run number 1 seven seconds after the onset of boiling. The boiling process started smoothly without superheat and as a consequence the saturation level stayed at the outlet. Due to a nearby mixing chamber also no additional two-phase pressure drop occurred. The length of the boiling zone in the bundle was smaller than 3 cm. Therefore we do not see any boiling indicated by the detectors below Ch 06.

The bubble frequency at Ch 07 is above 20 Hz, this means that if no slip occurs between liquid and vapour, bubbles with a length of 15 mm pass along the wall.

At the boiling inception of run number 6 a superheat of 28°C induced an initial void "burst" with a duration of 150 ms. The void network indicated an asymmetric bubble. After this initial burst stable boiling (Fig. 6) was reached again. The sodium flow has in this period oscillations with an amplitude of 4% of the nominal value. Ch 07 almost continuously detects vapour but also indicates that the liquid film on the wall obviously varies in thickness.

The vapour volume at Ch 07 level is fed with slug type bubbles from the Ch 06 and Ch 05 levels. There is also an oscillation between the void volumes at the Ch 07 and Ch 06/05 levels. When the vapour volume at Ch 06 increases the liquid film at Ch 07 grows and is sometimes completely wetting Ch 07. But at the same time sharp vapour peaks obviously resulting from small bubbles, appear at Ch 07. Combined with the persisting positive flow we must describe the flow pattern in the heated section of the bundle as a slug flow.

The length of the boiling zone is somewhat less than 12 cm which means that the mean bundle outlet temperature stays below the saturation level.

Run number 13 (Fig. 7) is the only experiment with a flow reversal. For approximately 1.2 s a negative flow of 3 cm/s existed. This was caused by a bulk superheat of 70°C, which means that already the last 17 cm of the bundle was above its saturation temperature. After a flow recovery to 40% of the initial value the boiling zone comprises 60% of the heated length of the bundle. Because of the additional two-phase pressure drop the flow recovery could not be complete. The appropriate Chen detectors in the boiling region almost continuously indicate vapour. Only from time to time liquid is detected.

This pattern agrees well with the volume of vapour produced in the boiling zone. Already a few centimeters within the boiling zone the void fraction has grown to 100%. At the end of the heated length the vapour velocity has risen far above 200 m/s. This type of boiling can be described as a combination of churn and dispersed annular flow. The annular flow regime is not fully developed, probably due to the restricted length of the boiling zone. A survey of the observed boiling modes is given in Table II.

TABLE II
Survey of the observed boiling patterns

Run number	Radial temperature gradient	Dominating boiling mode	Length of boiling zone
1	21°C	bubbly flow	3 cm
6	50°C	slug flow	12 cm
13	23°C	churn / annular dispersed flow	24 cm

3. AN EXTRAPOLATION TO LOF CIRCUMSTANCES IN FAST BREEDER CORES

We have seen in the previous chapter a sequence of boiling runs in which the length of the boiling zone steadily increased. After the initial superheats stable boiling with only minor oscillation in flow and temperature was observed. If we neglect the superheats because they are not expected in fast breeder cores, it must be concluded that the multi-bubble slug models developed from sodium experiments in tubes do not apply to these bundle experiments.

Therefore in the previous chapter the observed patterns were described in terms of the nomenclature which is applied mostly in two-phase flow through water cooled tubes.

A comparison with a rough prediction for sodium tube flow [10] fits reasonably well.

In a fuel element saturation is reached at a pressure of 1.8 atm and a heat flux of 180 W/cm². Therefore in a F.E. the volume of the vapour produced per m³ is smaller than it was in these bundle experiments. This means that the axial extension of the two-phase region could be the same for both cases, or even less for the Fuel Element case, and that stable boiling under these conditions is possible. Implementation in the WCAC of this stable boiling model which has a net positive flow through the fuel elements, seems worthwhile.

Another argument for the introduction of more refined boiling models in the WCAC is that as long as small superheats are assumed to be realistic, it is unlikely that boiling will start simultaneously throughout the whole cross section of a fuel element. The radial temperature differences across the fuel elements in core zone 1 of the SNR 300, excluding the steep gradients at the

wrapper tube, will exceed on the average 20°C at the onset of boiling. At low superheats the voided region will expand downstream into the upper blanket. Upstream it will follow the moving level at which the saturation temperature is reached.

Our experiments are not suitable for predicting dryout: due to the lower heat flux in the bundle the evaporation rate from the rest films is smaller than in the core. As a consequence dryout in the core may occur sooner than in the twelve rod bundle.

CONCLUSION

If we try to summarize cautiously we arrive at the following:

- The boiling models currently employed in whole core accident codes do not apply to our bundle experiments.
- The application of a more refined boiling model in these codes may lead to a more realistic description of the pre-disassembly phase of a Loss of Flow accident.
- For the safety evaluation of future bigger cores it seems worthwhile to include in the whole core accident codes a module which handles flow redistribution in the core due to voiding.

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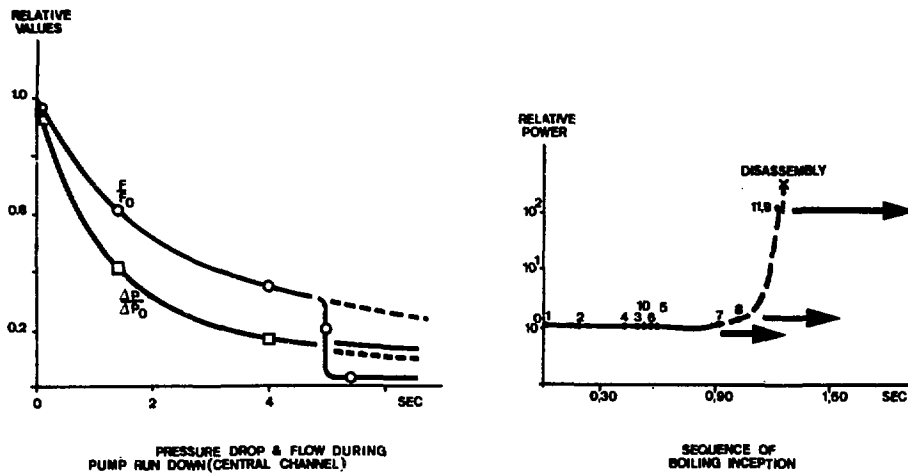


Fig. 1. Influences of Flow Redistribution

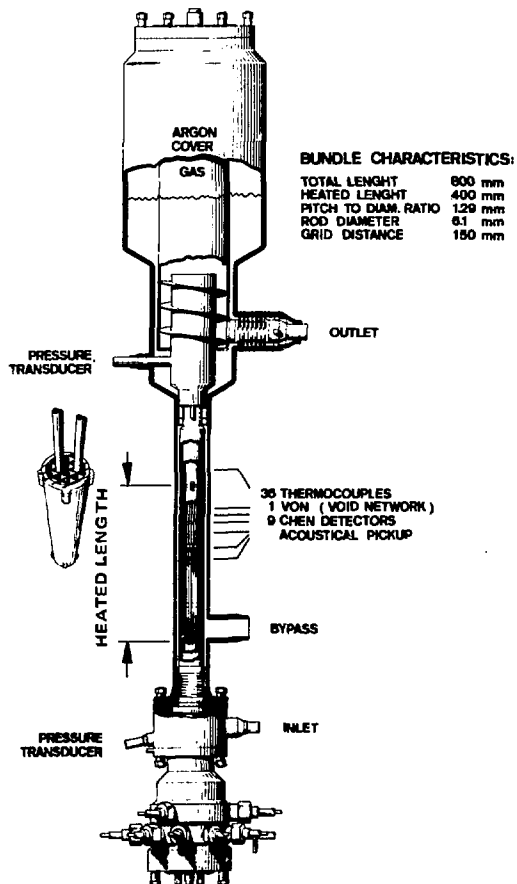


Fig. 2. 12 Rod Bundle in Test Section

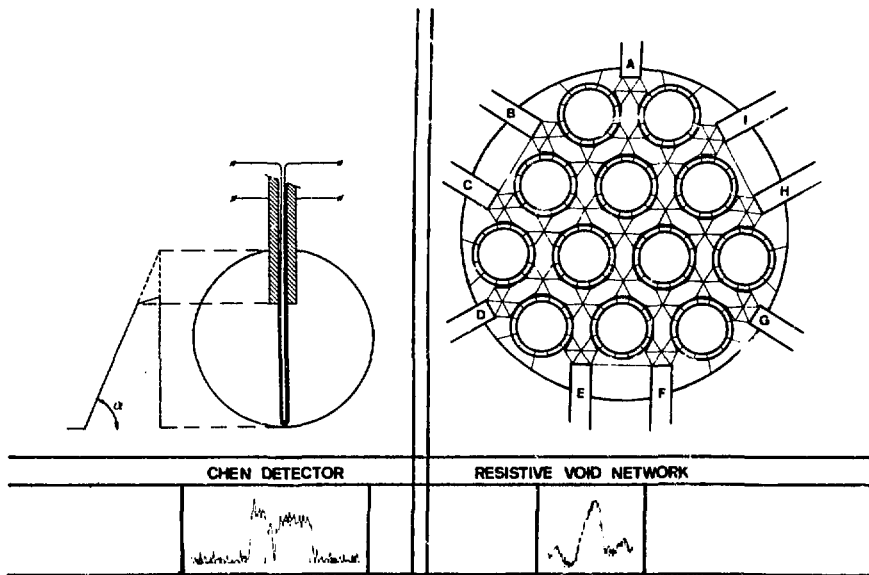


Fig. 3. Void Detectors

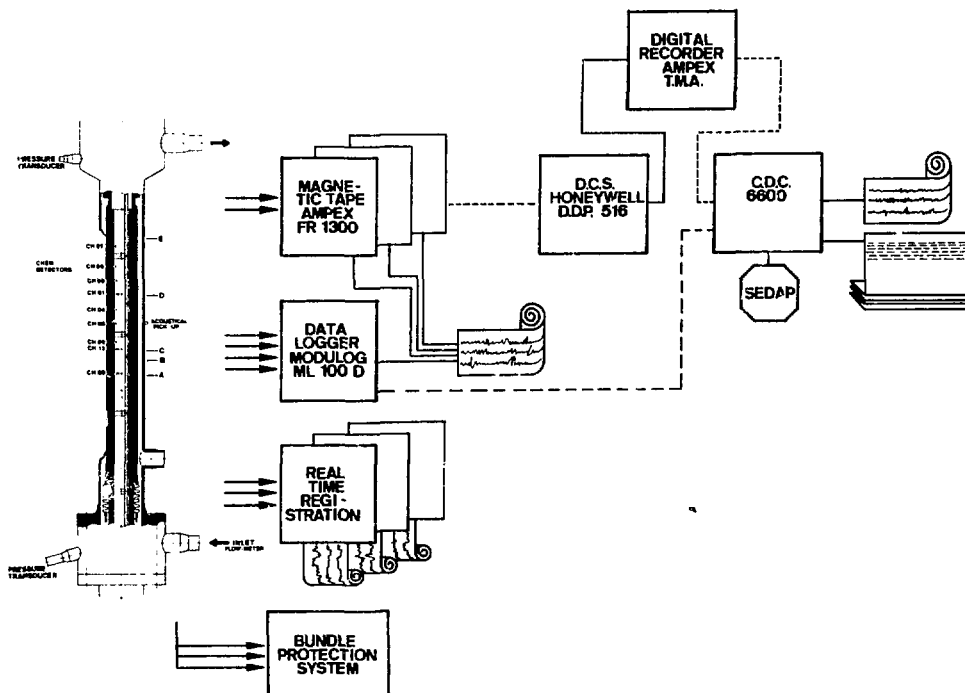


Fig. 4. Block Diagram, Data Registration System,
12 Rod Bundle

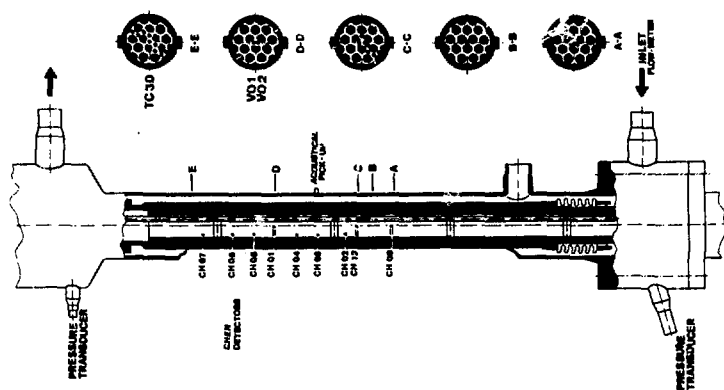
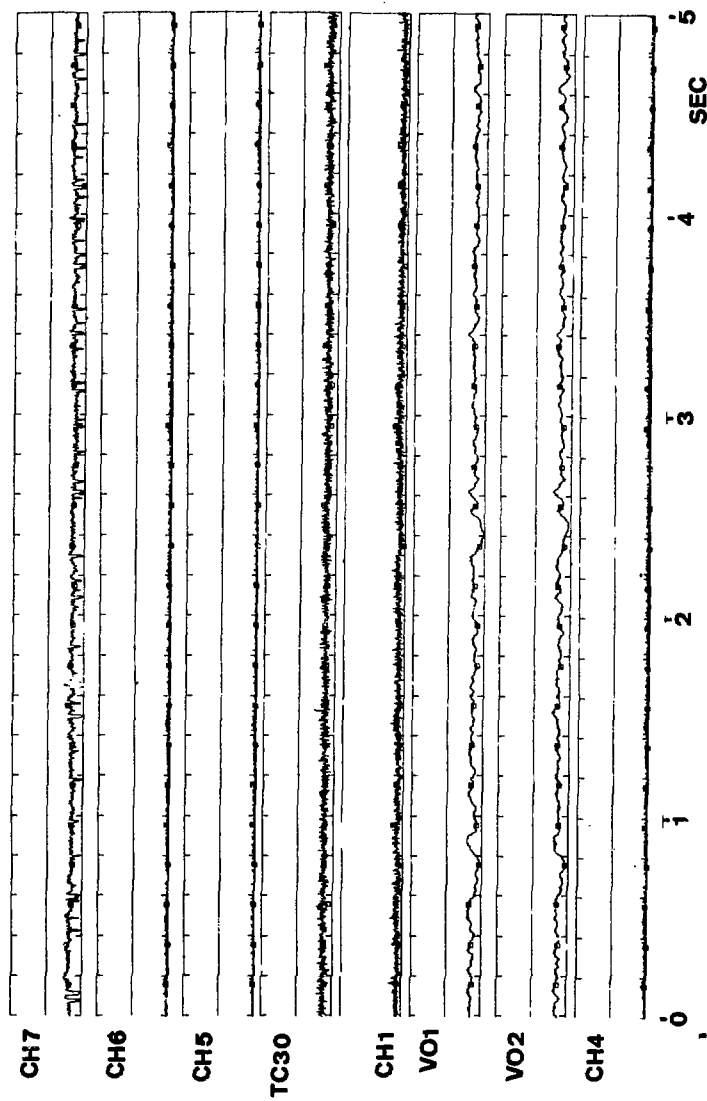
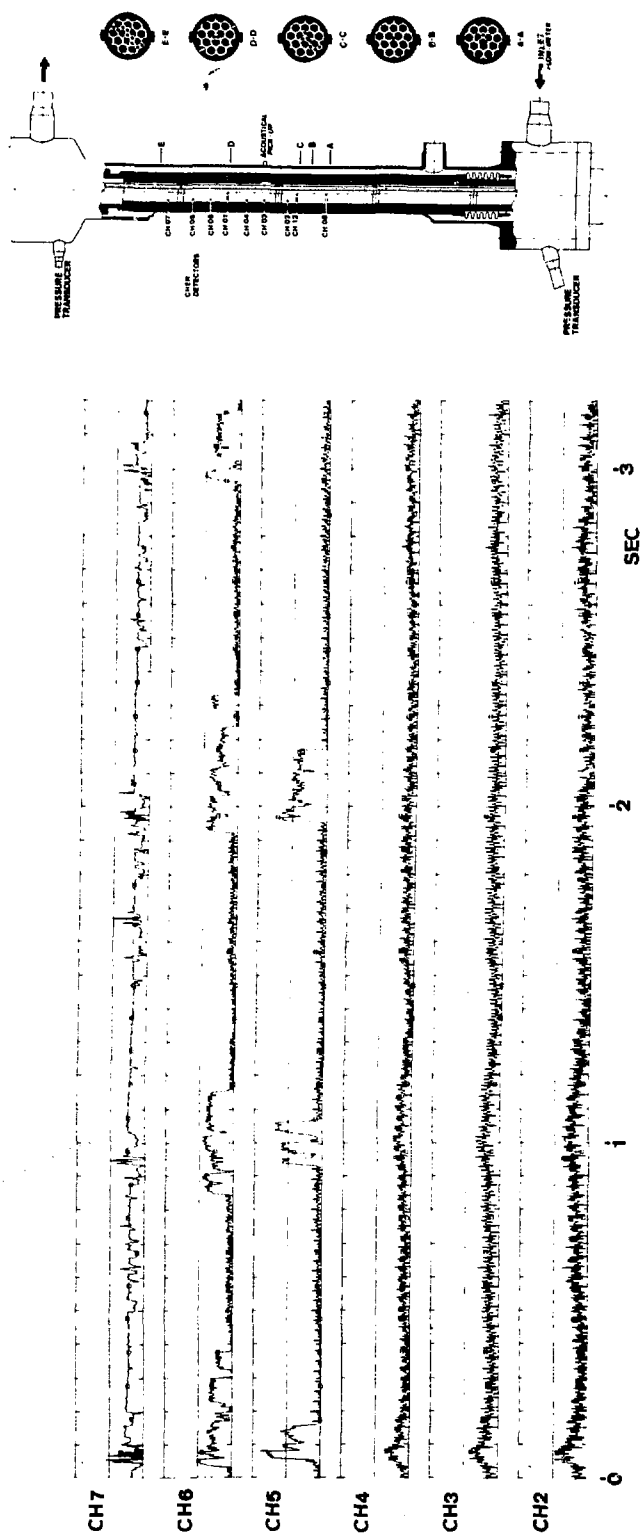
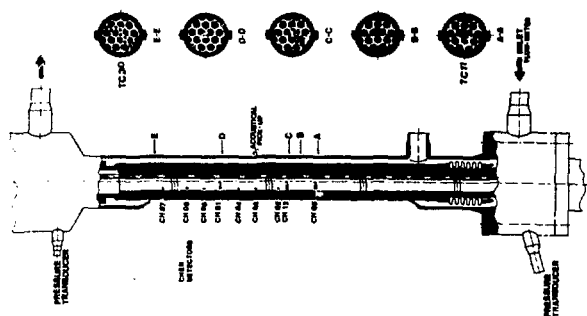
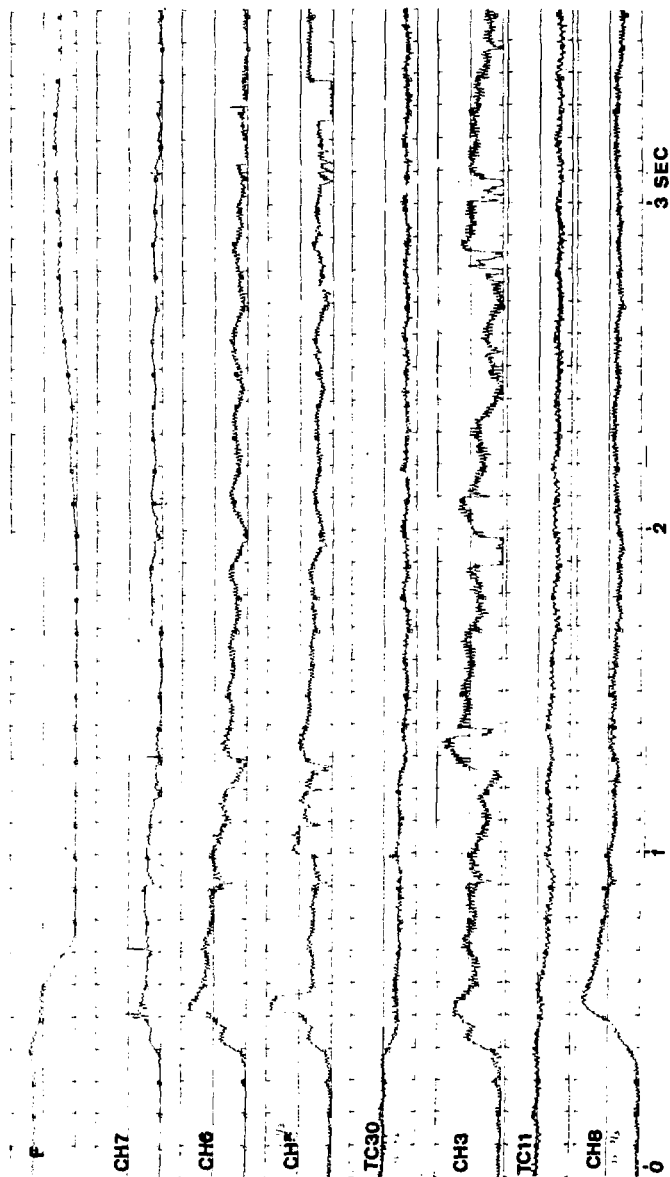


Fig. 5. RUN NO.1 $T_{SAT}=700^{\circ}$ $\Delta T_{SUP}<4^{\circ}$ $\Delta T_{RAD}=21^{\circ}$ $P=36 \text{ W/CM}^2$ $V=.3 \text{ M/S}$



BOILING RUN NO 6 $T_{SAT} = 888^{\circ}$ $AT_{SUP} = 28^{\circ}$ $AT_{RAD} = 50^{\circ}$ $P = 24 \text{ W/CM}^2$ $V = .2 \text{ M/S}$

Fig. 6



BOILING RUN NO.13 $T_{SAT} = 650^{\circ}$ $\Delta T_{SUP} = 70^{\circ}$ $\Delta T_{RAD} = 23^{\circ}$ $P = 24$ W/CM² $V = .25$ M/S

Fig. 7

A NUMERICAL SIMULATION OF THE TWO-DIMENSIONAL BOILING (VOIDING) IN LMFBR SUBASSEMBLIES

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ABSTRACT

Preliminary estimates of the two-dimensional voiding effects indicate that they are not likely to be very significant in affecting the time to flow reversal.

1. INTRODUCTION

The consideration of intra-subassembly incoherencies in boiling and dryout [1], clad melting and relocation [2,3], and fuel melting and relocation, is important in understanding in realistic physical terms the early evolution (initiating phase) of unprotected Loss-of-Flow Accidents (LOFA) in LMFBRs. These incoherencies are due to non-uniform radial temperature and power, distributions at steady-state, and their enhancement during the Loss-of-Flow transient. These radial variations can be responsible for departures from one-dimensional behavior within each one subassembly. These departures must be assessed within the context of system's accident analysis codes (i.e. SAS-3A) which by necessity are one-dimensional. This paper is concerned with boiling incoherencies or, as they usually are referred to, two-dimensional subassembly voiding phenomena.

The currently available experimental information in the area of two-dimensional voiding is limited to the 7-pin TREAT R-Series [4], and the corresponding OPERA tests [5]. Experiments with larger bundles are planned and/or are in progress both in the U.S. and abroad [6]. However, the calculational tools for the detailed evaluation of these experiments do not presently exist. The one-dimensional, SAS, slug-bubble type calculation has produced a shorter (by several hundred milliseconds) time interval between boiling inception and inlet flow reversal, than that determined experimentally [7]. This discrepancy has been attributed to SAS's failure to model the two-dimensional nature of the early boiling process [7]. It has been also argued that this discrepancy was overemphasized by the conditions of the 7-pin tests, and it would not be expected to be significant for full 217-pin LMFBR bundles.

Boiling incoherencies will have an impact upon the subsequent phenomena of cladding melting and relocation and fuel melting and relocation. However, they may also be important in their own right in that they could influence voiding histories and thus affect reactivity insertion histories. Further for large cores for which LOFA proceeds into an overpower transient, it is important to

predict as accurately and reliably as possible the degree and pattern of core voiding for all stages of the accident. Each one of these areas of relevance is affected to a different degree and thus exhibits different sensitivity, within the context of accident scenarios and system's behavior, to the details of the voiding process. The approach taken in this work is that the development of a "separate effects" detailed, computational tool, would be helpful in scoping the magnitude of these effects, and if necessary, in guiding the development of system's oriented models containing all the essential features of the problem.

In the prediction of two-dimensional boiling under the LMFBR conditions of high power densities, large and rapid changes in density and density gradients one encounters unknowns associated with both the "physics" and the "numerics" of the problem. In this discussion "physics" aspects refer to flow regimes, phase slip, frictional characteristics, heat transfer characteristics etc. The work presented herein deals primarily with the numerical aspects. It is the intention that having established computational capability, the "physics" aspects can be appropriately scoped to determine the areas of major influence.

2. ANALYTICAL

2.1 The Physical Model

The approach taken here is based on the following observations:

- (a) For the LMFBR conditions of interest a negligibly small amount of superheat would be expected [7];
- (b) The initial two-dimensional boiling would be characterized by an essentially homogeneous two-phase zone with the phases at thermodynamic equilibrium;
- (c) In general one is not interested in describing the details of the microstructure of this two-phase zone but only its behavior in a local "volume averaged" sense;
- (d) The time of interest for such calculations would not extend much beyond inlet flow reversal and transition to one-dimensional voiding.
- (e) Structure heat capacity effects are important in affecting vapor condensation and thus limit the rate of expansion of the two-phase zone;
- (f) The space-time evolution of the pressure field is extremely important for the definition of the boiling boundaries, and the onset of the flow instability.

Prior to further elaboration on the points mentioned above the system geometry and general conditions will be introduced. Figure 1 depicts the subassembly geometry with a schematic indication of the radial and axial enthalpy distributions, at the time of boiling inception (saturated conditions at the hot spot), as predicted by transient, single-phase, heatup calculations (i.e. COBRA) during the pump coastdown with normal power. The power distribution, is also shown over the active region of the subassembly.

The matter of superheat is not considered too controversial at this time. For SAS calculations boiling inception must be specified at some superheat level above $\sim 10^\circ\text{C}$. This is due to the detailed nature of the model, following each individual bubble, and limited to a certain maximum number of bubbles (per channel) by computer storage. In fact the initial superheat is expected to be very nearly zero; and our choice of an equilibrium two-phase zone is consistent with experimental evidence [7], and circumvents this SAS problem.

The homogeneous two-phase flow requirement imposed in our current formulation is likely to be the most controversial one. It is the intention to include slip effects in the next version of our code. The point to be made, however, is that for such fast transients, normally a fraction of a second, one could question the slip formulation inputs (i.e. slip constitutive equations, two-phase friction) just as justifiably as one could the non-slip one [8].

Previous two-dimensional codes (i.e. THINK, COBRA) have mostly preferred the subchannel method of formulation. The extremely large number of subchannels

present in an LMFBR bundle, prompted us to look at it as a porous medium. In such case one is not interested in the details of each pore but in a more macroscopic sense the "average" behavior of a group of pores. This leads to the "volume averaged" formulation [9] for the equations of change, constitutive equations and equations of state. An equivalent continuum is thus produced (a cylinder) with volume sources (or sinks) of energy and momentum; corresponding to the local fluxes of energy and momentum at the micro-scale level.

The effect of the structure is visualized as an energy reservoir, receiving a certain power input, and exchanging energy with the surrounding coolant through a specified heat transfer coefficient. This formulation can be made as time-and-space dependent as desirable.

We will be primarily interested at events from boiling inception to transition to one-dimensional flow by the onset of inlet flow reversal.

The code will be called HEV-2D (Homogeneous, Equilibrium Voiding - Two Dimensional).

2.2 The Mathematical Model

The basic tool utilized in converting the equations describing the thermal-hydraulic behavior of the fluid within the pin spaces to that of an equivalent continuum occupying the whole inner subassembly region is a theorem that converts the volume average of a gradient to a gradient of the volume average. For a quantity ψ , of any tensorial order, we have [9]:

$$\langle \nabla \psi \rangle = \nabla \langle \psi \rangle + \frac{1}{V_f} \int_S \psi \underline{n} dS' \quad (1)$$

The resulting differential equations are well known and need not be reported here. Suffice it to say, that we have lumped all the momentum fluctuation correlation terms to an effective volume friction, and the energy-velocity correlations to an effective thermal diffusivity. As it turns out the results are not very sensitive to these details.

A lumped parameter model was utilized for the structural mass (cladding), exchanging energy with the coolant.

2.3 Computational Techniques

The ICE scheme [10] for the implicit coupling of the continuity and momentum equations was utilized. Taking advantage of the fact that only a relatively small region of the subassembly is found with two-phase flow, while the rest remains in the liquid (incompressible) state, we have devised a technique for implementing the ICE method that resulted in dramatic computational savings. This technique reduces the size of the matrix to be inverted at each computational cycle to that corresponding to the number of boiling nodes. The energy equation is treated explicitly, as in the original ICE method.

2.4 Test Procedures

As an initial check of the computation a gas-dynamic problem with volume heating was considered. The gas is initially contained in a cylinder and is in pressure equilibrium with the atmosphere. Volume heating causes gas expansion into the atmosphere. For this one-dimensional problem an exact solution may be found, and was utilized for checking the computational procedures. A sample comparison is shown in Figure 2.

The overall numerical scheme proved quite stable, even for time steps as large as 2 milliseconds. As may be seen in Figure 16, however, the error in this case might be excessive. In view of the economy realized in the matrix inversion process a time step of $5 \cdot 10^{-4}$ is quite acceptable.

3. RESULTS

3.1 Background of Computations

At this first stage of the computations the primary interest was to identify any differences between the one-dimensional and two-dimensional representations. For the latter the same program was run with uniform, radially, initial enthalpy distributions (corresponding to the same radial averages of the distributions utilized in the two-dimensional computations).

We considered two bundles. One 1.5 ft. long and the other 6 ft. long. The overall pressure drop in the small bundle was 1.5 psi and that in the long bundle was 14 psi. The heat input in the small bundle represented the upper 0.5 ft. of the active core. That of the long one represented the whole active core. The remaining portions represented the upper blanket and fission gas plenum regions.

The homogeneous two-phase friction multiplier was utilized [12] in all these computations. The heat transfer coefficient was set at 5,000 or 10,000 BTU/hr ft² °F. The time origin in all calculations is set at boiling inception.

3.2 Small Bundle Calculations

The two-dimensional nature of the flow due to local boiling, as seen in local velocity changes (variations from the initial), is presented in Figure 3. Flow reversal occurred in 35 milliseconds. The time-wise variation of the coolant density and velocity along the center line of the bundle is shown in Figures 4 and 5 respectively.

3.3 Large Bundle Calculations

For the one-dimensional model, flow reversal occurs now at 170 msec and for the two-dimensional one at 190 msec. The time-wise variations of the center-line velocities are shown in Figures 6 and 7. Notice the more gradual variations predicted in the 2-D results. A similar comparison for the pressure may be found in Figure 8 and 9. The r-z plots of density at the instant of flow reversal for the 1-D and 2-D cases are presented in Figures 10 and 11. Similar r-z plots of the density at various times indicating the voiding pattern for the 2-D model are given in Figures 12 and 13. Finally the time-wise center-line density variations may be found in Figures 14 and 15.

Figure 16 provides a comparison of inlet and outlet flows for the 1-D and 2-D models. The difference does not appear to be negligible.

No efforts were made to provide detailed comparisons with SAS voiding results. SAS would predict inlet flow reversal at 800 milliseconds. Sensitivity studies indicate that this difference is not due to homogeneous two-phase friction multiplier being substantially larger than that utilized in SAS. A contribution to this difference should rather be due to the variations in heat input to the two-phase mixture. When normalized to the flow reversal time the two calculations compare as shown in Figure 17.

CONCLUSIONS

Preliminary estimates of the two-dimensional voiding effects, indicate that they are not likely to be very significant in affecting the time to flow reversal.

ACKNOWLEDGMENTS

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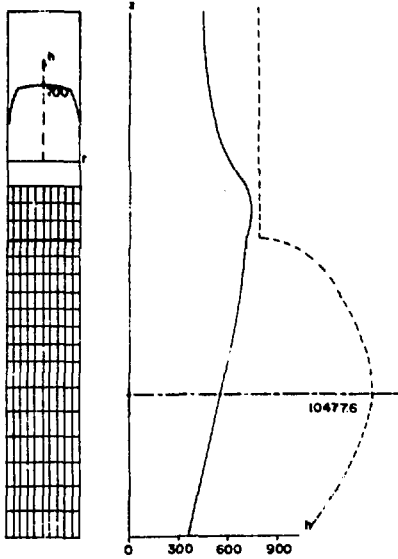


Fig. 1. Subassembly geometry.
Enthalpy and power profiles.

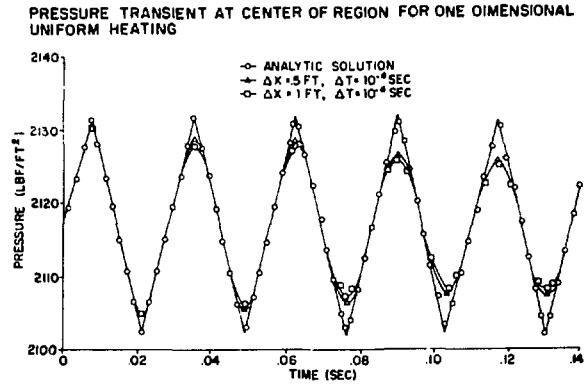


Fig. 2. Comparison between exact and numerical solution.

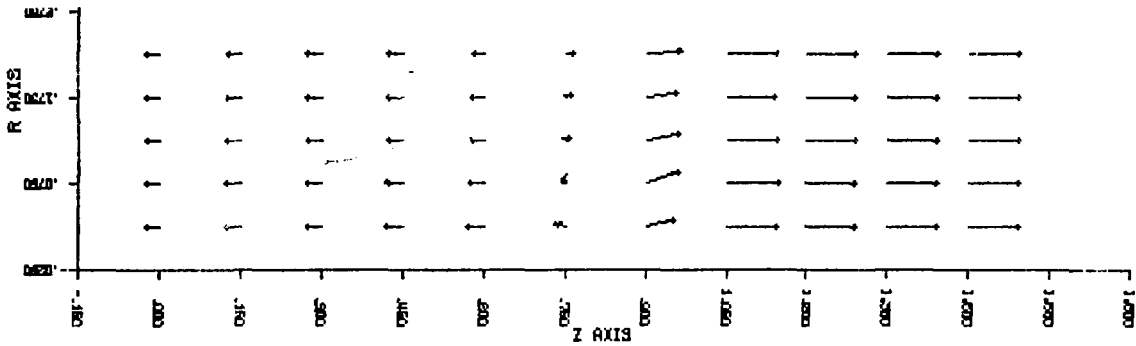


Fig. 3. Two-dimensional velocity distribution.

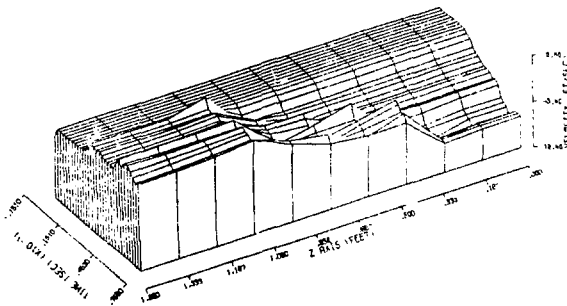


Fig. 4. Time-wise variation of centerline velocity.

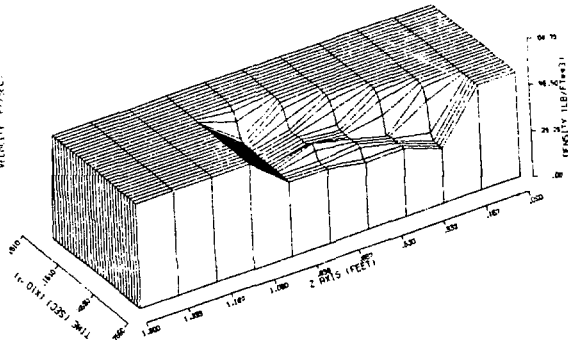


Fig. 5. Time-wise variation of centerline density.

Fig. 6. Time-wise variation of centerline coolant velocity for the 1-D model.

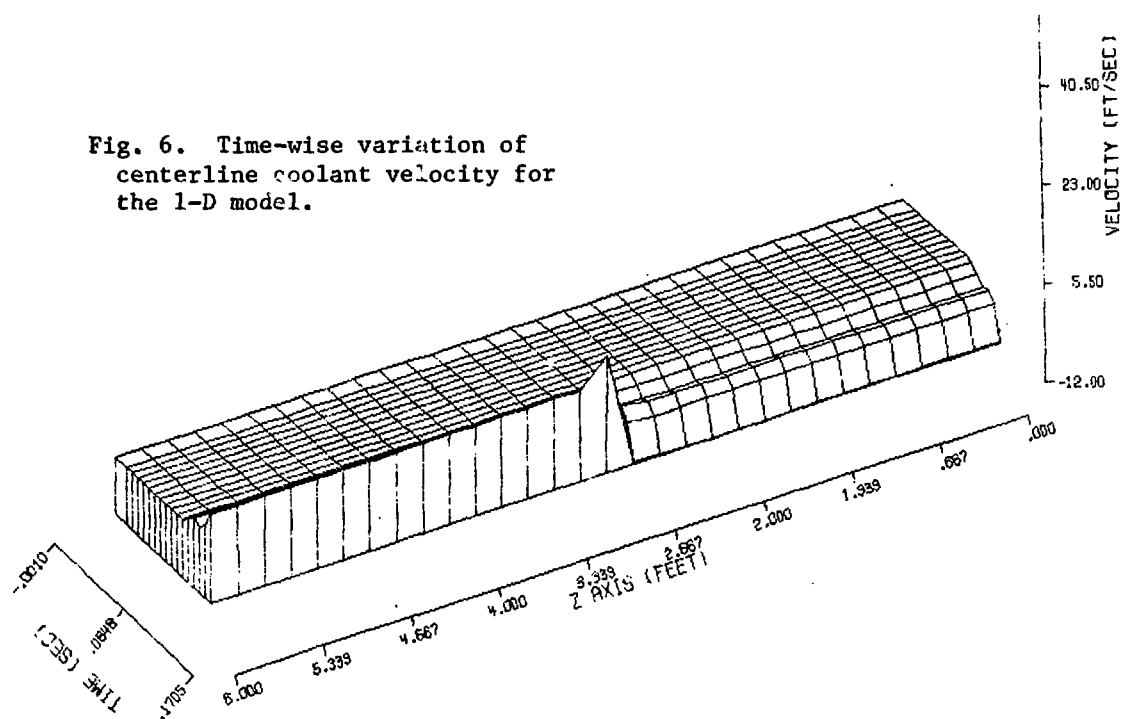


Fig. 7. Time-wise variation of centerline coolant velocity for the 2-D model.

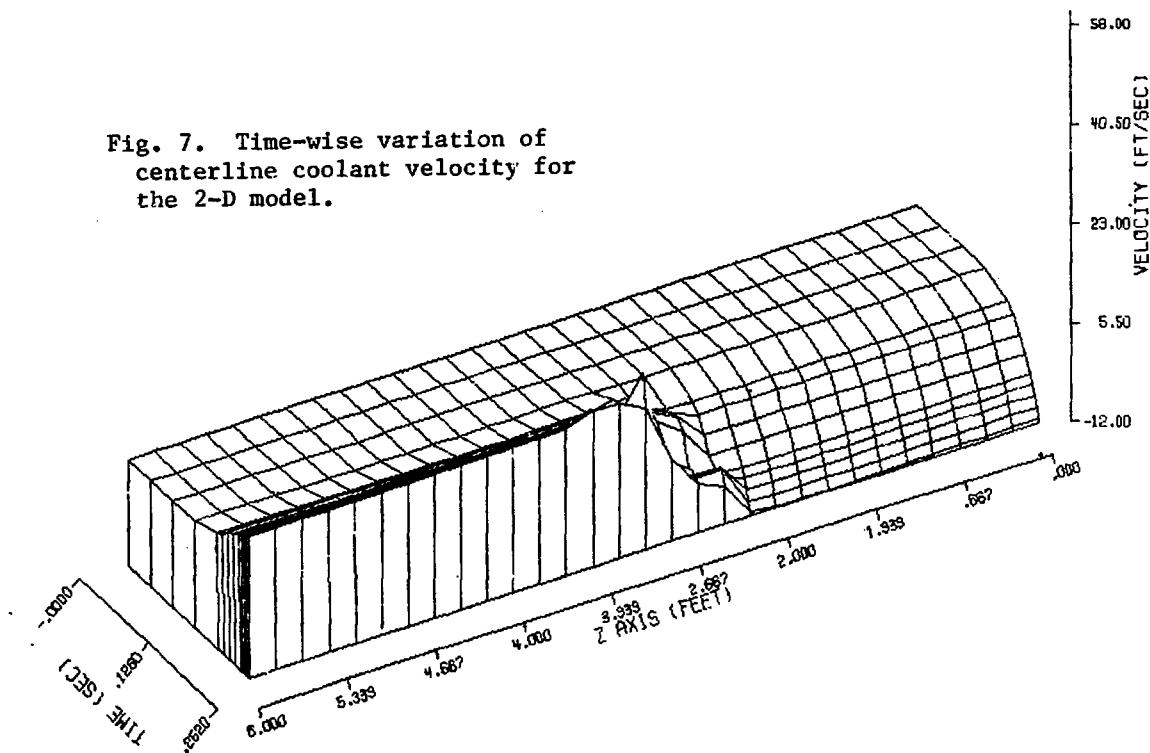


Fig. 8. Time-wise variation of the centerline pressure of the 1-D model.

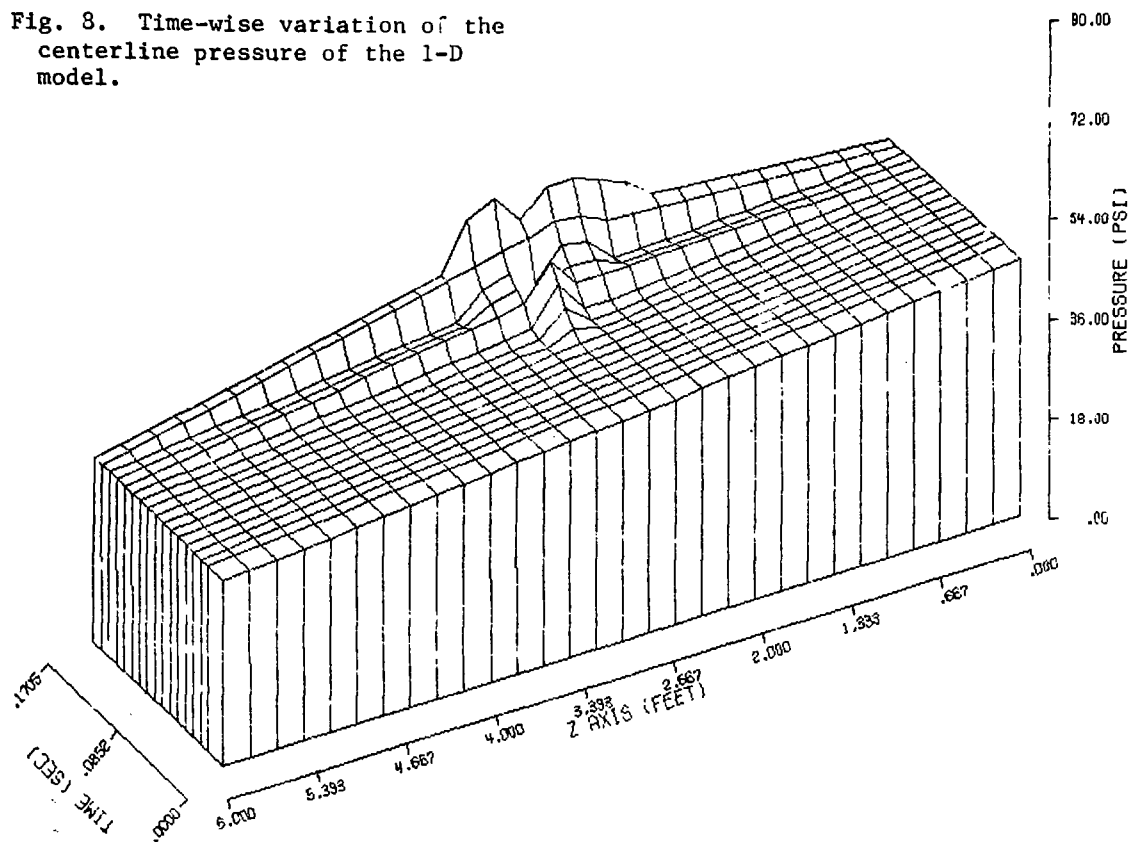


Fig. 9. Time-wise variation of the centerline pressure of the 2-D model.

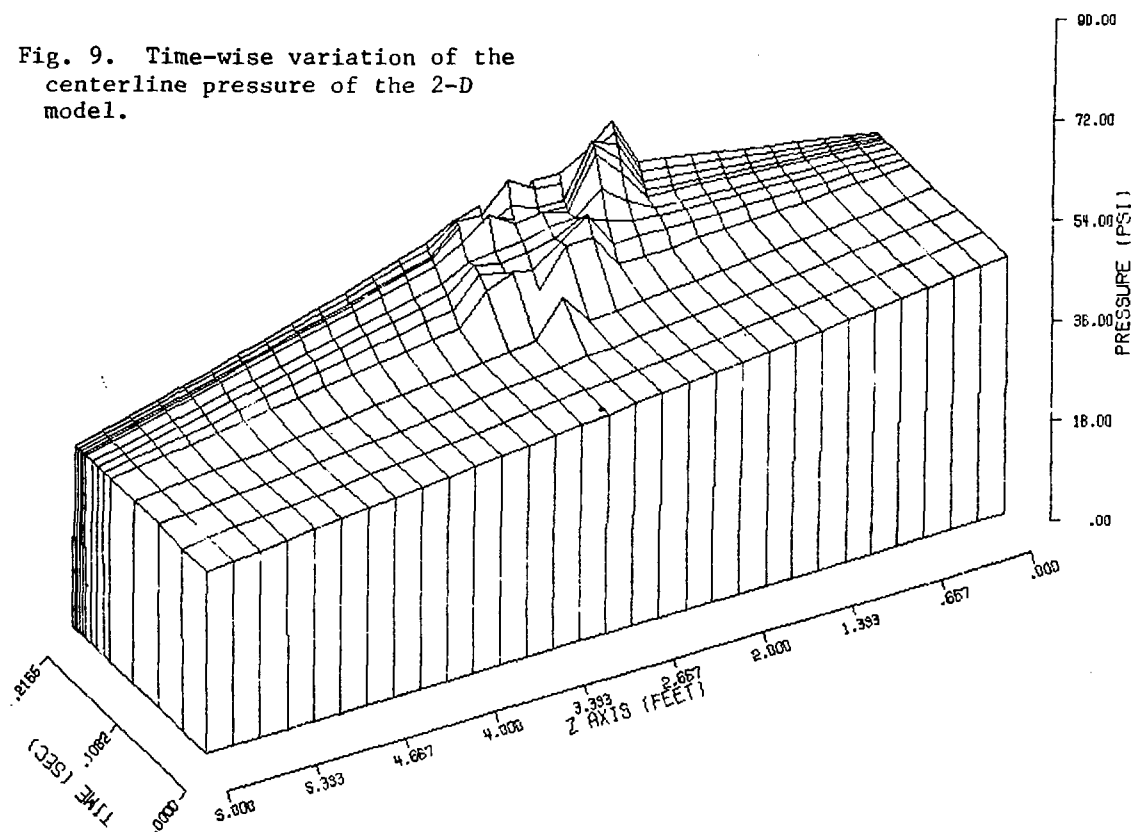


Fig. 10. Density distribution (r-z), at time of flow reversal for the 1-D model.

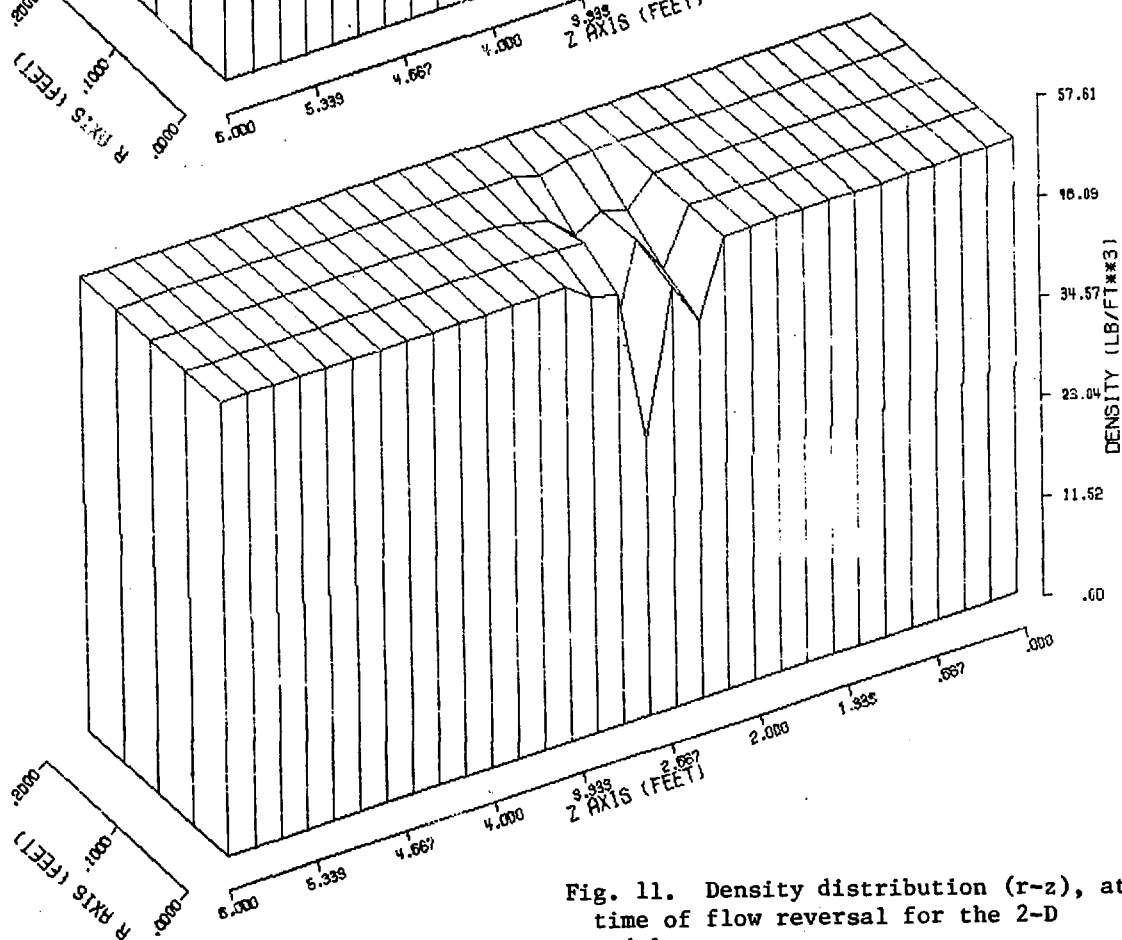
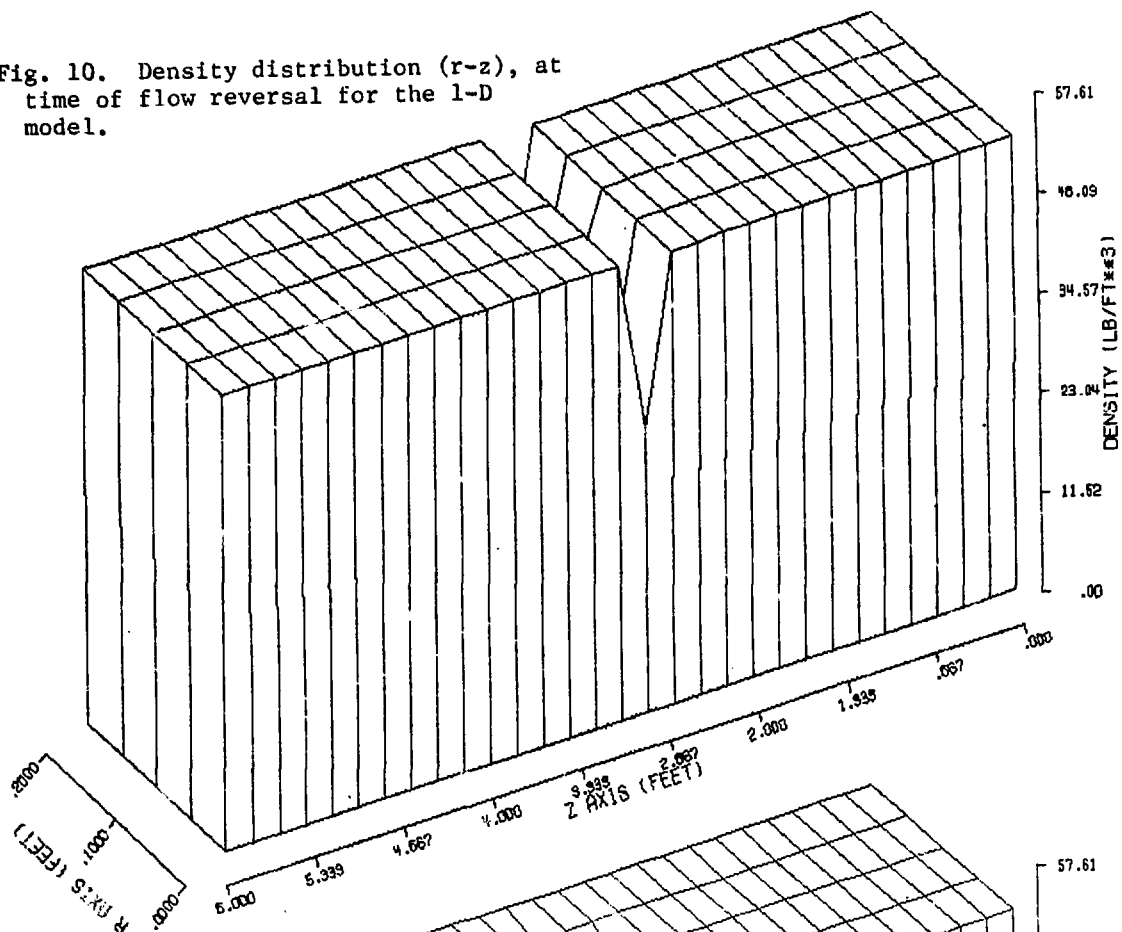


Fig. 11. Density distribution (r-z), at time of flow reversal for the 2-D model.

Fig. 12. Density distribution for the 2-D model at 15 msec after flow reversal.

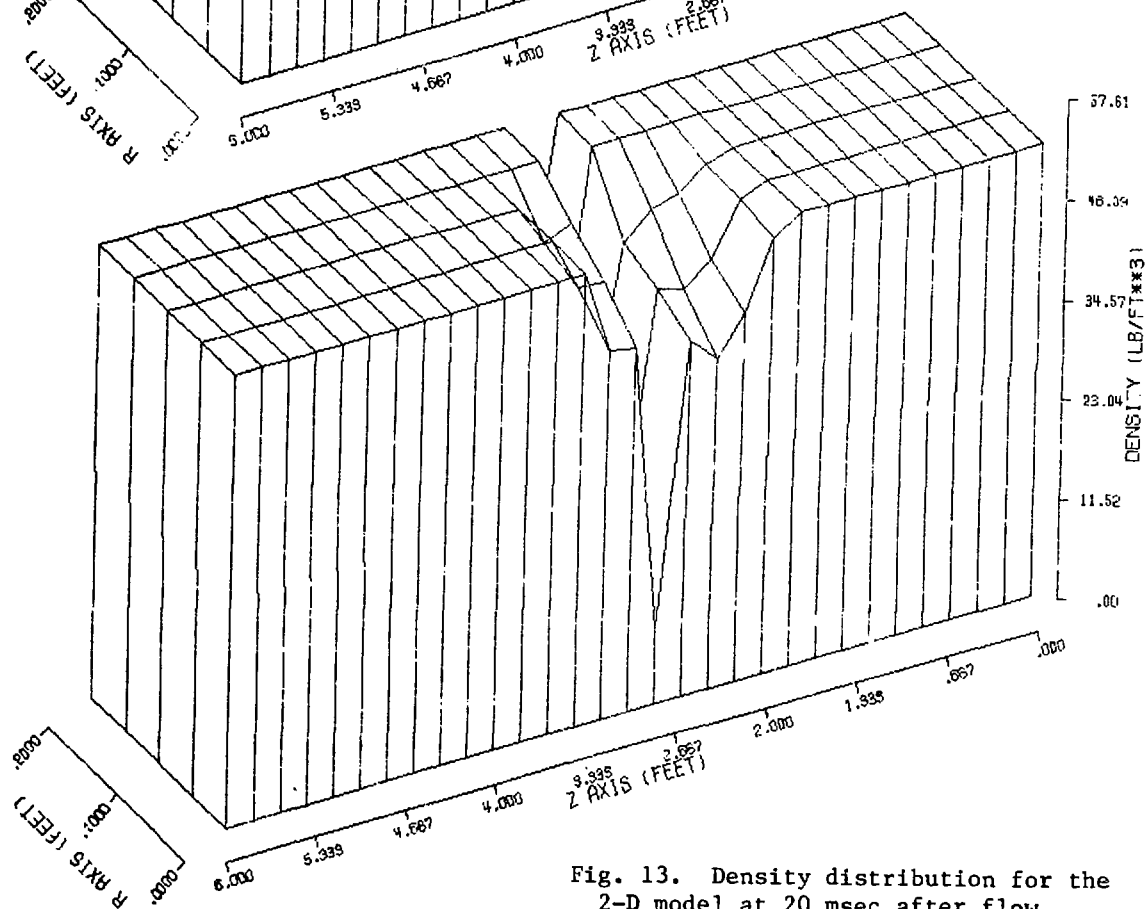
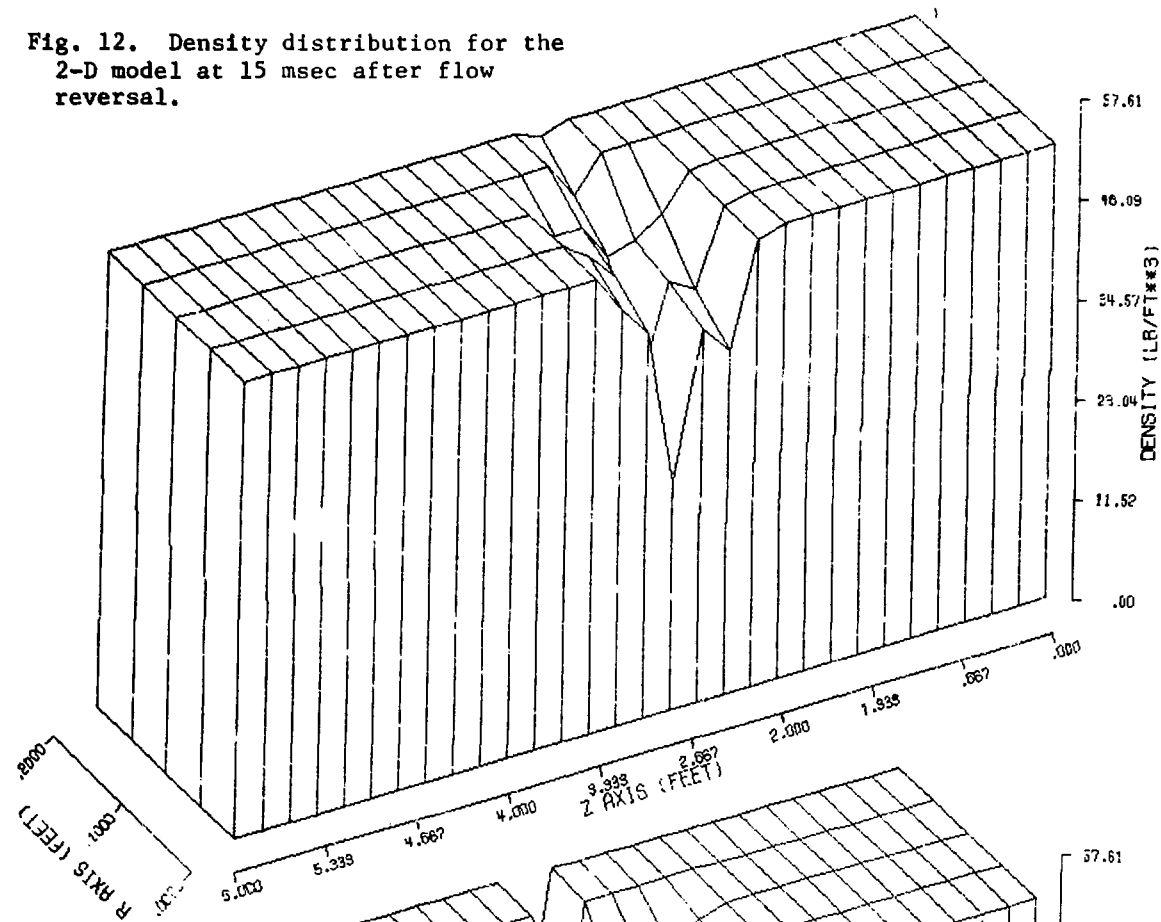


Fig. 13. Density distribution for the 2-D model at 20 msec after flow reversal.

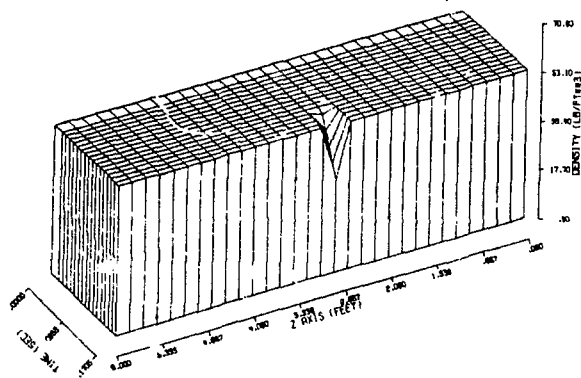


Fig. 14. Time-wise density variation for the 1-D model.

Fig. 15. Time-wise density variation for the 2-D model.

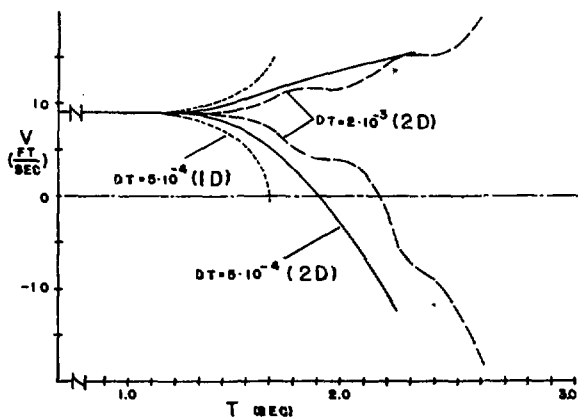
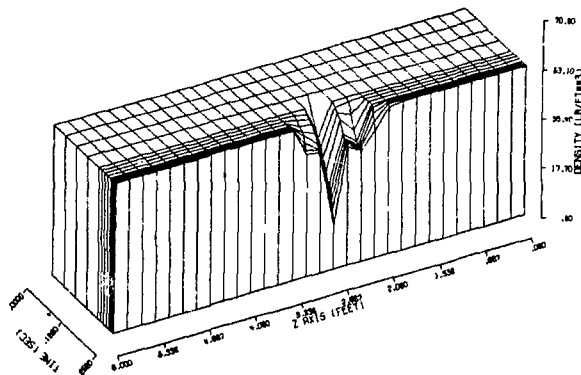
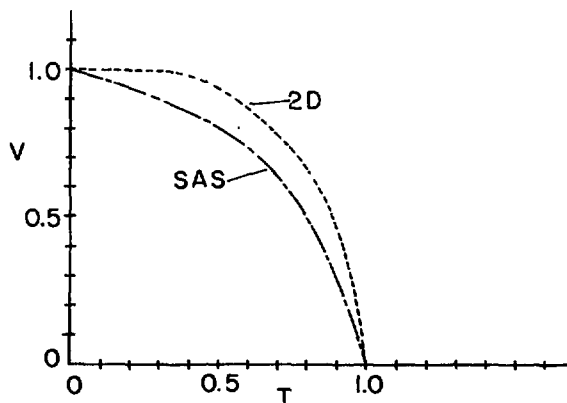


Fig. 16. Comparison of inlet flow histories between 1-D & 2-D model, and time step study for the 2-D model.

Fig. 17. Comparison of normalized inlet flows between SAS & the 2-D model.



AN INVESTIGATION OF FORCED CONVECTION BOILING OF SODIUM AT LOW
HEAT FLUXES IN ELECTRICALLY HEATED SEVEN PIN BUNDLES

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ABSTRACT

Sodium boiling at low flow and heat fluxes can be expected in a gamma-heated, in-reactor experiment. The results of experiments under these conditions (10-40 W/cm², 10-50 gm/s) in electrically heated seven pin rod bundles are described. The type of boiling is seen to depend upon the amount of superheat, the degree of inlet throttling and several system dependent parameters. The possible forms of acoustic and neutronic signals from an in-core experiment are discussed in terms of the underlying thermalhydraulics and the experimental findings.

INTRODUCTION

A number of potentially serious coolant disturbances in an LMFBR might conceivably begin in a single subassembly, the sources of which include: local blockages, enrichment errors and damage to a subassembly inlet. There is considerable motivation to develop systems to detect such disturbances at an early stage, both in order to prevent propagation to a whole core accident and to avoid expensive reactor downtime.

Among the various systems proposed to detect these disturbances are those designed to detect sodium boiling. Particularly promising are systems based upon global signals such as acoustic or neutronic signals or a combination of the two [1,2,3]. It appears however that before any such system can become an integral part of an LMFBR safety system, in-core testing will be necessary. Only in such reactor tests are the true signal to noise ratio conditions present.

One in-core experiment which appears worthwhile involves the use of the gamma flux in a reactor to heat tantalum rods to produce sodium boiling. The principal advantages of this method are that no complicated electrical leads are required and that safety considerations present in a fission heated experiment are avoided. On the other hand, because of the poor conversion efficiency of the gamma heating process, low coolant flowrates are needed in order to

*Experimental work performed at Institut für Reaktorentwicklung and Institut für Neutronenphysik und Reaktortechnik, Kernforschungszentrum, Karlsruhe, West Germany.

achieve boiling. Such an experiment has already been performed in the French test reactor Rapsodie [4], and a similar one has been proposed for EBR-II [5].

In terms of the thermohydraulics involved, these experiments can be classified as forced convection boiling at low heat flux since a small, but positive net flow exists during the boiling event. Such conditions could also be envisioned to occur in an LMFBR as a result of damage to a subassembly inlet incurred during refueling. In this case, boiling would begin during reactor startup, if the anomalous condition were not detected earlier by other methods.

To try to understand the phenomena present in a gamma heated experiment and to investigate the consequences of the startup accident postulated above, experiments have been performed in electrically heated seven pin rod bundles at the Kernforschungszentrum Karlsruhe. More specifically, the goals of the out-of-core experiments were:

- (a) to observe the type of boiling behavior at low heat flux in a rod bundle;
- (b) to estimate the type of acoustic and neutronic signals which would be observed in an in-core, gamma heated experiment; and
- (c) to provide experimental data for comparison with computer code calculations.

Work on this final point is still in progress.

EXPERIMENTAL EQUIPMENT AND PROCEDURE

The experiment was performed in the NSK sodium boiling loop at the Institut für Reaktorentwicklung (IRE). Information about this loop is available elsewhere [6]. Two seven pin bundles were used. The second of these, in which most of the tests were conducted, is shown in Figure 1. The rods themselves have an outer diameter of 6 mm and a pitch to diameter ratio of 1.32. This is the subchannel geometry for the German prototype reactor SNR-300. In both bundles the heated length is 600 mm, followed by 455 mm of unheated, follower rod. The major difference between the two bundles is in the outlet section where the extended length of the second bundle closely approximates an SNR subassembly outlet, and is therefore more suitable for boiling experiments involving transients.

Both bundles are heavily instrumented. The sensors which are referred to in this paper appear in Figure 2 and include thermocouples in the sodium (TC), permanent magnet flowmeters (D), void detectors (C), and quartz crystal pressure transducers (P). The void sensors are of the Chen type [7] with additional modifications made at the IRE.

All of the sensor signals were recorded using a 64 channel digital acquisition system. These signals were later analyzed using the data handling code SEDAP [8] on the Karlsruhe IBM 360/370 computer system. For signals with high frequency information a 14 track FM tape recorder was used. Fourier analysis was then performed at the Institut für Neutronenphysik und Reaktortechnik (INR) using a specially developed real-time analysis system.

After stationary conditions in the test section were reached, boiling was initiated by either increasing the pin power or reducing the flowrate. The former procedure was used in most tests since it is more representative of a gamma heated experiment. The experiments were carried out in the ranges

linear power	19-75 W/cm
mass flow	10-50 gm/s
outlet pressure	0.95-1.4 bar

These conditions approximate those found in a gamma heated experiment.

EXPERIMENTAL RESULTS

Since it was not known a priori what type of boiling will take place in a gamma heated bundle, it was considered desirable to obtain an envelope of boiling regimes that would bracket the most probable in-core situations. This is particularly important in trying to establish a basis for interpreting acoustic or neutronic signals from such tests.

Several different types of boiling were observed in the seven pin bundles depending upon the amount of incipient superheating, the degree of inlet throttling and the heat capacity of the system. This latter factor, which will of course vary from system to system, greatly influences the condensation process in the unheated upper section.

The cases considered in this paper are:

- (a) incipient boiling with superheat followed by stable boiling (Test 7-1/42);
- (b) boiling with superheat and minimal inlet throttling (Test 7-2/45);
and
- (c) transient boiling without superheat (Test 7-2/80).

In addition, analyses based upon several other test runs will be given. A test summary is given in Table I.

A. Incipient Boiling with Superheat Followed by Stable Boiling

This test was carried out in the first bundle at a linear rating of 26.8 W/cm. The transient was initiated by increasing the rod power, and boiling began with a superheat of 50°C. In Figure 3 the outlet flowmeter D3 at $h=650$ mm, the dynamic pressure sensor P4 ($h=600$) and the two void detector signals C3 ($h=550$) and C5 ($h=700$) for the initial boiling sequence are shown. The onset of boiling at $t=61$ s is visible in all the signals. The flowmeter D3 and the upper Chen detector C5 clearly indicate the expulsion of the upper liquid slug. The large pressure pulse in P4 is due to inertial effects as the rapidly growing bubble accelerates this plug upward. From the Chen detector C3 it can be seen that the initial bubble expands both upward and downward with an axial extent of at least 150 mm. For the first seconds of boiling the upper void detector C5 displays sharp liquid-vapor transitions, characteristic of the large single bubble mode of boiling in sodium [9,10].

This behavior is confirmed by the thermocouple signals in Figure 4. As the boiling starts, the thermocouples TC09 and TC08, located in a central and wall channel respectively, near the outlet of the heated section ($h=580$) indicate temperatures well above saturation. The radial temperature difference between the two channels is 31°C. Thermocouples in the clad also reflect this difference and emphasize the importance of boundary effects in such a small bundle. The thermocouple TC10 ($h=750$) located 150 mm above the heated section rapidly reaches the saturation temperature as the upper liquid-vapor interface moves upward. At the same time the temperatures farther down in the bundle are increasing as the transient continues.

Following approximately 30 seconds of transient behavior characterized by intermittent superheating, a period of stable boiling begins, as shown in Figure 5. Two points are of particular interest. First it can be seen that the temperatures remain constant at the saturation temperature, while the void detector signals fluctuate. This has been observed previously at a somewhat higher heat flux and is indicative of a homogeneous two phase flow [11]. Also the peaks in the pressure signal have been reduced considerably. This effect, which is believed due both to the expulsion of the upper liquid slug and to the damping effect of the two phase mixture, will be considered in detail later. Stable boiling of this type could be maintained indefinitely.

B. Boiling with Superheat and Minimal Inlet Throttling

In test 7-2/45, performed in the second bundle, the transient was initiated by reducing the Na flowrate, with a small amount of inlet throttling. An initial superheat of approximately 30°C was observed. Under these conditions the flow to the bundle can be reversed, as indicated by the inlet flowmeter D1 in Figure 6. The pressure drop during boiling is large in comparison to the available pump head and the lower liquid-vapor interface moves rapidly downward, accompanied by strong pressure pulses. Such a situation could arise in an LMFBR subassembly where the inlet throttling is relatively small.

C. Transient Boiling Without Superheat

With a high degree of inlet throttling and low superheat, the boiling regime is quite different. This is clear from Figure 7 for Test 7-2/80 at 20.4 W/cm. Boiling begins in this case at $t = 100$ s with a single, isolated bubble. Following a two second delay, a regular boiling pattern is observed. The bubble collapses are accompanied by sharp pressure pulses which correlate well with the fluctuations in the outlet flowmeter and the void sensor C4. The temperatures above the heated section increase very slowly as the upper phase boundary moves upward.

Inspection of the signal from the thermocouple TC10 ($h = 700$) reveals a temperature fluctuation which is correlated with the outlet flowmeter, and is a direct measure of the upper phase boundary motion. This motion can be seen more clearly with thermocouples TC12 and TC39, and will be discussed in detail shortly. The boiling remains remarkably constant for over 70 seconds and may be described as transient but stable boiling.

At the same linear power in the second bundle with superheat, slug type boiling is seen. The initial explosions are approximately 8-10 seconds apart. This decreases, more or less smoothly, to two seconds after about a minute of boiling. For this case, the pressure signals are quite different, and will be considered in the next section.

ANALYSIS OF RESULTS

As mentioned earlier, one of the goals of the research was to gain an understanding of the types of neutronic and acoustic signals which could be expected from a gamma heated, in-core experiment. Neutronic perturbations are produced by the reduction in coolant density during boiling. The magnitude of the detected perturbation depends upon the local sodium void coefficient and the reactor transfer function. Hence the motion of the liquid-vapor phase boundaries is the principal concern. For acoustic signals the pattern of bubble growth and collapse as well as the properties of the transmission path between the boiling and detector are of importance.

It should be noted that the observed signals will depend, to a large extent, upon the characteristics of the test section. It has been pointed out previously that the number of pins in a bundle is an important parameter [12]. In addition, the length of the outlet section must be taken into account. The following discussion therefore is concerned with the types of signals which would be produced if an in-core bundle were thermalhydraulically similar to the IRE bundles. This points out the need for computer analyses for the design and evaluation of an in-core experiment.

With respect to neutronic perturbations it is useful to recall that fluctuations in void volume or motion of the void within a neutron field are essential. That is, if a void were to remain constant in size and fixed in space, then its detection by neutron noise techniques would be extremely difficult. Accordingly, we consider here the fluctuations of the void volume.

In the experiments conducted at Karlsruhe, a variety of signals are available to indicate the liquid-vapor interface; these include the outlet flowmeter, void detector and thermocouples. Void detectors and many of thermocouples yield only binary information; the phase boundary has reached the sensor location or it has not. These signals therefore are useful in order to correct or calibrate the results of continuous void height estimates. For continuous estimates a flowmeter or a thermocouple located near the end of the outlet section is of value. For the analysis here, the temperature signals were most useful. In Figure 7 it was seen that in the second bundle the thermocouple TC10 displayed fluctuations related to the phase boundary motion. Since the interface soon reaches this height, its usefulness is limited. However the thermocouples TC12 ($h=1000$) and TC39 ($h=1100$) are much higher in the bundle.

The relationship between temperature and interface position can be formulated as follows. At the onset of boiling $t=0$, an axial temperature gradient exists in the upper unheated section due to the heat capacity of the follower rods and the assembly wall. To a first approximation this gradient can be approximated by a straight line. With this assumption the height of the upper phase boundary can be written in terms of the temperature at $h=S$ as

$$Z(T) = \frac{T_s(t) - T_s(0)}{KA_z/A_s} + Z_0$$

where

- K = slope of the temperature gradient
- A_z/A_s = ratio of flow areas
- A_0 = height at which boiling begins.

In practice this relationship must be modified to reflect the fact that the temperatures in the outlet region are increasing during the single phase transient prior to boiling. Also, because of the abrupt temperature change, the frequency response of the thermocouple must be considered.

With these corrections included, the effect of incipient superheating on the expulsion process can be examined. Two tests with varying amounts of superheat at the same heat flux are shown in Figure 8. The upper curve is for 50°C superheat, while the bottom curve is for 10°C superheat. For the case with high superheat the expulsion is much more rapid and abrupt. This is in sharp contrast to the low superheat case where the fluctuations are quite regular and increase in magnitude as the boiling continues.

Reactivity perturbations produced in the two cases would clearly be different. In this regard it should be noted that a gamma heated bundle could

well be shorter than the actual core height, so that the void coefficient would be relatively large in this oscillating condensation zone. Further it is important to emphasize that frequency domain analysis of such signals must be done using transient techniques in order to properly identify the two cases.

The pressure spectra during the tests were also observed to be time dependent. The auto power spectral density for transducer P4 for Test 7-2/86 appears in Figure 9 along with the background spectrum at the same flowrate without boiling. Boiling produces an elevation in the spectrum at low frequencies as well as resonance behavior between 10^2 and 10^3 Hz. These resonances are system dependent. The magnitude of the increase in the spectral density should be interpreted with caution since the sensor is located in close proximity to the boiling region.

Variations in the spectral densities from test to test were observed, as in Figure 10. The lower curve is for Test 7-2/87 with a linear power of 25.1 w/cm and a small initial superheat. The reason for the reduction both at low frequencies and in the resonance region is apparent when the time dependence of the spectra are taken into account. Spectral densities were calculated for 20 second records beginning with the start of boiling and with a time delay of 80 seconds. Examination of the spectrum in the resonance region for Test 7-2/87 in Figure 11a reveals some reduction in the magnitude of the spectrum but with little change in structure. On the other hand, for Test 7-2/87 in Figure 11b, much of the structure disappears entirely for the time-delayed spectrum.

The differences between these two cases can be explained in terms of the boiling regimes. Throughout 7-2/86, slug type boiling takes place. That is, abrupt ejections of superheated liquid occur as relatively isolated events. However in Run 7-2/87, the initial expulsion process is more rapid and is followed by low superheat boiling and a large two phase flow region. That these characteristics affect the pressure spectra is not surprising. It is known for example that the sonic velocity in a two phase flow depends upon the type of flow present [13]. Further there are both theoretical predictions [14] and experimental measurements [15] indicating that argon gas in a subassembly severely damps acoustic signals. One would expect the effect of the two phase sodium to be similar.

CONCLUSIONS

Several conclusions can be reached regarding the experimental results and their application to in-core gamma heated experiments.

- Several forms of boiling are observable at low power in a bundle depending upon the amount of superheat, degree of inlet throttling and system heat capacity.
- The design of the outlet zone where condensation takes place will affect the boiling process greatly. For the design of an in-core experiment careful calculations are therefore necessary.
- Analysis of global signals such as neutronic or acoustic signals from a gamma heated bundle must take into account the transient nature of the underlying thermalhydraulics.

ACKNOWLEDGMENTS

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TEST	BUNDLE	LINEAR POWER - W/cm -	MASS FLOW - gm/s -	OUTLET PRESSURE - bar -	OBSERVED SUPERHEAT - °C -
7-1/42	One	26.8	17.	1.40	50
7-2/45	Two	26.4	10.	1.18	30
7-2/80	Two	20.4	12.	1.04	<5
7-2/81	Two	25.1	12.	1.05	10
7-2/84	Two	25.1	12.	1.08	50
7-2/86	Two	20.4	12.	1.08	14
7-2/87	Two	25.1	12.	1.08	31

TABLE I: Summary of tests mentioned in this paper.

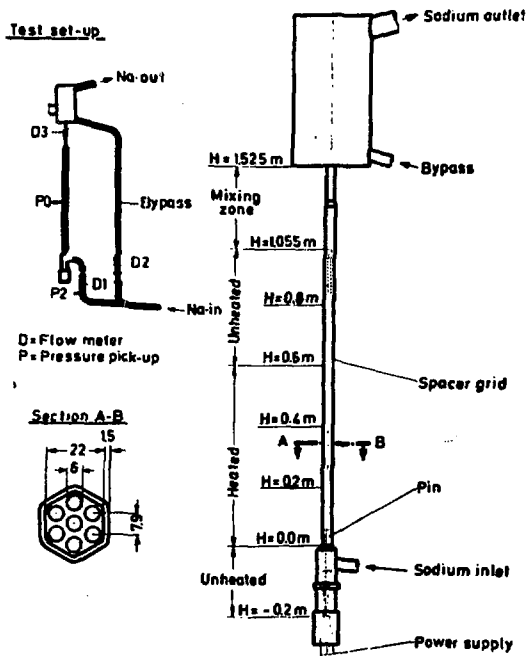


Fig. 1: Schematic of IRE seven pin bundle number two

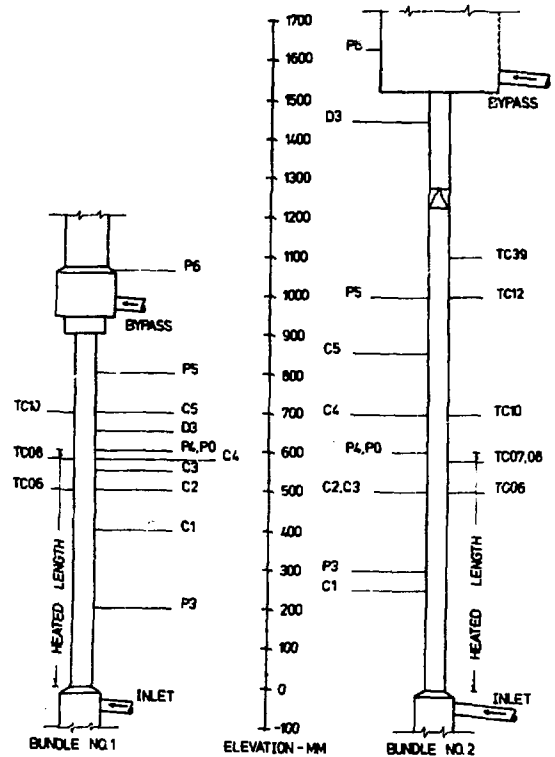


Fig. 2: Sensor locations in the seven pin bundles

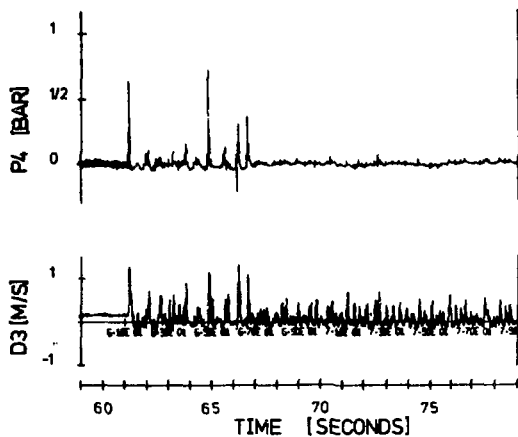
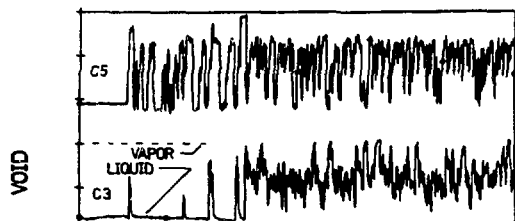


Fig. 3: Onset of boiling with superheat, Test 7-1/42

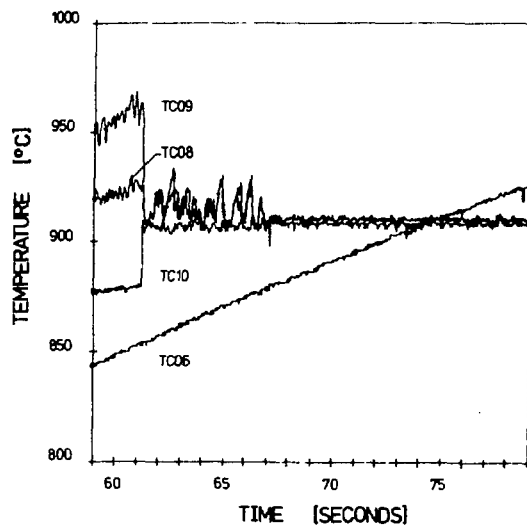


Fig. 4: Onset of boiling with superheat, Test 7-1/42

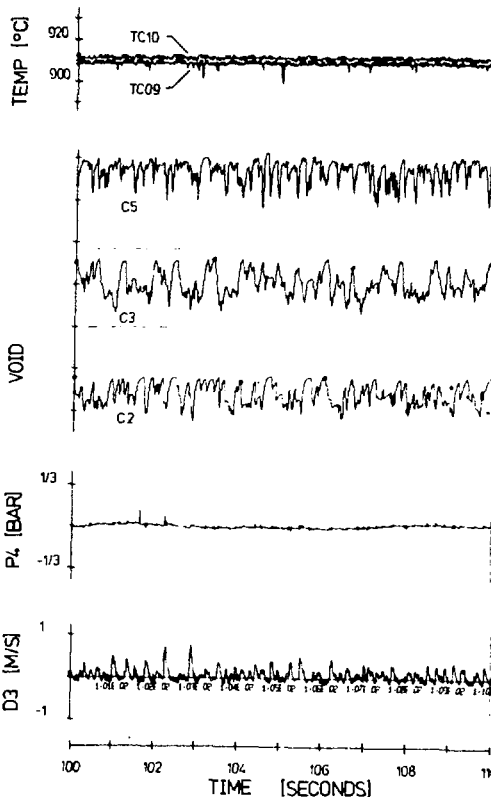


Fig. 5: Stable boiling in Test 7-1/42

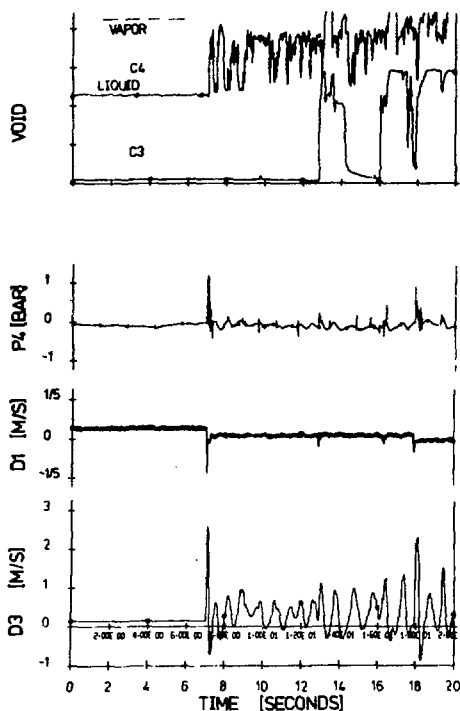


Fig. 6: Incipient boiling with superheat and reduced inlet throttling, Test 7-2/45

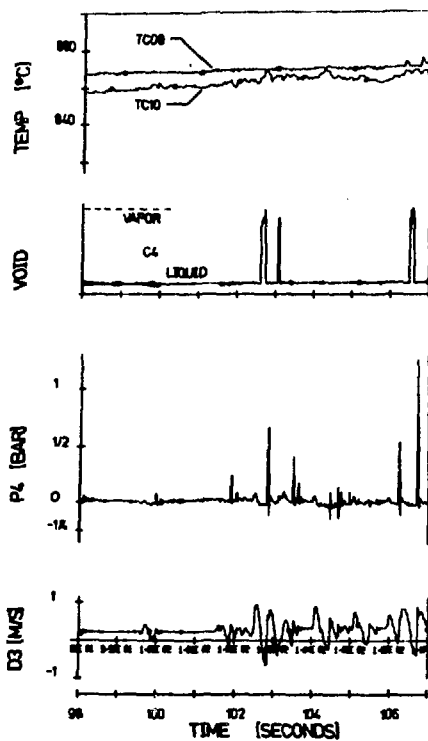


Fig. 7: Onset of boiling with minimal superheat, Test 7-2/80

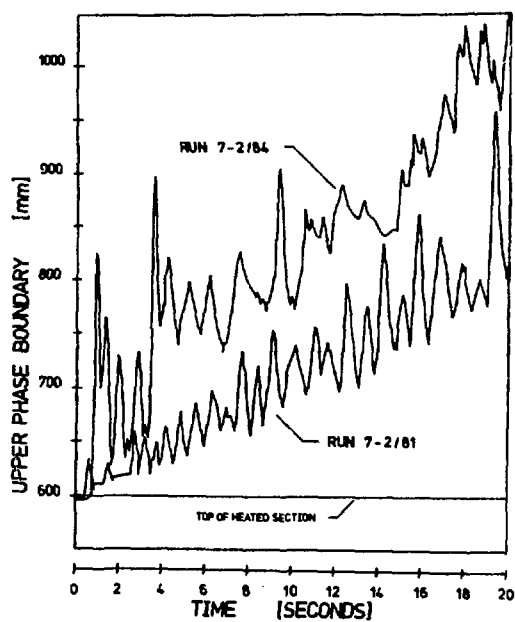


Fig. 8: Motion of upper liquid vapor interface for 50°C (upper curve) and 10°C (lower curve) superheat

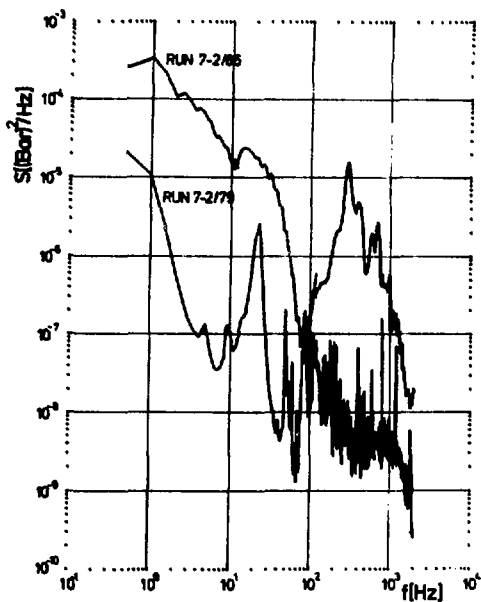


Fig. 9: Autopower spectral density of pressure sensor P4 with boiling and background (lower curve)

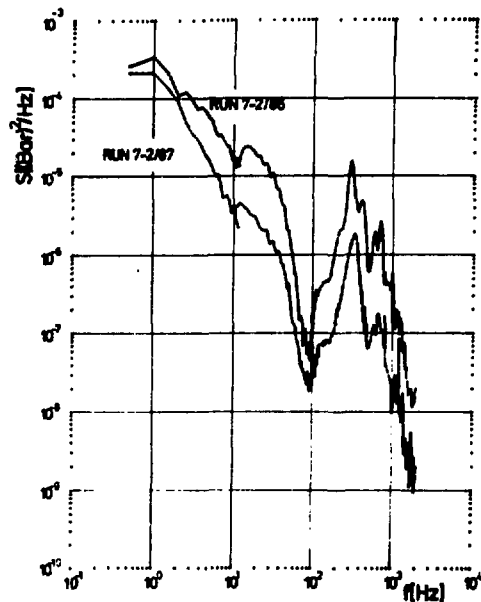


Fig. 10: Effect of boiling regime on the spectral density of sensor P4

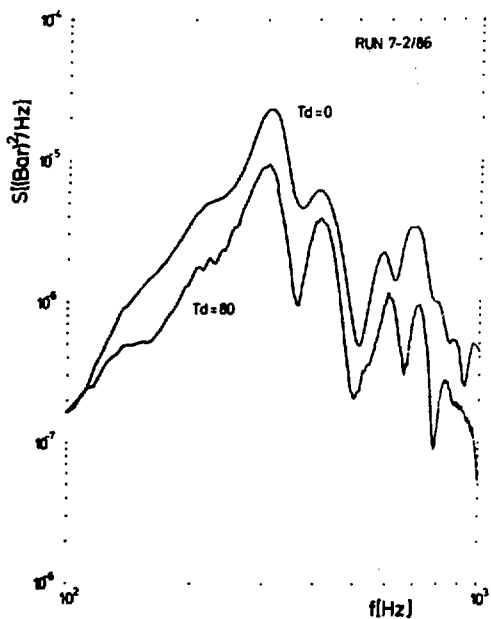


Fig. 11a: Time dependence of the spectral density of sensor P4

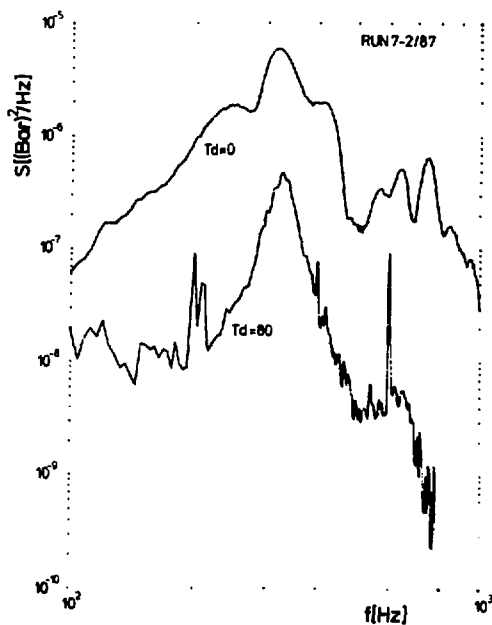


Fig. 11b: Time dependence of the spectral density of sensor P4

TRANSIENT SODIUM BOILING CHARACTERISTICS AT PUMP RUN DOWN

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ABSTRACT

The transient pattern of boiling in simple channel geometry for loss of coolant conditions were investigated at various heat flux levels and mass flow rates. Heat flux densities of up to 900 W/cm were chosen for the individual experiments, and were kept constant during the whole run down until dry-out was reached. The rate of change of flow during the run-down was determined by the pump control circuit. The boiling transients were analysed with the aim to answer the following questions:

- a) what is the history of the flow and temperature pattern following boiling inception;
- b) what is the influence of the inception superheat on the boiling transient.

INTRODUCTION

A temperature excursion arising through an eventual blockage in the cooling channels or a pump failure in a liquid metal cooled reactor could lead to boiling and subsequent melting of the whole core or parts of the core. Since blockages occur likely as local phenomena they are characterized by a local flow reduction and slow growing of the boiling area. A pump failure or a flow run-down affects the coolant hydrodynamics of the entire core. The transient between the beginning of the run-down and the onset of boiling depends on the characteristics of the pump and the reactor loop, whereas the boiling transient is essentially governed by the characteristics of the core itself. Power level, temperature and rate of decrease in mass flow determine the whole transient up to dry-out.

This paper reports on some run-down experiments over the whole transient period. Even if the experiments do not represent real reactor conditions they can be used for code development and code validation. Only experimental results are reported here. Their comparison with the Karlsruhe BLOW3-code accommodated to the experimental conditions is underway.

EXPERIMENTAL CONDITIONS

The experiments were performed in a test facility with known hydraulic characteristics. The rate of change in mass flow during run-down was determined by the response of the pump control circuit. The directly electrically heated test section was a hayness tube of 6 mm ID and 30 mm OD of 1 meter length, bypassed by a 48 mm ID tube. A quasi adiabatic unheated tube section of 1 meter length was installed between the outlet of the heated section and the confluence with the bypass. Voltage taps were located along the entire test section to measure the flow pattern and the propagation of the boiling front. Instrumentation for the measurement of temperature, pressure and mass flow was provided. The flow scheme of the loop, complete with test section, is shown in Fig. 1.

The run-down experiments were carried out as follows: from a steady state condition with a given heater power per unit length $N(\text{W/cm})$, initial flow rate $Q_i (\text{ltr/sec})$, and a given inlet tempera-

EXPERIMENTAL TECHNIQUES

Temperature profiles have been plotted for several moments before and after boiling inception. They are shown in Figs. 4, 5, 6 and 8:

2) The inner wall temperatures, $T_{\text{wall-in}}$ were measured by 12 thermocouples along the heated test section, located 1 mm from the inner wall. The outer wall temperatures, marked as $T_{\text{wall-out}}$, were measured by means of 5 thermocouples at the outer wall of the heated section, and by 6 thermocouples on the outer wall of the unheated quasi adiabatic section.

The location of the boiling inception and the expansion of the vapour phase was determined by means of the voltage taps along the test section. The general map of instrumentation is shown in Fig. 2.

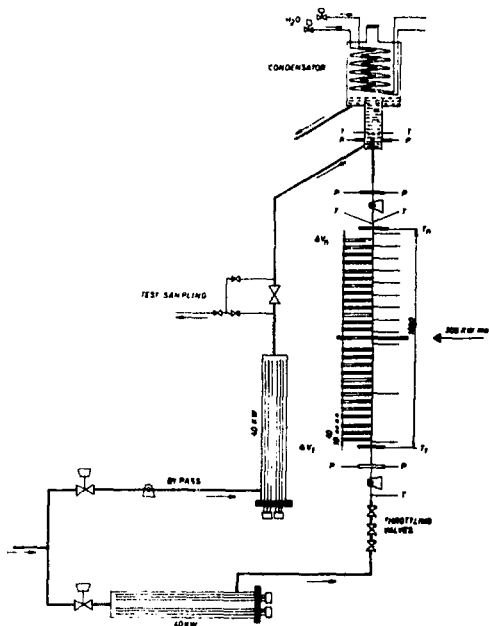


Fig. 2 : Test section (6mm ID)

EXPERIMENTAL RESULTS

Flow run-down experiments were carried out at 900, 850, 800, 700, 600, 500, 400, 350 and 300 W/cm applied to the test section.

Two examples of these tests will be discussed here.

1. Experiment No A34

In this experiment the run-down was started under the following initial conditions:

Heating power: 40 kW, equivalent to 400 W/cm

Volumetric flow of the coolant: 0.181 ltr/sec

Inlet temperature: 600°C, outlet temperature: 800°C.

The time scale origin $t = 0$ in the diagram represents the point at which signal recording started. The beginning of the run-down was 6.5 sec earlier, hence at $t = -6.5$. The decrease in volume flow, called ramp slope, was 0.01 ltr/sec². The development in the boiling transient is shown in Fig. 3. At $t = 0$ the inlet flow has dropped to 0.115 ltr/sec, the inlet temperature is still 600°C but the outlet temperature increased to 865°C.

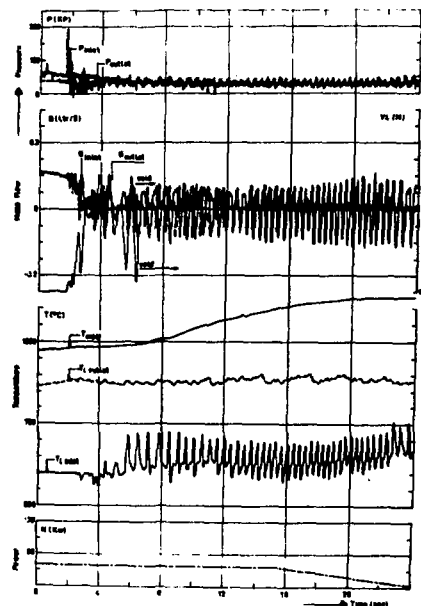


Fig. 3 : Pump run-down—thermohydraulic pattern

At $t = 1.1$ sec the bulk superheat is 10°C and the first indication of local boiling appears as pressure pulses at the outlet of the heated test section. The first noticeable change in outlet flow occurs at $t = 1.7$ sec, and the first voiding is signaled by the voltage tap at $x = 95$ cm at $t = 2$ sec, indicating the presence of small bubbles. Shortly afterwards a large vapour slug penetrates down into the test section in an oscillating manner and reaches the points $x = 75$ cm, $x = 60$ cm and $x = 25$ cm at $t = 3.0$, $t = 3.7$ and $t = 5.6$ sec respectively. Vapour expansion into the quasi adiabatic upper part of the test section is continued by voltage taps at $x = 145$ cm and $x = 148$ cm at $t = 3.9$ and $t = 4.8$ sec.

After $t = 6$ sec the vapour phase pulsates regularly and penetrates far down into the test section. The outlet flow meter downstream of the heated section shows intervals in which slug flow is likely to exist.

From $t = 16$ sec to $t = 25$ sec the heating power was linearly reduced to zero to terminate the experiment. All vapour produced in the heated part of the test section condensed in the unheated extension. Thus, no vapour escaped from the test section area during this run-down experiment.

The temperature profiles in the wall and in the coolant for different moments during the run-down are shown in Figs. 4 and 5.

After $t = 5$ sec, when boiling is well established, the inner wall temperature in the upper part of the heated section rises rapidly.

At $t = 12.5$ sec the temperatures at the inner and outer wall become equal, and at $t = 15$ sec the temperature on the inner wall exceeds that of the outer wall. Thereafter the temperature profiles are influenced by the decrease in heating power.

2. Experiment No A37

In this experiment the run-down was started under the following initial conditions:

Heating power: 60 kW, equivalent to 600 W/cm

Volumetric flow of the coolant: 0.23 ltr/sec

Inlet temperature: 590°C , outlet temperature: 785°C .

The signal recording started at $t = 0$, whereas the run-down began at $t = -5.5$ sec, with a ramp slope of 0.01 ltr/sec². The run-down history of this experiment is shown in Fig.6.

At $t = 0$ the coolant flow has dropped to 0.175 ltr/sec.

The first indication of local boiling is a slight pressure pulsation at the outlet of the heated section at $t = 2.4$ sec.

At $t = 2.9$ sec the growth and collapse of small vapour bubbles near the outlet of the heated section is confirmed by slight oscillations in the outlet flow record. At $t = 3.2$ sec an expansion of the vapour phase can be concluded from a momentary reduction in inlet flow and increase in outlet flow, and voltage taps readings. Following this flow reduction and vapour burst the vapour condenses and the test section gets flooded,

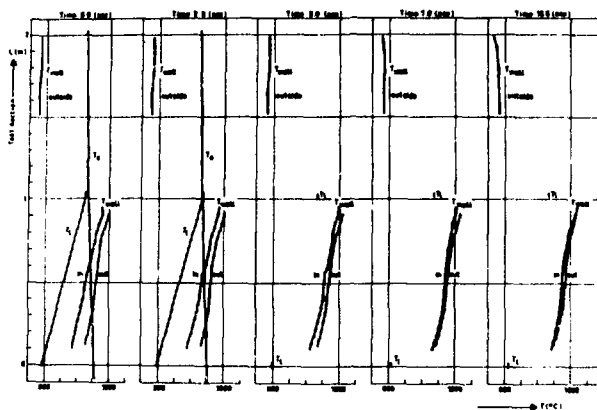


Fig. 4 : Pump run-down—temperature pattern

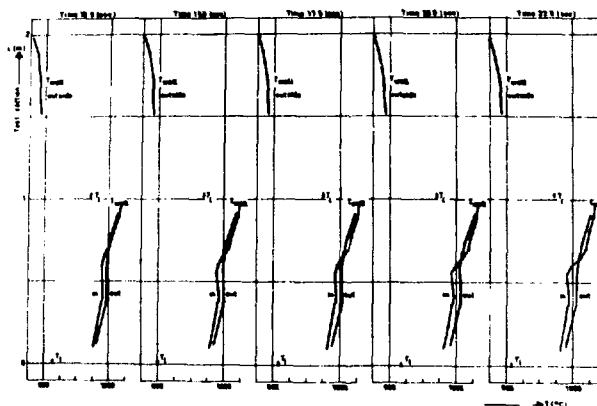


Fig. 5 : Pump run-down—temperature pattern

which leads to a superheat of the coolant of about 40°C . After the subsequent flashing boiling the vapour phase expands in an oscillatory manner, reaching $x = 95\text{ cm}$ at $t = 3.7\text{ sec}$ and $x = 135\text{ cm}$ at $t = 3.8\text{ sec}$. After $t = 5.5\text{ sec}$ vapour reaches the condenser at $x = 201\text{ cm}$ and after $t = 6.0\text{ sec}$ the outlet flow meter indicates only vapour flow.

The temperature profiles at various moments during the run-down are shown in Figs. 7 and 8. The temperature profiles show the history of the wall temperature and especially the process of dry-out in the upper part of the heated section, which becomes apparent through the rapid temperature increase at the inner wall. At $t = 16\text{ sec}$ the inner wall temperature exceeds 1200°C , which turned off the heating power.

Fig. 6 : Pump in run-down—thermohydraulic pattern

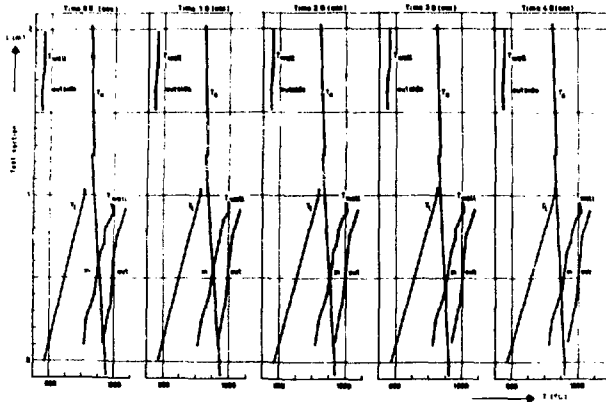
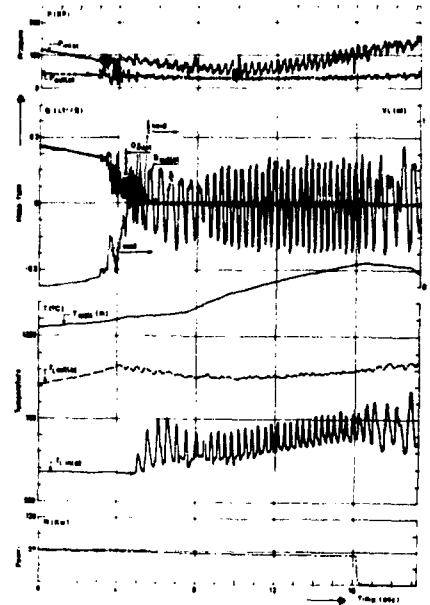


Fig. 7 : Pump run-down—temperature pattern

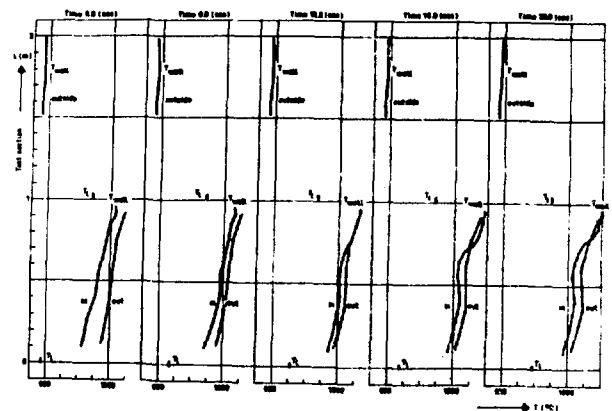


Fig. 8 : Pump run-down—temperature pattern

DISCUSSION OF THE RESULTS

The two examples described here show the boiling transients during flow run-down at different heat fluxes. The characteristic feature of the first example is a mild boiling inception at low bulk superheat, and the continuous growth of the boiling area. Boiling propagation is accompanied by strong flow oscillations. The small residual convective flow feeds liquid into the heated section, which causes the passage of liquid plugs through the test section, synchronized with the flow oscillations. When time goes on, the wall temperature increases and the liquid plugs exhaust before they reach the end of the heated section. The cooling capacity of the subsequent vapour flow is too small to prevent a rapid rise in temperature. Under such conditions dry-out is primarily a question of heat flux rather than of hydraulic conditions.

In the second example the boiling pattern is not very different from that of the first experiment with the exception that during the fast flow transient caused by the rapid growth of the vapour phase coolant is flooding into the heated section from both ends. In this case before a new boiling inception took place, a superheat of 40°C was involved.

After the first rapid evaporation the vapour phase continues to grow in an oscillating manner. In the initial stage liquid plugs pass through the test section.

The question arises whether such experiments can be used for the analysis of flow run-down accidents in a reactor.

When comparing the heat capacity and thermal diffusion of the test section with those of the portion of a fuel element heating one subchannel, the following values should be used:

Heat capacity per unit length of test section: $\approx 25.8 \text{ Jcm}^{-1} \text{ }^{\circ}\text{C}^{-1}$

Heat capacity per unit length of equivalent fuel element: $\approx 0.46 \text{ Jcm}^{-1} \text{ }^{\circ}\text{C}^{-1}$

Thermal diffusivity of the test section: $\approx 0.3 \text{ Jcm}^{-2} \text{ }^{\circ}\text{C}^{-1} \text{ sec}^{-1/2}$

Thermal diffusivity of the fuel element: $\approx 0.09 \text{ Jcm}^{-2} \text{ }^{\circ}\text{C}^{-1} \text{ sec}^{-1/2}$

The ratio of heat capacities of the test section and equivalent fuel pin: ≈ 60

The ratio of thermal diffusivities of the test section and fuel: ≈ 10

The difference in materials between test section and fuel leads to a mismatch of boundary conditions which have a significant influence on the whole run-down process.

The higher heat capacity of the test section tends to slow down the temperature increase and to temper the boiling process. On the other hand the higher thermal diffusivity leads to higher transient heat fluxes (assuming the same temperature difference), which compensates partly the damping effect of the higher heat capacity.

Taking these facts into account, the experiments can be used for the validation of "run-down" codes. Work is underway to accommodate the Karlsruhe BLOW3 code to our experimental conditions.

CONCLUSIONS

Flow run-down experiments starting from steady state single phase flow with constant flow ramp slope and various heater powers show that a characteristic feature of the boiling transients is a pulsating growth of the vapour space. At the initial stage of boiling liquid plugs pass through the vapour zone wetting the wall.

If superheat is involved, only the initial stage of the transient is influenced. Its influence on the further boiling transient seems negligible.

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STEADY STATE LIQUID METAL BOILING PRESSURE DROP CHARACTERISTICS

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ABSTRACT

Steady state Na-boiling experiments were performed in order to measure the boiling characteristics (pressure drop). These experiments were executed at various heat fluxes and mass flow rates up to dry-out.

For the analysis of the experiments a steady state boiling computation code (ESSO) was developed. In a first approach a parametric study has been made using the various two-phase flow models and friction multipliers proposed by Lockhart-Martinelli, Chen, Kalish and Peppier. A validation of the code with the experimental results was made.

This work is performed in collaboration with the CEA-Laboratory of Grenoble.

INTRODUCTION

Sodium boiling experiments in bundle geometries prove that boiling expands gradually downstream of boiling inception even if superheat is involved. It seems likely that in a bundle configuration a two-phase flow builds up surrounded by a single phase region. This pattern can remain stable for a long time and implies a flow redistribution across the bundle, which in turn depends on the pressure drop characteristics in the two-phase flow regime.

Experiments to investigate the boiling pressure drop ΔP as a function of mass flow and heat flux were performed by Costa and Sheriff [1] for annular geometry. Furthermore, measurements of the friction factor and of the critical heat flux in various simple test section geometries were published by Kaiser et al. [2] and Chen [3]. The studies reported here are a continuation of the investigations started by Costa and Sheriff.

An examination of the actually known theoretical and experimental work indicates a lack in detailed knowledge about the steady state two-phase flow of liquid metals. Discrepancies exist between theoretical predictions and known experimental results. It is for example uncertain whether the friction factor correlations for two-phase flow in boiling water can also be applied for sodium.

Despite the numerous experiments carried out all over the world, the uncertainty remains, partly due to the difficulties involved in visualizing the boiling flow pattern of sodium. The main purpose of these studies was:

- a) To provide experimental data on boiling flow pattern in a well defined simple geometry under stable hydraulic conditions and with special emphasis on the pressure drop characteristics, heat transfer and dry-out;

- b) To develop a physical model and a computation code for the prediction of the terms quoted above.

EXPERIMENTAL EQUIPMENT

The experiments were carried out in a test loop with the following characteristics:

- Maximum flow capacity : $100 \text{ m}^3/\text{h}$
- Maximum pump pressure : 10 Bar
- Maximum operating temperature of the loop : 600°C
- Maximum temperature of the test section : 1100°C

The test section is shown schematically in Fig. 1. Three throttling valves were installed upstream of the test section and an unheated length of 950 mm is located between the heated part and the confluence with the bypass.

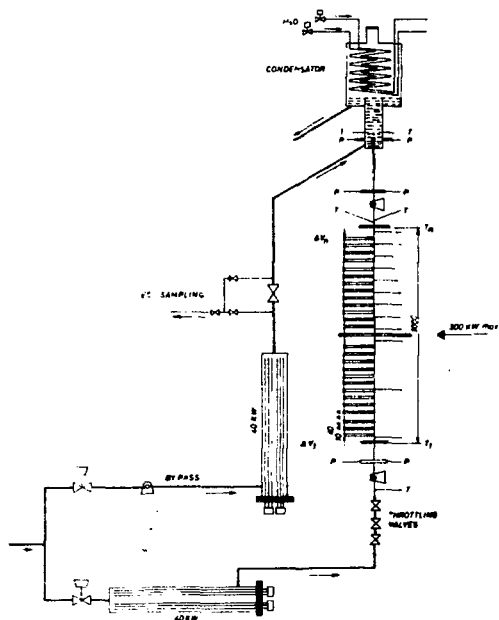


Fig. 1 : Test section (6 mm ID)

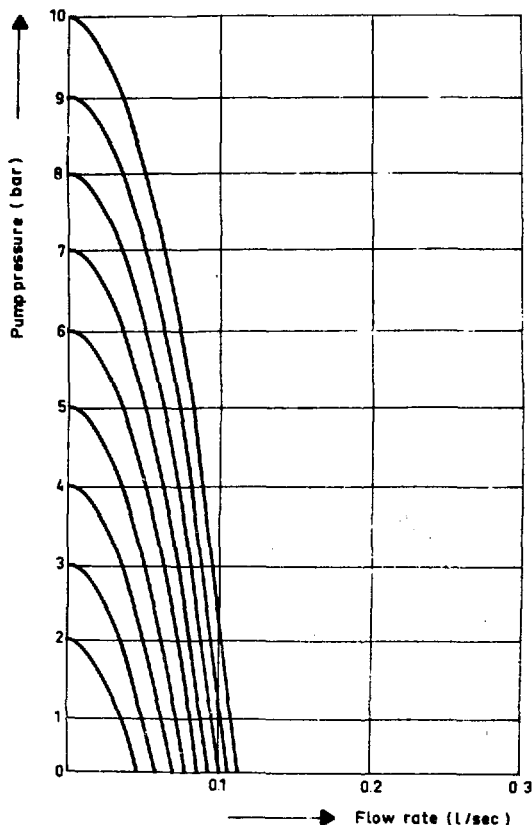


Fig. 2 : Loop characteristics

Pressure gauges were installed upstream and downstream of the heated test section and in the condenser. The flow pattern was measured by means of voltage taps along the entire test section and by an electromagnetic flowmeter.

EXPERIMENTAL CONDITIONS

Steady boiling under stable hydraulic conditions required a rather high pump pressure and heavy throttling upstream of the test section. The internal characteristics of the loop were measured for several settings of the throttling valves. The settings chosen for the experiments correspond to the characteristic curve of Fig. 2, which is as steep as possible over the desired mass flow range in order to satisfy the stability criteria as good as possible.

A large number of measurements was made for different heat fluxes and mass flow rates. For each measurement several readings were taken during a period of 15 to 20 minutes with steady boiling. Each reading of flow, pressure and temperature was made through a low-pass filter with a time constant of 30 seconds to average out rapid fluctuations.

The measurements were divided into different series. Each series was executed in either of two ways. With constant mass flow and increasing the heat flux stepwise between measurements until dry-out was reached, or with constant heat flux and decreasing the mass flow stepwise between measurements. The invariant experimental parameters were the cover gas pressure of 0.8 Bars, the inlet temperature of $585^{\circ}\text{C} \pm 15^{\circ}\text{C}$, and the bypass subcooling $T_{\text{sat}} - T_{\text{byp}} \approx 285^{\circ}\text{C} \pm 15^{\circ}\text{C}$. The heat flux along the test section remained uniform in all cases.

EXPERIMENTAL RESULTS

Flow pattern

For an analysis of the two-phase flow regime in boiling sodium a thorough study of the boiling pattern is indispensable. The simple geometry of the test section and its instrumentation with potential taps for void detection and with electromagnetic flowmeters permitted the measurement of the flow pattern both as a function of space and time. The interpretation of the measurements show clearly that in a simple geometry the steady state boiling regime is characterized by single vapour bubbles seizing nearly the entire flow cross section. The vapour bubbles are separated by liquid plugs with thicknesses down to that of a membrane. Once the two-phase flow has reached a steady state, its pattern is very regular. The liquid plugs passing through the channel supply liquid to the film between the wall and the bubbles. Due to evaporation of the film the bubbles grow and the liquid plugs diminish when they proceed along the test section. When the vapour slugs grow very long, the flow pattern becomes similar to annular flow.

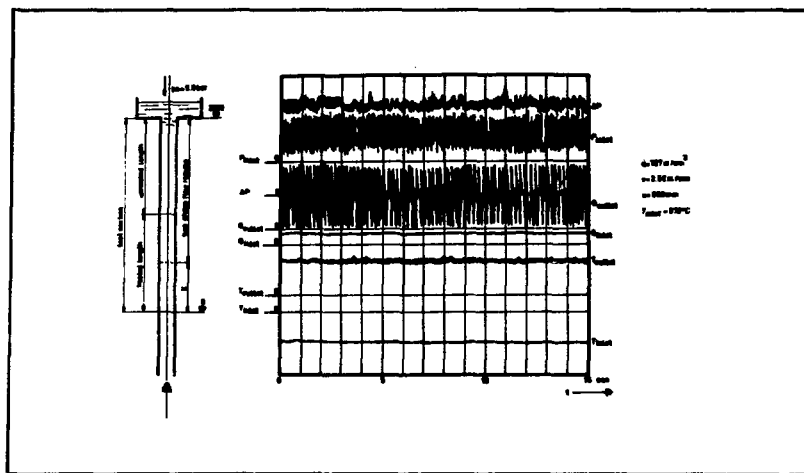


Fig. 3 : Steady state boiling registration

Fig. 3 shows a 15 second recording of sodium steady state boiling with a vapour quality of $X = 1.9\%$ and a void fraction of $\alpha = 79\%$. The recorded inlet pressure (P_i) and pressure drop (ΔP) show a constant pressure noise synchronous with the flow oscillations downstream the heated test section (Q_{out}). The inlet temperature (T_i) and inlet flow (Q_i) are constant and undisturbed, whereas the coolant temperature at the outlet is subject to permanent perturbation. (The voltage tap measurements confirming the slug flow have not been plotted here.)

Fig. 4 shows steady state boiling at the same heat flux as above, but at a reduced mass flow, which changed the vapour quality to $X = 5.3\%$ and the void fraction to $\alpha = 88\%$. Compared to Fig. 3 both the pressure perturbation and the frequency of the liquid plugs flowing through the outlet flow-meter increased.

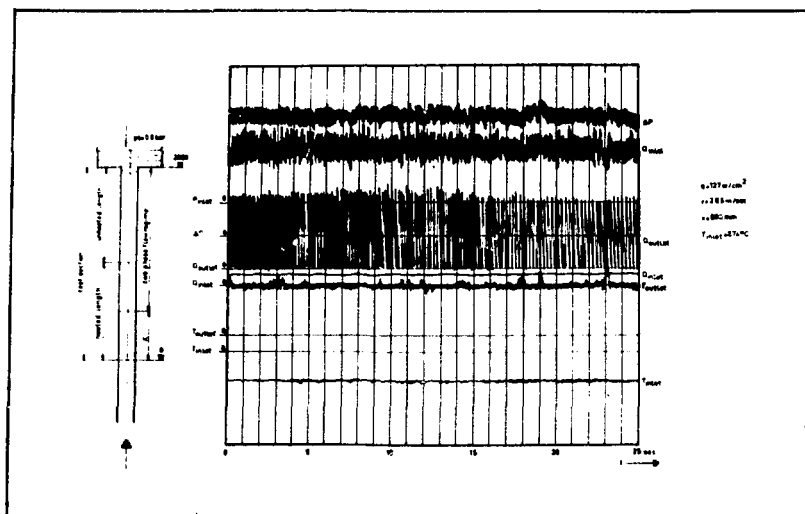


Fig. 4 : Steady state boiling registration

Fig. 5 shows again a boiling pattern at the same heat flux but at such a low flowrate that dry-out conditions were approached near the outlet of the heated test section. In this case $X = 15\%$ and $\alpha = 94\%$. The pressure drop rose further but the pressure perturbation diminished. In addition, Fig. 5 shows the transient from vapour flow at the outlet to liquid flow after power shut-off.

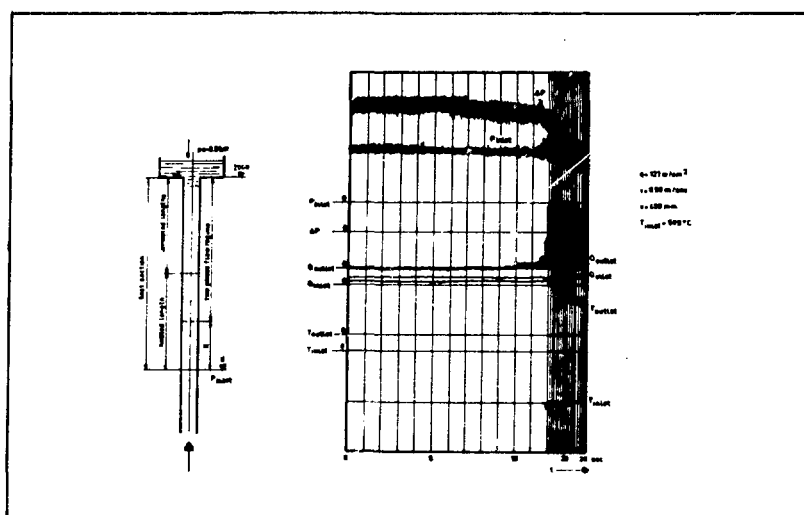


Fig. 5 : Steady state boiling registration

Figs. 3, 4 and 5 represent boiling flow pattern at the points X_1 , X_2 and X_3 of the characteristic curve for pressure drop as a function of coolant flow and heat flux, which is shown in Fig. 6. Following this curve from B to C by progressively reducing the flow, the two-phase region penetrates further down into the heated test section.

Boiling pressure drop characteristics

The results of the pressure drop measurements have been plotted in Figs. 6, 7 and 8 along with the ESSO-code predictions. The onset of dry-out limited the range of flow rates and prevented experiments at low mass flows in the region where the pressure drop maxima are expected. The part from A to B is the single-phase portion of the characteristic curve and the section B to C shows the steep rise in pressure drop associated with the two-phase flow regime. If it would have been possible to extend the measurements to much lower flow rates, a pronounced pressure maximum would have been noticed. At still lower mass flows the pressure drop decreases and approaches the single-phase vapour characteristic curve asymptotically.

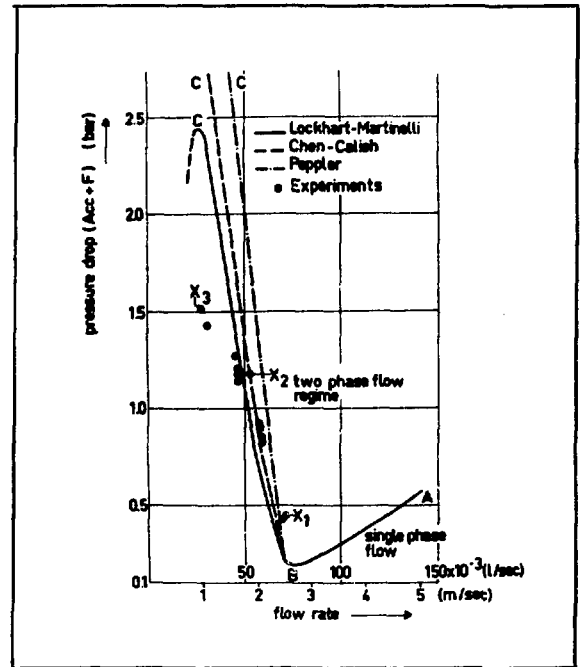


Fig. 6 : Pressure drop characteristic Na-steady state boiling ($q = 127 \text{ W/cm}^2$)

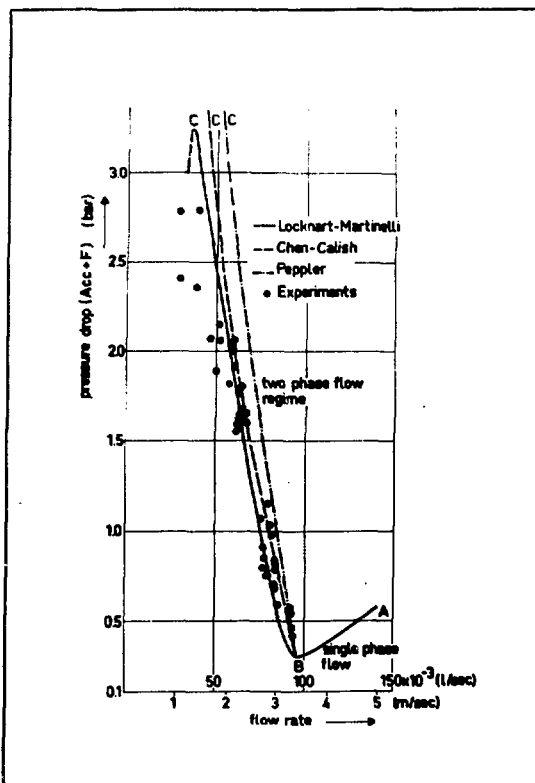


Fig. 7 : Pressure drop characteristic Na-steady state boiling ($q = 159 \text{ W/cm}^2$)

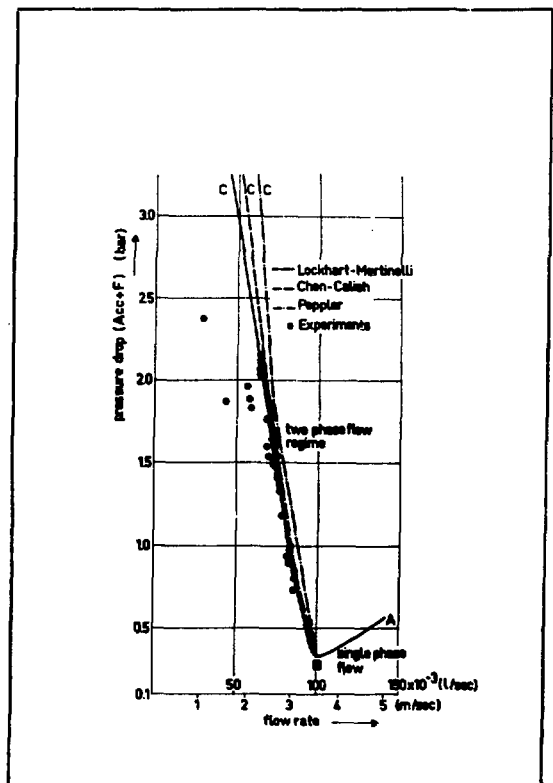


Fig. 8 : Pressure drop characteristic Na-steady state boiling ($q = 175 \text{ W/cm}^2$)

THEORETICAL APPROACH

The physical model of the two-phase flow regime chosen for the development of the ESSO-computation code is the *slug flow model* which, at least for boiling in simple geometries, is confirmed by experimental evidence. The individual assumptions are:

1. The vapour flows in the center of the channel and is separated from the wall by a liquid film;
2. The two-phase flow regime begins when the liquid reaches saturation temperature;
3. Thermodynamic equilibrium exists between vapour and liquid;
4. The liquid film on the wall remains undestroyed up to a void fraction of $\sim 95\%$;
5. All heat is supplied from the wall.

The equations of continuity, momentum and conservation of energy have been applied to the flowing medium. In the computation code these equations are solved for both single-phase and two-phase flow by using the technique of finite differences.

The pressure drop due to friction in the two-phase flow was calculated for each finite difference interval by adopting the slip model of Lockhart-Martinelli.

Eq. of continuity:

$$\frac{\partial}{\partial z} [\alpha \rho_g v_g + (1 - \alpha) \rho_l v_l] = 0 \quad (1)$$

Eq. of momentum:

$$\frac{\partial p}{\partial z} = - \frac{\partial}{\partial z} [\alpha \rho_g v_g^2 + (1 - \alpha) \rho_l v_l^2] - g[\alpha \rho_g + (1 - \alpha) \rho_l] - \frac{\partial \tau}{\partial z} \quad (2)$$

Eq. of conservation of energy:

$$\dot{q}(z) = \frac{\partial}{\partial z} \left\{ \dot{m}[(1 - x)H_1 + xL] \right\} \quad (3)$$

with: α = void fraction, ρ_g = vapour density [kg/m^3], ρ_l = density of the liquid [kg/m^3], V_g = vapour velocity [m/sec], V_l = liquid velocity [m/sec], $\dot{q}_{(z)}$ = power input [W/cm], $\dot{m}$ = mass flow [kg/sec], H_1 = enthalpy of the coolant [J/kg], L = latent heat of vaporization [J/kg], x = vapour quality, $\frac{\partial p}{\partial z}$ = total pressure gradient [bar/cm], $\frac{\partial \tau}{\partial z}$ = pressure loss due to friction [bar/cm].

For the calculation of the void fraction " α " the Lockhart-Martinelli correlation was used.

$$\alpha = 1 - \frac{1}{\sqrt{1 + \frac{21}{X_{tt}} + \frac{1}{X_{tt}^2}}} \quad \text{with} \quad (4)$$

$$X_{tt} = \left(\frac{1-x}{x} \right)^{0.9} \cdot \left(\frac{\rho_g}{\rho_l} \right)^{0.5} \cdot \left(\frac{\mu_l}{\mu_g} \right)^{0.1} \quad (5)$$

According to the "slip model" of Lockhart-Martinelli, the pressure drop of the two-phase flow regime along the interval Δz is given by:

$$\Delta P_{2f} = \Phi^2 \Delta P_{1f} \quad (6)$$

P_{1f} = pressure drop of the liquid phase assuming only liquid flow through the total cross section of the

channel, Φ_1 represents the frictional multiplier.

In order to assess the validity of the ESSO code for the prediction of the pressure drop characteristics, the frictional multipliers proposed by Lockhart-Martinelli, Chen-Kalish and Peppler were used alternatively.

$$\Phi_1 = 8.2 \cdot x_{tt}^{-0.55} \quad \text{Peppler [5]} \quad (7)$$

$$\Phi_1 = e^{-A} \quad \text{Chen-Kalish [6]} \quad (8)$$

$$\text{with } A = 1.59 + 0.518 \ln x_{tt} - 0.0867 (\ln x_{tt})^2$$

$$\log \Phi_1 = \frac{-z + \sqrt{z^2 + 9.2}}{4} \quad \text{Lockhart-Martinelli [7]} \quad (9)$$

$$\text{with } z = 2 \log x_{tt} + 0.176 x_{tt} + 0.382$$

A comparison of experimental data with the calculations is shown in Figs. 6, 7 and 8. For low vapour quality no preference can be given to one of the proposed frictional multipliers. For higher vapour quality the best agreement is obtained by using Lockhart-Martinelli's frictional multiplier.

A similar comparison with calculations using the FLINA-code from Grenoble is underway.

CONCLUSIONS

Steady state measurements of two-phase pressure drop versus mass flow in Na have been made over a range of conditions in a directly electrically heated tubular test section.

The measurements were compared with calculations using three different frictional multipliers in the ESSO code (Martinelli-Lockhart, Chen-Kalish, Peppler). It was found that for the two-phase flow regime in a heated tubular geometry at low vapour qualities the proposed correlations for the frictional multiplier fit with the experimental results. For higher vapour qualities Lockhart-Martinelli's frictional multiplier fits best.

A comparison of the results with those from the CEN Grenoble experiment with an annular test section shows a fair agreement [2]. A qualitative agreement is found with the Winfrith [4] simulation experiments with water, which demonstrates the usefulness of simulation experiments for code development.

Steady state boiling experiments in annular and bundle geometries are in preparation especially in order to measure flow pattern and pressure drops which will give lead to a realistic model for code development.

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INFLUENCE OF THE AXIAL DISTRIBUTION OF HEAT FLUX ON SODIUM BOILING IN FORCED CONVECTION

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ABSTRACT

The axial distribution of heat flux and single phase transient influence on incipient boiling superheat and sodium boiling dynamics have been studied by two tests series in forced convection. These experiments were carried out in the same conditions of velocity, pressure, inlet temperature and mean heat flux on two wire wrapped single pin test-sections, geometrically similar, having respectively flat and sinusoidal ($\phi_{\max}/\phi_{\min} = 1.7$) axial distribution of heat flux.

Moreover the influence of the bundle geometry on sodium boiling is carrying out on the basis of the preliminary experiments on sodium boiling in seven pins bundle.

INTRODUCTION

A first valuation of the influence of the axial distribution of heat flux on sodium boiling has been made on the basis of two experimental tests series performed on the ENA 2 loop of the CSN Casaccia with wire wrapped single pin test-sections with different axial distribution of the heat flux (flat and sinusoidal).

On the loop presently is at work a seven pins test-section, a brief description of which will be given now.

The work presented here is the result of the picking up again of an activity started in the CNEN [1] many years ago; within the general outline of the CNEN-CEA agreement concerning the fast reactors, this activity belongs to the "safety" activity line, on the domain of which has been initiated a very active collaboration

between Italian and French research people.

TEST SECTION

As schematically drawn in fig. 1, the test-section consists of an annular passage (I.D. tube 10.6 mm, O.D. heater 6.8 mm, heating length 300 mm) with wire spacers (diameter 1.25 mm, pitch 150 mm). Two types of high flux heaters, manufactured in the CNEN 2, have been utilized. In the first one the heating element (graphite) is unaltered giving an axially flat distribution of the heat flux; in the other one a set of grooves of variable depth allows a sinusoidal heat flux distribution with ratio $\phi_{\max}/\phi_{\min} = 1.7$.

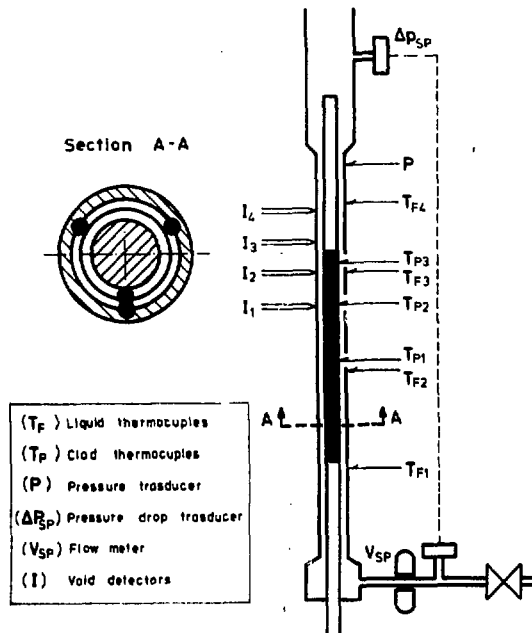


Fig. 1 - Test Section

POWER TRANSIENTS

The two sets of experiments have been realized in the same experimental conditions of pressure (1.7 - 1.75 bars), velocity (0.8 - 1.2 m/sec) and mean heat flux (110 - 210 W/cm²) at the inception of boiling.

The measures of the pressure drop versus velocity at different temperatures (fig. 2) seems in good agreement with the usual theoretical relation using Blasius friction factor. The unity value of the corrective term Z for wires effect seems to be confirmed by the experimental results available in literature 3. The heat balance curve, calculated by hypothesizing a sinusoidal distribution of the heat flux, seems to agree with the experi-

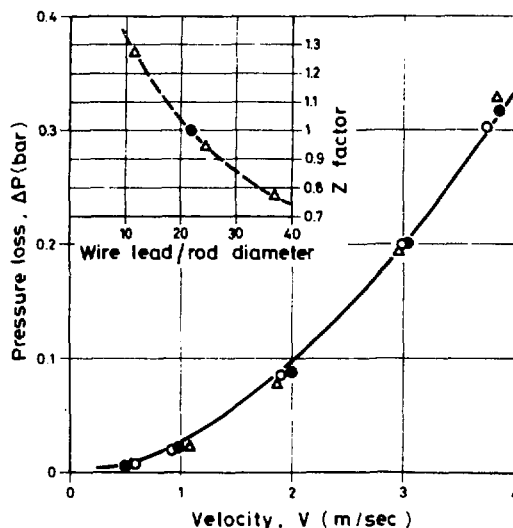


Fig. 2 - Pressure drop versus velocity

mental values of the temperatures in the stationary conditions (fig. 3).

The measured superheat values, ranging from 0°C to 30°C, seem to be in congruence with the other experimental results available in literature [5], [6], [7] (fig. 4) if interpreted in the light of the theoretical correlation [4]. On the basis of the present experiments it seems that any influence of the axial distribution of the heat flux on the I.B. superheat has to be excluded.

Boiling generally starts in the upper blanket zone, with the exception of some flat heat flux experiments in which it starts at the upper end of the heating section because of the greater value of the wall-liquid temperature jump.

On the basis of the turbulence theory [4], the experimental velocity and pressure conditions let foresee small I.B. overpres-

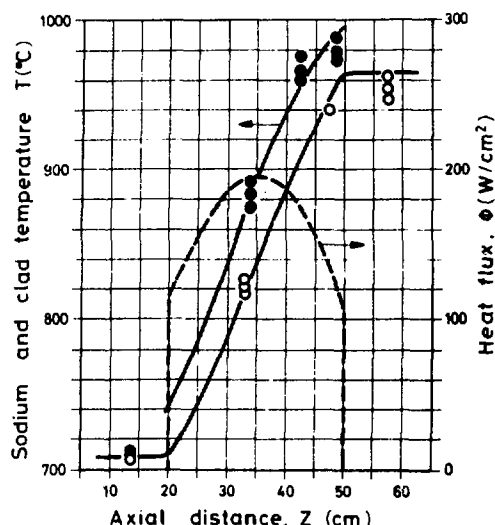


Fig. 3 - Temperature axial distribution in sinusoidal heat flux experiments

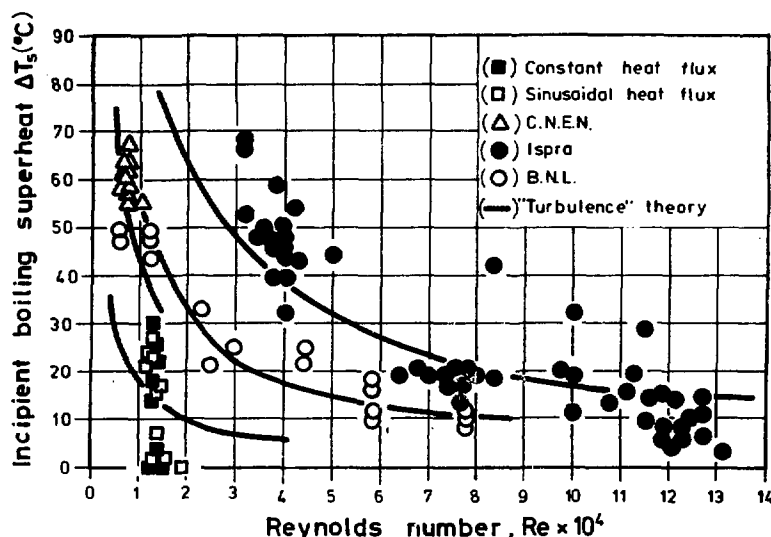


Fig. 4 - I.B. superheats in forced convection

sure and superheat values and hence just a brief initial phase of the superheat release without flow reversal (fig. 5). With regard to such phase no relevant difference has been observed between

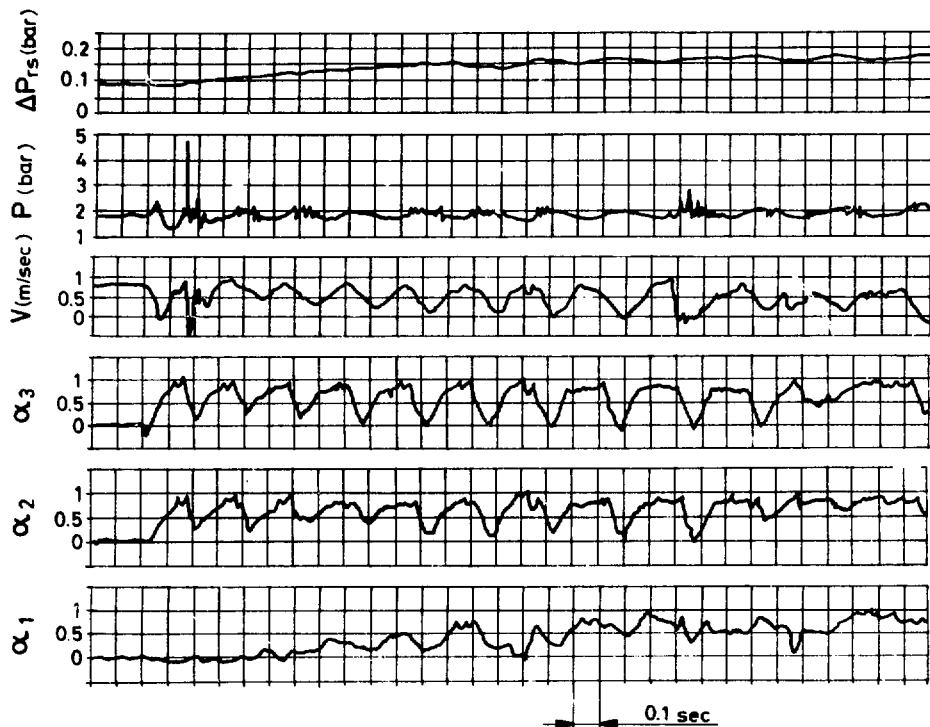


Fig. 5 - Sinusoidal heat flux test 7/5: dynamic behaviour during power transient

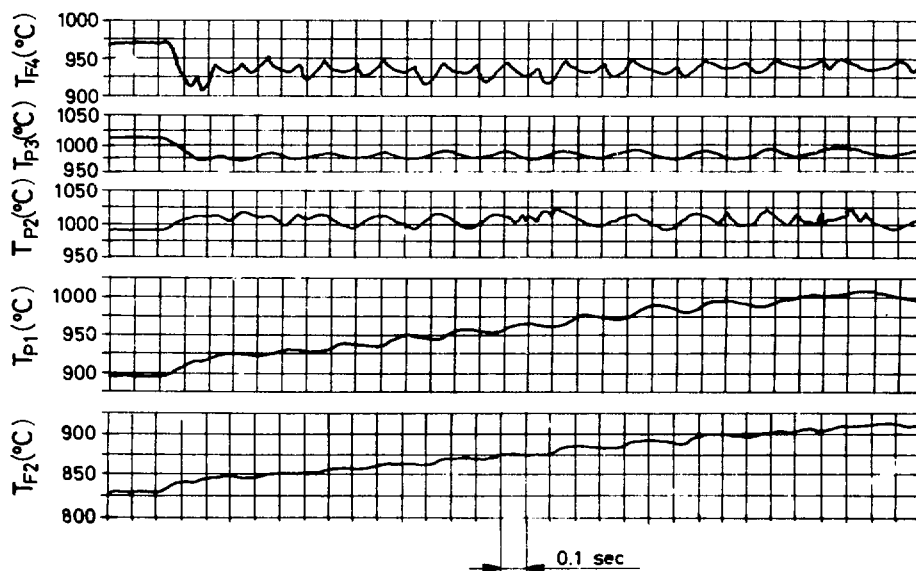


Fig. 6 - Sinusoidal heat flux test 7/5: thermal behaviour during power transient

flat and sinusoidal heat flux experiments. The effect of the bigger mass of overheated liquid in the sinusoidal heat flux experiments is evidently neglectable and partly counter-balanced by the lower overheating of the wall at the upper end of the heating section. During the following phase of stationary boiling, the periodical nullifying of the velocity tends, in the sinusoidal heat flux experiments, to a fast increasing of the liquid and clad temperature in the central zone of the heating section and provokes the downward propagation of the two phase separation front (fig. 6). In the light of this fact, which is not to be neglected because of the influence of the voids on neutronic feedbacks, one can say that the flat heat flux experiments are not conservative.

FLOW TRANSIENTS

Fig. 7 compares the courses of the overheating of liquid versus velocity in two flow transients realized by means of a quasi-stationary reduction (test 12/5) and a step-wise enclosure (test 11/6) of the test-section valve in the same conditions of pressure (1.7 bars) and mean heat flux (150 W/cm^2). Because of the thermal inertias there is an evident shoving off between the experimental results 11/6 and the stationary thermal balance curve. In accordance to the turbulence theory [4] and the experimental map of the two phase regimes [8], in test 12/5 boiling starts at a low I.B. superheat and it is stable, whereas in 11/6 the I.B. su-

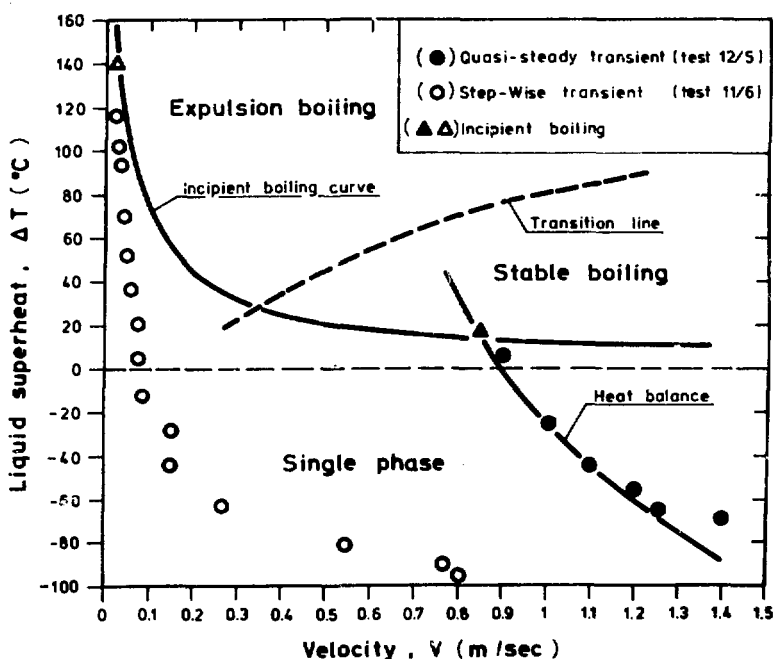


Fig. 7 - Overheating of liquid versus velocity in tests 12/5 and 11/6

perheat is much higher and the boiling is ejecting-divergent.

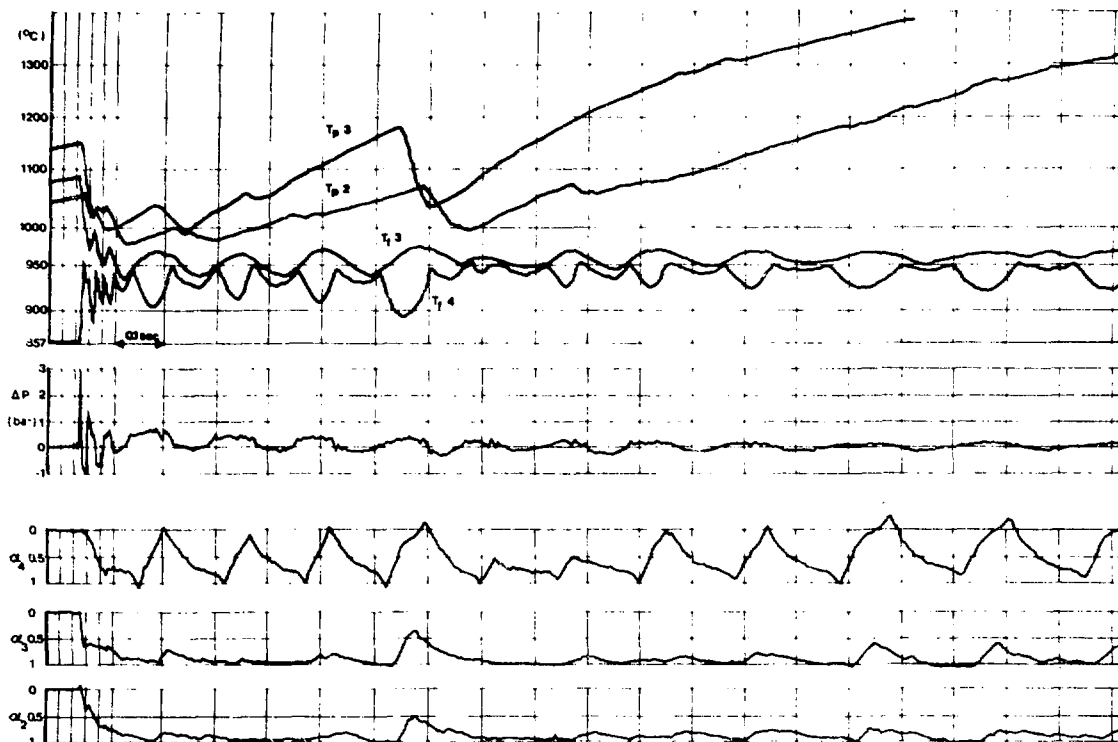


Fig. 8 - Test 11/6: sodium boiling

The figs. 8, 9, 10, 11 show much clearly the boiling model in test 11/6. After an initial phase of superheat release there is a total emptying of the channel and hence the drying-out and the melting of the clad (fig. 8). The melted goes upwards (probably because of the dragging force of the vapor) following the trace of the wire, and solidifies causing an obstruction in the upper part of the test-section (fig. 9). Re-entering by opening the test-section valve (5 seconds after the inception of the boiling), the sodium interacts violently on the melted clad (fig. 10), producing a vapor, the initial temperature of which coincides with the temperature of the sodium in contact with melted clad.



Fig. 9 - Heater clad melted during test 11/6

Fig. 11 shows, through the signals of velocity and pressure drop on the ends of the test-section, the complete time behaviour of the test 11/6. Figs. 8, 10, 11 allow an immediate comparison of the temperature, pressure and velocity signals in two phenomena as much different as sodium boiling and sodium-melted clad interaction.

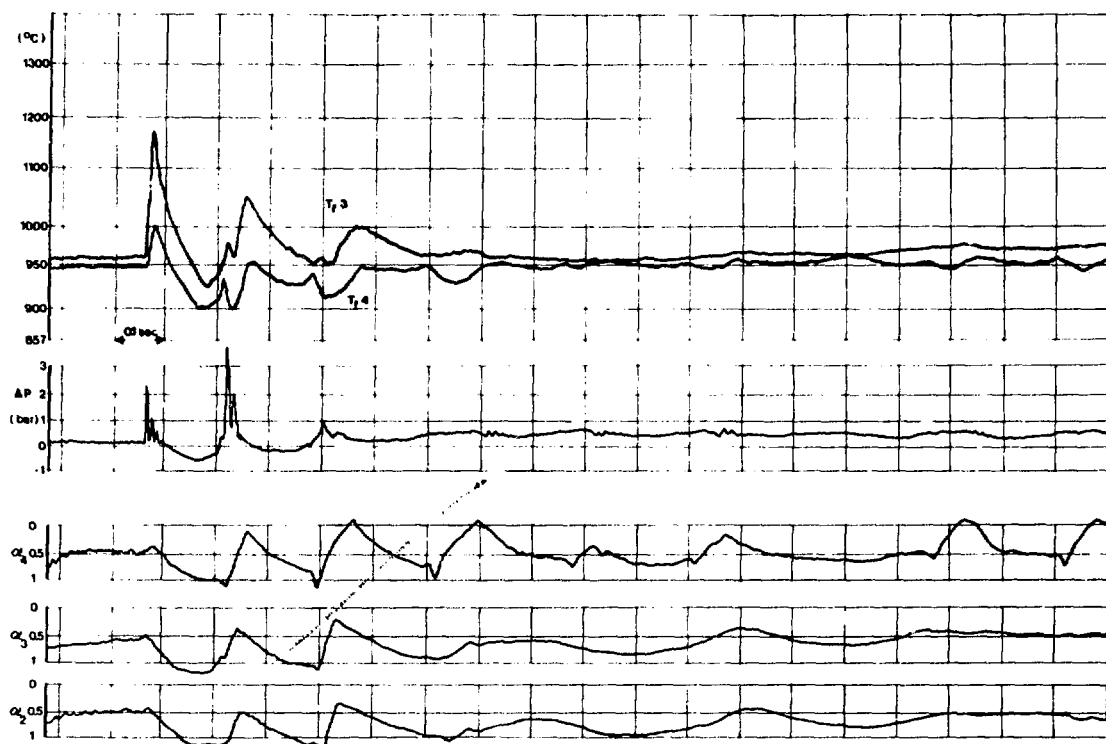


Fig. 10 - Test 11/6: sodium-melted clad interaction

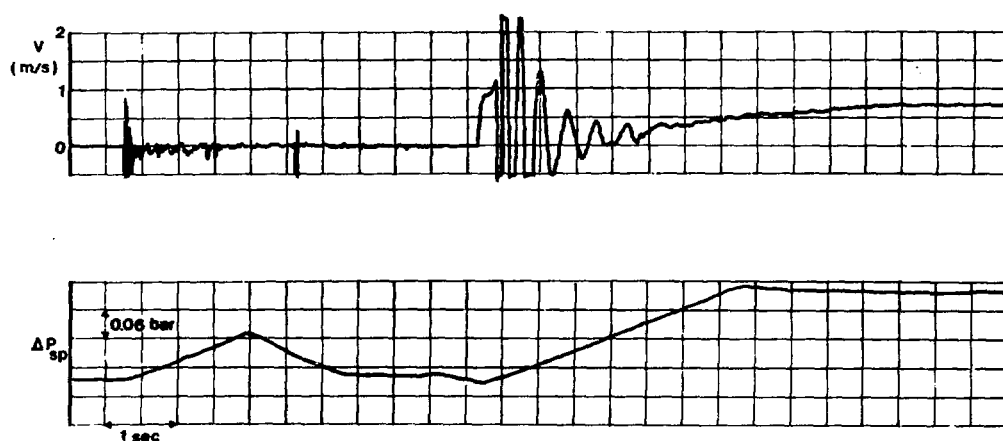


Fig. 11 - Test 11/6: velocity and pressure drop signals

SEVEN PINS BUNDLE

At the moment preliminary experiments on a seven pins test-section (fig. 12) are in progress, which will allow to value the influence of complex geometry on sodium boiling.

Moreover, in an advanced phase is already the planning of a new seven pins test-section (heating length 720 mm, variable axial distribution of the heat flux), which will allow to realize a better simulation of the thermohydraulic conditions of the fuel element.

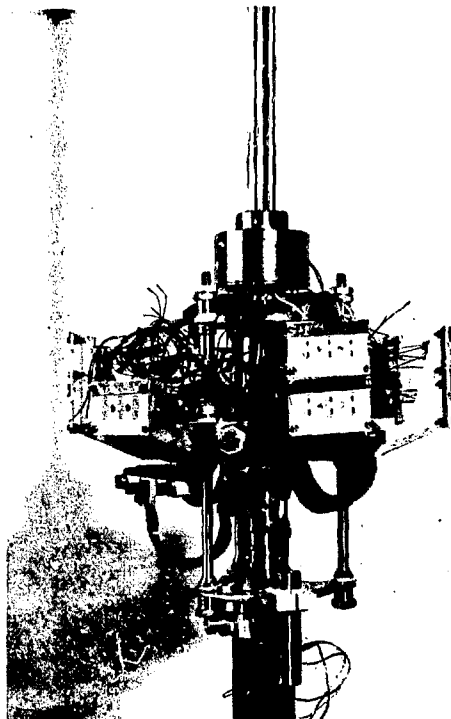


Fig. 12 - Seven pins bundle

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This work, though being signed only by the research people that has taken part in the analysis of the experimental results, has been achieved only thanks to the contribution of the experimental and service groups of the "Divisione Ricerca e Sviluppo" of the Casaccia directed by Mr. E. Santandrea. The authors want to give particular thanks to Mr. G.C. Pinchera under whose leadership the research activities about the Safety of the Fast Reactors have begun and thusfar have reached positive results and to Mr. L. Falzetti for his heaters utilized in the experiments and for the seven pins bundle design.

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MULTIPHASE FLUID DYNAMICS WITH APPLICATIONS IN LMFBR SAFETY ANALYSES

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ABSTRACT

The multifield fluid dynamics equations are presented and discussed relevant to the Hypothetical Core Disruptive Accident (HCDA). A numerical calculation of a submerged air jet in a water pool, which simulates the HCDA, is performed to demonstrate the accuracy of the numerical solution technique. The results of the calculation are in substantial agreement with the laboratory experiment.

INTRODUCTION

The safety analysis of the Liquid Metal Fast Reactor often requires a detailed study of the complex dynamic interactions between solid or structural materials, fluids in the liquid phase, and fluids in the vapor phase. During a hypothetical accident sequence these materials can undergo violent time dependent interpenetrations of one another in which the dynamics are governed by mass, momentum, and energy transfer. Under such conditions large differences in velocity, temperature, and density can occur among the various materials so that a multifield [1,2] (multifluid) model is needed for an accurate description of the dynamics.

MULTIFIELD ANALYSIS

In the multifield model each phase is governed by a separate set of field equations for mass

$$\frac{\partial \rho'}{\partial t} + \nabla \cdot (\rho' \underline{u}) = S_{\rho} \quad , \quad (1)$$

momentum

$$\frac{\partial (\rho' \underline{u})}{\partial t} + \nabla \cdot (\rho' \underline{u} \underline{u}) = \underline{S}_m - \alpha \nabla p + K(\underline{\underline{u}} - \underline{u}) + \rho' \underline{g} + \underline{V} \quad , \quad (2)$$

and specific internal energy

$$\frac{\partial(\rho' I)}{\partial t} + \nabla \cdot (\rho' \underline{u} I) = S_1 + \frac{\alpha p}{\rho} \left[\frac{\partial \rho}{\partial t} + \underline{u} \cdot \nabla \rho \right] + R(\bar{T} - T) + \nabla \cdot (k \alpha \nabla T) + \Lambda + V_1, \quad (3)$$

and an equation of state of the form

$$\rho = \rho(p, I), \quad T = T(p, I), \quad (4)$$

in which the microscopic density, ρ , is related to the macroscopic density, ρ' , and the volume fraction, α , by

$$\rho = \rho' / \alpha. \quad (5)$$

In Eqs. (1-3), interactions between the phases due to phase transitions are described by S_ρ , S_m , and S_1 which are respectively, sources or sinks of mass, momentum, and internal energy density. Additional coupling between the phases exists through the effective drag function K , where $\bar{u}$ is the mean resistive velocity for the phase, and the energy exchange function R , where $\bar{T}$ is the mean exchange temperature for that field. Other terms account for the effects of viscous stress, $\underline{V}$, viscous work, V_1 , and the rate of production of internal energy density due to momentum exchange, Λ .

In the absence of dissipative mechanisms, Eqs. (1-5) possess complex characteristics for all non-zero relative velocities, which has been of great concern to some investigators [3] and has even led some of them to call this set of multiphase field equations mathematically "ill-posed" when applied to initial value problems. On the contrary, if the physical instabilities that actually occur in nature for multiphase flows are to be described, the field equations must contain the potentiality for the growth of perturbations. Since the multiphase formulation is highly nonlinear, a better criterion for well-posedness is the "high-frequency test" of Lax [4], which states that the growth rate for infinitesimal perturbations must be bounded as the wave number becomes infinite. This condition is met naturally when the physical dissipative mechanisms of viscosity, conductivity, and diffusion or, for separated flows, the effects of surface tension [5] are included in the equations. From the practitioners point of view, when seeking the numerical solution of the multiphase field equations with finite difference methods, we introduce dissipation sufficient to damp wavelengths smaller than the cell size, but not so much as to damp the large scale instabilities of physical interest [6].

In certain multiphase problems it is necessary to write at least three sets of field equations, i.e., Eqs. (1-5) are written for each phase that has relative motion to the other phases. For example, in the application of a transient air-sand jet into a water pool with an air cover gas, which simulates the core material transport of an HCDA analysis [7], it is necessary to write field equations for the three materials. When the sand is

disperse, the three materials share the same pressure; however, as the sand becomes close-packed and approaches its physical packing limit, the pressure of the sand phase must quickly increase to prevent nonphysical sand overcompaction while the pressure associated with the air and/or water phase remains suitable for describing penetration velocities throughout the sand matrix.

Another example occurs in the post-accident heat removal phase of an HCDA where debris-bed heat transfer and self-leveling experiments are analyzed [8]. In these experiments, particulate UO_2 or stainless steel debris-beds are heated while immersed in water or liquid sodium. Three phases are present when boiling commences at the surface of the solid pellets. It is observed that spontaneous fluidization of the beds occurs with instabilities developing into the formation of channels that facilitate the escape of vapor from the bed. The channels are highly unstable, often collapsing and reforming while drifting around the bed. During the unstable dynamics of the debris bed it is important, as in the sand jet, to prevent nonphysical overcompaction of the solid particulate while allowing gross motion and interpenetration of the liquid and vapor phases.

A numerical method is described in Ref. 2 that uses the ICE (Implicit Continuous-Fluid Eulerian) technique [9] to solve the three field equation sets.

In many LMFBR safety analysis problems [10], a simpler two field model is sufficient; e.g., the SIMMER-I computer code [11], which derives its fluid dynamics module from the KACHINA [1,12] computer code. In KACHINA and K-FIX [13] (KACHINA-Fully Implicit EXchange Functions) the field equations are written for the liquid and vapor phases with coupling through the exchange of mass, momentum, and energy and the assumption of pressure equilibrium. In developing the two field model, we define

$$S_{\rho} = J_e - J_c \quad (6)$$

for the vapor mass equation, and

$$S_{\rho} = J_c - J_e \quad (7)$$

for the liquid mass equation, where J_c and J_e denote the condensation and evaporation rates respectively. Similarly, the momentum exchange from phase transitions is given by

$$S_m = J_e u_l - J_c u_v \quad (8)$$

for the vapor momentum equation, and

$$S_m = J_c u_v - J_e u_l \quad (9)$$

for the liquid momentum equation. The velocity of the donor material is used in these momentum relations as a first order approximation to the effective

mixing velocity at the vapor-liquid interface. For the specific internal energy equation, the sum of the pressure work and the source or loss of internal energy density due to phase transitions can be rewritten for the vapor energy equation as

$$\frac{\alpha_v p}{\rho_v} \left[\frac{\partial \rho_v}{\partial t} + \mathbf{u}_v \cdot \nabla \rho_v \right] + S_i = -p \left[\frac{\partial \alpha_v}{\partial t} + \nabla \cdot (\alpha_v \mathbf{u}_v) \right] + (J_e - J_c) H_v, \quad (10)$$

and for the liquid energy equation as

$$\frac{\alpha_l p}{\rho_l} \left[\frac{\partial \rho_l}{\partial t} + \mathbf{u}_l \cdot \nabla \rho_l \right] + S_i = -p \left[\frac{\partial \alpha_l}{\partial t} + \nabla \cdot (\alpha_l \mathbf{u}_l) \right] + (J_e - J_c) H_v, \quad (11)$$

where H_v is the specific enthalpy of the vapor. We assign the drag dissipation, Λ , to the vapor energy equation in the form

$$\Lambda = K(\mathbf{u}_v - \mathbf{u}_l)^2. \quad (12)$$

A NUMERICAL CALCULATION USING A TWO FIELD MODEL

In order to evaluate the dynamics and energy yield of an HCDA bubble ejected upwards into the sodium pool, a number of experiments have been performed [7] on the transient behavior of two phase submerged jets. Our objective is to calculate one of these experiments to verify the computational technique of the multifield method used in K-FIX [13] with the eventual purpose of assessing the mechanical work done by the sodium pool on the vessel head.

The two dimensional computational mesh in cylindrical geometry was set up to model the apparatus used for the high pressure air jet experiment designated G1 in Ref. 7. The radial cell size, δr , is 8.45 mm and the axial cell size, δz , is 20.0 mm with the two regions represented with 2 radial by 19 axial computational cells in the high pressure air chamber and 9 radial by 30 axial computational cells in the water pool and cover gas volume. Initial pressures are 0.27 MPa (2.7 atmospheres) and 0.1 MPa (1.0 atmosphere) in the high-pressure air chamber and the water pool and cover gas volume respectively. Initial temperatures are uniformly set in both regions to 294°K. The height of the water pool and cover gas are both 300.0 mm. The air in the high pressure chamber and cover gas volume is described by a polytropic equation of state.

The calculation was performed on a CDC 7600 computer in approximately 25 minutes of computer time. The results are presented in Fig. 1, which shows a frame from the high speed motion picture of the experiment (courtesy of the Argonne National Laboratory) and a marker particle plot of the air bubble and associated velocity field from the calculation for comparison at five different times. Marker particles and velocity vector plots have been reflected about the centerline and the results for only a small portion of the computational mesh are shown, so that comparison with the laboratory film frames can be made more easily. The velocity vectors are normalized to the maximum velocity on the displayed mesh. Transient pressure transducers were used in the experiment to record pressure traces at the top of the cover gas on the vessel head and on the base plate at the bottom of the water pool.

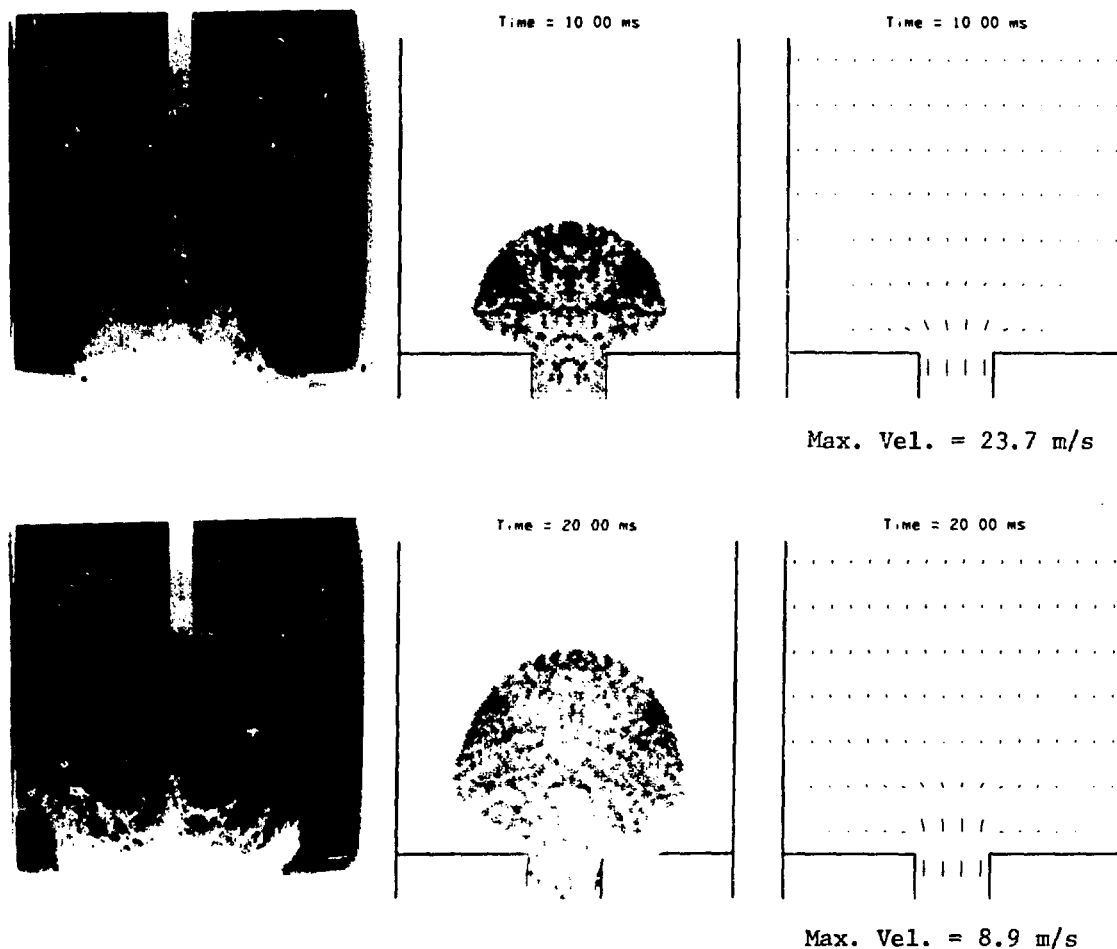


Fig. 1. Frame from the Laboratory Film (courtesy of the Argonne National Laboratory) and calculated marker particle configuration and velocity vectors for 10 ms, 20 ms, 30 ms, 40 ms, and 50 ms. Velocity vectors are normalized to the maximum velocity as denoted below each velocity vector field.

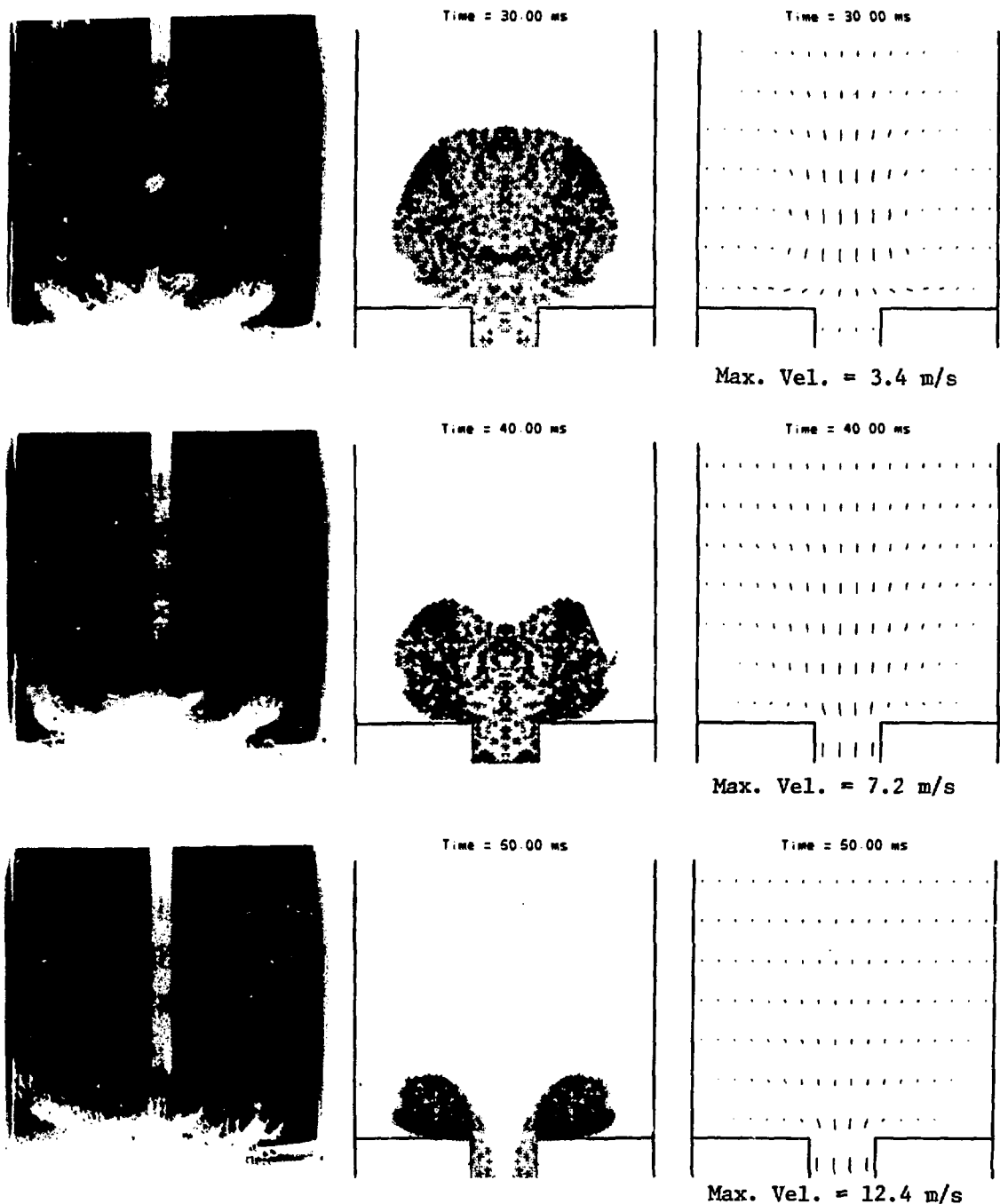


Fig. 1. (continued) Frame from the Laboratory Film (courtesy of the Argonne National Laboratory) and calculated marker particle configuration and velocity vectors for 10 ms, 20 ms, 30 ms, 40 ms, and 50 ms. Velocity vectors are normalized to the maximum velocity as denoted below each velocity vector field.

We present in Figs. 2 and 3 the calculated pressures as a function of time at positions 50.7 mm and 63.4 mm from the centerline on the vessel head and base plate respectively. These calculated pressure traces, when examined by the experimenters, were found to be in substantial agreement with the experimental measurements [14].

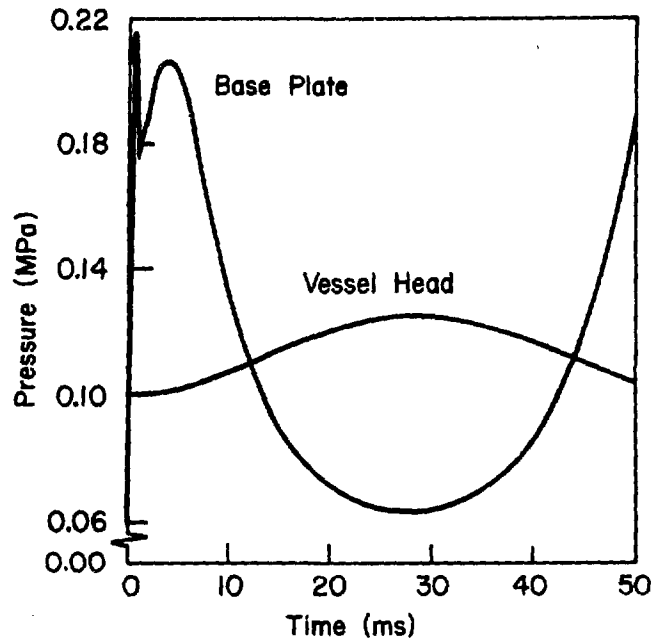


Fig. 2. Calculated long time pressure history at the base plate and at the vessel head.

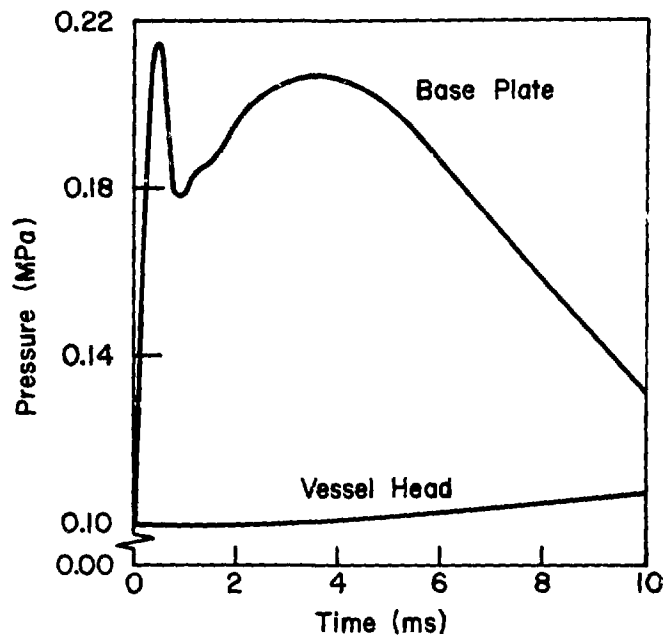


Fig. 3. Calculated early time pressure history at the base plate and at the vessel head.

As the diaphragm to the high pressure chamber is ruptured the pressure on the base plate rises rapidly from the initial surge of air. The initial momentum imparted to the water quickly leads to an overexpansion of the air and subsequent drop in pressure. At about this time (1 ms), the initial release wave reflects off the bottom of the high pressure chamber and the air experiences an increased upward acceleration. The pressure begins to rise again from the increased upward air flow and continues rising until about 3.5 ms. At this time, the bubble begins a second overexpansion. The pressure continues to decrease until the bubble reaches its maximum volume at 28 ms. The maximum pressure in the cover gas is reached at this time and the bubble begins to collapse into the torus shape shown at 40 ms and 50 ms in Fig. 1. During the collapse the pressure increases from the downward directed momentum of the water. Beyond 50 ms the pressure tends to equilibrate and the bubble breaks up under the action of turbulence and buoyancy.

The velocity field shows a spherical distribution at 10 ms and 20 ms. At later times, the bubble collapse is very evident by the reversal of the velocity vectors and the secondary flow vortex pattern set up between the centerline and outside boundary. The vortex becomes smaller in time until at 50 ms it is isolated in a corner with most of the velocity vectors directed toward the lower chamber opening.

Although this calculation did not make use of the full power of the multifield method, it provides an excellent test of the code's ability to calculate separated two phase flow in the low Mach number range. Similar experiments have been performed where saturated liquid and superheated steam are used in the high pressure chamber in place of air to demonstrate the relative importance of phase transitions. Calculations of these experiments will be extremely useful for evaluating phase transition models now that substantial agreement has been obtained with the air-water experiments.

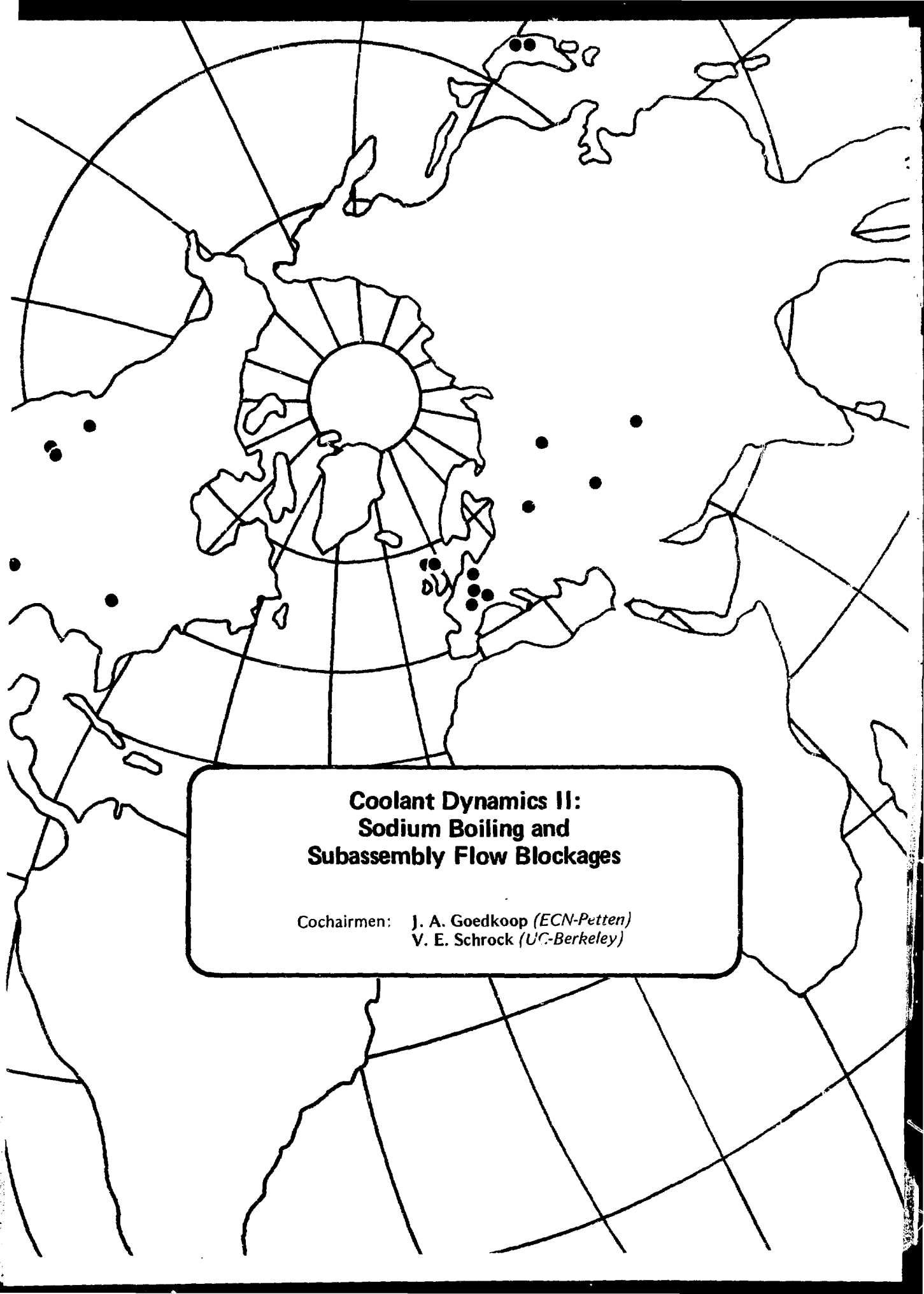
ACKNOWLEDGMENTS

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Coolant Dynamics II: Sodium Boiling and Subassembly Flow Blockages

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SODIUM BOILING EXPERIMENTS IN A 19 PIN BUNDLE :
TWO PHASE COOLANT DYNAMICS

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ABSTRACT

Permanent sodium boiling experiments have been performed in a 19 wire-wrapped pin bundle on the forced convection loop CFNa II of the Service des Transferts Thermiques at the Grenoble Nuclear Research Center. These experiments have shown that subcooled boiling, highly sensitive to the multidimensional aspects of bundles, is very noticeable : the singular coolant dynamics during subcooled boiling could lead to consequences such as Burn-Out and pin failure. Nevertheless, the behaviour of the sub-assembly during saturated boiling becomes grossly analogous to the behaviour of a monodimensional channel, and the pressure drops which develop in the adiabatic parts of the subassembly may be of primordial influence on subsequent events. The physical models that should be developed in order to describe the successive stages of subcooled and of saturated boiling should be different.

INTRODUCTION

Two of the hypothetical incidents, considered in the fast reactor safety studies, which could lead to sodium boiling, are the following :

- a flow restriction due to blockage at the inlet could be sufficient to induce boiling in a subassembly
- the extremely improbable event of pump run down without scram could also lead to boiling propagating to the whole core.

Many studies have already provided valuable information on sodium boiling in single channel configurations. The up-to-date question is now to specify up to which point this information can be used for the prediction of boiling in real subassemblies.

Following the previous works on boiling in annular channels -both for steady state [1] and for transient flows [2]- and in a seven pin bundle [3] extensive experiments of boiling in a 19 pin bundle were planned at the Service des Transferts Thermiques. It was hoped that calculational models for sodium boiling in bundles could be found and that codes grossly predicting the sodium boiling in real subassemblies of 217 or even more pins could be derived

from the extrapolation of compared phenomena in single channel and in bundles with few pins (7, 19, ...).

The purpose of performing experiments under permanent boundary conditions was to provide fundamental results on the particularities of sodium boiling in bundle geometries. Such basic information may be useful for the difficult interpretation of further experiments under loss of flow conditions. Furthermore, they give good indications about sodium boiling appearing during slow flow transients.

THE EXPERIMENTAL DEVICE AND PROCEDURE

The CFNa Loop

The risks of hydrodynamic instabilities following sodium boiling in a loop, and the phenomena due to thermal inertia are such that it is very difficult to maintain constant boundary conditions for a test section with sodium boiling. The characteristics of the CFNa II loop (fig. 1) and the experimental procedure which was used were defined in order to obtain sodium boiling in a 19 pin bundle under rigorously permanent boundary conditions :

The constant speed mechanical pump (flow-rate : $15 \text{ m}^3/\text{h}$; pump head 100 m), the by-pass circuit and the fine adjustment of throttling valves were such that the flow-rate through the test section was imposed whatever the internal pressure drop.

The pressure (1 bar) was imposed in the argon cover of the expansion tank.

The two regulated heat exchangers (600 kW and 400 kW) and the adjustable preheater (up to 32 kW) imposed the temperature at the entry of the test section (400°C most of the time).

The power was supplied respectively to 9 and 10 pins by two independent, power regulated rectifiers (maximum 400 kW each).

The series of tests were defined by the distribution of the power supplied (maximum 30 kW per pin). Each series was constituted of several runs obtained for decreasing flow-rates by successive shutting of the throttling valve located at the inlet of the test section. Each run lasted about 7 minutes : 2 minutes were enough to reach thermal equilibrium, and really permanent boundary conditions were maintained during 5 minutes at least in order to record sufficient information on the flow regime in the test section.

The GR. 19 Test Section

The GR. 19 test section has been designed so as to simulate a fast reactor sub-assembly (fig. 2). The 19 wire wrapped pin-bundle is composed of a 600 mm long heated part located between a 100 mm tranquillisation length and a 494 mm downstream unheated length, simulating respectively the fissile and the fertile parts of the assembly. The duct, hexagonal around the bundle, is extended by an adiabatic tube of 19.3 mm inner diameter and 910 mm length, simulating the integrated radiation shielding.

The main characteristics of the bundle, shown on fig. 3, are the following :

- pin outer diameter $D = 8.65 \text{ mm}$
- triangular array ; pitch between pin centroids $P = 9.95 \text{ mm}$ ($P/D = 1.15$)

- one helical spacer wire per pin (diameter $d = 1.28$ mm, helical pitch $h = 180$ mm)
- 45.85 mm between opposite faces of the hexagonal duct
- maximum linear power 500 W.cm^{-1} produced by a resistor insulated by boron nitride from the tantalum sheath of each pin.

The instrumentation of the test section comprised numerous thermocouples, pressure transducers and electromagnetic flowmeters (Fig. 2). The 200 measures were scanned and recorded in 20 seconds and several records of the same run could be made in order to verify that the flow was really quasi permanent. Furthermore, some of the more fluctuating signals were continuously recorded on graphical recorders.

Acoustical and thermal noises, measured with special devices were also recorded, during 5 minutes for each run, by experts from the Laboratoire d'Etude de l'Instrumentation en Sodium of the Département des Réacteurs à Neutrons Rapides of Cadarache Nuclear Research Center.

EXPERIMENTS OF SUBCOOLED BOILING WITH 19 - PIN HEATING

The first series of experiments were performed in a range of parameters of interest for the study of pump run down without scram. The total power was 120 kW. The power was evenly distributed for the first series (6.3 kW per pin) whereas the second series was performed in order to simulate grossly a transverse power skew (9 pins producing 5 kW each and the 10 remaining producing 7.5 kW each). The boundary conditions and test procedure were those described in the presentation of the loop, and the range of mass flow-rates studied extended from 2300 g.s^{-1} to 167 g.s^{-1} .

Single Phase Flow And Hot Spot Boiling

For flow velocities greater than $.8 \text{ m.s}^{-1}$, the hottest region is located as expected : around the central pin (uniform power) or in the internal part of the maximum power region (power skew). More surprising results, obtained for lower mean flow velocities (lower than $.5 \text{ m.s}^{-1}$), could be attributed to the beginning of a kind of mixed convection flow in which helical wires have unexpected effects. Anyway, there was no longer any basic distinction between the series of experiments with and without power skew : the general description of boiling which follows applies to both.

Q_{sat} being the flow rate for which mean saturation should be obtained at the top of the heating length, the first evidence of boiling is detected for : $Q_{\text{hSb}} \approx 1.4 Q_{\text{sat}}$: the mean coolant temperature at the outlet of the heating length is about 760°C whereas the saturation temperature is 910°C and the maximum temperature indicated by the thermocouples is 812°C (fig. 4). Little bubbles of vapor periodically developed around a superheated hot spot and collapsed in the surrounding subcooled liquid. The acuteness of the hot spot -which may be an evidence of badly cooled zones about the helical wires- was such that incipient superheat, if noticable at all, was of no bulk consequence.

Located Boiling

When the flow-rate Q_{LB} becomes sufficiently low ($1.25 Q_{\text{sat}} \geq Q_{\text{LB}} > Q_{\text{sat}}$) a new flow pattern, called "located boiling", develops in the test section. A zone of two-phase mixture develops mainly in the axial direction towards the inlet and includes some of the thermocouples (then measuring T_{sat}). The vapor produced in the heating bundle travels through the two-phase region and conden-

ses in the unheated part of the bundle. The boundaries between the two-phase region and the subcooled liquid flowing round are stationary (for a run at given mean flow-rate) except at the upstream and downstream ends, where boiling and condensation induce oscillations.

The interpretation of stable located boiling with the multichannel sodium boiling code FLICA [4] shows that, as a consequence of an "internal flow excursion", the remaining mass-flow rate through the two phase region is very low because most of the flow rate by-passes through the surrounding liquid region : the flow pattern inside the two phase region is likely to be a kind of annular flow and burn-out should occur if the liquid film is not conveniently fed. Anyway, no burn-out was detected during these experiments performed with low heat fluxes.

Occurrence Of Saturation

Due to the pressure losses through the adiabatic parts of the test section, the saturation temperature at its downstream end is 15°C below the saturation temperature at the outlet of the bundle : when mean conditions of saturation are nearly met, flash boiling develops at the outlet of the test section. When saturation is just reached at the top of the heating length, the two zones of heat-generated boiling and of flash boiling meet. Then, the two phase region extends nearly to the whole section of the bundle along the parts where mean saturation is reached, whereas located boiling persists in some of the subcooled parts (fig. 5). With the exception of this latter zone, coolant dynamics should then be described using a single-channel modeling.

EXPERIMENTS PERFORMED AT NOMINAL HEAT FLUX

During the two first series of experiments more than 1 hour of subcooled boiling was performed under conditions which locally could be very severe : the pins which had suffered failed when an attempt was made to make them work at nominal power. Thus, some of the pins were not heated during the last series of experiments which were performed in a range of parameters representative of a slowly growing blockage at the inlet of a subassembly. Nevertheless, the important extent of the range of parameters in which "located boiling" occurs has been confirmed by these experiments. Furthermore, some particularities of sodium boiling dynamics in bundles with nominal heat flux (187 W.cm^{-2}) and high flow velocities were pointed out.

Burn-Out Due To Internal Flow Excursion

As a result of the internal flow excursion, the flow rate of coolant inside the two phase region during located boiling may not be sufficient to evacuate the heat generated by pins working at high power : thus burn-out is likely to occur in the first stages of boiling. Such burn-out, leading to the final destruction of the test section, was observed during our experiments [5].

The first burn-out occurred when the mean outlet temperature was more than 100°C under saturation : the sheath of one pin had cracked along a helical wire near the top of the heated length. This clearly shows that the regions of pin sheathes at the contact of helical wires favour the development of hot spots. The last burn-out, which occurred a certain time after the obtention of mean saturation, put a final end to the experiments : five tantalum sheathed pins were partly molten.

Influence Of The Unheated Lengths During Saturated Boiling

When saturation occurs at the outlet of the heating bundle, the remaining flow rate is such that high two phase pressure drops develop in the unheated lengths of the test section (fig. 6). Although it was not proved, an analogy with previous experiments [6] suggests that critical-flows occurred at the outlet of the test section.

Under these conditions, the pressure drop through the test section was such that LEDINEGG instability would lead to a global flow excursion -well known in single channel sodium boiling dynamics [2]- with a pump head as great as the nominal one available in a fast reactor.

CONCLUDING REMARKS

As a conclusion to this work, some indications -useful for the Safety Analysis of Sodium Cooled Fast Breeder Reactors- can be drawn on what appears to be an appropriate way of modelling the successive stages of boiling which appear during slow flow transients in subassemblies of wire wrapped rods (fig. 7). The main conclusion to be recalled is that subcooled boiling, which is of interest over a large range of parameters, may have consequences such as destructive burn-out. The description of subcooled boiling implies that :

- Phenomena such as mixed convection and hot spots developing in the vicinity of helical wires have to be taken into account in order to predict the exact time and location of boiling incipency.
- Interconnected subchannel boiling codes have to be developed in order to describe the internal flow excursion around the point where boiling starts. Experimental and theoretical work is needed in order to estimate the risks and consequences of burn-out occurring during the so-called stage of "located boiling".

When mean conditions of saturation are reached at the outlet of the heating bundle, the pressure drops developing in the adiabatic parts become consequent and the overall behaviour of the assembly is the same as the behaviour of an equivalent channel : as long as deep alteration of the geometry of the assembly, or even fuel-coolant interaction, occurring during subcooled boiling as a consequence of the internal flow excursion, can be excluded, the sequences of events following mean saturation could be described by single channel calculations.

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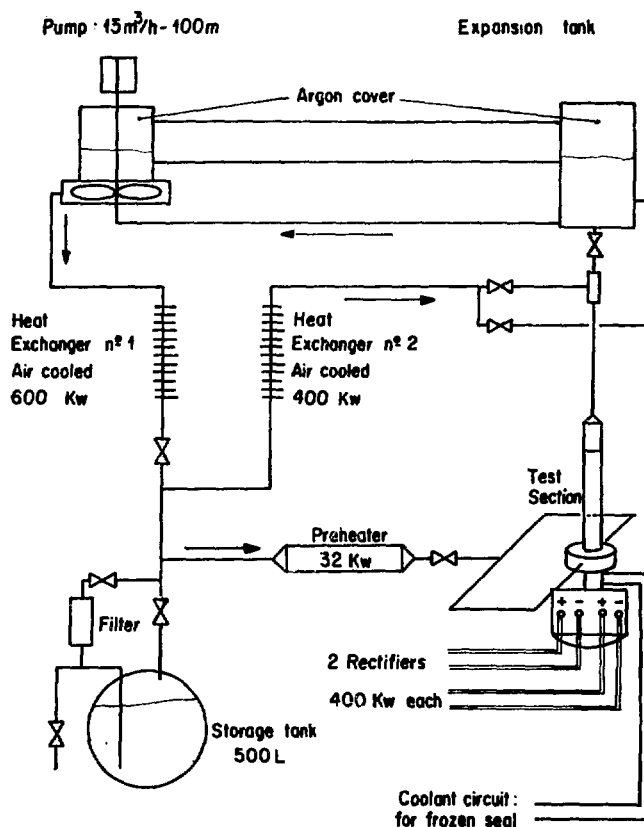


Fig. 1. The CFNa II Loop

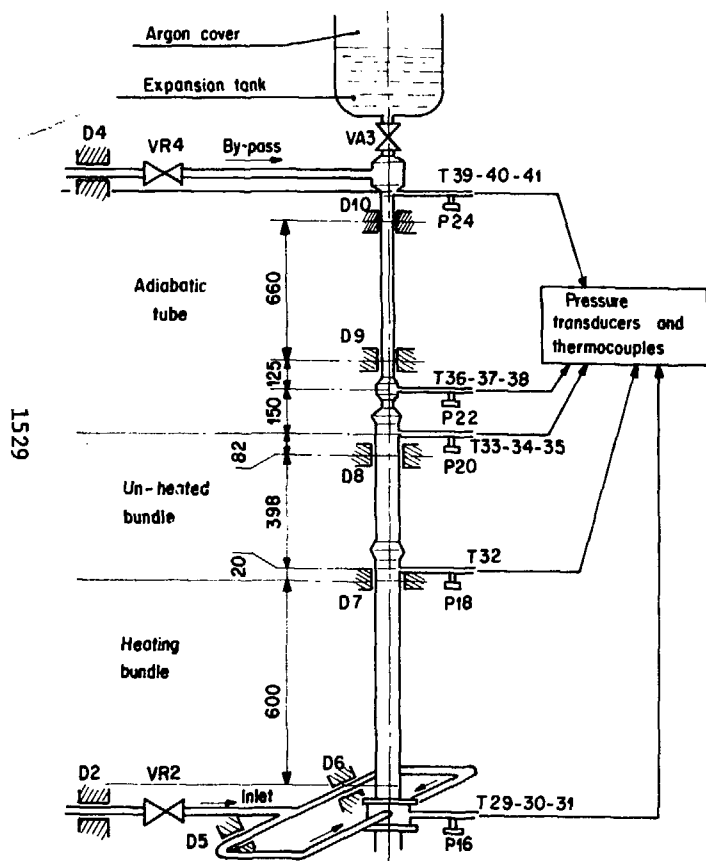


Fig. 2. The GR. 19 Test Section

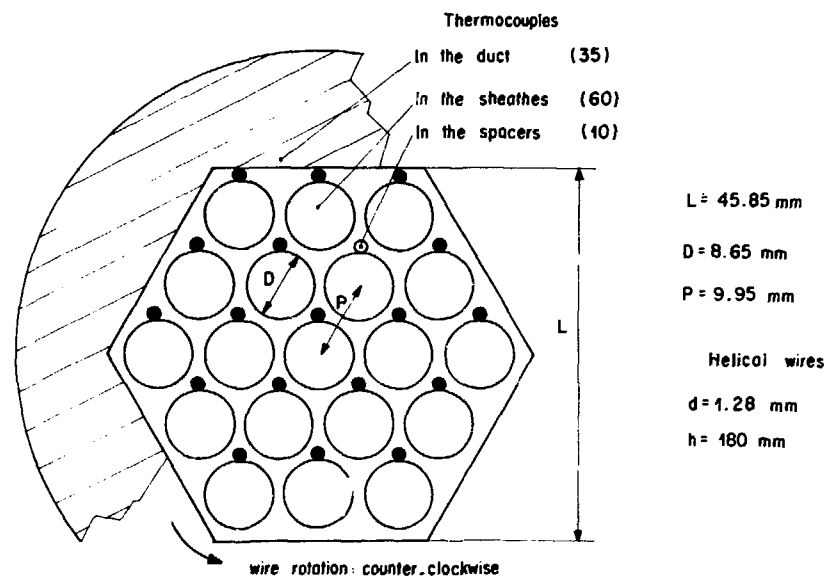


Fig. 3. Section of the GR. 19 Bundle

Hot spot boiling: all the measured temperatures are at least 100°C under saturation

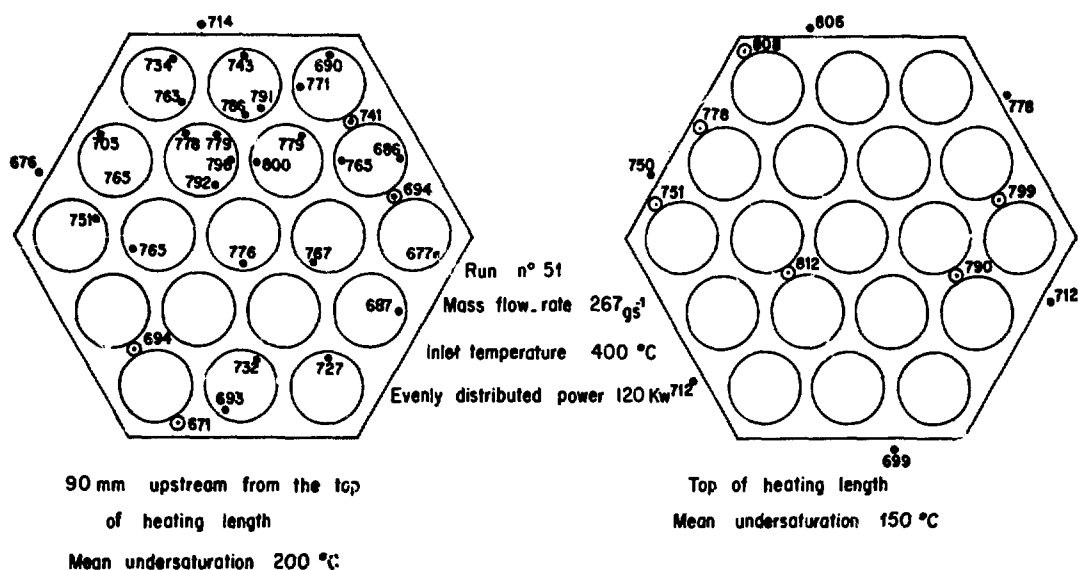


Fig. 4. Temperatures Measured during Hot-spot Boiling

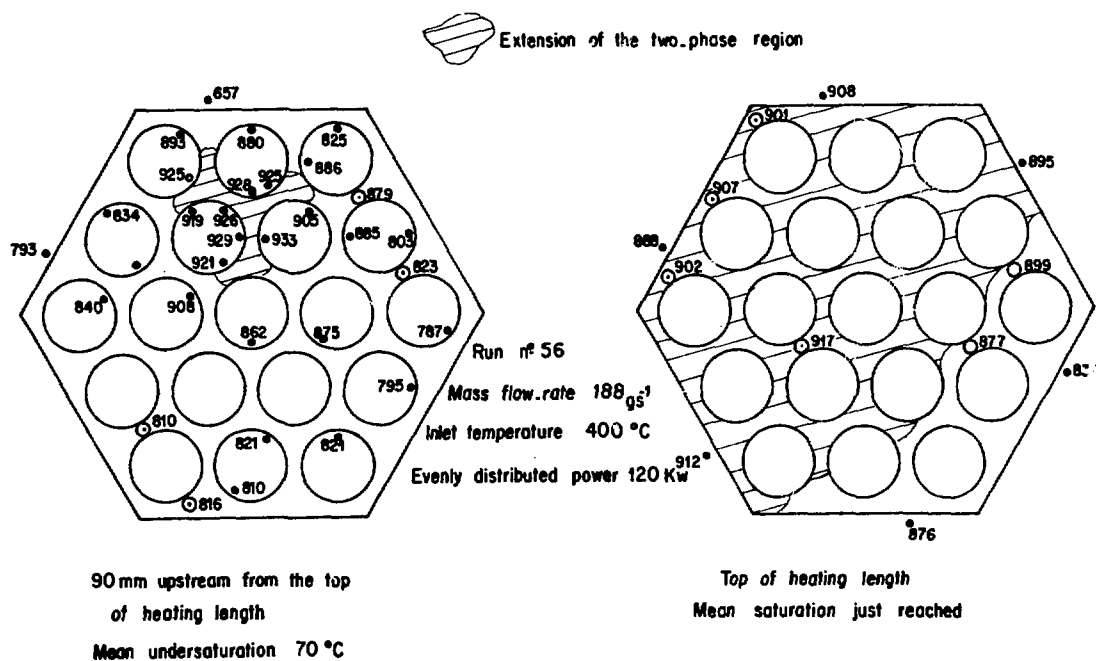


Fig. 5. Temperatures Measured during Saturated Boiling

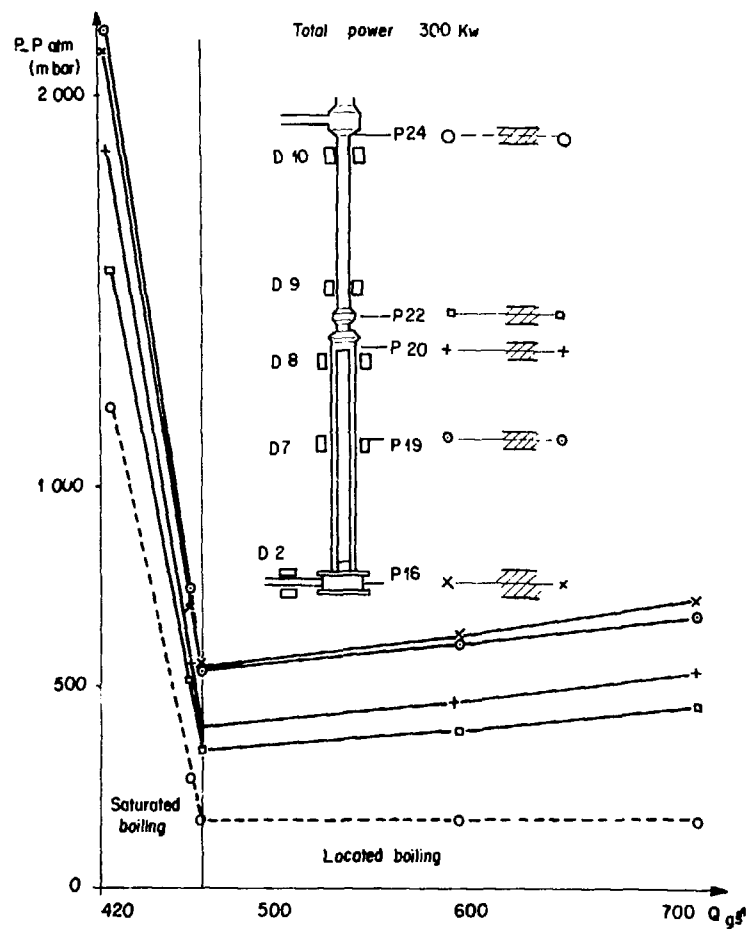


Fig. 6. Evolution of the Pressure along the GR. 19 Test Section

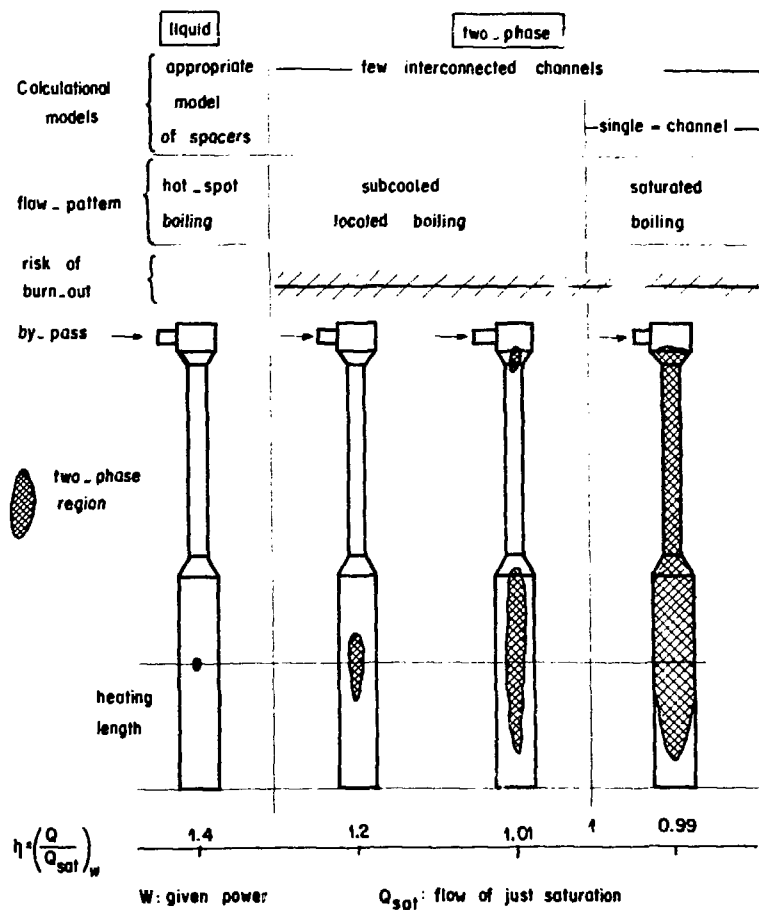


Fig. 7. First Stages of Boiling during Slow Flow Transients

TEMPERATURE DISTRIBUTION AND LOCAL BOILING BEHIND A CENTRAL BLOCKAGE IN A SIMULATED FBR SUBASSEMBLY

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ABSTRACT

A series of experiments has been carried out to investigate the effects of localised disturbance to the normal coolant flow in a fast reactor fuel element. The tests involved an electrically heated bundle of 169 pins, with a centrally located blockage extending over 49 % of the flow area. Test section geometry corresponded to the SNR 300 Mk 1a fuel element.

Measured temperature distributions behind the blockage agreed well with those measured in corresponding water experiments. In this paper the observed features of local boiling are discussed, and it is shown that a continued capability for cooling the blockage region is preserved, even with intensive local boiling.

INTRODUCTION

In the safety considerations of the LMFBR a considerable amount of attention is devoted to the problem of local failure propagation [1]. Such propagation processes may be initiated by local blockages within the fuel elements caused by deposition of fuel particles and foreign debris or by fuel swelling after the occurrence of cladding defects. The consequences of such blockages are local temperature increases and possibly local boiling. This may accelerate the propagation process considerably. To judge the damage potential of local boiling three areas have to be investigated:

- Temperature distribution behind blockages and the nature of local boiling;
- Detectability of blockages and boiling by temperature and acoustic noise surveillance;
- Possible damage propagation by dryout or by hydrodynamic instability.

DESCRIPTION OF TEST FACILITIES

Test Section and Instrumentation

Fig. 1 illustrates the arrangement of Test Section No.1 with its 49 % centrally located blockage. The cross section corresponds to the SNR 300 Mk 1a

^{*} United Kingdom Atomic Energy Authority, Risley

sub-assembly, with 169 pins of 6 mm external diameter on a pitch of 7.9 mm. The electrical resistance heaters used can produce a maximum heat flux of 170 W/cm^2 distributed linearly over the heated length of 290 mm. Maximum power rating for the bundle is 818 kW. The blockage is made of 3 mm thick steel plate, and is located within a spacer grid 40 mm along the heated section. Limiting pin surface temperature is 950°C .

A total of 91 pins lie within the blockage boundary, of which 88 are heated (3 being required for bubble detectors). It is known from the results of water experiments that the absence of heating in the two pin rows outside the blockage boundary has minimal effect on the temperature distribution behind the blockage.

Hydraulically, and in scale, the test section corresponds to the upper part of the SNR fuel element, the flow mixer on the outlet side being included. In comparison with a fuel bundle, the heated length of the test section is less, but this can be compensated by adjustment of inlet temperature, and has little influence on the results. The frictional and accelerational pressure losses in the lower part of the fuel element are simulated by those in the inlet leg and throttle valve.

The test section is instrumented as follows:

- About one hundred thermocouples around the blockage to obtain temperature distributions and to monitor temperature fluctuations. About 60 of these were embedded in the heater pin walls.
- 20 Chen-type bubble detectors distributed throughout the boiling region (10 - 120 mm above the blockage)
- 9 static and dynamic pressure monitors at various positions
- Acoustic monitors at various positions in the test section and loop, to assess the detectability of boiling noise remote from its source
- Flow meters at inlet and outlet. The difference in the output from these may be integrated to give vapour volume.

Data recording was by means of magnetic tape (partly analog, partly digital). About 110 signals could be simultaneously recorded.

The KNS Loop (Kompakter Natrium Siedekreislauf)

The KNS loop is represented schematically in Fig. 2. This loop was developed and built especially for this type of experiment, and is designed for a maximum temperature of 700°C . The maximum mass flow is 18 kg/s, somewhat greater than that through the SNR sub-assembly.

Major components of the loop are the pump tank which contains the pump and the Na/Na heat exchanger, and the buffer tank. Coolant flows from the pump tank through the buffer tank to the test section. The inlet plenum of the reactor is simulated by the buffer tank with its gas plenum. In the mixer section, the coolant from the test section is joined by that from the bypass, from where it is passed through the heat exchanger and back to the pump.

In the heat exchanger, the energy added to the circuit in the test section is transferred to the sodium coolant of a second loop. The energy is thereby dispersed into about 6 m^3 (5 tons) of sodium in the dump tank. The estimated cooling capability of the system is 1.2 MW at 700°C . For safety reasons, the whole loop is contained within a protective tank filled with inert gas.

SINGLE-PHASE EXPERIMENTS

Test Parameters:

A number of single-phase experiments were made in order to fully investigate the temperature field behind the blockage, and to assess its dependence on the main physical parameters, i.e. upon heat flux, flow velocity and inlet temperature. Table 1 indicates the range of parameters used in these experiments, though it should be pointed out that not all possible parametric combinations were utilized.

TABLE 1:

Range of Parameters in Single-Phase Experiments

Inlet Temp. ($^{\circ}\text{C}$)	400	500	590			
Flow velocity (m/s)	1	2	3	4		
Heat flux (W/cm^2)	18	33	51	68	130	136
Reynolds Number	$1.7 \cdot 10^4 - 8.2 \cdot 10^4$					

The first experiments involved an investigation into the influence on the results of coolant flow and heat flux. Later experiments were undertaken to measure the temperature field close to boiling conditions.

The Measured Temperature Field

In representing the single-phase results in this paper, we have used a definition in which the temperature increase behind the blockage is normalised in terms of the temperature gradient in an unblocked cluster, allowance being made of course for the use of a partially heated bundle.

A typical set of results is illustrated in Fig. 3, in which radial temperature distributions at levels of 10, 20, 40, 80 and 120 mm behind the blockage are shown. The blockage form is hexagonal, so that although the number of subchannels is the same in each direction, the physical distance to the sides is less than to the corners, and the flow path towards the sides correspondingly more tortuous. Temperature distributions are therefore illustrated in Fig. 3 towards both sides and corners. The path is the same as far out as subchannel (s/c) 4, beyond which point some differences emerge, though generally these are small. These radial temperature distributions may be interpolated to obtain isotherms, enabling the temperature field as a whole to be obtained, as in Fig. 5.

The principal feature of the results is the clearly annular form of the peak temperature region. The hottest point measured is normally in s/c 7, 40 mm behind the blockage: the hottest point in the system may be slightly further downstream than this, perhaps around 50 mm, but there is no instrumentation at this level. This annular high temperature region occupies the area which flow visualization experiments reveal to be the centre of the recirculating flow system [2]. At the center of the annulus, temperatures are lower: this is a region of downward (i.e. reverse) flow. Beyond the hottest area, towards the blockage boundary subchannels, temperature gradients become very steep. This is an area of apparently great turbulence, as cold fluid moving rapidly past the blockage interacts with hot, slower moving fluid in the recirculating wake.

At 120 mm, the profile is essentially flat over most of the blocked (i.e. heated) subchannels. This level is close to the rearmost stagnation point, which flow visualization experiments show to be about 130 mm, or 1.5 blockage diameters downstream of the blockage.

The normalised temperature fields obtained have essentially the same form and magnitude in all the single-phase experiments. There are some differences, but at the time of writing, it is not possible to say to what extent these are due to parametric changes.

Temperature Fluctuations:

Study of the temperature fluctuations within the wake region reveals that conditions remain remarkably constant. Although there appears to be a high statistical element in the variation of RMS fluctuation magnitude between different measuring points, values of the order of less than 1 % are recorded in the central subchannels: in the peak temperature region, values of about 2 % are typical, whereas on the shoulder of the curves, slightly smaller values are recorded. Fluctuations at outlet (above and below the flow mixer) are normally well below 1 %. These values are normalised with respect to the temperature increase behind the blockage, and should be seen of course in the perspective of in situ thermocouple response time. It has been calculated that 57% of a surface temperature change will be perceived by a heater pin thermocouple at a fluctuation frequency of 5 Hz, and 30 % at 20 Hz.

The small size of these variations in the single-phase experiments would suggest that there were no large scale instabilities in the pattern of recirculating flow.

Comparison with Water Experiments:

Blockage experiments using water have been made at GfK and elsewhere (3,4). The results of these may be brought together in the manner of Clare et al. (3). When reduced in a similar way, the KNS results agree well with these water experiments. The results of water tests with a 49 % blockage (Fig. 3) lie somewhat higher than those of KNS, but are also higher than the results of other water experiments for both larger and smaller blockages. The reason for the disagreement of this single water test series is not yet clear.

LOCAL BOILING TESTS

Test Procedure:

To aid understanding of the experimental results, the test procedures adopted should be known. When sodium temperature had reached the desired value at the start of the test series, power was applied to the test section at the rate selected, with coolant flow sufficiently high to guarantee boiling would not take place. Data recording was commenced, and the flow through the test section reduced in stages by means of the inlet throttle valve. The time intervals between flow reductions were long enough to ensure steady state conditions were reached. At some stage, boiling began, and intensified at each subsequent stage. The duration of each test run was limited principally by the length of recording tape used.

Test Parameters:

The range of parameters so far investigated is illustrated in Fig. 6. The curves appearing above saturation level have been calculated from experimental

data obtained in the single-phase tests. It will be seen from the diagram that at an inlet velocity of about 1.6 m/s, the temperature difference between saturation and inlet temperature in the non-boiling region is about the same as that between the fictitious maximum temperature and the saturation temperature in the boiling region. This gives an indication how much of the heat is removed by the boiling process alone.

Characteristic Boiling Behaviour:

In Fig. 4, some sample signals recorded at various stages in Test 17 are illustrated. The first group (61-62s) demonstrates the single phase behaviour. Inlet and outlet velocities are constant at the selected level, and indications of finite vapour volume and pressure pulsations are completely absent.

The second extract (157-158s) corresponds to small scale local boiling, saturation temperature being recorded by T 7/40. Flow variations cannot yet be recognised as systematic, but bubble volumes up to about 25 cm³ and a periodic pressure variation can be observed.

In the third stage of the test (226.6-227.5s), local boiling is fully developed. Thermocouples 7/10 and 7/40 indicate boiling temperature, and a regular fluctuation of 5 Hz frequency is superimposed on inlet and outlet flows. The amplitude of this fluctuation is greater on the outlet side because of the hydraulic characteristics of the test section including the buffer tank. The vapour volume generated, which is also periodically varying, reaches a maximum of about 100 cm³, and its complete collapse (which does not occur on every contraction) is marked by sharp pressure peaks. The pumping action of the oscillation is reflected by the temperature fluctuations indicated by thermocouple 7/120.

In the last part of the test (297-298s), intensive boiling which extends into the core of the wake was found to occur. Saturation temperature is attained at all points illustrated except T 7/120. The amplitude of flow fluctuations is increased somewhat, and maximum vapour volumes reach about 120 cm³. Pressure peaks marking complete collapse are here absent (which suggests the bubbles no longer collapse completely). Because of this sort of oscillatory behaviour, one could speak of single bubble boiling, although the void detector signals suggest the presence of at least several bubbles with a high degree of liquid entrainment.

In Fig.5, the mean isotherms deduced for the time periods described above are illustrated. From these, one can clearly see the pattern of recirculating flow, especially in the relatively cold central core. In the second and third Figures, clearly annular shaped boiling regions are developed. From the low core temperatures, which are subjected only to small fluctuations, it can be firmly concluded that boiling is confined to the surrounding annulus. Intensive reverse flow in the core of the wake remains fully developed, and corresponds closely to the single phase behaviour.

In the fourth stage, the boiling zone extends into the centre of the wake, but reverse flow remains developed, the central subchannels at 40 mm remaining below saturation temperature, with generally small fluctuations suggesting they are seldom reached by vapour. The test series ended here, so it is not possible to say what would happen if flow were reduced further.

Bubble Volume and Dryout:

It has been shown that the pulsations of bubble volume are very regular. It is of interest to consider the extent and direction of vapour penetration into

adjoining channels. For the fourth period considered, the measured vapour volume of 120 cm³ is about double that of the shaded region in Fig. 5 (65 cm³), which corresponds to boiling temperature. From the large temperature fluctuations (up to 14 °C at 5 Hz frequency) in subchannels 6 to 10 at 80 to 120 mm behind the blockage, it would appear that the vapour expands preferentially in the axial direction, while still maintaining its annular form.

At some locations in the boiling region, particularly in the last stages of the test, repeated local dryouts were recorded, lasting for up to about half the period of the 5 Hz pulsations. Because of this short time interval, increases in temperature of only 5 to 10 °C were recorded, so that overall cooling capability was not hazarded. It is hoped that further tests may go further to define the limits of coolability.

To consider the implications of the results for the SNR 300, it can be expected that even under drastically reduced flow conditions, cooling in the region behind a plane blockage can be sustained even with intensive local boiling. There are no indications that the total flow through the fuel element is adversely influenced by boiling - i.e. there are no signs of flow instability developing. The influence of higher reactor pressure may be expected to be advantageous in this respect, although its effect on the incidence of dryout is less clear. Furthermore, the ability of reactor fuel pins to withstand the very severe temperature fluctuations to which they may be subjected must be queried.

SUMMARY AND CONCLUSIONS

Single Phase

- The normalised temperature fields obtained have essentially the same form and magnitude in all single-phase experiments as in water.
- A principal feature of the results is the clear annular form of the peak temperature region. In the centre of the annulus temperatures are lower.
- The rearmost stagnation point seems to be between 120 and 140 mm behind the blockage.
- The study of the temperature fluctuations in the wake region shows that there were no large-scale instabilities in the pattern of recirculating flow.

Two Phase

- The overall behaviour corresponds closely to oscillatory single bubble boiling although detailed evaluation shows the boiling involved many co-existent bubbles and considerable liquid entrainment in the boiling region.
- The boiling region is annular shaped.
- Boiling is confined to this annulus. Intensive reverse flow in the core of the wake remains developed, and corresponds closely to the single-phase behaviour.
- The vapour expands preferentially in the axial direction.

- Although repeated short term local dryouts were recorded the overall cooling capability was not interfered with.

Finally it can be expected that even under drastically reduced flow conditions, cooling in the region behind a plane blockage can be sustained even with intensive local boiling.

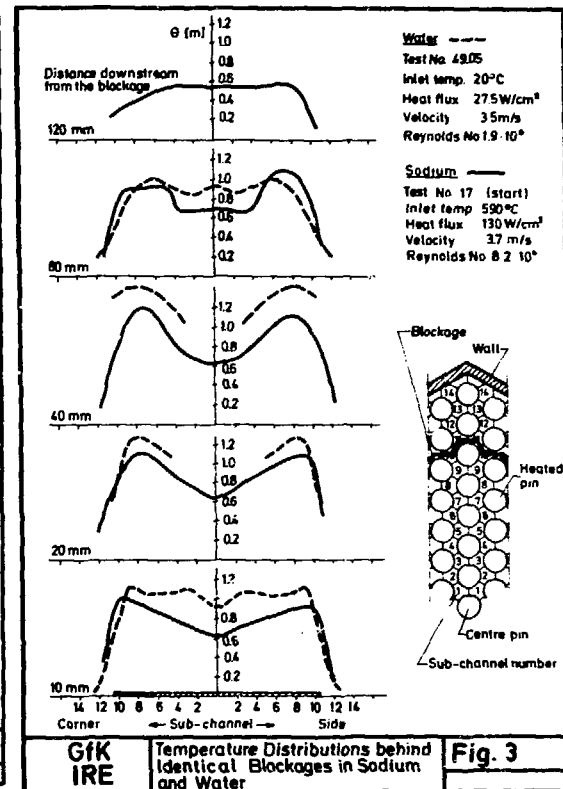
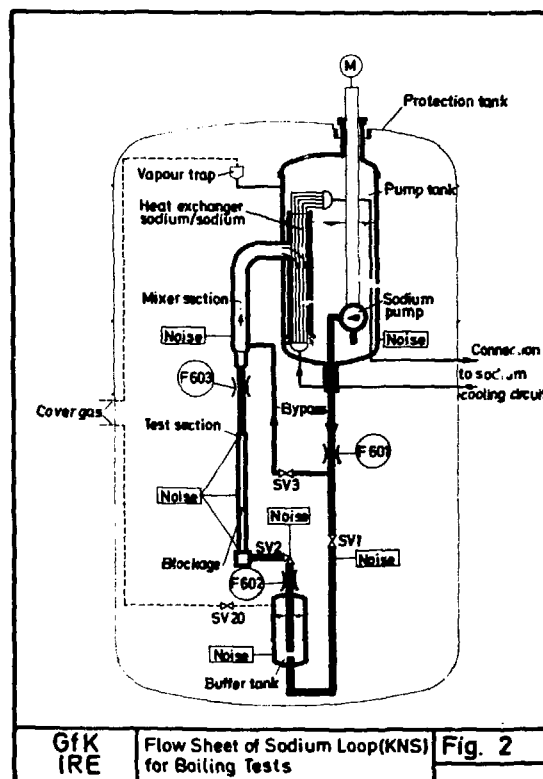
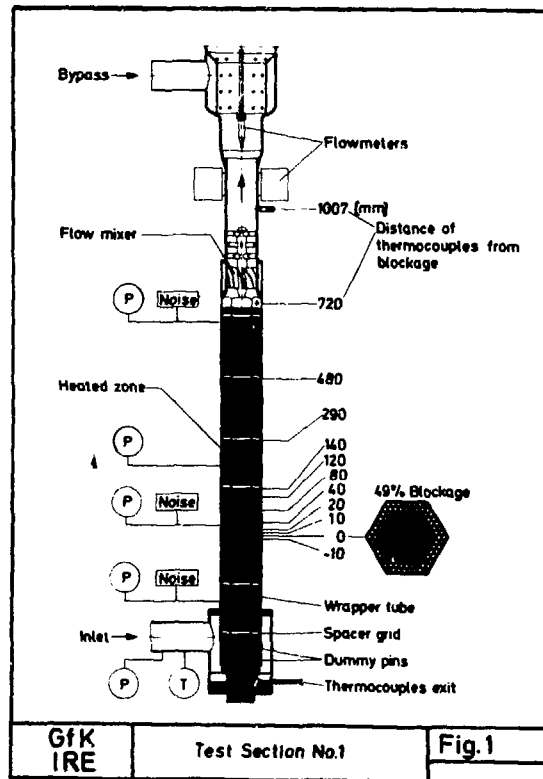
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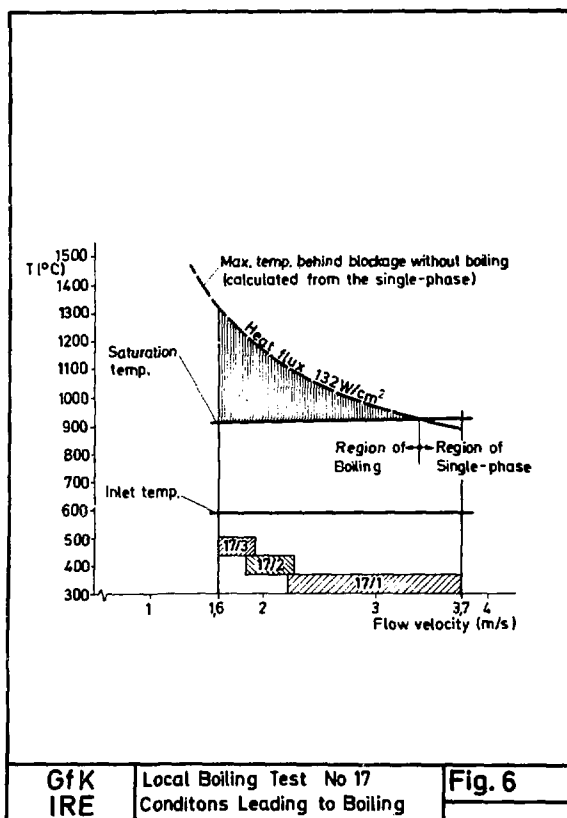
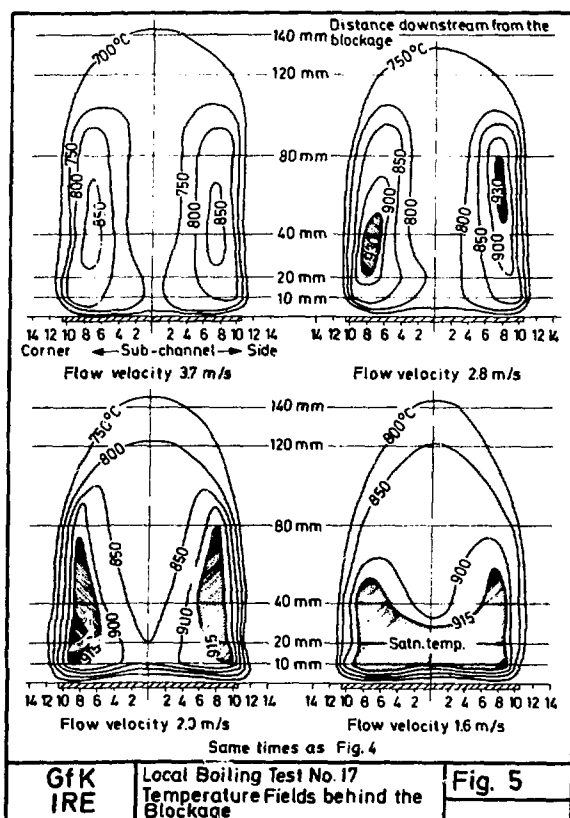
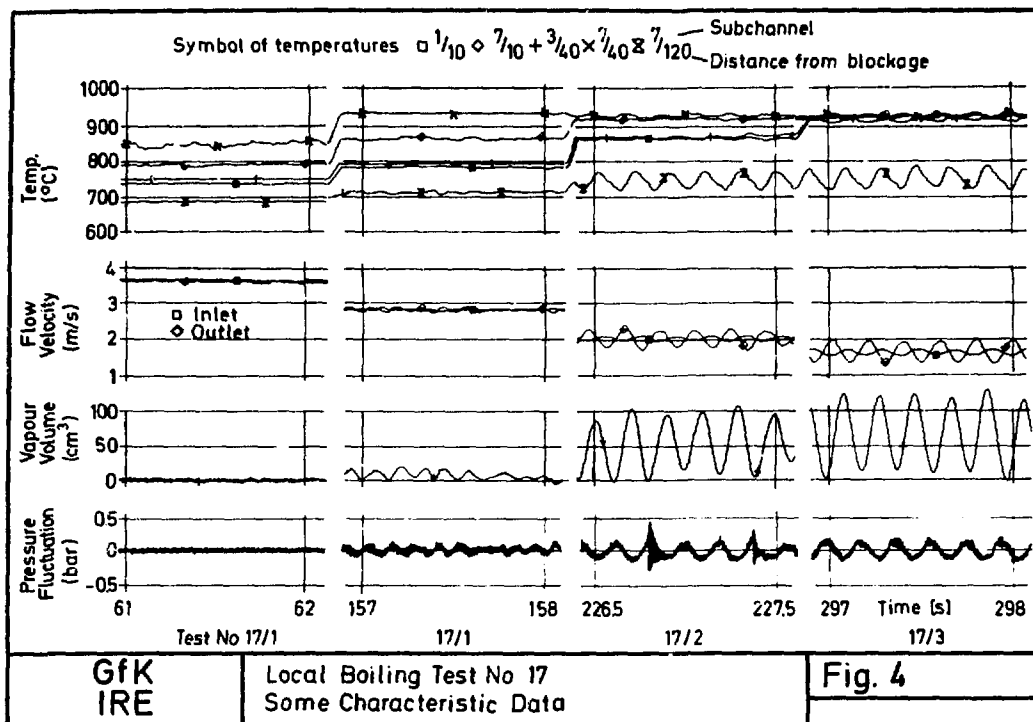
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Thermal-Hydraulic Correlations of a Six-Channel Blockage
in a Sodium-Cooled Simulated LMFBR Fuel Assembly*

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ABSTRACT

The temperature measurements in the wake behind a six-channel internal blockage in a simulated CRBR fuel assembly have been studied and analyzed. The coolant residence time in the wake and the mass exchange rate between the wake and the adjacent free stream are calculated using a simple physical model. Correlations are obtained for the average coolant temperature rise in the wake, the maximum coolant temperature rise in the wake, the maximum (inner) cladding temperature and the mean Nusselt number in the wake with Reynolds numbers in the range of $10^4 \leq Re \leq 10^5$ and the power per pin in the range of 16 to 33 kW/m. The results can be easily applied to predict the temperature rises in the wake behind a six-channel blockage located anywhere in the fuel assemblies of the CRBR and the FFTF, providing the blockage is located two rows or more away from the duct wall and has a negligible heat generation rate. Furthermore, it should be pointed out that no sodium boiling nor pin failure occurred in the tests and therefore, the reactor can still be operated safely under this kind of blockage condition.

INTRODUCTION

Channel blockage in the fuel assemblies of the liquid-metal fast breeder reactor (LMFBR) can be caused by the swelling and bending of pins, or the lodging of debris carried in the sodium. Its occurrence is possible during the life time of a commercial plant since there are a very large number of fuel pins in the reactor core, on the order of 10^4 to 10^5 . In order to assess the blockage effect upon the safety and the design of the LMFBR fuel subassemblies, experiments have been carried out at the Thermal-Hydraulic Out-of-Reactor Safety (THORS) facility, formerly the Fuel Failure Mockup (FFM) facility, at Oak Ridge National Laboratory.

The THORS bundle 3A[1] is a 19-pin, electrically-heated and sodium-cooled bundle in which the central six channels are blocked with a stainless-steel plate. The same pin diameter, pin pitch and wire-wrap spacing were used as those in the fuel assemblies of the Clinch River Breeder Reactor (CRBR) and the

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Fast Flux Test Facility (FFTF). A series of tests has been performed at various heater powers and sodium flows to simulate a six-channel internal blockage in these LMFBR fuel assemblies under design conditions. Temperature measurements were obtained at axial and azimuthal locations in the wake (specifically, the recirculating zone) behind the blockage.

TEST SECTION

The test section of THORS bundle 3A is shown in Fig. 1. The bundle was inserted from the bottom of the test section with the free ends of the heaters facing upward. A thermocouple rake, entering from the opposite end of the test section, was used to monitor sodium exit temperatures. Sodium flowed upward and exited near the top of the test section. A stainless-steel plate of 6.35 mm (0.25 in.) in thickness was located axially between 381 mm (15 in.) and 387 mm (15.25 in.) above the start of the heated section which has a heated length of 533 mm (21 in.).

Figure 2 shows the THORS flow diagram in which the test section is located on the left.

Figure 3 shows the instrumentation layout and the blockage location. The large circles represent the heaters that simulate the fuel pins. The number in the center of each pin is the identification number of that pin. Each pin has an outside diameter of 5.84 mm (0.230 in.) and is wrapped by a wire with a diameter of 1.42 mm (0.056 in.) and a helical pitch of 305 mm (12 in.). The wire-wrap spacers are shown in Fig. 3 in the smaller circles. The number in each of these smaller circles indicates that a thermocouple junction is located at so many "inches" above the start of the heated section and at the wire center. Exceptions are for those circles containing two pairs of black dots which represent the grounded-junction thermocouples at the azimuthal locations shown — one near the pin and the other on the opposite side. The flow channels are identified by the numbers in the triangles between three adjacent pins. The small circles with interior crosses indicate channels that were monitored by exit thermocouples. The fuel-pin-simulator heaters have thermal elements attached to the inner surface of the cladding as indicated by the dots labeled A, B, C, etc., inside the large circles. The ends of these thermal elements were grounded to the inner cladding at 15° azimuthal intervals and at 6.35-mm (0.25-in.) axial intervals; thus the junction formed by two thermal elements measures an average temperature along the spiral path at the inner cladding between the two junctions. Notice that the thermal elements in pin 1, for example, measure temperatures from 381 to 406 mm (15 to 16 in.) above the start of the heated section in 6.35-mm (0.25-in.) increments.

Nine tests have been performed[1] at heater powers in the range of 16 to 33 kW/m (5 to 10 kW/ft) with the mean coolant velocity in the range of 4 to 8 m/sec (13 to 26 ft/sec). All thermocouples were isothermally calibrated before each test, and all tests can be well reproduced.

ANALYSIS

Figure 4 shows a blockage located internally in a fuel assembly, where a is the blockage diameter and b is its location downstream from the start of the heated section ($x = 0$). u_m is the mean coolant velocity in the assembly and T_{in} is the coolant inlet temperature. M is the coolant mass in the wake and L is the axial wake length.

Mass Exchange Rate

Assuming that the coolant in the wake behind the blockage is well mixed and that for turbulent flow the energy transfer between the wake and the adjacent free stream is primarily caused by the mass exchange between them, the following energy conservation equation is obtained.

$$Q = \sum_{i=1}^N \int_b^{b+L} q_i(x) dx = \dot{m} C_p (T_{WK} - T_{\infty}) \quad (1)$$

where Q = total power generated by fuel pins in the wake
 N = number of fuel pins in the wake
 b = location of the downstream side of the blockage
 L = axial wake length
 q_i = heat generation rate per unit length of i th fuel pin
 C_p = coolant specific heat at constant pressure
 T_{WK} = average coolant temperature in the wake
 T_{∞} = average free stream temperature surrounding the wake
 $\dot{m}$ = mass exchange rate between the coolant in the wake and the surrounding free stream

T_{WK} was measured in the tests and the wake length, L , has been estimated to be 44.5 mm (1.75 in.) from the THORS Water Mockup tests[2] by flow visualization using air bubble injection. T_{∞} is taken to be the bulk mean coolant temperature at the axial midplane of the wake (at $x = b + L/2$). The mass exchange rate between the coolant in the wake and the adjacent free stream, $\dot{m}$, is thus calculated from equation (1).

Residence Time

The residence time of the coolant in the wake, τ , is defined[3] as the ratio of the coolant mass in the wake to the mass exchange rate.

$$\tau = M / \dot{m} \quad (2)$$

where M = coolant mass in the wake = $\rho_{WK} V_{WK}$
 ρ_{WK} = coolant density in the wake
 V_{WK} = volume occupied by the coolant in the wake

Temperatures in the Wake

Assuming that the average coolant temperature in the wake, T_{WK} , is approximated by that of the surface of the wire wrap nearest the channel center, it is calculated as the arithmetic mean of the temperature readings from those thermocouples inside the wire-wrap spacers, with adjustment being made to take into account the temperature gradient across the wire.

$$T_{WK} = \frac{1}{4} \left\{ T_{15,sc2} + T_{15,sc4} + T_{17,sc3} + \frac{1}{2} (T_{17A,sc1} + T_{17B,sc1}) \right\} - \frac{1}{2} (T_{17A,sc1} - T_{17B,sc1}) \quad (3)$$

where $T_{15,sc2}$ = temperature of the wire-wrap thermocouple located at $x = 15$ in. inside channel 2 as shown in Fig. 3
 $T_{15,sc4}$ = temperature of the wire-wrap thermocouple located at $x = 15$ in. inside channel 4
 $T_{17,sc3}$ = temperature of the wire-wrap thermocouple located at $x = 17$ in. inside channel 3
 $T_{17A,sc1}$ = temperature of the grounded wire-wrap thermocouple located at $x = 17$ in. inside channel 1 and adjacent to pin 2
 $T_{17B,sc1}$ = temperature of the grounded wire-wrap thermocouple opposite to the one for $T_{17A,sc1}$

The maximum cladding temperature of the fuel pins in the wake, $T_{MAX CLAD}$, is located at the inner surface of the cladding and is determined from the maximum temperature of those thermocouples inside the fuel pins behind the blockage as shown in Fig. 3. It is worthwhile to point out that there are over twenty inner-cladding thermocouples at various locations in the wake.

The maximum coolant temperature in the wake, $T_{MAX WK}$, is the wall temperature of the pin where $T_{MAX CLAD}$ is located. Based on radial heat conduction in a cylinder, the following equation is obtained.

$$T_{MAX WK} = T_{MAX CLAD} - q \ln(d/d_i) / (2\pi K_c) \quad (4)$$

where q = heat generation rate per unit length of fuel pin
 d = outer diameter of fuel pin
 d_i = inner diameter of fuel pin
 K_c = cladding thermal conductivity

Similarly, the average wall temperature in the wake, T_w , is calculated from the average inner cladding temperature $T_{AVG INNER CLAD}$ which is the mean value of those thermocouples inside fuel pins in the wake.

$$T_w = T_{AVG INNER CLAD} - q \ln(d/d_i) / (2\pi K_c) \quad (5)$$

Nusselt Number in the Wake

The mean Nusselt number for heat transfer in the wake is defined by

$$Nu_{WK} = Q D_h / [KA(T_w - T_\infty)] \quad (6)$$

where D_h = hydraulic diameter of internal channel in fuel assemblies
 $D_h = 3.03$ mm (0.1194 in.)
 K_h = thermal conductivity of the coolant in the wake
 A = total surface area of fuel pins in the wake

The following values are used in the analysis: $A = 2450$ mm² (3.79 in.²), $a = 12.1$ mm (0.476 in.), $b = 387$ mm (15.25 in.), $d = 5.84$ mm (0.230 in.), $d_i = 4.98$ mm (0.196 in.), $D_h = 3.03$ mm (0.1194 in.), $L = 44.5$ mm (1.75 in.), $N = 13$, $V_{WK} = 2310$ mm³ (0.141 in.³).

RESULTS

The mass exchange rate between the coolant in the wake and the adjacent free stream is shown in Fig. 5 as a function of the mean coolant velocity u . With increasing velocity, there is an increase in the turbulent mixing and thus an increase in the mass exchange rate as expected. Figure 6 shows that the

residence time decreases as the coolant velocity increases. The residence time obtained by Kirsch and Schleisiek[3] for the blockage in an annulus also shows the same trend and has the same order of magnitude.

The dimensionless residence time, τ_m/a , is shown in Fig. 7 for those obtained in the present study as well as those obtained by D. G. Thomas[2] using the salt concentration data of the THORS Water Mockup tests. The agreement between the sodium and the water results is good. The sodium and the water results of Kirsch and Schleisiek[2-3] for the flow in a blocked annulus simulating SNR fuel assemblies are also shown in Fig. 7. The agreement between their results and the THORS results is surprisingly good. A straight line can be used to fit the THORS results for a six-channel internal blockage in a simulated fuel assembly of the CRBR and the FFTF. This gives:

$$\tau_m/a = 12 \text{ for } 4 \times 10^4 \leq Re_B = u_m a/\nu \leq 4 \times 10^5 \quad (7)$$

where a = blockage diameter = 12.1 mm (0.476 in.)

Re_B = blockage Reynolds number

ν = coolant kinematic viscosity

Equation (7) can be rewritten in terms of the channel hydraulic diameter D_h and the corresponding Reynolds number:

$$\tau_m/D_h = 4.8 \text{ for } 10^4 \leq Re = u_m D_h/\nu \leq 10^5 \quad (8)$$

Substituting equations (1) and (2) into equation (7) yields the average coolant temperature in the wake behind a six-channel blockage.

$$T_{WK} = T_\infty + 12 aQ/(M C_p u_m) \quad (9)$$

It should be noted that the THORS water result at $Re_B = u_m a/\nu = 2 \times 10^4$ falls below the straight line as shown in Fig. 7. This is probably because the Reynolds number, $Re = u_m D_h/\nu$, is only around 5000 for which the flow in the pin bundle is in the transition regime and the molecular transport effect may not be negligible.

Figure 8 shows the ratios of the maximum wake temperature rise to the average wake temperature rise above the coolant inlet temperature as a function of the Reynolds number and the power generation rate per unit length per pin. Figure 9 shows the maximum wake temperature rise above the bulk mean coolant temperature. Note that three data points at lower Reynolds numbers from other THORS tests have been added to these two figures. The maximum (inner) cladding temperature rise above the bulk mean temperature is presented in Fig. 10 as a function of the Reynolds number.

The Nusselt number for sodium heat transfer in the wake behind the six-channel internal blockage is shown in Fig. 11 as a function of the Reynolds number. Its value increases with the Reynolds number, and is in the same order of magnitude with the experimental result obtained by Daigo et al.[4] for a six-channel blockage in a 7-pin, sodium-cooled bundle.

SUMMARY AND CONCLUSIONS

Thermal-hydraulic correlations have been obtained from the THORS experimental results for a six-channel internal blockage in a simulated LMFBR fuel assembly. These correlations can be easily applied to obtain the following information for a six-channel non-heat-generating blockage located anywhere in the fuel assemblies of the CRBR and the FFTF, providing the blockage is two rows or more away from the assembly duct wall:

- (1) The average sodium temperature in the wake can be calculated from equation (9).
- (2) The maximum sodium temperature in the wake is given by Fig. 8 and Fig. 9.
- (3) The maximum cladding temperature in the wake can be estimated from Fig. 10.
- (4) The Nusselt number for sodium heat transfer in the wake is given in Fig. 11.
- (5) If the blockage is made of impermeable heat-generating material, the mass exchange rate shown in Fig. 5 is still valid. This is because the mass exchange between the wake and the adjacent free stream is a hydrodynamic phenomenon.
- (6) It should be noted that no sodium boiling nor pin failure occurred in the present tests. Therefore, this kind of blockage will pose no problem to the reactor safety under design conditions.

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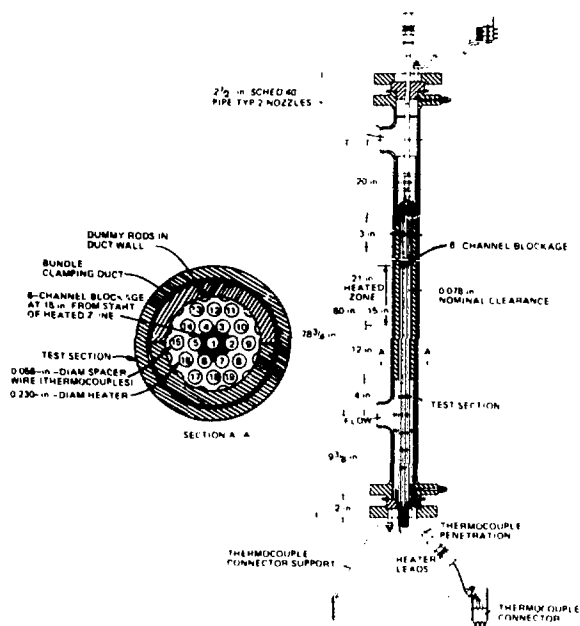


Fig. 1 Test section of the Thermal-Hydraulic Out-of-Reactor Safety (THORS) facility bundle 3A.

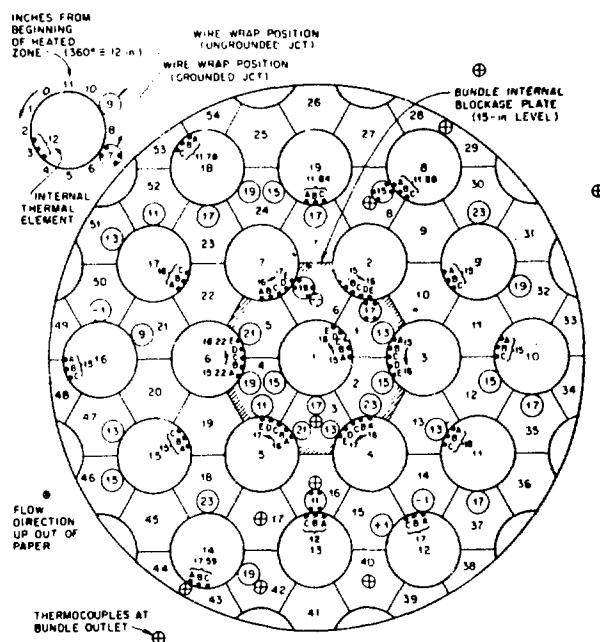


Fig. 3 Spacer wire and internal thermal locations for THORS bundle 3A.

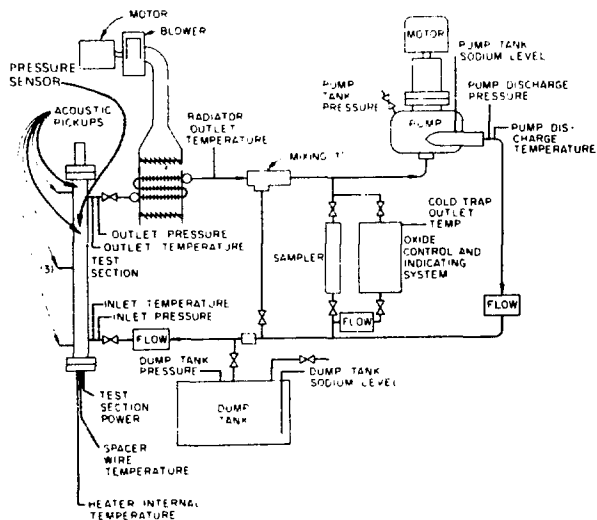


Fig. 2 THORS flow diagram.

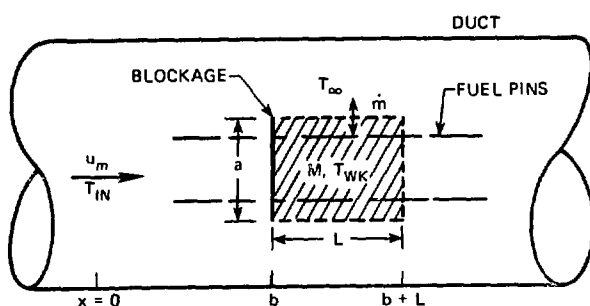


Fig. 4 Internal blockage in a fuel assembly.

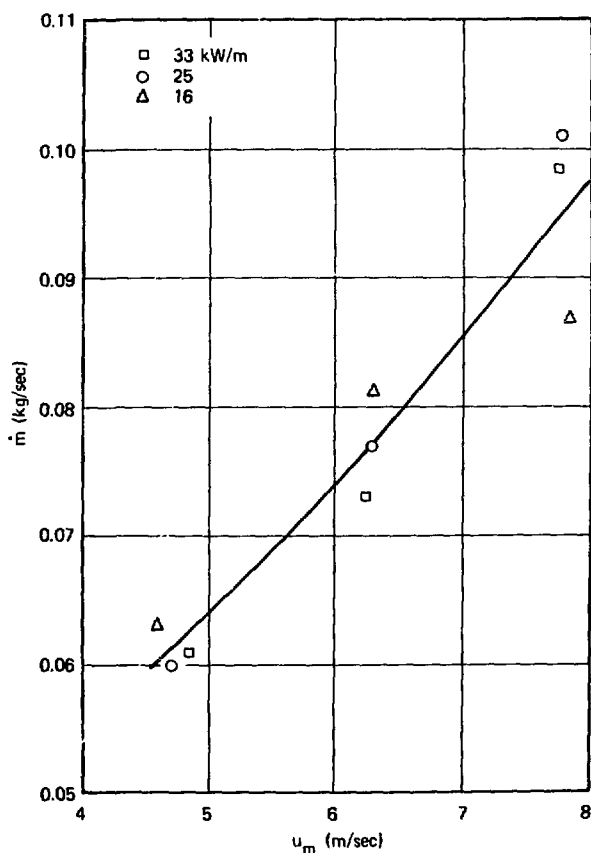


Fig. 5 Mass exchange rate for the wake behind a six-channel blockage in a simulated CRBR fuel assembly.

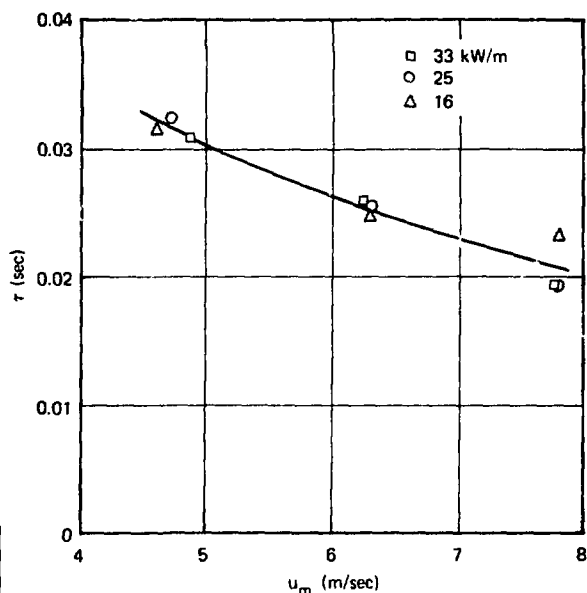


Fig. 6 Residence time for the wake behind a six-channel internal blockage in a simulated CRBR fuel assembly.

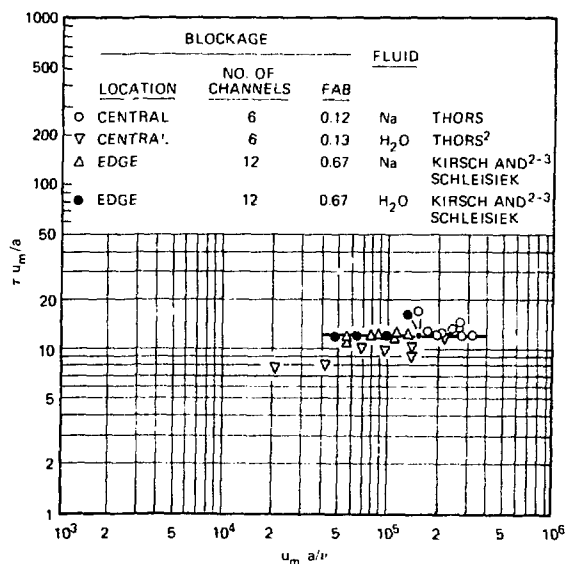


Fig. 7 Dimensionless fluid residence time behind blockages in simulated fuel assemblies (FAB = fraction of flow area blocked).

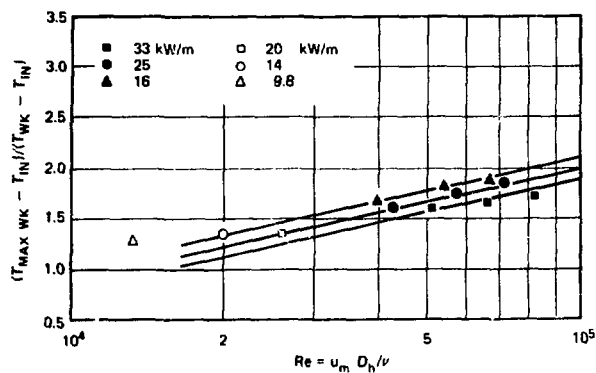


Fig. 8 Ratios of the maximum wake temperature rise to the average wake temperature rise.

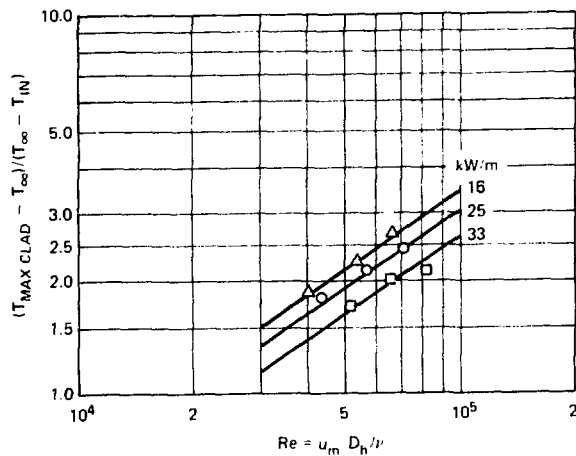


Fig. 10 The maximum (inner) cladding temperature in the wake.

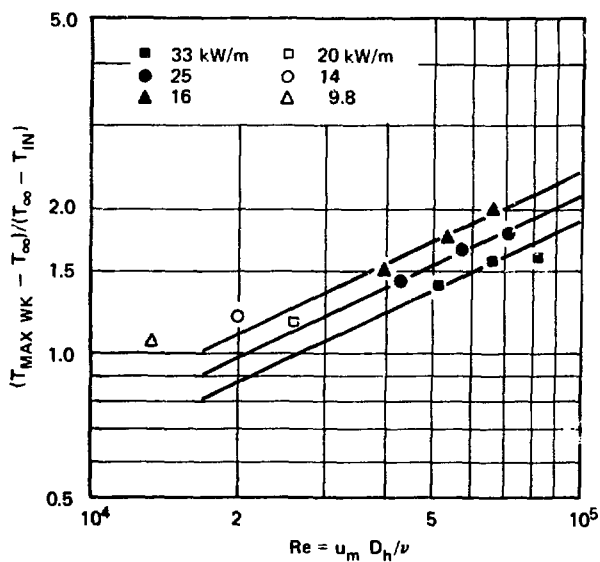


Fig. 9 The maximum wake temperature rise above the bulk mean temperature.

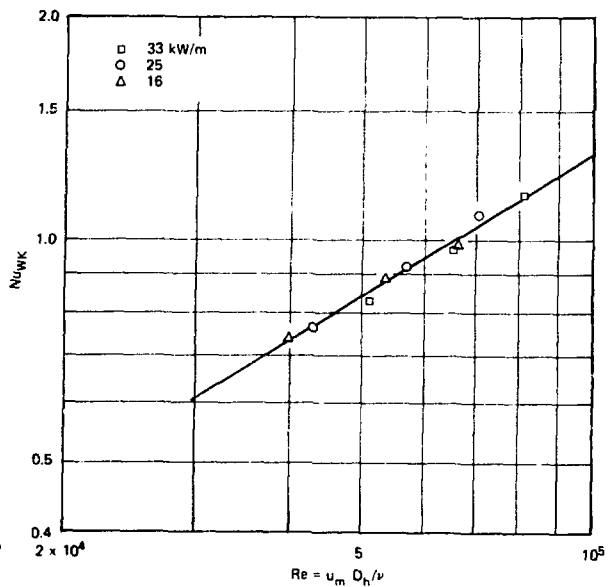


Fig. 11 Nusselt number in the wake behind a six-channel internal blockage.

QUASI-STEADY STATE BOILING DOWNSTREAM OF A SIX-CHANNEL CENTRAL BLOCKAGE IN A 19-ROD SIMULATED LMFBR SUBASSEMBLY*

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ABSTRACT

Results of quasi-steady state sodium boiling tests in a centrally blocked 19-rod simulated LMFBR subassembly have been analyzed. The results indicate that, during two quasi-steady state boiling periods (13 sec and 27 sec), there was no evidence of radial propagation of the local boiling zone from the 6-channel blockage wake into the surrounding, unblocked flow channels. All thermocouple temperatures in the region downstream of the blockage were stable during the final 3 sec and 13 sec of these periods, respectively.

An idealized extrapolation of these results to a 217-pin LMFBR subassembly leads to the conclusion that a 6-channel blockage in a 217 pin assembly, having the same boiling intensity as experienced in the boiling tests, does not cause the local boiling zone to propagate radially into the unblocked channels for the time periods investigated.

The experimental results also showed that, during these two periods, there was no evidence of clad dryout in the local boiling zone downstream of the blockage.

INTRODUCTION

An understanding of the effects of in-core blockages is of paramount importance in LMFBR safety. These blockages can result from the accumulation of debris circulated in the primary sodium or from swelling or bowing of the fuel pins. High temperatures occurring downstream from a blockage could under certain adverse conditions lead to localized boiling. The consequences of this local boiling should be known.

Specifically, it should be determined if,

- (1) the local boiling zone in a blockage wake spreads radially or axially during quasi-steady conditions of flow and power (stable boiling), or
- (2) local dryout occurs in the local boiling zone in the wake during prolonged periods of constant flow and power

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The subsequent analysis addresses these two phenomena by examining the experimental results of boiling downstream of a 6-channel central non-heat generating blockage in a 19-pin electrically-heated simulated LMFBR subassembly (approximately 12.5% of the total flow area is thereby blocked). The extrapolation of the 19-rod (Bundle 3B) experimental results to 217-pin LMFBR subassemblies will be discussed in relation to non-prototypicalities inherent in the present configuration of the Oak Ridge National Laboratory Thermal-Hydraulic Out-of-Reactor Safety (THORS) Facility [formerly the Fuel Failure Mockup (FFM) Facility]. Neither Bundle 3B nor the facility as used for these tests was designed for sustained sodium boiling.

A detailed description of the facility and the data obtained in this test program are given in Reference 1.

DESCRIPTION OF TEST FACILITY AND ROD BUNDLE 3B

The THORS Facility is an engineering-scale sodium test loop in which 19-rod bundles, simulating LMFBR subassembly segments, have been subjected to thermal-hydraulic testing during normal and abnormal LMFBR conditions. The LMFBR fuel pins are simulated by electric cartridge heaters having the same outside diameter as a reactor fuel pin; each fuel pin simulator can contain up to 4 grounded thermocouple junctions attached to the inner surface of the cladding. Helically wrapped wire spacers, containing either two grounded or two ungrounded thermocouple junctions, space the fuel pin simulators as in reactor subassemblies.

Test Bundle 3B (the third bundle tested in the facility) is shown in Fig. 1. An attenuator, a perforated pipe which was slipped over the thermocouple rake support to within 102 mm (4 in.) of the outlet end of the bundle, was installed to inject "cold" sodium (at the bundle inlet temperature) into the relatively hot sodium exiting from the bundle to protect the test section exit piping from excessive temperatures.

Bundle 3B consists of 19 electrically-heated rods in a scalloped duct. The rods have a diameter of 5.84 mm (0.230 in.) and are spaced by 1.42 mm (0.056 in.)-diameter helical wire wraps on a 305 mm (12.0 in.) pitch. The circular scalloped duct encloses the 19 rods and simulates a partial hydrodynamic section of an infinite rod array by having unheated rod segments (with helical wire wrap) attached to the duct wall. Sodium, flowing vertically upward through the bundle, first encounters a 406 mm (16.0 in.)-long unheated rod section, then a 533 mm (21.0 in.)-long uniformly heated section, and finally a 76 mm (3.0 in.) long unheated rod section before exiting vertically from the rod bundle. The 6.4 mm (0.25 in.)-thick non-heat generating, stainless steel blockage plate is located 381 mm (15.0 in.) downstream from the start of the 533 mm (21.0 in.) long heated section and blocks the 6 central flow channels (12.5% of the flow area). All bundle 3B heaters have a rated power of 33 kW/m (10 kW/ft) and were not specifically designed for boiling conditions.

The thermocouple instrumentation layout for Bundle 3B is shown in Fig. 2. Thermocouple junctions which were found to give unreliable responses prior to operation of Bundle 3B are crossed out in Fig. 2 and were not used to record experimental data.

TESTS CONDUCTED AND TEST RESULTS

Seven test runs were conducted in an effort to determine the consequences of local boiling downstream of the blockage. Six of the seven tests (test 3, runs 101a, b, c, d, e, f) consisted of decreasing the test section flow at constant heater power just enough to initiate sodium boiling at approximately 955°C (1750°F), after which the flow was kept constant. The resulting quasi-steady state boiling was maintained for a period which hopefully would not endanger the integrity in the fuel pin simulators. In test 4, at the end of the scheduled

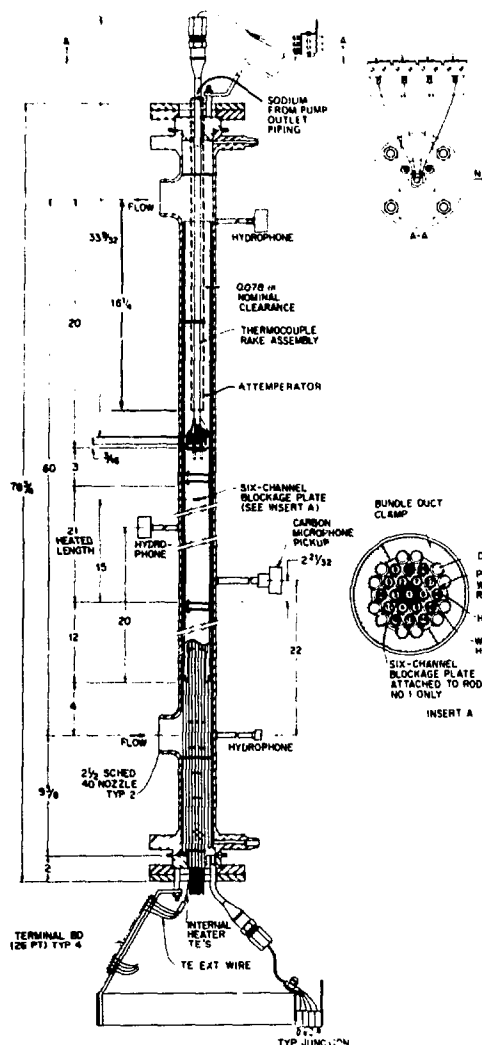


Fig. 1. Test section for Bundle 3E.

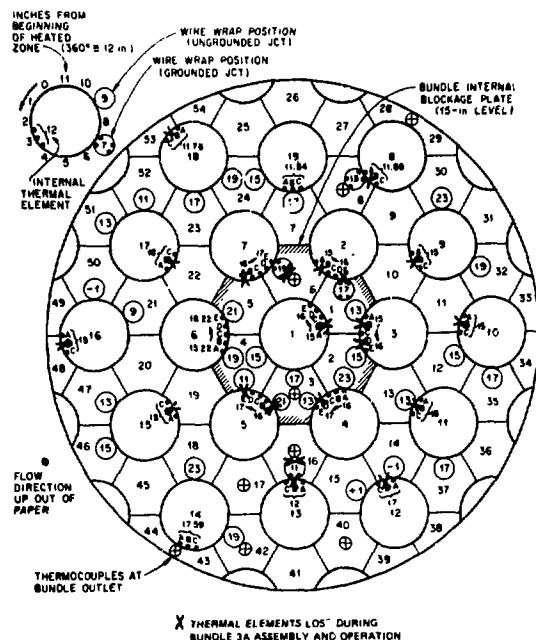


Fig. 2. Thermocouple instrumentation for Bundle 3B.

test program, once boiling had been established by decreasing the flow, the heater power (only 18 heaters powered) was varied in order to suppress and initiate periods of quasi-steady state boiling. Figures 3 and 4 show typical temperature, flow and heater power responses of test 3, run 101c and test 4. These two runs were chosen for analysis since they contained the longest, most intense (lowest flow-to-power ratio) quasi-steady state boil-

ing periods (13 and 27 seconds, respectively) and because boiling conditions were most easily recognized from their thermocouple responses.

The onset of boiling was determined during test operation by monitoring

- (1) all bundle thermocouple temperatures on a continuous visual display,
- (2) acoustic energy detector signals on oscilloscopes, and
- (3) the amplified audio signal from an electromagnetic microphone attached to the rod bundle housing.

In order to establish the extent of vapor generation (or the subcooled boundaries of the vapor) from the experimental data, several criteria were used to determine if a thermocouple response indicated sodium vapor in the thermocouple vicinity during the quasi-steady state boiling periods. Thus,

- (a) if the temperature of a thermocouple is independent of a change in flow and/or power, it is assumed that the forced convection nucleate boiling regime (associated with small rod wall temperature changes for increasing heat flux) has been entered. In Fig. 3, note the difference in thermocouple responses of wire wrap thermocouples 0621U and 1113U during the three successively lower flow periods (50-60 sec,

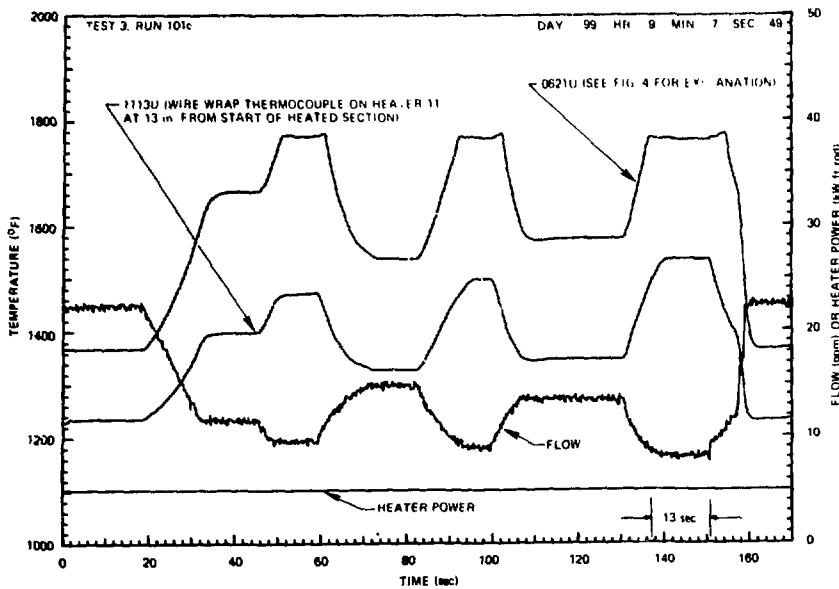


Fig. 3. Responses of electromagnetic flowmeter, average heater power and two thermocouples for test 3, run 101c.

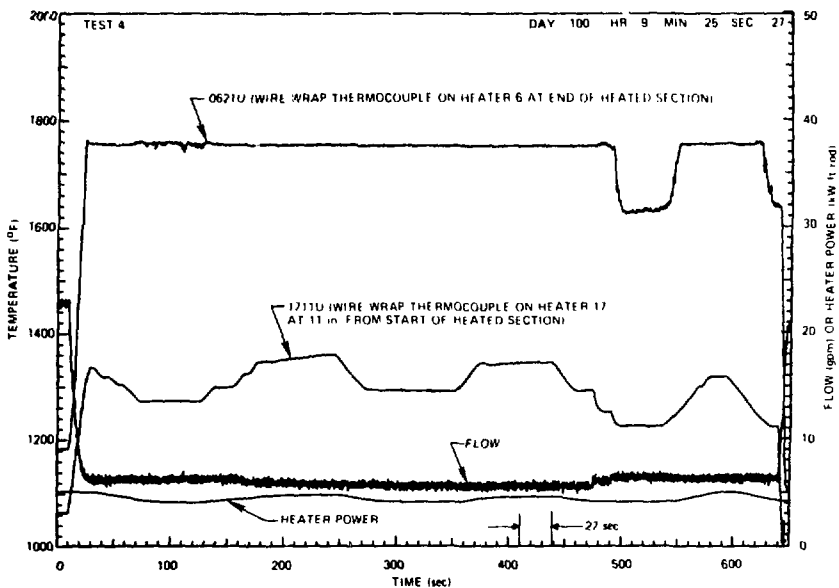


Fig. 4. Responses of flowmeter, average heater power, and two thermocouples for test 4.

94-100 sec, and 138-151 sec); 0621U is considered to indicate the presence of sodium vapor.

- (b) as an extension of criterion (a), if the temperature of a thermocouple stabilized prior to the time that the decreasing flow has reached its minimum, the temperature is also considered independent of flow and indicates boiling. An example is thermocouple 0621U in Fig. 3; it stabilizes in temperature at approximately 136 sec, two seconds prior to the flow reaching its lowest point at 138 sec.
- (c) if the temperature of a thermocouple reaches its highest plateau after the flow has reached its minimum value and criterion (a) does not apply, the thermocouple temperature is compared to a calculated single-phase sodium saturation temperature. If the thermocouple temperature is well below this calculated saturation temperature, it is

designated as a "non-boiling" thermocouple. If the thermocouple temperature is approximately equal to or above the saturation temperature and it reaches its final temperature plateau prior to the time that any "non-boiling" thermocouple does, sodium vapor in the vicinity is considered established. Otherwise, no conclusion as to vapor in the vicinity can be made.

DISCUSSION OF TEST RESULTS

Axial Propagation of Local Boiling Zone

The test results in Figs. 5 and 6 as well as Table I are presented in an effort to determine local boiling zone propagation. It must be pointed out, however, that axial propagation during quasi-steady state boiling could not be investigated with Bundle 3B because of two non-prototypicalities in the THORS test facility. In particular, *upstream propagation* of the void during quasi-steady state boiling periods was not likely because the test section flow was not significantly reduced by an increase in pressure drop as in LMFBR subassemblies; this was due to the fact that flow was being throttled at the test section inlet and the major portion of the loop pressure loss occurred at that point. *Downstream propagation* of the local boiling zone was limited by an attenuator, which injected "cold" sodium (at test section inlet temperature) into the two-phase sodium flow at approximately 305 mm (12 in.) downstream of the blockage (see Fig. 1); this resulted in essentially all sodium vapor being condensed. The large temperature fluctuations of thermocouples at the farthest measuring point downstream of the blockage (see exit rake thermocouples in Figs. 5 and 6) reflect the flow agitation caused by this vapor collapse.

Radial Propagation of the Local Boiling Zone

The results of Table I (test 3, run 101c) show that during the 13-sec quasi-steady state period, definite evidence of sodium vapor was only found in the blocked region [channels 1 to 6 in Fig. 2]. Two wire wrap thermocouples in the unblocked channels [1917U at the 432 mm (17 in.) level on heater 19 and 1819U at the 483 mm (19 in.) level on heater 18] showed temperatures near the sodium saturation temperature, but none of the criteria for evidence of vapor generation was applicable. Despite the fact that these two thermocouples are close to indicating vapor in their vicinity, the major point to note is that all thermocouples in the unblocked channels had stabilized in temperature at least 3 sec prior to the end of the 13 sec quasi-steady state boiling period (150.5 sec into the run). The stable temperature conditions reached in the unblocked channels after 10 sec of quasi-steady state boiling together with the large amount of subcooling in the outermost flow channels [$T_{\text{sat}} - T_{\text{minimum measured}}$, (see Fig. 5)] still evidenced at the end of the 13-sec period, indicate no radial propagation of the local boiling zone from the blocked to the unblocked channels for the 13-sec quasi-steady state period analyzed.

The results of test 4 (Table I) with 18 heaters powered (pin 7 heating element had failed during the earlier boiling tests) indicated essentially the same results as test 3, run 101c except for the two wire-wrap thermocouples at the unheated, unblocked 584 mm (23 in.) level. This difference is primarily due to a lower flow/power ratio in Test 4; as a result, test 4 had 20% less subcooling [$(T_{\text{sat}} - T_{\text{bulk}})_{\text{single-phase}}$] at this level (see Figs. 5 and 6) than test 3, run 101c. In effect, vapor swept out from the blocked channels to the unblocked channels at this 23 in. level does not encounter sodium at temperatures low enough to condense completely. Therefore, radial propagation of the vapor zone in Bundle 3B could have occurred to a limited extent in the unheated section of the bundle at 205 mm (8.0 in.) downstream of the blockage. However, in the first 103 mm (4.0 in.) downstream of the blockage (which includes the blockage

Table I. Quasi-steady state boiling results of Bundle 3B

Test 3, Run 101C						Test 4					
Test conditions: Flow: 8.2 gpm [15% nominal flow] Heater power: 5.0 kW/ft Inlet temperature: 1040°F Time of quasi-steady state boiling period: 137.6-150.5 sec						Test conditions: Flow: 5.6 gpm [10% nominal flow] Heater power: 4.5 kW/ft Inlet temperature: 910°F Time of quasi-steady state boiling period: 410.2-456.8 sec					
Distance from start of heated section mm (in.)	Thermocouple ^a	Measured temperature at end of period [°C (°F)]	Estimated surface temperature at end of period [°C (°F)]	Evidence of vapor generation during period	Time of temperature stabilization ^b for period (sec)	Measured temperature at end of period [°C (°F)]	Estimated surface temperature at end of period [°C (°F)]	Evidence of vapor generation during period	Time of temperature stabilization ^b for period (sec)		
Blocked Region											
381 (15.0)	0115U	924 (1695)	927 (1700)	No	141.6	960 (1760)	960 (1760)	?	406.3		
381 (15.0)	0315U	910 (1670)	915 (1675)	No	141.9	954 (1750)	954 (1750)	?	383.9		
390 (15.35)	R0115AD	968 (1775)	954 (1750)	?	141.5	unreliable	-	-	-		
390 (15.35)	R0615AB	979 (1795)	966 (1770)	?	142.6	977 (1790)	963 (1765)	Yes	370.3		
391 (15.38)	R0215AD	935 (1715)	921 (1690)	?	141.3	957 (1755)	943 (1730)	?	407.8		
396 (15)	R0615BC	966 (1770)	952 (1745)	Yes	140.7	960 (1760)	946 (1735)	Yes	365.4		
397 (15.63)	R0215CB	977 (1790)	963 (1765)	Yes	138.2	unreliable	-	-	-		
397 (15.63)	R0116CD	977 (1790)	963 (1765)	Yes	136.4	977 (1790)	963 (1765)	Yes	366.3		
305 (15.85)	R0616CD	974 (1785)	960 (1760)	Yes	139.0	968 (1775)	954 (1750)	Yes	372.8		
403 (15.88)	R0116DE	977 (1790)	963 (1765)	Yes	138.1	unreliable	-	-	-		
409 (16.10)	R0616DE	974 (1785)	960 (1760)	Yes	139.5	971 (1780)	957 (1755)	Yes	370.4		
410 (16.13)	R0416AB	971 (1780)	957 (1755)	Yes	140.1	968 (1775)	954 (1750)	Yes	370.0		
416 (16.38)	R0716BC	982 (1800)	968 (1775)	Yes	140.1	unreliable	-	-	-		
416 (16.38)	R0416BC	971 (1780)	957 (1755)	Yes	138.8	966 (1770)	952 (1745)	Yes	367.5		
416 (16.38)	R0516BC	996 (1825)	982 (1800)	Yes	140.2	988 (1810)	974 (1785)	Yes	363.7		
422 (16.63)	R0417BE	974 (1785)	960 (1760)	Yes	138.3	968 (1775)	954 (1750)	Yes	351.2		
422 (16.63)	R0517CO	996 (1825)	982 (1800)	Yes	140.1	983 (1810)	974 (1785)	Yes	362.8		
422 (16.63)	R0717BE	991 (1815)	977 (1790)	Yes	139.0	unreliable	-	-	-		
432 (17.0)	0117U	963 (1765)	960 (1760)	Yes	138.8	957 (1755)	954 (1750)	Yes	351.4		
432 (17.0)	0217A	966 (1770)	963 (1765)	Yes	139.3	960 (1760)	957 (1755)	Yes	364.2		
432 (17.0)	0217B	963 (1765)	963 (1765)	Yes	138.3	957 (1755)	947 (1755)	Yes	351.2		
483 (19.0)	0619U	966 (1770)	963 (1765)	Yes	138.3	957 (1755)	954 (1750)	Yes	356.2		
483 (19.0)	0719A	966 (1770)	963 (1765)	Yes	137.6	954 (1750)	954 (1750)	Yes	361.2		
533 (21.0)	0521U	963 (1765)	960 (1760)	Yes	138.2	957 (1755)	954 (1750)	Yes	343		
553 (21.0)	0621U	966 (1760)	957 (1755)	Yes	136.3	951 (1750)	957 (1745)	Yes	374		
584 (23.0)	0423U	929 (1705)	929 (1705)	Yes	136.3	952 (1745)	952 (1745)	Yes	371.2		
Unblocked region											
330 (13.0)	0313U	857 (1575)	854 (1570)	No	140.5	899 (1650)	896 (1645)	No	432.5		
330 (13.0)	0413U	854 (1570)	852 (1565)	No	140.4	904 (1660)	902 (1655)	No	409.3		
330 (13.0)	1113U	835 (1535)	832 (1530)	No	140.7	868 (1595)	866 (1590)	No	426.3		
330 (13.0)	1513U	793 (1460)	791 (1455)	No	141.3	874 (1615)	821 (1510)	No	422.8		
330 (13.0)	1713U	749 (1380)	746 (1375)	No	143.9	732 (1350)	729 (1345)	No	427.6		
381 (15.0)	0815A	857 (1575)	860 (1580)	No	142.6	885 (1625)	885 (1625)	No	406.2		
381 (15.0)	0815B	860 (1580)	860 (1580)	No	141.7	885 (1625)	885 (1625)	No	410.6		
381 (15.0)	1015U	860 (1580)	863 (1585)	No	142.6	899 (1650)	898 (1650)	No	407.8		
381 (15.0)	1315U	774 (1425)	777 (1430)	No	145.3	793 (1460)	793 (1460)	No	409.0		
381 (15.0)	1915U	871 (1600)	874 (1605)	No	142.7	846 (1555)	846 (1555)	No	124.4		
432 (17.0)	1017U	849 (1560)	846 (1555)	No	142.8	938 (1720)	935 (1715)	No	400.2		
432 (17.0)	1117U	824 (1515)	821 (1510)	No	147.6	879 (1615)	877 (1610)	No	406.9		
432 (17.0)	1817U	893 (1640)	891 (1635)	No	145.1	863 (1585)	860 (1580)	No	408.8		
432 (17.0)	1917U	932 (1710)	929 (1705)	?	142.6	929 (1705)	927 (1700)	No	409.2		
447 (17.59)	R1418AB	821 (1510)	807 (1485)	No	145.4	unreliable	-	-	-		
447 (17.59)	R1418BC	829 (1525)	816 (1500)	No	145.6	unreliable	-	-	-		
483 (19.0)	0919U	868 (1595)	866 (1590)	No	143.1	938 (1720)	935 (1715)	No	401.9		
483 (19.0)	1419U	896 (1645)	893 (1640)	No	143.9	907 (1665)	904 (1660)	No	408.8		
483 (19.0)	1819U	960 (1760)	957 (1755)	?	141.8	949 (1740)	946 (1735)	?	399.2		
584 (23.0)	0923U	910 (1670)	910 (1670)	No	145.4	949 (1740)	949 (1740)	Yes	369.3		
584 (23.0)	1423U	854 (1570)	854 (1570)	No	145.5	949 (1740)	949 (1740)	Yes	382.8		

^aNumbers of wire-wrap thermocouples consist of four digits followed by a single letter; e.g., 0315U, 0217A, and 0217B. The first two digits have the heater number (see Figure 2) adjacent to the wire wrap, the second two digits give the axial location downstream of the start of the heated section, and the single letter stands for the physical location inside the wire wrap [i.e., U indicates an ungrounded type in the center of the wire wrap, A indicates a grounded thermocouple adjacent to the heater, and B indicates a grounded type adjacent to sodium]. Numbers of grounded heater internal thermocouples start with the letter R, and are followed by four digits for the heater number and the axial location downstream from the start of the heated section; the final two letters indicate the thermal elements which form the thermocouple junction.

^bTemperature stabilization is defined as being reached whenever 99% of total temperature rise is reached during flow coastdown or power increase.

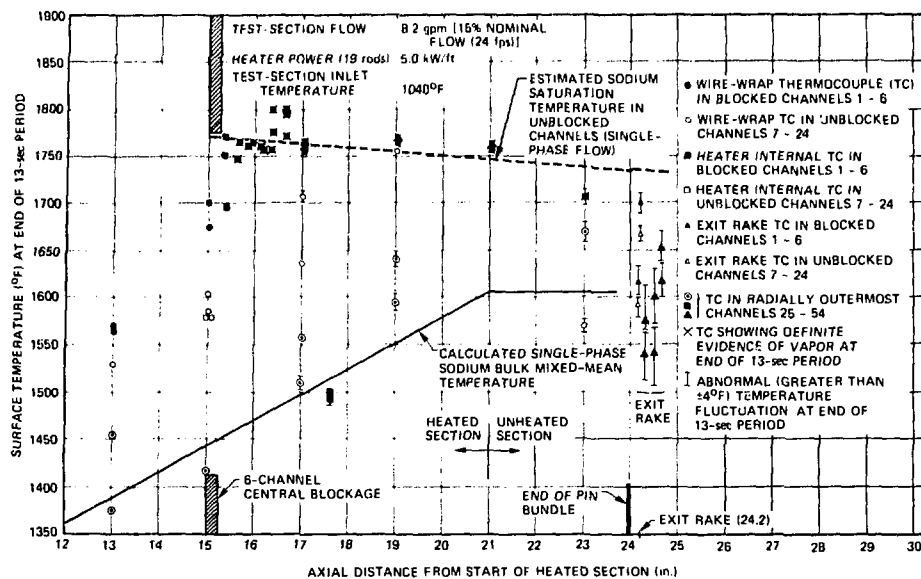


Fig. 5. Experimental temperatures in vicinity of blockage at end of 13 sec. period in Test 3, Run 101c.

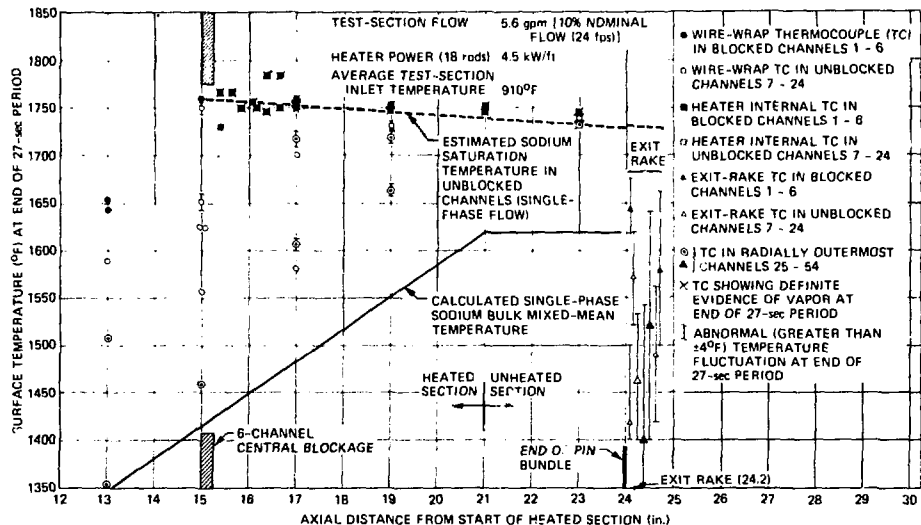


Fig. 6. Experimental temperatures in vicinity of blockage at end of 27 sec period in Test 4.

wake), the results of test 4 showed no evidence of radial propagation of the local boiling zone. In particular, local boiling zone stability at the end of the quasi-steady state boiling period in test 4 is found by examining the time of temperature stabilization in Table I; all thermocouples in the unblocked region (downstream of the blockage) had stabilized in temperature at least 13 sec prior to the end of the 27-sec quasi-steady state boiling period.

Dryout in Local Boiling Zone

For clad dryout of a pin surface during the boiling periods, surface temperatures at dryout locations would be expected to increase sharply (above 1800°F) as the liquid film on the pin surface evaporates completely. None of the thermocouples, which indicated the presence of vapor in their vicinity,

exhibited this behavior during the quasi-steady state boiling periods investigated.

CONCLUSIONS OF BUNDLE 3B QUASI-STEADY STATE BOILING

Axial propagation of the sodium vapor zone in the 6-channel blockage wake was limited in both axial directions by the test facility design.

Evidence of radial propagation of the sodium vapor zone from the blocked to the unblocked channels was not found during the 13-sec quasi-steady state period investigated. Although two thermocouples in the unblocked channels [at 51 mm (2 in.) and 102 mm (4 in.) downstream of the blockage] were close to the estimated sodium saturation temperature, evidence of sodium vapor could not be established. Stable temperatures were reached by all thermocouples in the unblocked channels 3 sec prior to the end of the 13-sec period.

Evidence of radial propagation of the local boiling zone in the first 103 mm (4.0 in.) downstream of the blockage was not found during the 27-sec quasi-steady state boiling period, although one thermocouple in an unblocked channel at 103 mm (4 in.) downstream of the blockage was close to the saturation temperature. Evidence of sodium vapor was found in the unblocked channels at the unheated 203 mm (8 in.) position downstream of the blockage, indicating that radial propagation of sodium vapor could have occurred to a limited extent. Stable temperatures were reached by all thermocouples in the unblocked channels 13 sec prior to the end of the 27-sec period.

No evidence of sharp temperature increases leading to dryout was found on any heated pin surface during either the 13-sec or the 27-sec quasi-steady state boiling periods investigated.

EXTRAPOLATION OF CONCLUSIONS TO LMFBR SUBASSEMBLIES

The conclusions expressed above for quasi-steady state boiling in a 19-rod centrally blocked bundle are significant to LMFBR safety to the extent that they can be extrapolated to full-scale 217-pin fuel subassemblies.

The subsequent discussion of the effect of non-prototypicalities on the extrapolation of Bundle 3B results will necessarily be limited to radial propagation of the boiling zone behind a 6-channel blockage. Bundle 3B test results could not give an insight into axial propagation of the boiling zone in the wake because of two non-prototypicalities in the THORS Facility which limited axial propagation as discussed earlier. An additional factor influencing the extent of the axial vapor region in Bundle 3B was the high bulk temperature downstream of the approximately 51 mm (2 in.) wake zone, a necessary condition in order to initiate boiling in the blockage wake. Because of this high temperature, sodium vapor was produced downstream of the wake, a condition unlikely to exist in an LMFBR where a relatively low temperature downstream of a blockage wake would not allow any vapor in this region except by axial propagation from the wake. Thus, the axial boiling region generated in Bundle 3B is definitely more extensive than that initially expected downstream of a similar blockage in an LMFBR assembly.

In order to apply the radial boiling propagation results from Bundle 3B to a 217-pin LMFBR subassembly, a comparison of idealized radial temperature profiles at 25 mm (1.0 in.) downstream of the blockage is shown in Fig. 7 for both Bundle 3B and a 217-pin bundle, where the bundle cross sections are subdivided into ring-like parallel flow channels. The temperature profile in the 217-pin assembly is obtained by assuming that a 6-channel blockage in a 217-pin bundle at 100% nominal flow and a 14 kW/ft has caused the temperature in the wake to exceed the sodium saturation temperature to the same degree as in the case of Bundle 3B (10% nominal flow and 4.5 kW/ft). As shown in Fig. 7, the two factors which are assumed to govern the stability of radial boiling propagation are the vapor producing capability ($T_{max} \text{ single-phase} - T_{sat}$) in the

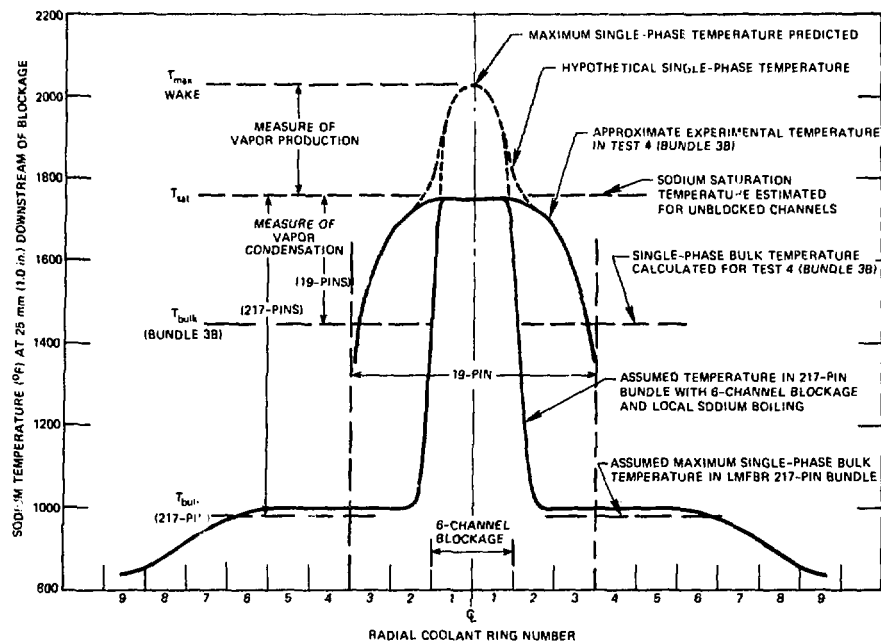


Fig. 7. Idealized radial temperature profiles during quasi-steady state boiling with 6-channel blockage.

wake and the vapor condensing capability ($T_{sat} - T_{bulk}$, single-phase) in the subcooled channels surrounding the wake. The hypothetical maximum single phase sodium temperature in the wake was predicted as a function of flow and power from experimental single-phase THORS data as shown by Han et al.²

If a radial stability factor for radial boiling propagation is then defined as the ratio of vapor-condensing capability (restraining force) and vapor-producing capability (driving force), $[(T_{sat} - T_{bulk}) / (T_{max, wake} - T_{sat})]_{single-phase}$, a quantitative comparison can be made between Bundle 3B results and a hypothetical 6-channel blockage temperature profile in a 217-pin bundle as shown in Table II. Thus, assuming that the saturation temperatures around the wake are the same in both the 19-pin and 217-pin bundles, the radial stability factor in the 217 pin bundle is calculated to be greater than that of run 101c and test 4 by factors of 1.7 and 2.8, respectively. Therefore, since run 101c and test 4 showed no definite evidence of radial propagation of the boiling zone into the unblocked channels, it is concluded that radial boiling zone propagation is also unlikely in a 217 pin subassembly with a 6-channel blockage and with the same degree of boiling as produced in Bundle 3B.

Two other non-prototypicalities of Bundle 3B must be discussed before extrapolating any results. One non-prototypicality in Bundle 3B is the length of the unheated section downstream of the heated section. The short 3-in. unheated length in Bundle 3B compared to the 48-in. unheated length (in addition to the 12-in. axial blanket region) in an LMFBR subassembly leads to a lower frictional pressure drop downstream of the heated section in Bundle 3B and therefore lower static pressure (sodium saturation temperature) downstream of the blockage in Bundle 3B (a similar Bundle 3B test section exit pressure and LMFBR argon cover gas pressure is thereby assumed). The resulting higher sodium saturation temperature immediately downstream of the blockage in a 217-pin LMFBR subassembly than in Bundle 3B would lead to an even higher radial stability factor for boiling in a 6-channel blockage wake of a 217-pin bundle (see Table II).

A further non-prototypicality in the 19-rod Bundle 3B is the proximity of the duct wall to the 6-channel blockage, which results in steeper radial temperature gradients in the subcooled channels near the duct wall of Bundle 3B

Table II. Radial boiling propagation for the wake of 6-channel blockages during quasi-steady state boiling

Experiment	Bundle size	Nominal flow %	Inlet/Outlet temperature (°F/°F)	Calculated single-phase bulk temperature [°C (°F)]	Calculated sodium saturation temperature [°C (°F)]	Predicted single-phase maximum wake temperature ^a [°C (°F)]	Radial stability factor
		Linear power (kW/ft)					$\left(\frac{T_{sat} - T_{bulk}}{T_{max} - T_{sat}} \right)$ single-phase
Test 3 (Run 101c)	19	$\frac{15}{5.0}$	1040/1605	800 (1470)	965 (1765)	1060 (1940)	1.7
Test 4	19	$\frac{10}{4.5}$	910/1620	790 (1450)	960 (1755)	1110 (2030)	1.1
LMFBR (hypothetical 6-channel blockage with local sodium boiling)	217	$\frac{100}{14.0}$	600/ 980	525 (980) (assumed maximum)	960 (1755) (assumed same as Test 4)	1110 (2030) (assumed same as Test 4)	2.8

^aSee Han et al.,² (preceding paper at this meeting).

than would occur in the same radial flow channels of a 217-pin assembly with a 6-channel blockage. It is important to note from Fig. 7, however, that the steepness of the radial temperature gradient between the wake boiling temperature and the adjacent subcooled region (approximately at single-phase bulk temperatures) is considered to be much larger for the 217-pin assembly than the 19-rod assembly; this indicates that it is not the proximity of the duct wall, but rather the bulk single-phase sodium temperature in the subcooled region, which is the governing factor for radial boiling zone propagation when extrapolating 1.-rod bundle results to 217-pin bundles with similar internal blockages. Thus, because of the larger bulk temperatures in Bundle 3B (more likelihood of radial boiling zone propagation) the proximity of the duct wall is not considered a hindrance in extrapolating the 19-rod results. Furthermore, because of this difference in bulk temperatures between the 19-rod and 217-pin bundles, the 6-channel blockage could be located anywhere within the 217 pin bundle (but at least 2 rows away from the duct wall as in Bundle 3B) and Bundle 3B radial boiling propagation results could still be extrapolated to 217-pin bundles.

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RECENT RESULTS OF A LOCAL BLOCKAGE EXPERIMENT
IN A SODIUM COOLED ELECTRICALLY HEATED BUNDLE

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ABSTRACT

The test section represents a 60-degree section of a SNR-300 fuel element having a 70% flat type central blockage. Experiments have been performed under single-phase and local boiling conditions. A parameter study of the wake temperatures has been carried out. It has been found that the normalized vortex temperatures depend on the Reynolds number. Apart from the temperature maximum in the vortex another relative temperature maximum has been found downstream the vortex.

The boiling experiments showed that stable single-bubble boiling was dominating and the cooling capacity in the vortex enough to avoid dryout in this region.

INTRODUCTION

The experimental work reported in this paper is a co-operative project of ECN (The Netherlands) and GfK (West-Germany) and forms part of a wider programme of safety studies on local failure propagation in LMFBR-cores. The aim of the experiment is to investigate the consequences of a local flow blockage in a subassembly of the SNR-300 Mark 1a core. Of interest is the sodium coolant behaviour under local boiling conditions. The question is if the boiling sodium is capable to cool the fuel rods in the blocked region, so that the clad temperatures do not exceed appreciably the sodium saturation temperature. Interruption of the cooling may occur in different ways. It may be a dryout of the fuel rod clads in the blocked region of the subassembly or it may be a dryout in a larger part. The latter may take place after a transition from local boiling to integral boiling as consequence of a thermohydraulic instability during the local boiling phase.

Another aim of this experiment is the testing of potential incore boiling detection devices like probe type flowmeters, acoustic detectors and thermocouples for measurement of temperature noise at the subassembly outlet.

Prior to the boiling experiments a parametric single phase experiment was carried out in order to check the prediction of the coolant temperatures behind local blockages by simulation experiments in water and code calculations.

In this experiment one sixth part of a fuel element having a 70% flat type central blockage is simulated. As shown in Fig. 3 a 60-degree section of the hexagonally shaped subassembly gives a triangularly shaped test section. The blockage size is derived from considerations concerning the detectability of flat type blockages in subassemblies by measurement of the coolant temperature at the subassembly outlet. Supporting experiments in water mock-ups [1] showed the rather amazing fact that the wake temperatures do not change much behind this type of blockages with blockage degree between about 30% and 60%. Also the chosen blockage size of about 70% for the present experiment is not expected to allow boiling under SNR-300 conditions. The flow or the pressure has to be reduced to achieve boiling in these experiments.

DESCRIPTION TEST FACILITIES

All 28 rods of this test bundle are electrically heated over a length of 40 cm, extending from 10 cm upstream to 30 cm downstream the blockage plate. The distance between the grids is in general 15 cm, but an additional grid has been mounted at 7 cm behind the blockage plate in order to reduce possible pin-bowing effects. The bundle outlet part (inclusive the flow mixer) resembles the SNR-type fuel element outlet (see Fig. 1). The inlet side of the test section is provided with a throttling section to make the hydraulic behaviour of the test section (stationary as well as dynamically) equal to that of a fuel element. The dynamic effect of the bypass flow around a fuel element in a reactor core is simulated by an expansion vessel at the test section inlet.

A great number of thermocouples is located in the expected boiling region in measuring planes at 0.5 - 2.5 and 5.5 cm behind the blockage and some thermocouples are mounted on the grids at 15 and 30 cm behind the blockage. Sodium temperatures as well as heater wall temperatures are measured and a part of the TM-type couples have been used for void detection.

Acoustic and pressure transducers are located in the boiling region and in other parts of the test section. Furthermore temperature noise is measured in the test section outlet upstream and downstream the flow mixer and EM-flowmeters are installed at the bundle inlet and outlet. The test section has been installed in the ECN-sodium loop which has a cooling capacity of 500 kW.

About 140 dynamic signals are recorded by a digital data registration system with a data rate of about 50,000 digital words (16 bits) per second and a registration time of about 6 minutes.

Acoustic signals and temperature noise are stored on analogue tape recorders.

SINGLE PHASE EXPERIMENTS

To make a parametric study of the coolant behaviour behind the flow blockage possible, about 60 experiments have been performed. The parameters heat flux, sodium velocity and the mean sodium temperature have been varied over the following ranges:

heat flux	5	- 120 W/cm ² ,
sodium velocity	0.25	- 3 m/s,
mean sodium temperature	250	- 600 °C.

In this way the subchannel Reynolds number reached the values 3.1×10^3 - 6.4×10^4 and the kinematic viscosity of the liquid sodium 0.25×10^{-6} - 0.44×10^{-6} m²/s. To uncover possible influences of geometrical deformations of the test section walls or pins, part of the measurements have been carried out under conditions with low radial and axial temperature gradients.

In accordance with the convection model [2] the temperatures in the observed recirculation flow were normalized as follows:

$$\Theta_i = \frac{T_i - T_o}{\Delta T / \Delta z} \quad (m),$$

where T_i is the temperature in the vortex, T_o is the temperature in the undisturbed flow at the blockage level and $\Delta T / \Delta z$ the axial temperature gradient in the parallel flow region of the bundle. An example of the velocity field of the recirculation flow is shown in the left part of Fig. 2. This velocity field has been deduced from supporting experiments at ECN in a unheated mock-up of the bundle with air flow. From these experiments it has been found among others that the length of the recirculation flow in the triangular test bundle does not depend on the Reynolds number. In the right half of Fig. 2 isotherms are shown as measured in the 60-degree bundle with sodium coolant. The maximum wake temperature $\Theta_1^{\max}$ is measured in instrumentation plane 1 at 0.5 cm behind the blockage. On comparison of these isotherms with (GfK) water experiments in a mock-up without grids, it is found that the temperature field is affected by the grid located 7 cm behind the blockage. The isotherms are bent to the left due to an increased mass exchange near this grid. The maximum normalized vortex temperature in the water experiment and in the sodium experiment compare reasonably.

The single phase experiments with the sodium bundle show that the normalized vortex temperature field depends on the sodium velocity and the mean sodium temperature. It is found that these two parameters enter the relation through the Reynolds number, which demonstrates the hydraulic origin of the above mentioned effect. The normalized maximum temperature in the vortex $\Theta_1^{\max}$ is independent of the Reynolds number, but the location of this temperature maximum shifts from the inner side to the outer side of the blockage plate with increasing the Reynolds number. On allowing higher temperature gradients in the bundle it has been found that $\Theta_1^{\max}$ increases linearly with the axial temperature gradient $\Delta T / \Delta z$:

$$\Theta_1^{\max} = 0.72 \text{ m for } \Delta T / \Delta z = 50 \text{ }^\circ\text{C/m},$$

$$\Theta_1^{\max} = 0.80 \text{ m for } \Delta T / \Delta z = 230 \text{ }^\circ\text{C/m}.$$

Bundle distortion, possibly pin-bowing, is believed to cause this effect. Rather unexpectedly, about 30 cm behind the flow blockage (near measuring plane 5), another relative temperature maximum $\Theta_5^{\max}$ has been found. Recent GfK water experiments as well as ECN air experiments confirm that in this region low sodium velocities exist. The maximum normalized temperature in measuring plane 5 depends on the Re-number and reaches a maximum value for $Re = 4 \times 10^4$. This maximum temperature turns out to be independent of the axial temperature gradient $\Delta T / \Delta z$. From Fig. 4 it is seen clearly that for higher Re-numbers and lower $\Delta T / \Delta z$ the temperature maximum at 30 cm behind the blockage reaches nearly the temperature maximum within the vortex.

BOILING EXPERIMENTS

In the first half of this year a number of boiling experiments has been performed. In the list below the most principal data of some boiling experiments are given. In most of these experiments boiling has been initiated and sustained by a slow and continuous pressure reduction. Characteristic for the experiments carried out so far is that the maximum sodium velocity V_o was 2.0 m/s, the maximum heat flux 165 W/cm^2 and the pressure P_o at the start of boiling about 1 bar, corresponding to a maximum sodium temperature of 885°C . An important parameter for these boiling runs is the reduction of the saturation temperature ($\Delta T_{\max}$) during the boiling: it characterizes the enlargement of the two-phase region or the intensity of the boiling. In the last column of the table we see that in these experiments its maximum value is about 30°C . In the last experiment no. 85 the boiling is intensified by stepwise reduction of the sodium flow. Here, $\Delta T_{\max}$ is 45°C ; this is the temperature increase of $T_{\max}$

during boiling in the theoretical case that $T_{\max}$ is not limited by the saturation temperature.

TABLE
Review Boiling Experiments

EXP	V_o (m/s)	ϕ'' (W/cm ²)	$\Delta T/Lz$ (°C/M)	P_o (bar)	$T_{\max}$ (°C)	$\Delta T_{\max}$ (°C)
69	1.5	99	380	0.45-0.38	800	-15
70	1.5	100	383	0.47-0.39	804	-18
72	1.5	110	422	0.62-0.45	830	-31
81	1.5	135	499	1.01-0.75	882	-32
82	2.0	165	457	1.05-0.83	885	-24
83	1.5	124	458	0.74-0.57	848	-26
84	2.0	160	443	0.67-0.61	838	-10
85	1.57-1.43	135	447-523	0.96	875	-45

The experiments showed that single-bubble boiling with some characteristic bubble-frequencies was dominating.

Dryout has not been observed in the vortex region. The vortex flow has been found to be very persistent, even under the most intensive boiling. This persistent recirculation flow is responsible for the good cooling behind the flow blockage. Some transitions in the boiling mode have been observed. Detailed analysis of the boiling signals in the time-and frequency domain showed that two boiling regions were active. Interaction between the evaporation- and condensation processes in both regions has been found. These interactions are partially responsible for the transitions in the boiling mode.

In Fig. 5 some characteristics of experiment number 72 are illustrated. In the upper half of the figure the slow pressure transient of -2 mbar/sec. is shown. The boiling starts somewhere in region 1 (just behind the blockage plate) at time t_0 . The coolant pressure in the boiling region is then 0.62 bar. The maximum sodium temperature decreases now according to the reduction of the saturation temperature of sodium. At time t_1 a first transition has been found. The low frequency acoustic noise level and the pressure pulses increase. The temperatures and flow signals show stable harmonic oscillations after this transition. After the second transition at time t_2 the oscillations become irregular, the low frequency acoustic noise level and the pressure pulsations in the boiling region decrease considerably.

The oscillations on the temperatures and the flow are very stable in the second phase of the experiment and a continuous decrease from 7.5 to 5 c/s of the bubble frequency has been found. Frequency analysis has shown that after the second transition other more or less stable oscillations of about 20 c/s appear in the flowmeter signals.

DISCUSSION OF THE BOILING EXPERIMENTS

In this experiment the boiling started in region 1 just behind the flow blockage. The nature is irregular boiling with one or more small bubbles. After

a reduction of the saturation level by about 10°C, stable single bubble boiling in this region dominates.

The bubble lifetime agrees quite well with calculations using a superheat local boiling model [3]. On a further decrease of the pressure boiling starts also in the region 5 behind the vortex. The boiling in this region is less regular, but sometimes short "trains" of bubbles with a dominating 20 c/s repeating frequency appear. During the transition at t_2 the evaporation and condensation in region 5 is clearly triggered by the boiling in region 1.

Figure 6 shows flowmeter- and pressure signals during single bubble boiling in the vortex region. The harmonic character of the oscillations can be seen clearly. Acoustic pulses occur when the bubble condenses completely. When boiling in the region downstream the vortex starts the flowmeter signals are truncated. A positive pressure pulse during evaporation in region 1 stimulates condensation in region 5 and evaporation in region 5 is stimulated by the negative pressure pulse during the condensation phase in region 1.

Analysis of the flowmeter signals makes it possible to calculate (by integration in time) the void volumes belonging to the different characteristic frequencies, and from that to the different boiling regions in the bundle. Because of the extensive boiling instrumentation in the vortex also geometrical data of the void could be deduced from the signals. The voided regions at different phases of experiment 72 are shown in Fig. 7.

The shape of the void in region 5 is only a rough estimate. During the most violent boiling in this experiment, when the saturation temperature is reduced 27°C below its value at the start of boiling, the maximum volume of the bubble in region 1 is 14 cm³ and that of the bubble in region 5 is 4 cm³. The repeating frequencies are about 5 and 20 c/s respectively.

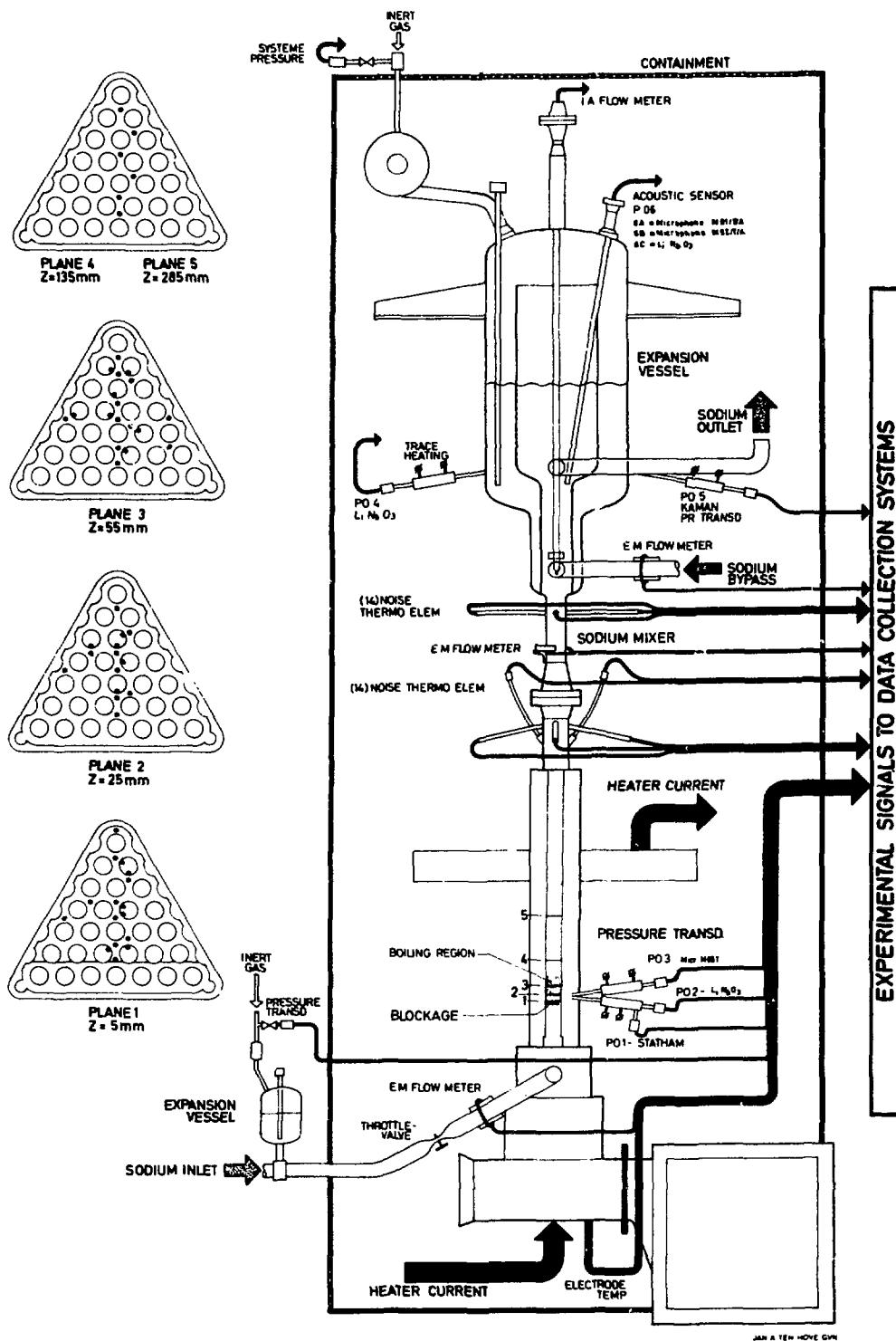
CONCLUSIONS

The experiments carried out so far, showed that the recirculation flow is very persistent both under single phase- and local boiling conditions. The cooling capacity in the vortex region is sufficient to avoid dryout of the cladding, even when the saturation temperature has been reduced by 50°C. Thermohydraulic instabilities have not been seen; stable single-bubble boiling was dominant in the vortex region. The boiling behaviour in the region behind the vortex is ill-defined, but sometimes characteristic bubble frequencies appear. The nature of this high temperature region is not well understood at the moment nor are its cooling properties during boiling.

The experiments will be continued. The investigations will be concentrated on the cooling capacity in both boiling regions under more severe boiling conditions than hitherto.

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INSTRUMENTATION 28 ROD BUNDLE

FIGURE 1

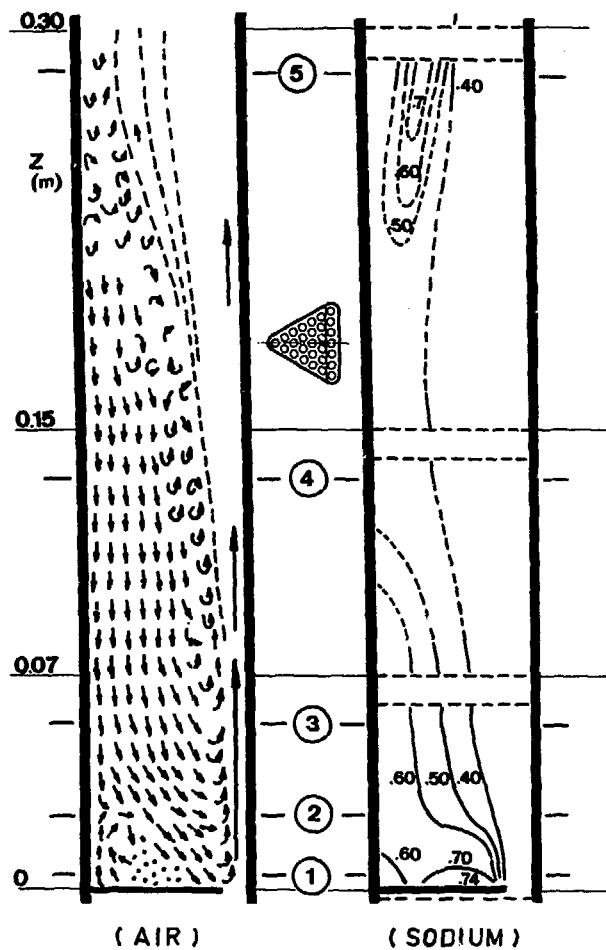
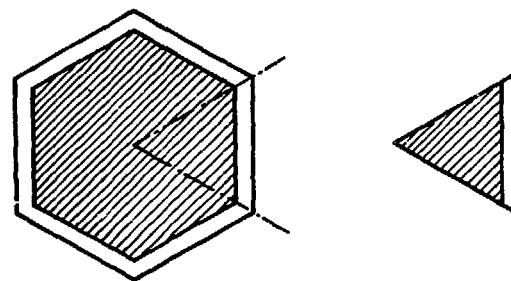


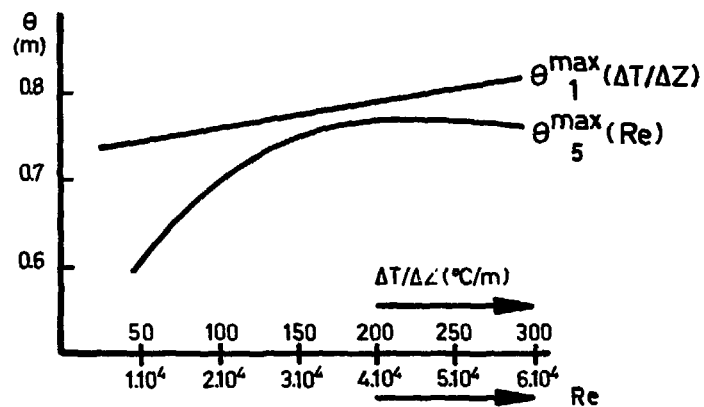
FIG.2 WAKE VELOCITIES AND ISOTHERMS

SNR FUEL ELEMENT 60 DEGREE BUNDLE
169 FUEL RODS 28 ELECTRICALLY
HEATED RODS



70 % CENTRAL FLOW-BLOCKAGE

FIG.3 CROSS SECTION F.E. AND BUNDLE.

FIG.4 INFLUENCE OF Re AND $\Delta T/\Delta Z$ ON THE TWO TEMPERATURE MAXIMA.

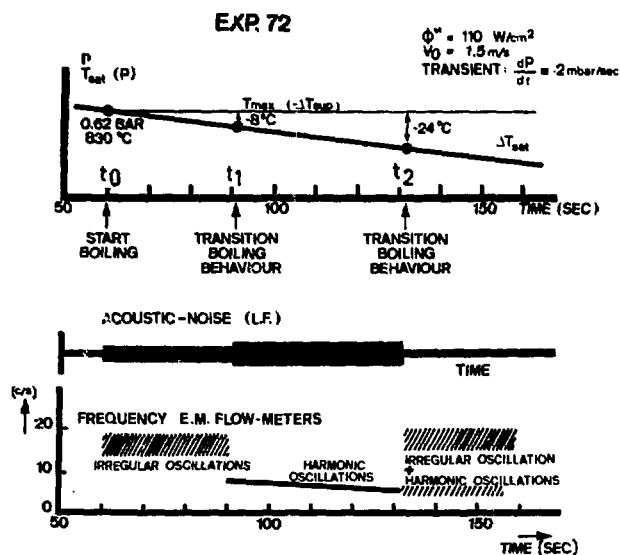


FIG. 5 BOILING RUN 72

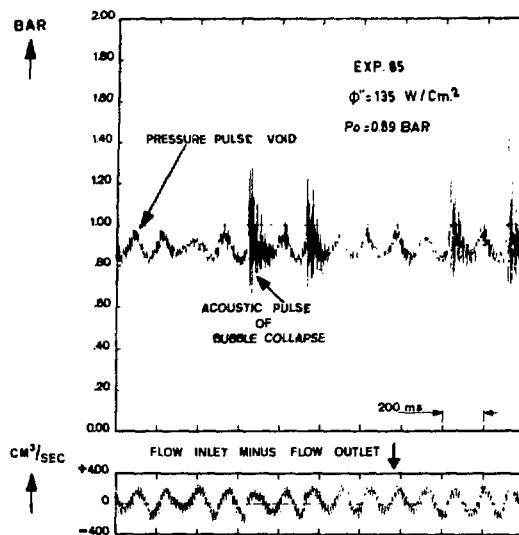


FIG. 6 BOILING SIGNALS DURING SINGLE-BUBBLE BOILING IN THE VORTEX

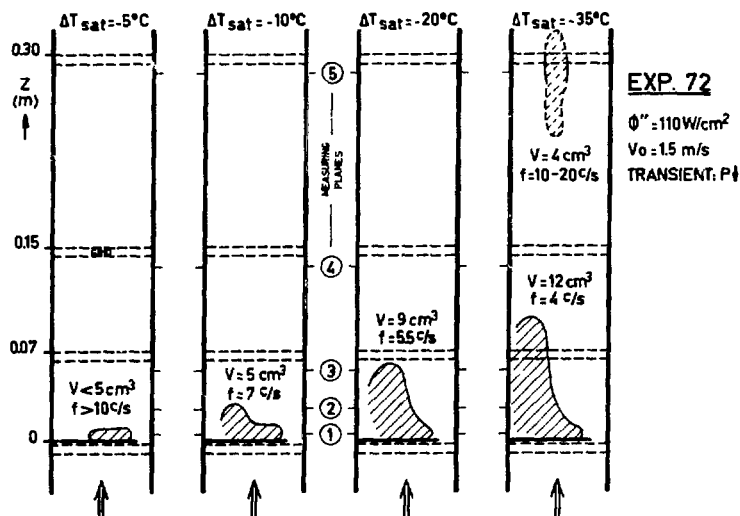


FIG. 7 VOIDING DURING BOILING RUN

LOCAL BOILING OF SODIUM IN DOWNSTREAM OF LOCAL FLOW BLOCKAGE IN A SIMULATED LMFBR FUEL SUBASSEMBLY

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ABSTRACT

This paper deals with local sodium boiling in the downstream of a six-sub-channel blockage in an electrically heated LMFBR fuel subassembly mockup.

The first series of experiments were conducted to measure temperature distributions in the downstream of the blockage under non-boiling conditions. The measured temperature rise due to the blockage agreed fairly well with the calculation by the LOCK code. When the experimental results are extrapolated to reactor conditions, the temperature rise due to the six-subchannel blockage is estimated to be less than 250°C.

The second series of experiments were performed to investigate local boiling phenomena. In the local boiling region, no flow instability was observed since the subchannels near the wrapper wall were still filled with subcooled liquid. In the nearly bulk boiling region, however, considerable upstream voiding occurred and then the inlet flow was reduced, leading to final dryout.

The boiling caused a considerable increase in acoustic noise intensity. The root-mean-square (RMS) noise level of approximately 20 mbar obtained in the present local boiling experiments with sodium is much higher than that (approximately 0.5 mbar) in the ordinary nucleate boiling experiments with water. The peak observed in the hertz ranges was due to the repetition of bubble formation and collapse. In the kilohertz ranges, however, resonance peaks were superposed on a smooth curve with a broad peak at approximately 7 kHz.

The boiling caused a considerable increase in outlet flow fluctuation. The maximum RMS value of the fluctuation was 1.33 m/s. The peak observed in the hertz ranges was again due to the repetition of bubble formation and collapse.

The frequency (2.9 s^{-1} and 20.2 s^{-1}) of bubble formation decreased with the increase of the bubble size at its point of maximum development. The product of the bubble frequency and the equivalent diameter was found to be constant.

INTRODUCTION

Anomalous sodium boiling caused in some accidents is an important problem for safety considerations in the liquid metal cooled fast breeder reactor (LMFBR), since sodium boiling might lead to fuel failure propagation, and finally total core disruption. Experimental studies have been carried out in this connection, using the Sodium Boiling and Fuel Failure Propagation Test Loops, SIENA installed at the O-arai Engineering Center, Power Reactor and Nuclear Fuel Development Corporation.

In the earlier bulk boiling experi-

ments under loss-of-flow conditions^[1], it was revealed that the voiding pattern observed in the single-pin geometry agreed fairly well with the calculation by the NAIS-P2 code (single-bubble slug-ejection model), whereas in the seven-pin geometry, two-dimensional voiding patterns were dominant. No significant effects were observed of the temperature ramp rate on the incipient-boiling (IB) wall superheat. The pressure rise at the initial vaporization compared well with the vapor pressure corresponding to the IB wall superheat. The pressure pulse occurring at the vapor collapse correlated reasonably

well with the re-entry velocity of the liquid column and agreed well with the prediction by sodium hammer analysis. The residual liquid film thickness (0.05 mm and 0.45 mm) became thinner with higher IB wall superheat.

In the single pin forced convection boiling experiments [2], however, the observed two-phase flow pattern was in the sequence of bubbly flow, slug flow and annular (mist) flow in sodium as in the case of water. The spectra of acoustic noise emitted with boiling did not differ distinctly from that registered when boiling was not apparent. When the noise was filtered to eliminate its low-frequency components, the noise intensity level associated with boiling first increased sharply with rising heat flux to attain a maximum value, then decreased somewhat and remained constant thereafter. The frequency (3.9 s^{-1} and 9.3 s^{-1}) of bubble formation decreased with increasing bubble size at its point of maximum development. The product of the bubble frequency and the equivalent diameter with the value of 77.8 mm/s ($=280 \text{ m/h}$) stayed relatively constant as suggested by Jakob [3] for pool boiling with water.

The present experiments have been conducted to investigate local sodium boiling in the downstream of a local flow blockage in a seven-pin bundle. This paper gives the experimental results of local boiling phenomena, with particular emphasis on the behavior of two-phase flow pattern, the characteristics of boiling acoustic noise and outlet flow fluctuation and the frequency of bubble formation. In addition, the experimental results of temperature distributions in the downstream of the blockage under non-boiling conditions will be compared with the calculation by the computer code LOCK. [4]

EXPERIMENTAL EQUIPMENT AND OPERATING PROCEDURES

Loop

A series of experiments were carried out in the Sodium Boiling and Fuel Failure Propagation Test Loops, SIENA at PNC's O-arai Engineering Center. The details of the SIENA loops are described in reference 1. Only a brief recapitulation will be given here.

The SIENA loops have three test sections: T-1, for single-pin experiments; T-2, for 7-pin experiments; and T-3, for 19-pin or 37-pin experiments.

The main circulation pump can provide sodium flow up to $5 \times 10^{-3} \text{ m}^3/\text{s}$. The maximum power of 650 kW can be supplied to the electrically heated pins.

Test Section

Figure 1 shows a sketch of the locally blocked test section. In order to simulate an LMFBR fuel subassembly, an electrically heated seven-pin bundle was centered in a hexagonal tube, 24 mm flat-to-flat distance inside. The heater pins, which were specially made for the present study by Sukegawa Electric Co., Ltd., were 6.5 mm in diameter and approximately half as long as the fuel pins of the Japanese prototype LMFBR, MONJU. The heater pins can be operated at the maximum heat flux of 300 W/cm^2 in the flowing sodium of 900°C . The pitch-to-diameter ratio (P/D) was 1.22. At the distance of 350 mm from the start of the heated section the six central sub-channels were blocked by a 0.5 mm thick stainless steel plate welded on the upstream side of a grid spacer. The grid spacer, which consisted of seven stainless steel tubes of 7.9 mm in outer diameter and 5 mm in height, was specially made for the present study. The blockage and the grid spacer covered 42 % of the total flow area.

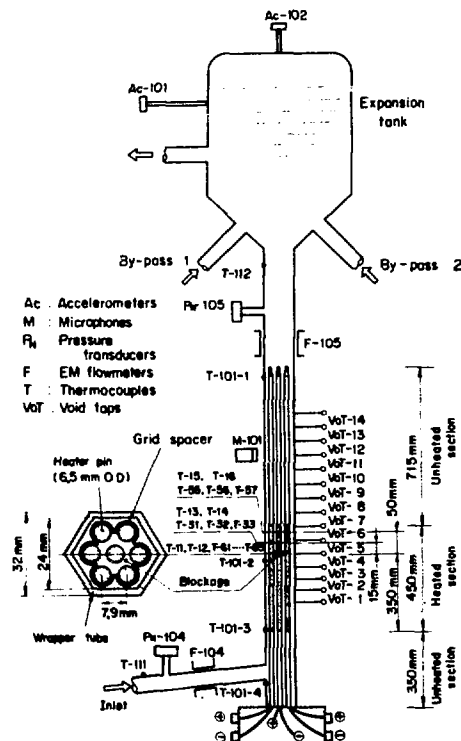


Fig. 1 Locally blocked seven-pin bundle

A compensating heater and a thermal insulator were equipped on the outer wall of the hexagonal tube to compensate heat dissipation from the wall and to keep the outer wall in an adiabatic condition. The by-pass of the test section simulated other subassembly channels adjacent to the boiling subassembly.

The pin surface temperatures (T-11, through T-65) were measured by many chromel-almel thermocouples of 0.3 mm in diameter, which were embedded in the outer surface of each pin. Their hot junctions were located at the axial locations: 0 mm, 15 mm and 50 mm downstream from the blockage. Thermocouples T-101-1, through T-101-4 were welded on the outside surface of the hexagonal tube to measure the local tube temperatures. The inlet and outlet temperatures were measured by the thermocouples T-111 and T-112, respectively.

Potential-tap type void meters (VoT-1, through VoT-14) were used for detecting the formation of vapor bubbles at boiling inception and their subsequent behavior. The sodium velocities at the inlet and outlet were measured by the electromagnetic flowmeters F-104 and F-105, respectively.

Three types of acoustic transducers were used in order to measure the boiling acoustic noises. The first were accelerometers Ac-101 and Ac-102 — Kistler Model 815A5 — mounted onto waveguides which were placed to the expansion tank. Secondly strain-gauge type pressure transducers P_N-104 and P_N-105 — Shinkoh Model PR-5S and PR-10S — were provided at the inlet and outlet of the test section to measure the pressure changes in boiling sodium. The third was a condenser type microphone M-101 — Sony Model ECM-21 — which was set outside the test section.

All the signals from these instruments were recorded by analog data recorders as well as by a digital data acquisition system.

Operating Procedure

The experiments were performed in the following manner. The oxygen concentration in sodium was controlled down to 10 ppm with a purification system. All the instruments were calibrated before the experiments and checked periodically during experiments.

In the first series of experiments, where temperature distributions were measured in the downstream of the blockage under non-boiling conditions, the

flow rate was set and the heater pins were adjusted to a fixed power level. Sufficient time was allowed for the test section to reach steady-state conditions prior to experiments. To verify the consistency of the data, several runs were repeated.

The non-boiling experiments were conducted under the following conditions:

Flow velocity	0.27 ~ 5.00 m/s
Inlet temperature	261.5 ~ 374.6°C
Heat flux	3.8 ~ 63.1 W/cm ² .

In the second series of experiments, however, where local boiling phenomena were investigated, the inlet temperature and flow rate were held constant and the heat flux was gradually increased up to boiling inception. After boiling had thus set in, the heat flux was further increased step by step until dryout occurred as it was confirmed that the test section had not failed even under the final dryout condition.

The boiling experiments were conducted under the following conditions:

Flow velocity	0.40 ~ 2.52 m/s
Inlet temperature	463 ~ 537 °C
Heat flux	54.2 ~ 270.1 W/cm ²
Cover gas pressure	1.03 ~ 1.05 bars.

RESULTS AND DISCUSSIONS

Temperature Distributions in the Downstream of the Blockage under Non-Boiling Conditions

Figure 2 shows a typical measured longitudinal pin surface temperature distribution in the downstream of the blockage under a non-boiling condition. The experimental results were compared with the analytical results calculated by the LOCK code, whose description is given in reference 4. In this figure the horizontal axis X is the distance from the blockage, and the vertical axis T-T_B(X=0) is the temperature difference between the pin surface temperature and the bulk coolant temperature at the blockage section. It can be seen from the figure that the temperature attains a local peak on the surface of the central pin at the blockage (X=0 mm) and decreases with the distance from the blockage. The temperature rise also occurs on the surface of an outer pin facing the hexagonal tube. This may be attributed to the heat resistance effect of the grid spacer covering the outer pin surface.

Calculations by the LOCK code were performed for the same conditions: flow

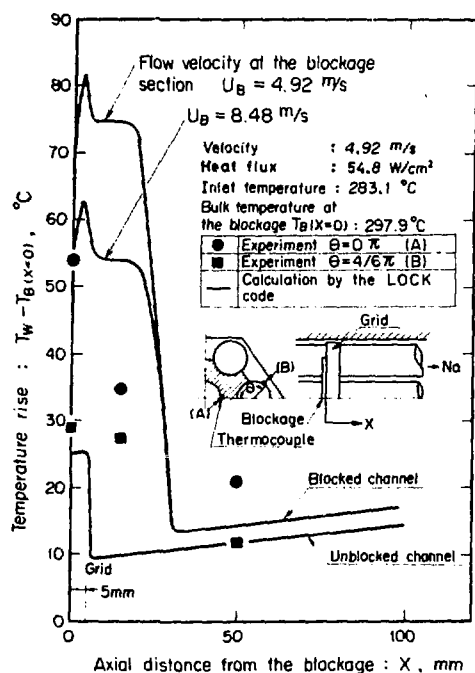


Fig. 2 Comparison of axial pin surface temperature distribution in the downstream of the blockage observed in the experiment with that calculated by the LOCK code

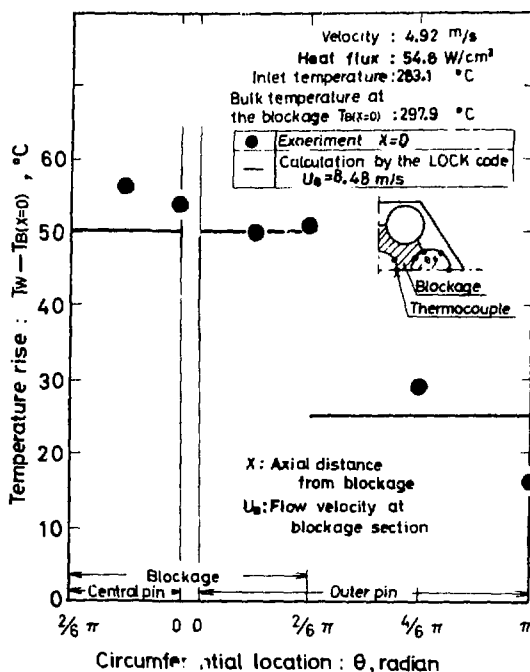


Fig. 3 Comparison of circumferential pin surface temperature distribution at the blockage section observed in the experiment with that calculated by the LOCK code

velocity, 4.92 m/s (at the normal cross section); heat flux, 54.8 W/cm²; and inlet temperature, 297.9°C. The parameter U_B is the flow velocity in outer subchannels at the blockage section. $U_B = 4.92$ m/s corresponds to zero increase in flow velocity, and $U_B = 8.48$ m/s is the flow velocity, with the increase of flow velocity at the blockage section taken into account. The calculated curve for $U_B = 8.48$ m/s agrees fairly well with the measured temperature rise due to the blockage.

Figure 3 shows the measured circumferential pin surface temperature distribution at the blockage section. The experimental results were again compared with the LOCK code calculation. The thermocouple arrangement to measure the temperature of each pin surface is shown in Fig. 3. The experimental conditions were: flow velocity, 4.92 m/s; heat flux, 54.8 W/cm²; and inlet temperature, 297.9°C. This figure shows that the temperature is nearly constant in the blocked region (between 0 and $2/6\pi$) and decreases steeply in the unblocked region (between $2/6\pi$ and π). The calculation was conducted for $U_B = 8.48$ m/s and agrees well with the measured temperature distribution at the blockage section.

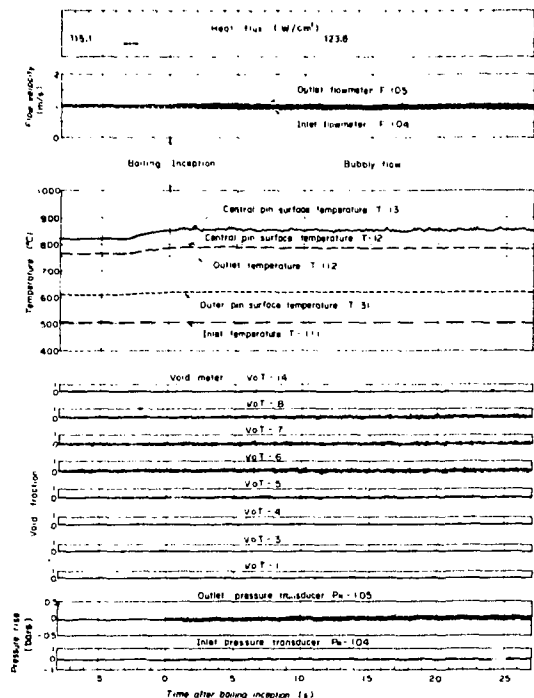
If the experimental results are extrapolated to the prototype reactor conditions, in which the heat flux is 200 W/cm² and the flow velocity is 5 m/s, the temperature rise due to the central six-subchannel blockage is estimated to be less than 250°C. In the estimation by LOCK no increase in flow velocity is assumed at the blockage section, i.e. U_B is taken as 5 m/s in spite of a little increase in flow velocity at the blockage section in the fuel subassembly.

Two-Phase Flow Pattern

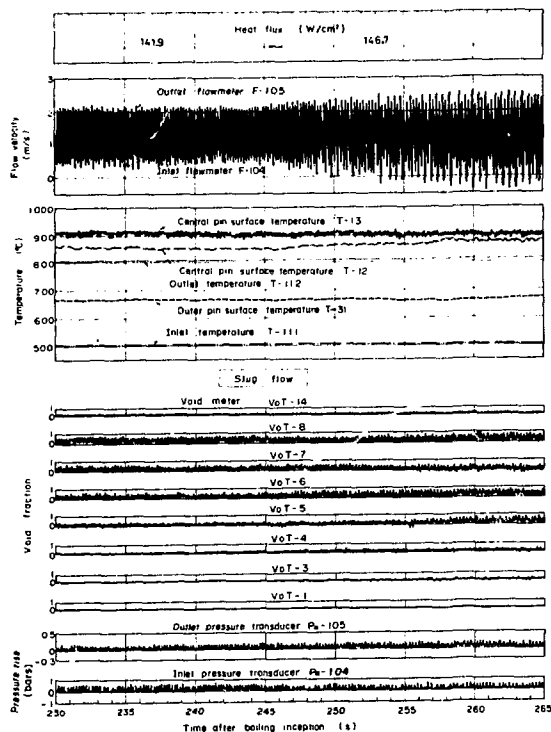
Incipient Boiling and Bubbly Flow

Figure 4(a) represents typical patterns of inlet and outlet flow velocities, inlet and outlet temperatures, pin surface temperatures, void fractions, and inlet and outlet pressure rises during the incipient boiling and bubbly flow regimes. The experimental conditions were: inlet temperature, 502°C; the inlet flow velocity, 0.96 m/s; and cover gas pressure, 1.05 bars.

As can be seen in the figure, the outlet flowmeter F-105 registers an abrupt change upon boiling inception followed by oscillations. These oscillations indicate a repetition of bubble



(a) Incipient boiling and bubbly flow

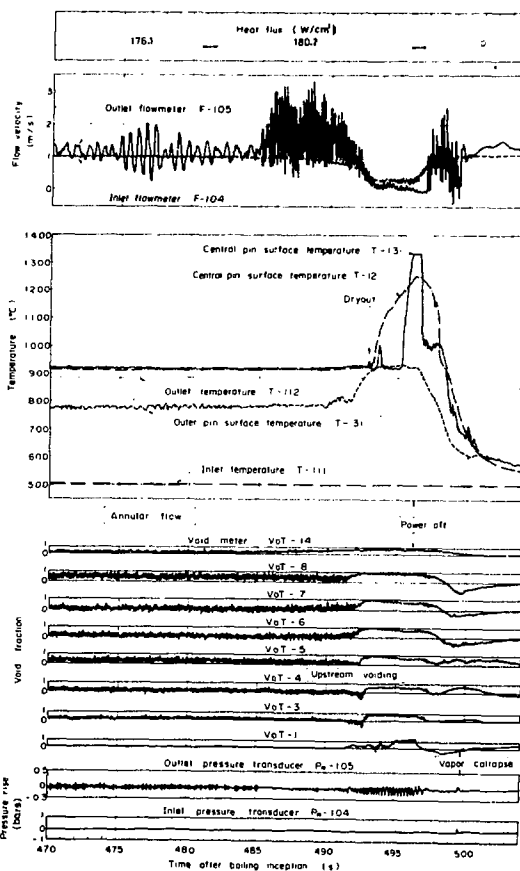


(b) Slug flow

formation and collapse at a fixed frequency. But the inlet flow rate remains constant, as confirmed by the record of F-104.

On the other hand, the center pin surface temperature T-13, which is close to the boiling region, fluctuates after boiling inception. The other temperatures T-12 and T-31 indicate no fluctuations, these thermocouples being located out of the region influenced by boiling. It is clear from the outlet temperature (T-112) that the liquid around the vapor bubbles is highly subcooled. When the tip of the bubble enters the subcooled liquid, its growth is markedly inhibited. Thus, the void meters cannot detect incipient boiling since they send no output signals before a considerable amount of bubbles has accumulated.

The inlet and outlet pressure transducers (P_N-104 and P_N-105 , respectively) indicate oscillations due to a



(c) Annular flow and dryout

Fig. 4 Records of signals from flowmeters, thermocouples, void meters and pressure transducers during steady-state boiling in a locally blocked seven-pin bundle — run No. 7(6)LB-214

repetition of bubble formation and collapse after boiling inception.

Slug Flow Figure 4(b) shows the changes in the heat flux, flow velocities, temperatures, void fractions and pressure rises after the establishment of a slug flow. It is to be noted that violent oscillations are recorded for the outlet flow velocity (F-105), the void fractions (VoT-6, VoT-7 and VoT-8), and the inlet and outlet pressure rises (P_N-104 and P_N-105 respectively), while the inlet flow velocity (F-104) remains at a nearly constant value. These violent oscillations indicate that bubbles, which repeatedly form and collapse, are larger in this regime than in the bubbly flow regime.

The pin surface temperature (T-13) fluctuates and attains the saturation temperature of sodium. The other temperatures, except the inlet temperature T-111, rise with increasing heat flux, but these temperatures are still below saturation, and the boiling continues in a subcooled environment.

Annular Flow and Dryout Figure 4(c) shows the changes in the heat flux, flow velocities, temperatures, void fractions and pressure rises under annular flow and dryout conditions. In the first half of this period covered, the violent oscillations in the outlet flow rate (F-105) established during slug flow settle down to a wavy pattern of a fairly long period. This change is brought about by the bubble collapse being slowed down, since the outlet temperature (T-112) is nearly at the saturation temperature of sodium, which begins bulk boiling. The foregoing behavior indicates an annular flow (or annular mist flow) regime, in which saturated sodium vapor or vapor mixed with liquid sodium globules flows between films of liquid sodium flowing along the wall surface. During this period the pressures are lower than in the slug flow regime, in particular, no signals appear in the inlet pressure transducer, P_N-104. The decreasing pressures can be attributed to the two factors: firstly the subcooling decrease slows down the bubble collapse rate and hence reduces the pressure source, and secondly the large amount of vapor increases the pressure absorption.

A further increase of the heat flux up to 180.2 W/cm² produces an alternate mode in the outlet flow signals from F-105. This change is due to the ejec-

tion of liquid films and/or liquid globules, then to the succeeding generation of a large amount of vapor bubbles. This succeeding generation of vapor then suppresses the inlet flow rate, as revealed by the sharp valley in the record of F-104. The rapid upstream voiding occurs, which is indicated by the rapid increase of void fractions. A ripple pattern is superposed on the void-meter signals. This ripple pattern, which is similar to those in the outlet flow velocity (F-105) and the outlet pressure (P_N-105), occurs at the time when the disturbance waves of the liquid film on the wall surface travel rapidly in the annular mist flow. In this period the pin surface temperatures (T-12 and T-13) register a sharp rise, which implies the occurrence of dryout of the residual liquid film. The heater pins being exposed to the danger of burnout, the input power to the heater pins was quickly shut off. The pin surface temperatures, however, decrease slowly due to the heat capacity of the heater pins.

Boiling Acoustic Noise

Among the various methods proposed for early detection of local sodium boiling, the measurement of acoustic noise signals associated with boiling phenomena appears to be most promising.

Instrumentation System Three types of acoustic transducers were used in the present experiments, as described in the section on the experimental equipment. The signals from these acoustic transducers were then amplified and recorded on an Ampex Model FR-1800L tape recorder. The tape recordings of the acoustic signals were analyzed using an EMR Model 1510 real-time analyzer. An NF Circuit Model M-172TA AC voltmeter was used for measuring the acoustic noise intensity, regulated by a Multimetrix Model AF-120 filter.

Acoustic Noise Intensity Figure 5 shows the effect of changes in heat flux q on the intensity ratio I/I_0 measured by the pressure transducer P_N-105 during a typical boiling run 7(6)LB-214, where I is the noise intensity at a given heat flux q , and I_0 the noise intensity in the absence of boiling. It is seen that the acoustic noise intensity first increases with the rise in the heat flux, and after attaining a maximum, decreases somewhat to remain more or less constant thereafter. This tendency is similar to the previous single-pin experiments [2].

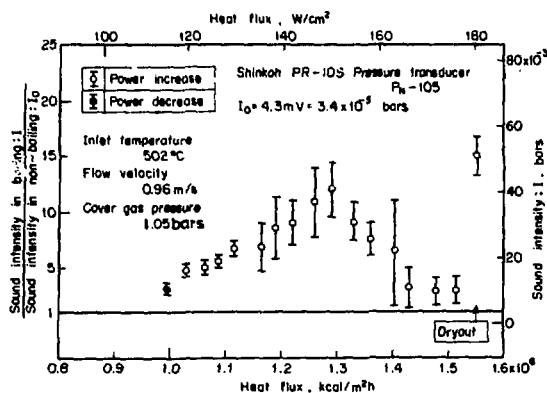


Fig. 5 Effect of heat flux on intensity of acoustic noise with boiling in a locally blocked seven-pin bundle — steady-state boiling run 7(6)LB-214

At the final rapid upstream voiding which resulted in dryout, however, the acoustic intensity again increases sharply.

During the first stage of boiling, preceding the attainment of maximum noise intensity, the boiling is considered to be in a state of developing nucleate boiling (mainly bubbly and slug flows). In this state the acoustic noise caused by the collapsing bubbles increases with rising heat flux since the liquid around the bubbles is yet so highly subcooled that the bubbles collapse completely although the bubbles become larger with higher heat flux.

Fully developed boiling may be established after the point of maximum noise intensity. The decreasing noise intensity can be attributed to the two factors. The first is the decrease in subcooling, which slows down the bubble collapse and so reduces the noise source. Secondly, the increased amount of vapor in the channel increases the acoustic absorption.

The final noise increase resulting from the rapid upstream voiding is due to the liquid globules and/or liquid films travelling rapidly in the annular mist flow and colliding with the downstream liquid pool.

A similar effect of heat flux on the acoustic noise intensity was measured by the other acoustic transducers.

The acoustic noise intensity (approximately 20 mbar in the RMS value) in the present local boiling of sodium is much higher than that (approximately 0.5 mbar) obtained in the ordinary pool boiling in water^[5]. The above observation indicates that the measurement of

acoustic noise signals associated with sodium boiling phenomena is promising for early detection of local sodium boiling in LMFBR fuel subassemblies.

Spectrum of Acoustic Noise Figure 6(a) shows the frequency spectra of acoustic noises measured by the pressure transducer P_N-105 during the boiling run 7(6)LB-214. It can be seen that the boiling causes a considerable increase in intensity at all frequencies. The peak observed at the frequency of 19.6 Hz for the heat flux of $120.0 W/cm^2$, is due to the repetition of bubble formation and collapse. The peak at 4.4 Hz for $146.7 W/cm^2$ is also due to the same repetition.

Figure 6(b) shows the frequency spectra of acoustic noises measured by the accelerometer A_C-102 during the same boiling run 7(6)LB-214. The frequency range in this figure is higher than that in the former Fig. 6(a). The boiling causes a considerable increase in intensity at all frequencies, but the frequency spectra do not change markedly during boiling, from boiling inception until dryout. Resonance peaks are superposed on a smooth curve with a broad peak at approximately 7 kHz for all the measurements.

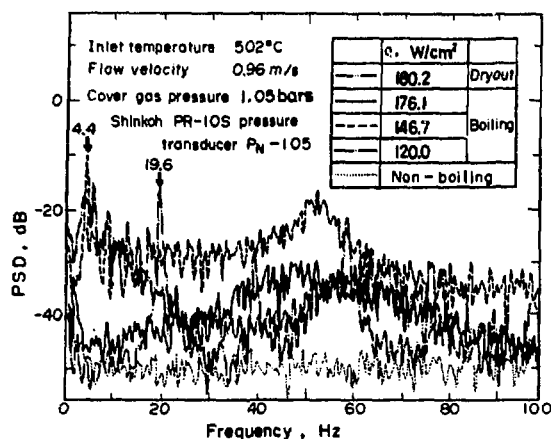
Outlet Flow Fluctuation with Boiling

The boiling causes an increase in outlet flow fluctuations, as shown in the former Fig. 4. From this fact the measurement of outlet flow fluctuation signals with boiling appears to be promising for the early detection of local boiling.

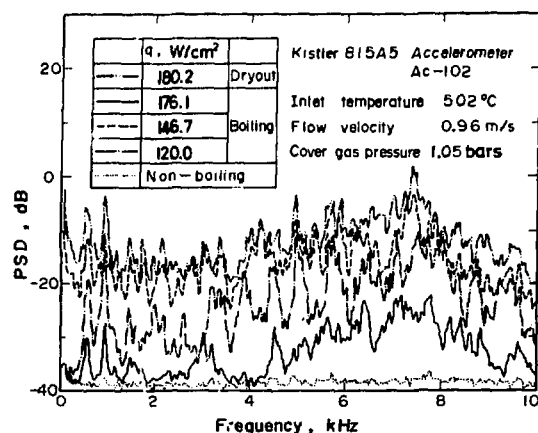
Intensity of Flow Fluctuation

Figure 7 shows the effect of the heat flux on the intensity of outlet flow velocity fluctuation during the boiling run 7(6)LB-214. In this figure the fluctuation intensity is expressed in root-mean-square values. It is seen that the fluctuation intensity first increases with rising heat fluxes, and after attaining a maximum (1.33 m/s), then decreases. Upon the final rapid upstream voiding which resulted in dryout, however, the fluctuation intensity again increases sharply. The heat flux effect shown in this figure is similar to that already shown in Fig. 5.

During the first stage, preceding the attainment of maximum fluctuation intensity, the boiling is considered to be in a state of developing nucleate boiling and no bubbles reach the posi-



(a) Low frequency ranges



(b) High frequency ranges

Fig. 6 Frequency spectra of acoustic noise with boiling in a locally blocked seven-pin bundle — steady-state boiling run 7(6)LB-214

tion of the outlet flowmeter. In this state the flow fluctuation caused by the repetition of bubble formation and collapse increases with rising heat fluxes, since the liquid around the bubbles is yet so highly subcooled that the bubbles collapse completely although the bubbles become larger with higher heat fluxes.

The decreasing fluctuation intensity after the maximum can be attributed to the decrease in subcooling, which slows down the collapse of bubbles and so reduces the flow fluctuation.

The final fluctuation increase resulting from the rapid upstream voiding is due to the ejection of liquid films and/or liquid globules, then to the succeeding generation of a large

amount of vapor bubbles.

Spectrum of Flow Fluctuation

Figure 8 shows the frequency spectra of outlet flow fluctuation during the same boiling run 7(6)LB-214. The peak observed at the frequency of 19.6 Hz for the heat flux of 120.0 W/cm², is due to the repetition of bubble formation and collapse. The peak at 4.4 Hz for 146.7 W/cm² is also due to the same process.

Bubble Size and Frequency

The bubble size and the frequency of bubble formation are important for the estimation of dryout in local boiling. But there are little data on sodium boiling. This led the present

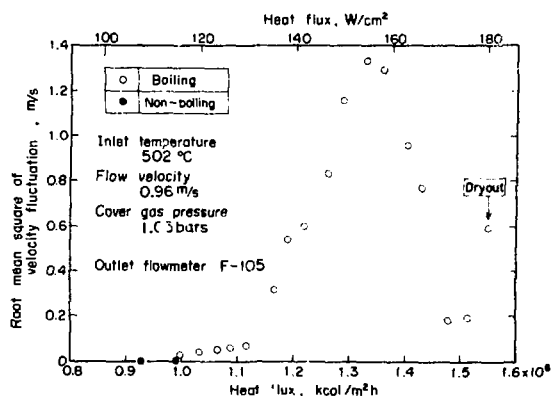


Fig. 7 Effect of heat flux on root mean square of outlet flow velocity fluctuation with boiling in a locally blocked seven-pin bundle — steady-state boiling run 7(6)LB-214

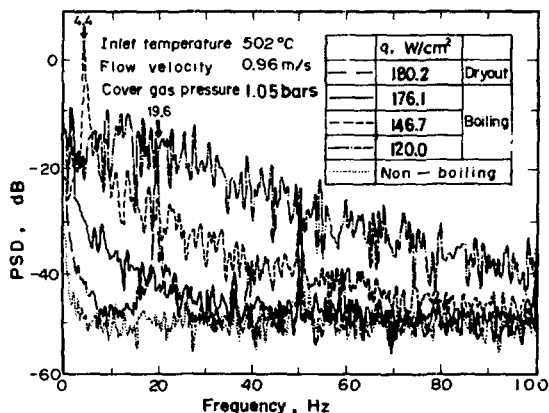


Fig. 8 Frequency spectra of outlet flow velocity fluctuation with boiling in a locally blocked seven-pin bundle — steady-state boiling run 7(6)LB-214

authors to carry out a study on sodium to examine the bubble size and frequency in subcooled boiling under forced convection. In Fig. 9 the frequency, f is plotted against the diameter d_0 of an equivalent sphere having the same volume as the bubble when it attained its maximum size. In this figure are also shown the earlier single-pin experiments [2] and the pool boiling experiments with potassium by Bobrovich et al. [6]

The sodium bubble is considerably larger than the water bubble, because of the high liquid-to-vapor density ratio of sodium. It is considered that the generation of a second bubble is inhibited by the pressure rise due to the development of the first bubble in the narrow space of the channel. This justifies the assumption that only one bubble exists at one time, which permits evaluation of the frequency of bubble formation from the observed oscillations of the outlet flowmeter records during the period preceding the arrival of the bubbles to the flowmeter position. On the other hand, the volume of the bubble may be evaluated by integrating the increment of outlet flow rate between the instant of bubble formation and that of its attaining maximum volume.

From Fig. 9, it is seen that the frequency decreases with the increase of the bubble diameter, and that this diameter is a little influenced by flow velocity. A broken-line curve can be drawn, which roughly represents the relation between the diameter and the

frequency in the present seven-pin experiment. This relationship is expressed by

$$f \cdot d_0 = 139 \text{ mm/s } (=500 \text{ m/h}).$$

This product value is higher than that of $f \cdot d_0 = 77.8 \text{ mm/s } (=280 \text{ m/h})$ obtained in the previous single-pin experiment, and agrees well with the pool boiling experiment for potassium by Bobrovich et al. This fact can be attributed to the two-dimensional bubble growth and collapse which was absent in the previous single-pin geometry. The bubble can expand and collapse not only in the axial direction, but also in the radial direction.

CONCLUSIONS

Experimental studies were carried out on local sodium boiling in the downstream of a local flow blockage in a seven-pin electrically heated LMFBR fuel subassembly mockup. In the first series of experiments the temperature distributions in the downstream of the blockage were measured under non-boiling conditions. In the second series of experiments, however, local boiling phenomena were investigated, with particular emphasis on the behavior of the two-phase flow pattern, the characteristics of boiling acoustic noise and outlet flow fluctuation, and the relation between the bubble size and the frequency of bubble formation.

Analyses of the results obtained permit the following conclusions to be drawn:

(1) In the non-boiling experiments the hottest surface temperature of the central pin was observed immediately downstream from the blockage. The measured temperature rise was in fairly good agreement with the calculation by the LOCK code. When the experimental results with the LOCK code are extrapolated to the prototype reactor conditions, namely the heat flux of 200 W/cm^2 and the flow velocity of 5 m/s , the temperature rise due to a central six-subchannel blockage will be less than 250°C .

(2) In the local boiling region, no flow instability was observed since the subchannels near the wrapper wall were still filled with subcooled liquid. The pressure drop appeared to remain constant. This is an important point to be borne in mind in reactor safety considerations, since local boiling of

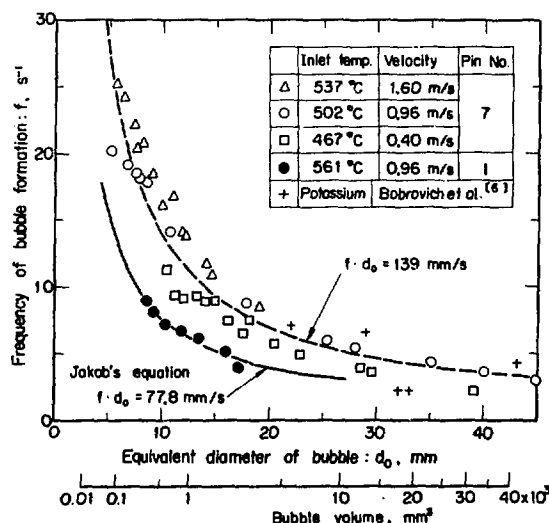


Fig. 9 Relation between equivalent diameter of bubble and frequency of bubble formation for sodium boiling

sodium will not necessarily always bring about instability of flow. In the nearly bulk boiling region, however, considerable upstream voiding occurred and then the inlet flow decreased. The inlet flow reversal occurred and resulted in a final dryout.

(3) The boiling caused a considerable increase in acoustic noise intensity. The noise intensity with boiling first increased with rising heat fluxes, and after attaining a maximum, decreased somewhat to remain constant thereafter. Upon the final upstream voiding which resulted in the dryout condition, however, the noise intensity again increased sharply. The noise intensity of approximately 20 mbar in the RMS value obtained in the present sodium boiling experiment is much higher than that of approximately 0.5 mbar in the ordinary pool boiling experiments with water. The above observation indicates that the measurement of acoustic noise signals associated with sodium boiling phenomena is promising for early detection of local sodium boiling in LMFBR fuel subassemblies.

The peak observed at the low-frequency hertz ranges was due to the repetition of bubble formation and collapse. In the high-frequency kilohertz ranges, however, resonance peaks were superposed on a smooth curve with a broad peak at approximately 7 kHz.

(4) The boiling caused an increase in the outlet flow fluctuation, and the maximum RMS value of the flow fluctuation was 1.33 m/s. Therefore the measurement of outlet flow fluctuation signals appeared to be promising for early detection of local sodium boiling in LMFBR fuel subassemblies.

(5) The frequency of bubble formation (2.9 s^{-1} and 20.2 s^{-1}) decreased with the increase of the bubble size at its point of maximum development. The product of the bubble frequency and equivalent diameter obtained in the present

seven-pin experiment agreed fairly well with the potassium pool boiling experiments by Bobrovich et al., and can be expressed in the relationship of $f \cdot d_0 = 139 \text{ mm/s}$ ($=500 \text{ m/h}$), which is higher than that of $f \cdot d_0 = 77.8 \text{ mm/s}$ ($=280 \text{ m/h}$) obtained in the previous single-pin experiment.

(6) Larger-scaled experiments both in 19-pin and 37-pin bundles are scheduled for this year and next, and the propagation of local boiling to bulk boiling will be investigated in detail, since the present seven-pin experiments were insufficient for this purpose.

ACKNOWLEDGMENTS

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DECAY HEAT REMOVAL FROM A PIN BUNDLE

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ABSTRACT

A number of sodium experiments have been carried out using an electrically heated seven pin bundle to investigate steady-state heat dissipation capability of a LMFBR fuel element under complete inlet blockage conditions. Tests have been performed in a 37 pin bundle to assess the amount of heat which can be removed under natural convection conditions in a system consisting of test section, by-pass and cold leg. The results of several test runs are reported.

The results of the complete inlet blockage tests are compared with those of a simple theoretical model. Because of the influence of the honeycomb grids, the capability for steady state heat dissipation is markedly reduced. Amounts of heat which can be transferred by boiling under natural convection conditions correspond broadly to reactor decay heat levels.

INTRODUCTION

Within the context of LMFBR safety analysis, two questions relating to the dissipation of decay heat have been studied in detail.

- How much heat can be removed from a fuel pin bundle by steady-state sodium boiling when the inlet to the bundle is completely blocked?
- How much heat can be removed from the bundle by boiling under natural convection conditions?

The answer to the first question depends upon the amount of coolant which can flow downwards as a liquid film against the upward flow of vapour out of the fuel element. In earlier tests using annular geometry with external heating, measured values of disposable heat were compared with the predictions of a quasi steady-state theoretical model [1-7].

The second question assumes the loss of integrity of the reactor primary circuits, and the necessity to transfer the decay heat by natural convection to the emergency cooling system provided in the reactor vessel. To this end, several tests using an electrically heated 37 pin bundle have been carried out, at various power levels, with the aim of ascertaining the extent to which

liquid flow resistance (eg. at the inlet of the bundle) in a loop can be increased before cooling capability under natural convection conditions is disrupted.

TEST SECTIONS

The tests were carried out in the NSK sodium boiling loop of the Karlsruhe Institut für Reaktorentwicklung. The seven pin test section is illustrated schematically in Fig. 1.

Fig. 2 shows the 37 pin bundle test section. This test section had been previously used for preliminary tests in connection with the in-pile experiment Mol 7C, and had been designed with a central blockage. To eliminate the influence of the blockage on the boiling process the six central pins which appear shaded in Fig. 2 were unheated. The natural convection loop consisted in its main parts of the 37 pin bundle with a throttle valve mounted upstream a flow bypass, an expansion tank in the highest part of the circuit, and the cold leg consisting of a heat exchanger and an EM pump. The difference in height between the highest and lowest parts of the loop was about 2.6 m.

EXPERIMENTAL RESULTS

Complete Inlet Blockage

The most important parameters of the three tests carried out in the 7-pin-bundle under complete inlet blockage conditions are summarised in Table 1. The heat fluxes in this table relate to the surface of the heated parts of the pins, the radial heat losses along the test sections being taken into account. All tests showed the occurrence of dryout, i.e. with the power levels applied, it was not possible to maintain continuous cooling by means of sodium boiling.

As an example of the course of boiling, the results of Test 7-1/46 will be described. After the interruption of coolant flow (Fig. 3), boiling began at a time 21 s, with a little superheat, in the upper part of the heated section (T3 and T16). The boiling zone then proceeded to extend gradually upwards into the unheated section. This expansion phase was followed by periods of local dryout, which were repeatedly terminated by clad re-wetting. Two mechanisms can be proposed [2] for this re-wetting process:

- Liquid slugs which are carried upwards by vapour through the boiling region and/or
- A sodium film which flows downwards from above over the heated surfaces into the boiling zone.

When these re-wetting mechanisms were lost the pins became overheated and power supply was terminated by the excess temperature protection system. Tests 7-1/43 and 45 exhibited similar behaviour. The first re-wetting mechanism works as long as there is liquid in the lower part of the heated section. The conditions under which re-wetting can be expected for the second case are investigated below by means of a theoretical stationary model.

Natural Convection

The most important test parameters are again collated in Table 1. In order to understand the influence of liquid flow resistances in the natural convection loop on the capability for cooling the bundle, the throttle valve was progressively closed after the onset of boiling at intervals of about one minute,

but never to the state of complete closure. As an example, Test 37-20/53 will be described.

Following the switch off of the pump, natural convective circulation in conjunction with intensive boiling began after a delay of about 30 seconds. The increased buoyancy resulting from boiling increased the convective flow and thereby reduced coolant temperature in the test section. Boiling therefore ceased, with a corresponding decrease in the convective head. Convection slowed until boiling recommenced with a superheat of about 25 °C, whereupon convection again accelerated. This cyclical procedure was repeated until more stable steady boiling was eventually reached through further throttling of the flow.

In the upper part of the bundle, a two-phase flow system was developed, which with increasing throttling, extended ever more deeply downwards into the heated section. Fig. 4 illustrates axial temperature distributions obtained for various stages of flow throttling under steady state boiling conditions. Mass flow rates $\dot{m}$ are also illustrated. Dryout was never observed in this test series.

Several of the most important parameters, such as the driving pressure head p_G , vapour quality etc, were calculated for this test (37-20/53), and are summarised in Table II. The pressure drop at the throttle valve p_T was obtained from its measured characteristics. The two-phase pressure drop p_{2Ph} was calculated between the top of the heated section and the position of thermocouple 49 by means of the two-phase pressure drop multipliers reported in [2] for bundle geometry, and extrapolated to cover the entire two-phase region. The remaining liquid flow resistances p_R of the loop (i.e. in the cold leg besides the throttle valve and in the liquid part of the heated section) are obtained from the following equation:

$$p_R = p_G - p_T - p_{2Ph}$$

They show good consistency with reduction in flow rate ($p_R \propto \dot{m}^2$). The saturation temperature at the end of the heated section obtained from the pressure drop calculation is recorded in Fig. 4. This temperature together with that of thermocouple T49 determines the illustrated temperature distribution in the two-phase region.

MODEL OF COOLING CAPABILITY WITH A COMPLETE BLOCKAGE

Assumptions

In order to develop a simple model of a liquid film flowing over the pins counter to the vapour flow, thereby maintaining cooling, it was assumed:

- The highest vapour velocity occurs at the top of the heated section at which point a spacer grid is situated, and it is through this reduced cross section where the total heat produced is transported as vapour flow
- At this point, the mass of liquid transfer through the film must correspond to the mass flow in the vapour
- The balance of gravitational and shear forces between the two flows must in this cross section, which will be referred to as the critical cross section, be equal
- Film and vapour flows lie in the laminar regime

- There is complete wetting of the walls by the liquid
- The surface of the grids themselves do not contribute to liquid transport by a film.

Validation

For the case of a two-phase counter-current flow between vertical parallel plates, it can be shown that the mean film velocity is given by

$$\bar{v}_F = \frac{y_o^2}{k_1 + k_2 \cdot \left(\frac{y_o}{s/2 - y_o}\right)^3 \cdot \left(\frac{s}{2y_o} - \frac{1}{3}\right)} \quad (1)$$

$$\text{with } k_1 = \frac{3\eta_l}{\rho_l \cdot g}, \quad k_2 = \frac{3\eta_v}{\rho_v \cdot g}$$

The derivation of this equation is given in (1). For the case that the film is flowing only over one of the surfaces, Eqn. (1) must be modified to

$$\bar{v}_F = \frac{y_o^2}{k_1 + 2k_2 \cdot \left(\frac{y_o}{s - y_o}\right)^3 \cdot \left(\frac{s}{y_o} + \frac{1}{3}\right)} \quad (2)$$

According to this model, the capacity for heat dissipation is maximised when the product $\bar{v}_F \cdot y_o$ is maximised under the appropriate boundary conditions. These are:

- the geometry is that of the critical cross section
- saturated vapour pressure is assumed in the critical cross section.

It is now further assumed that these equations which were derived for flow between parallel plates, can also be applied, by use of the hydraulic diameter, to bundle geometry. I.e., Eqn.(1) operates on single sub-channels of the bundle, and Eqn.(2) applies to the cross section in the region of a spacer grid. Doing this, one obtains the results illustrated in Fig. 5 for SNR geometry.

APPLICATION TO REACTOR CONDITION

Complete Inlet Blockage

The maximum amounts of heat which can be carried away from the 7 pin bundle and from the SNR 300 MK1A sub-assembly have been calculated and appear in Table III. The influence of the spacer grid is so great in the seven pin bundle that the amount of power which can be stationary removed is only about one fifth that which can be lost from an ungridded bundle. Because of the occurrence of dryout in every experiment, it was not possible to estimate, from the experimental results, the maximum value of disposable heat, so the calculated value could not be confirmed. It can however be stated that at a boiling pressure of 1.55 bar (Table I), a heat flux of 8.3 W/cm² is clearly greater than that which can be dissipated. The calculated value for the ungridded cross section was 9.7 W/cm², whereas allowance for the presence of the grids reduces this

EXPERIMENT	HEAT FLUX [W/cm ²]	SATURATION PRESSURE [BAR]	INCIPIENT BOILING SUPERHEAT [°C]	INLET VALVE	CONVECTION	REMARK
7-1/43	18.7	1.55	15	CLOSED	-	DRYOUT
7-1/45	13.2	1.55	26	"	-	"
7-1/46	8.3	1.55	26	"	-	"
37-20/50	9.2	0.84	8	THROTTLED	NATURAL	NO DRYOUT
37-20/51	13.8	0.84	0	"	"	"
37-20/53	13.7	0.84	25	"	"	"

TABLE I : EXPERIMENTAL CONDITIONS IN DECAY HEAT REMOVAL TESTS

FLOW RATE [Kg/s]	VAPOR QUALITY x [% c]	VOID FRACTION α [% c]	GRAVITATIONAL PRESSURE P _G [BAR]	THROTTLING PRESSURE DROP P _T [BAR]	2-PHASE PRESSURE DROP P _{2PH} [BAR] B)	REMAINING FLOW RESISTANCES P _R [BAR] A)
50.6	1.6	79	0.0305	~0	0.0124	0.0181
38.0	4.8	89	0.0364	0.010	0.0163	0.0101
27.4	11.0	93	0.0407	0.0125	0.0230	0.0052

A) $P_R = P_G - P_T - P_{2PH}$

B) CALCULATED BY MEANS OF LOCKHART-MARTINELLI CORRELATION AS DETERMINED FOR
7-PIN BUNDLE [2]

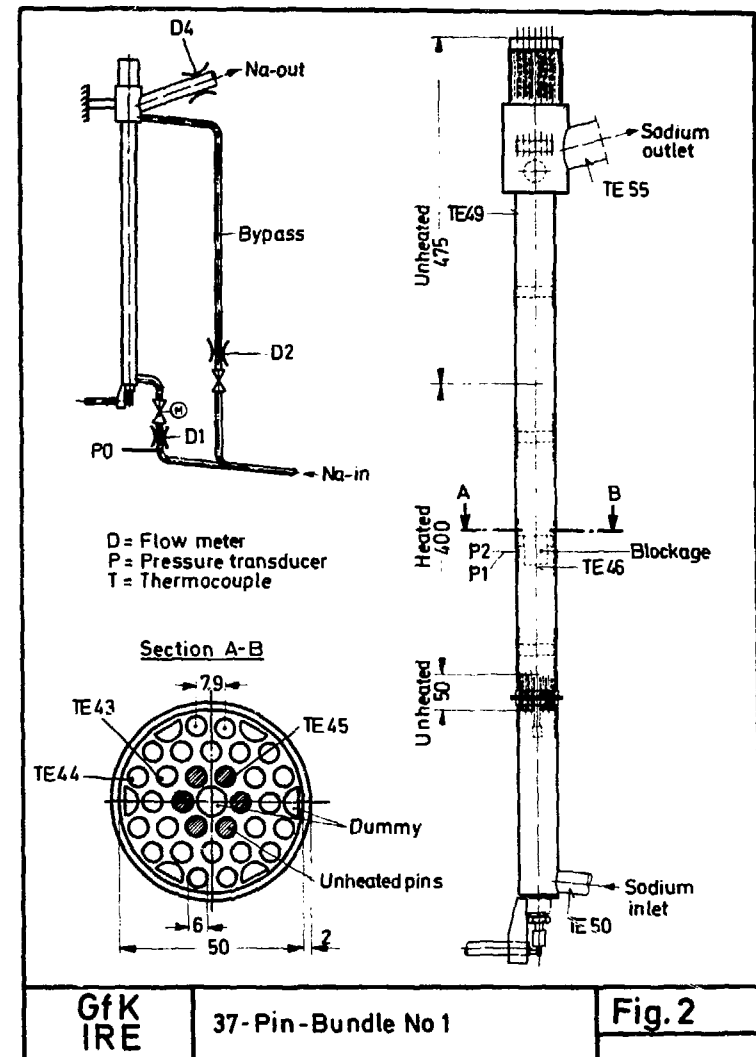
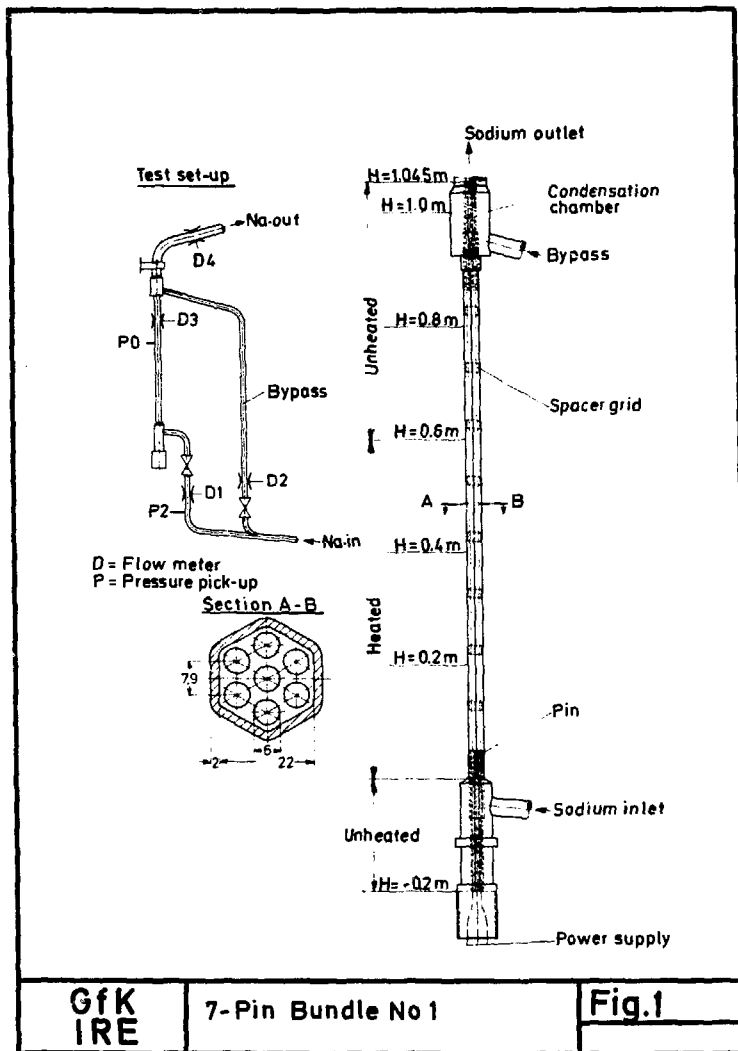
C) AT THE END OF THE HEATED SECTION

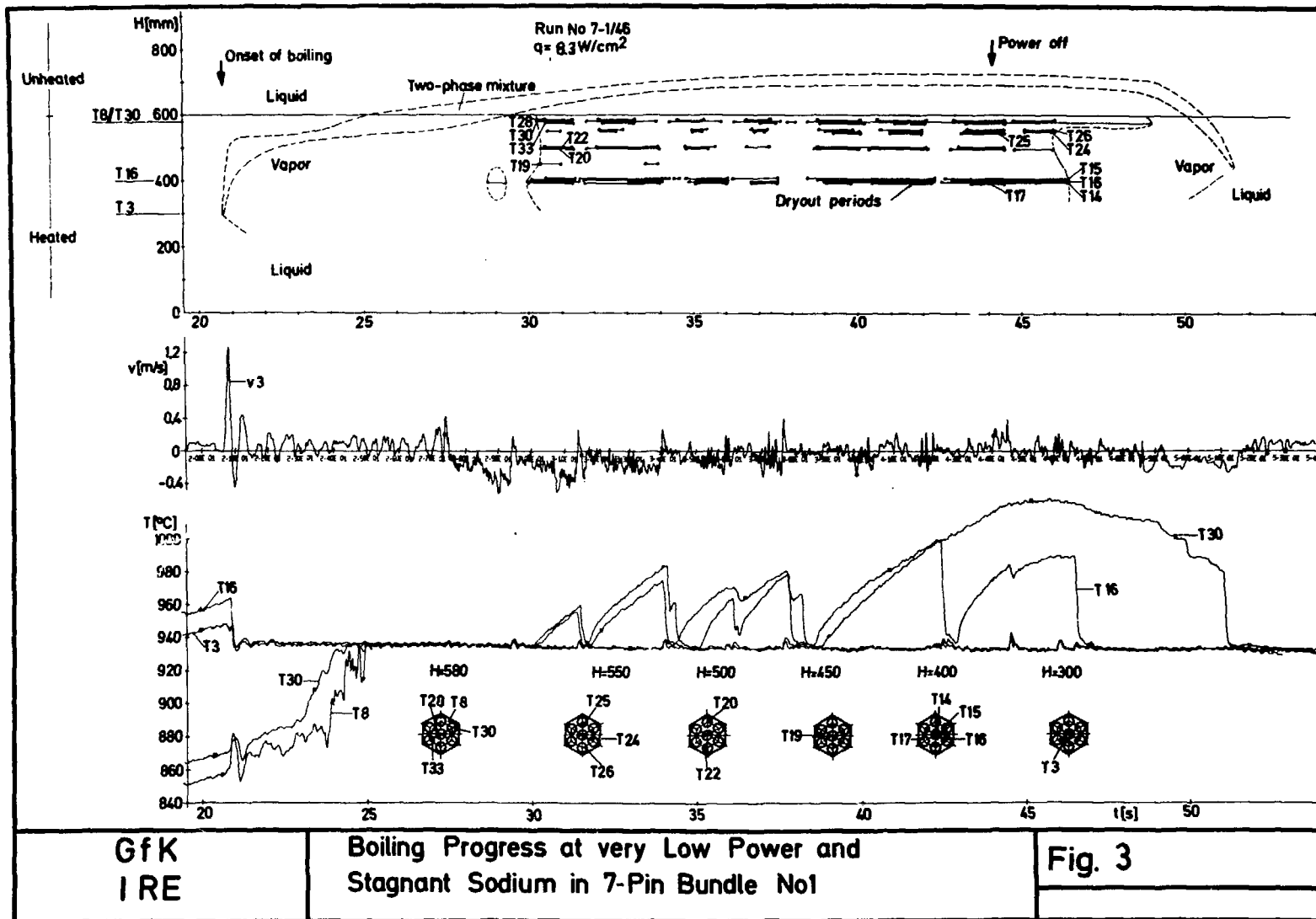
TABLE II : SOME SIGNIFICANT QUANTITIES CALCULATED FROM THE EXPERIMENT 37-20/53

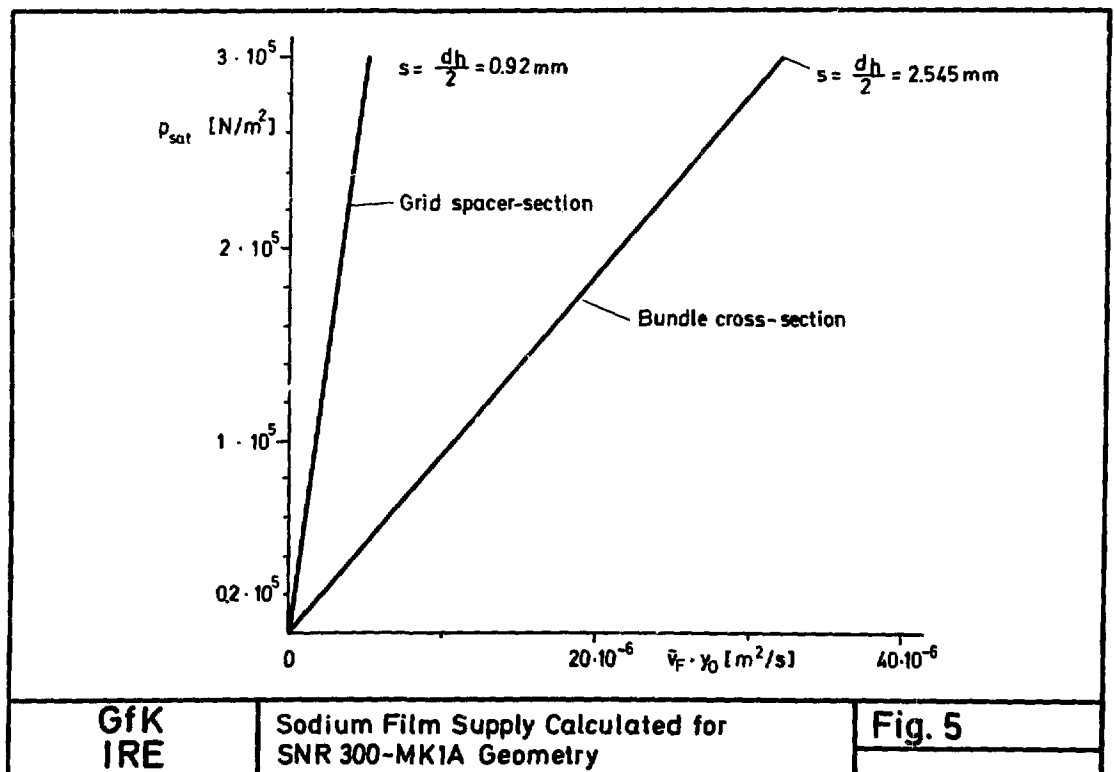
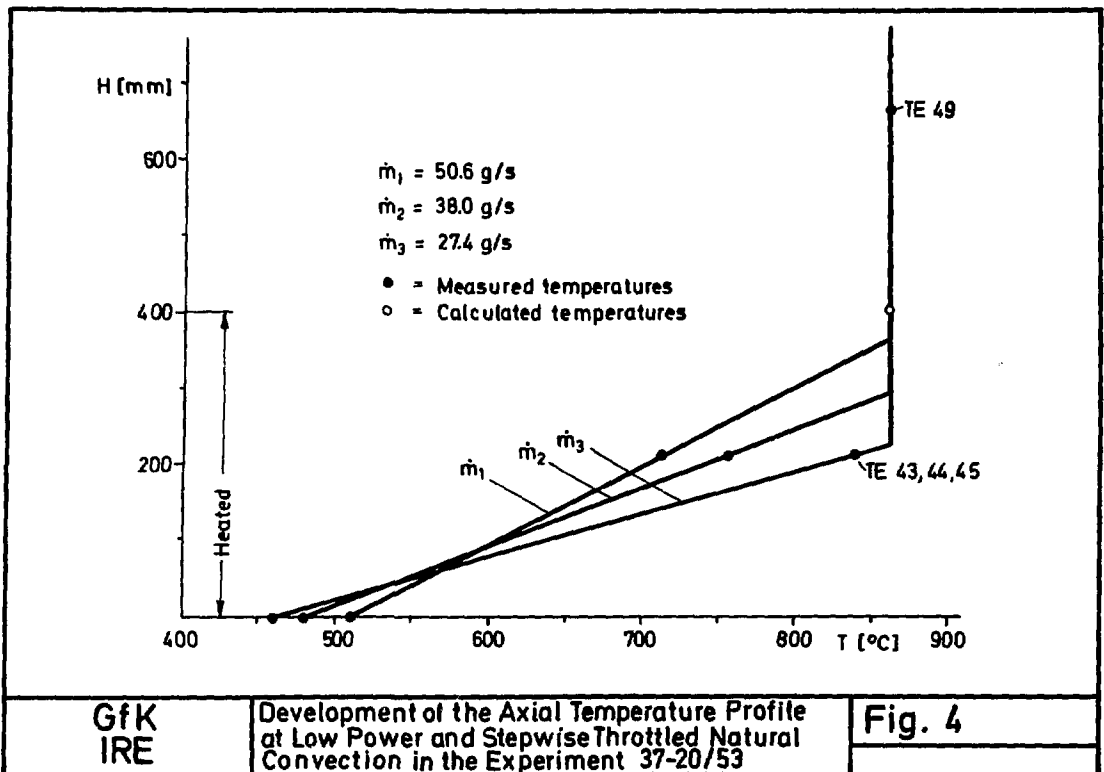
QUANTITY	GEOMETRY							
	BUNDLE CROSS-SECTION				GRID SPACER-SECTION			
HYDRAULIC DIAMETER [MM]	4.25 ^{A)}		5.09 ^{B)}		1.84 ^{A)} B)			
SATURATION PRESSURE [BAR]	0.7	1.1	1.6	2.3	0.7	1.1	1.6	2.3
RATE OF SODIUM SUPPLY · 10 ³ [Kg/s]	0.97 ^{A)} 21.18 ^{B)}	1.37 ^{A)} 30.74 ^{B)}	1.96 ^{A)} 42.23 ^{B)}	2.8 ^{A)} 59.55 ^{B)}	0.22 ^{A)} 3.42 ^{B)}	0.31 ^{A)} 4.96 ^{B)}	0.44 ^{A)} 6.82 ^{B)}	0.63 ^{A)} 9.62 ^{B)}
REMOVABLE HEAT [KW]	3.8 ^{A)} 82.9 ^{B)}	5.3 ^{A)} 118.9 ^{B)}	7.5 ^{A)} 161.6 ^{B)}	10.6 ^{A)} 225.3 ^{B)}	0.87 ^{A)} 13.4 ^{B)}	1.2 ^{A)} 19.2 ^{B)}	1.7 ^{A)} 26.1 ^{B)}	2.4 ^{A)} 36.4 ^{B)}
HEAT FLUX [W/cm ²]	4.9 ^{A)} 2.6 ^{B)}	7.1 ^{A)} 3.7 ^{B)}	9.7 ^{A)} 5.1 ^{B)}	13.3 ^{A)} 7.1 ^{B)}	1.1 ^{A)} 0.4 ^{B)}	1.6 ^{A)} 0.6 ^{B)}	2.2 ^{A)} 0.8 ^{B)}	3.0 ^{A)} 1.1 ^{B)}

7-PIN ELECTRICALLY HEATED BUNDLE A) SNR 300-MK1A-FUEL ELEMENT B)

TABLE III: HEAT REMOVAL CAPABILITY CALCULATED FOR DIFFERENT GEOMETRIES (TOTAL BLOCKAGE)







A MODEL FOR PREDICTING TEMPERATURE FLUCTUATION CHARACTERISTICS BEYOND THE EXIT OF LMFBR SUBASSEMBLIES WITH FAULTS

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ABSTRACT

In this paper a model based on the theory of turbulent diffusion and including the effect of the coolant conductivity is described, which relates the main characteristics of the fluctuating temperature field in the plenum to those of the time average temperature profile at the bundle outlet, which is distorted as a consequence of a flow blockage. With the aim to show the potential of an early failure detection method based on temperature fluctuation measurements in the coolant plenum, the model has been applied to a number of practical situations. The applications concern the signal optimisation, "background" temperature fluctuations and signals caused by typical cooling anomalies.

INTRODUCTION

A local cooling deficiency inside the pin bundle of liquid metal cooled fast breeder fuel element caused by partial blockage of the flow section or by compaction of bowing rods is considered as one of the possible initiating events of a whole core accident. The accident sequence would be a rapid pin-to-pin and fuel-element-to-fuel-element failure propagation after the cooling deficiency zone has reached a critical size. Therefore much attention has been paid in the last years to the exploration of the possibilities to detect cooling deficiencies in an early stage. One of the detection methods considered is based on the measurement of the coolant temperature fluctuations behind the fuel bundle outlet.

In a cooling deficiency zone the coolant temperature is increased by reduction of the local mass flow. The resulting temperature perturbation is convected by the flow towards the bundle outlet. In an early stage of the event no significant global flow reduction in the fuel element is to be expected. In this case the distortion of the temperature profile at the outlet with respect to the known nominal profile is the only signal which could be used to detect the cooling deficiency developing inside the bundle.

In the coolant plenum behind the bundle outlet the time average coolant temperature profile decays by random motion of the fluid particles (turbulent diffusion), which gives rise at the same time to coolant temperature fluctuations. A model based on the theory of turbulent diffusion and including the effect of the coolant conductivity has been developed, which relates the main characteristics of the fluctuating temperature field in the plenum to those of a distorted time average temperature profile at the bundle outlet.

THE PHYSICAL MODEL

Principles

According to the theory of homogeneous turbulence (Taylor [1], Kampe de Fériet [2]) the turbulent diffusivity $\bar{D}_t$ can be expressed by

$$\bar{D}_t = \Gamma_t \cdot \bar{v}^2 \quad (1)$$

$\bar{v}$ is the rms-value of the fluctuating velocity, Γ_t is the Lagrangian integral time scale of turbulence defined by

$$\Gamma_t = \int_0^{\infty} R_L(\tau) d\tau \quad (2)$$

$R_L(\tau)$ is the Lagrangian correlation coefficient. Introducing the Lagrangian integral spatial scale of turbulence Γ_s

$$\Gamma_s = \bar{v} \cdot \Gamma_t \quad (3)$$

$\mathcal{D}_t$ can be expressed in a similar way as the Brownian diffusivity by the product of a length and a velocity

$$\mathcal{D}_t = \bar{v} \cdot \Gamma_s \quad (4)$$

The analogy to the Brownian diffusion process suggests the following interpretation of the turbulent heat diffusion and of the related temperature fluctuations. The fluid consists of particles which have a mean lifetime Γ_t , during which they travel an average distance Γ_s with the average velocity $\bar{v}$, and during which they retain their temperature. At the end of the time period Γ_t the particles take the temperature of the surrounding fluid and start again with the new temperature. A probe placed in the fluid observes during the time interval Γ_t particles arriving from different locations and consequently having different temperatures, which produce at the probe position a time dependent, fluctuating signal. The shape of this signal depends on the surrounding time average temperature field and on the probability $P(y)$, that particles originating from arbitrary distances y reach the probe position in the time period Γ_t . The particle displacement y is related to the instantaneous fluctuating velocity v by

$$y = \Gamma_t \cdot v \quad (5)$$

The above describes the effective turbulent transport only, if the molecular diffusive exchange of the transported quantity with the surrounding fluid during the time period Γ_t is negligible, as generally is the case for turbulent momentum transport. If this exchange becomes important, as is to be expected for the turbulent heat transport in liquid metals, then according to Hinze [3] one may expect for the integral time scale instead of equation (2) an expression of the form

$$\Gamma_t = \int_0^{\infty} R_L(\tau) \cdot f(\alpha, \tau) d\tau \quad (6)$$

$f(\alpha, \tau)$ describes the time dependent heat exchange with the surrounding fluid, α being the respective exchange coefficient. In this case the integral time scale becomes different for turbulent heat and momentum transport.

The situation in the coolant plenum

Near the outlet of the pin bundle the structure of the flow field is determined by the turbulence produced at the position of the sudden increase of the flow section. The turbulence in this region is considered similar to a (quasi) homogeneous and (quasi) isotropic decaying turbulence behind a grid in a wind tunnel. This assumption is directly supported by experimental work of Comte-Bellot and Corrsin [4], presenting turbulence decay measurements for several grids. One of these consisted of round discs having a diameter of 32 mm. Its shape resembles the outlet cross section of a 24 x 18 pin bundle in a quadratic array with a pitch-to-diameter-ratio of 1.58. This grid as well as the conventional types of grids produced the same decay behaviour of the turbulence field.

From the numerous studies on decaying turbulence the following information is obtained:

- Γ_s and $\bar{v}$ can be described by power laws of the type

$$\Gamma_s \sim x^{n_1}, \bar{v} \sim 1/x^{n_2} \quad (7a,b)$$

x being the distance from the turbulence generating grid. According to theoretical considerations of Saffman [5] confirmed by the measurements reported in [4] the exponents n_1 and n_2 have numerical values of 0.4 and 0.6 respectively.

- Increasing the axial convection velocity U and scaling up the dimensions of the grid conserving geometrical similarity, v and Γ_s respectively increase proportionally in terms of the axial distance relative to a characteristic dimension of the grid.

- The probability distribution $P(v)$ of the fluctuating velocity components is represented with a good approximation by a Gaussian normal distribution (see for example Townsend [6]), i.e.

$$P(v) \sim e^{-1/2(\frac{v}{\bar{v}})^2} \quad (8)$$

The influence of the thermal fluid conductivity on Γ_t has been studied by Bobkov et al. [7,8] for the bulk region of fully developed flow in circular tubes. In these studies Γ_t has been determined from measurements of the statistical characteristics of temperature fluctuations. It has been found that Γ_t is proportional to the factor

$$\psi = 1 - e^{-0.62 \cdot 10^{-4} \cdot \text{Re} \cdot \text{Pr}^{1/3}} \quad (9)$$

Reynolds and Prandtl-number in the exponent are respectively measures of the magnitude of the turbulent diffusivity of momentum $\bar{D}_{m,t}$ and of the relative importance of the thermal conductivity. Introducing for $\bar{D}_{m,t/\nu}$ in the bulk region of a circular tube (ν being the kinematic viscosity) the relation

$$\bar{D}_{m,t/\nu} = 0.00155 \cdot \text{Re} \quad (10)$$

eq.(9) takes the more general form

$$\psi = 1 - e^{-0.04 \cdot \bar{D}_{m,t/\nu} \cdot \text{Pr}^{1/3}} \quad (11)$$

This expression can be used to estimate the influence of the thermal conductivity on Γ_t in all flow situations, for which $\bar{D}_{m,t}$ is known.

Based on the above and with the pin radius R_s as characteristic dimension of a pin bundle with a specified pitch-to-diameter-ratio, the following expressions are used for $\bar{v}$ and the turbulence scales Γ_s and Γ_t (the subscripts m and h refer to momentum and heat transport respectively):

$$\bar{v} = C_v \cdot U \cdot (X/R_s)^{-0.6} \quad (12)$$

$$\Gamma_{s,m} = C_{I'} \cdot R_s (X/R_s)^{0.4}, \quad \Gamma_{s,h} = \psi \cdot \Gamma_{s,m} \quad (13,14)$$

$$\Gamma_{t,m} = (C_{I'}/C_v) \cdot (R_s/U) (X/R_s), \quad \Gamma_{t,h} = \psi \cdot \Gamma_{t,m} \quad (15,16)$$

Assuming furthermore the validity of eq.(1) also for decaying turbulence, $\bar{D}_{m,t}$ and the total diffusivity for heat $\bar{D}_{h,tot}$ can be expressed by

$$\bar{D}_{m,t} = (C_{I'} \cdot C_v) \cdot U \cdot R_s (X/R_s)^{-0.2}, \quad \bar{D}_{h,tot} = \psi \cdot \bar{D}_{m,t} + a \quad (17,18)$$

Here a is the thermal fluid diffusivity. C_v and $C_{I'}$ depend on the geometrical characteristics of the bundle outlet cross section and are to be determined by experiment. From the measurements reported in [4] with the grid consisting of discs a value in the range of 0.55 - 0.80 is to be expected for C_v .

The particle statistics are based on the relations (5) and (8). For isotropic turbulence it follows from these relations that the probability distribution for the spatial displacement ρ of a fluid particle between a specified position and the probe position is also a Gaussian distribution, i.e.

$$P(\rho) \sim e^{-1/2(\rho/\Gamma_s)^2} \quad (19)$$

Applying this relation to all fluid particles surrounding the probe, a probability can be attributed to the former that they arrive at the probe position. The same probability $P(\rho)$, that a particle travels the distance ρ , is assumed for the record of its temperature T at the probe position. In this way the temperature of each particle that has a chance to reach the probe is associated with a probability, to be recorded at the probe position. Summing up the probabilities associated with identical particle temperatures a function $P(T)$ is constructed representing the temperature amplitude distribution at the probe position. The characteristics of this function (rms-value, skewness) are used for the description of the fluctuating temperature field.

Determination of the "grid" parameters

In order to determine the parameters C_v and C_T and to ensure that the model describes the real situation in a coolant plenum, recent temperature fluctuation measurements of Greef and Brown [9] have been simulated by model calculations. Fig. 1 shows schematically the arrangement of the test section used in these experiments. A bundle consisting of 31 heater pins and 6 half dummy pins attached to the channel wall is mounted in a round channel and cooled by a water flow. Arbitrary combinations of the pins can be heated. With the aid of an axially movable thermocouple rake radial profiles of the fluctuating temperature signals can be measured at different axial positions in the coolant plenum. In the specific experimental series for which results are presented here pins no. 1, 2 and 3 in Fig. 1 have been uniformly heated, the flow in the plenum being characterized by a Reynolds-number of 41,000. In Fig. 2 theoretically and experimentally determined radial profiles of the time average

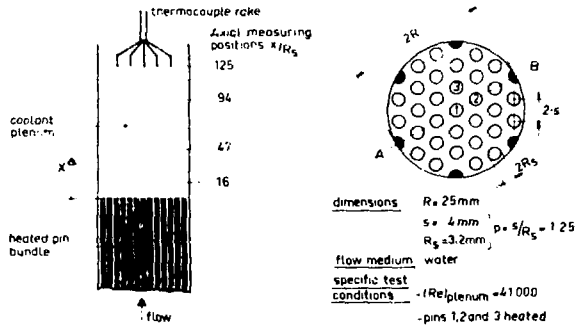


Fig. 1: Experiments of Greef and Brown: test arrangement

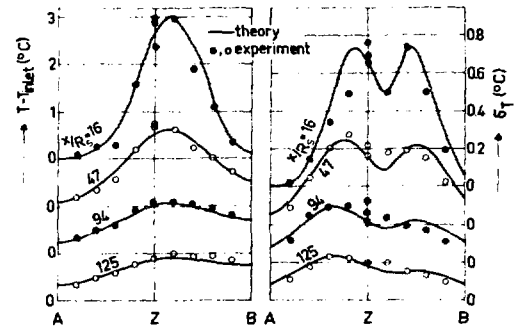


Fig. 2: Comparison of theoretical and experimental results

temperature ($T - T_{inlet}$) and of the rms-value σ_T of the fluctuations are represented for different axial positions behind the bundle outlet. σ_T is defined by

$$\sigma_T^2 = \int_{-\infty}^{+\infty} T^2 P(T) dT \quad (20)$$

T in this relation being the difference from the local time average temperature. The theoretical results have been obtained with numerical values of 0.55 and 0.31 for C_v and C_T respectively. A very satisfactory agreement is observed. Relatively large scattering of the experimental data at the axial position nearest to the outlet indicate that in this region the turbulence cannot yet be considered homogeneous, as also reported in literature for decaying turbulence behind grids in wind tunnels. The numerical value of 0.55 for C_v is in reasonable agreement with the value-range deduced from ref. [4]. That this value is at the lower limit of this range may be due to the fact that both pitch-to-diameter-ratio and type of array are different in the two cases. Due to the relatively high Prandtl-number of water the deviation of the factor ψ from unity is negligible, i.e. the successful simulation of this experiment can be considered a test for the purely turbulent part of the model. A verification of the influence of increasing coolant conductivity on the turbulence properties is still outstanding. To show however the order of magnitude of this effect $\bar{D}_{h,tot}$ and Γ_s are represented in Figs. 3 and 4 respectively, varying the

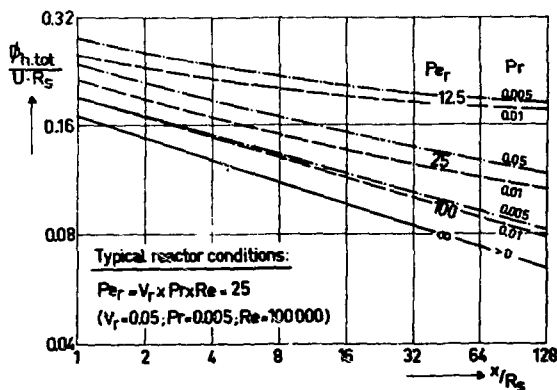


Fig. 3: Total heat diffusivity in a decaying turbulence field

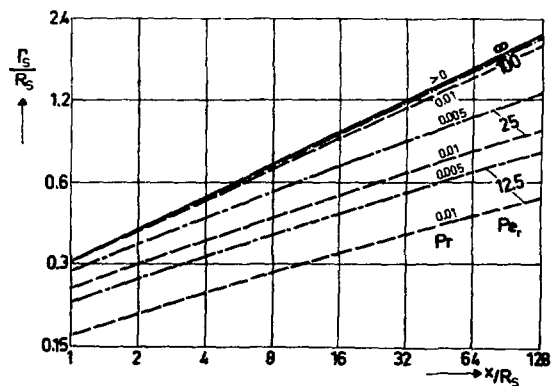


Fig. 4: Lagrangian integral spatial scale of decaying turbulence

thermal conductivity parameters Pe_r and Pr . Pe_r is the Peclet-number based on the pin diameter, defined by the relation

$$Pe_r = V_r \cdot Re \cdot Pr \quad (21)$$

Pr is the coolant Prandtl-number, Re is the Reynolds-number in the plenum based on its hydraulic diameter and V_r is the ratio of the pin diameter to the hydraulic diameter of the plenum. It is observed that the increase of $\bar{D}_{h, tot}$ and the decrease of Γ_s , both reducing the fluctuation intensity, can be quite significant for typical reactor conditions.

APPLICATIONS

Signal optimisation

First the situation illustrated in Fig. 5 is considered. Inside a uniformly heated 217 pin bundle with a pin radius of 3 mm and a pitch-to-diameter-ratio of 1.3 [10] a blockage covering 12% of the flow section [11] is assumed. As a consequence the coolant in the region concerned is uniformly overheated by 50°C. This leads to an undercooling of 6.8°C for the coolant in the remaining region. The time average temperature profile at the bundle outlet, specified for the diagonal AB in Fig. 5, has slightly decayed by heat exchange processes inside the bundle. In Fig. 6 profiles along the diagonal AB of the time average temperature T and of the rms-value σ_T are shown for different axial positions x behind the bundle outlet. The largest distance considered is 30 cm. This may roughly correspond to the space available in a real fuel element. Even at a distance of 30 cm important temperature fluctuations exist only in the region of the sharp temperature gradients. No significant signal could be recorded by a probe in the centre of the plenum, i.e. a corner blockage of this size could not be detected. To enlarge the intensity of the fluctuations use is made of the inherent features of decaying turbulence. Increasing the characteristic dimension of the turbulence generating grid, the initial level of turbulence is increased, turbulent diffusion is accelerated and consequently temperature fluctuations are intensified. One could therefore imagine that behind the bundle outlet a suitable grid is mounted as sketched in Fig. 7. Here a grid made up of discs has been chosen, arranged in a triangular array with a pitch-to-diameter-ratio of 1.3. This allows the use of the same numerical values for C_v and C_r as used for the original bundle outlet. Calculations have been executed assuming the grid consisting of a decreasing number of discs with increasing diameter. Radial profiles of T and σ_T at an axial distance of 30 cm are represented in Figs. 8 and 9 respectively as a function of the disc radius. As expected the time average

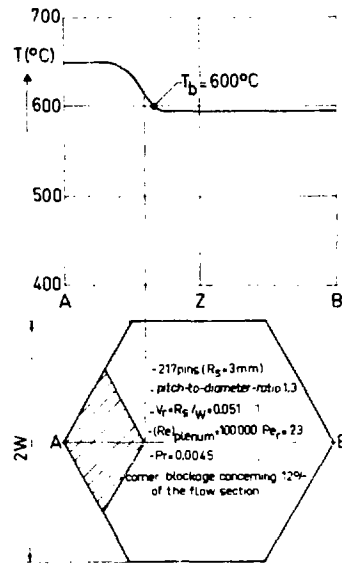


Fig. 5 : Definition of flow blockage and related temperature field at the bundle outlet

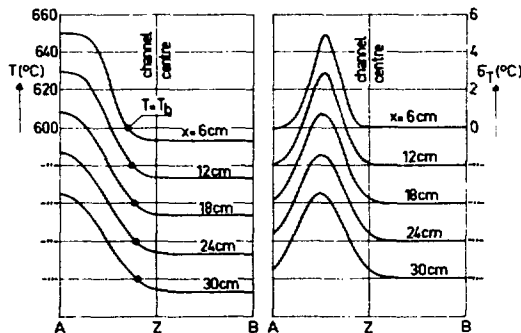


Fig. 6 : Time average temperature and fluctuation intensity profiles for the blockage defined in Fig. 5

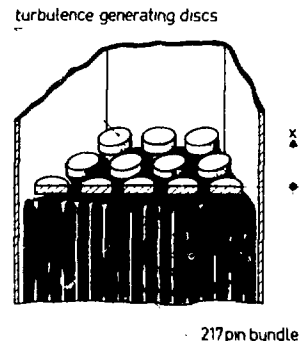


Fig. 7 : Schematic arrangement of turbulence generating discs

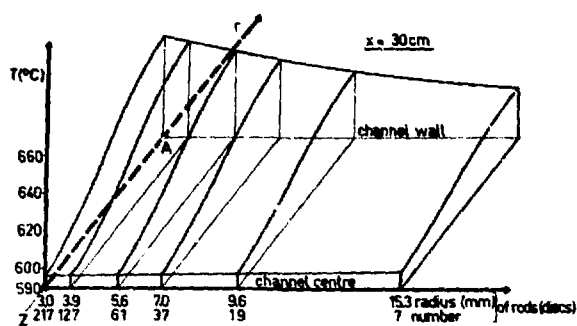


Fig. 8 : Time average temperature profile as a function of increasing disc radius for $x = 30$ cm and the blockage defined in Fig. 5

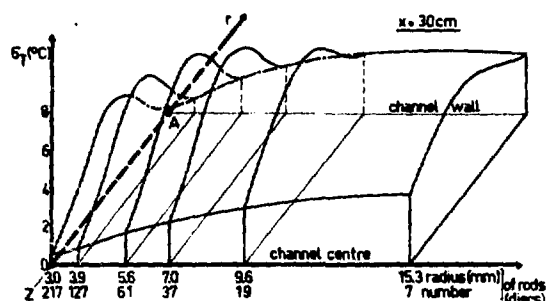


Fig. 9 : Fluctuation intensity profile as a function of increasing disc radius for $x = 30$ cm and the blockage defined in Fig. 5

temperature is levelled with increasing disc radius, whereas the intensity profile first rises, the increasing turbulence scale being dominant, and is then levelled due to growing importance of decreasing temperature differences over the cross section, which are necessary for the generation of fluctuations. The initial rise of the intensity profile is partially due also to the decreasing effect of the coolant conductivity with the rising turbulence level. For the results represented in Figs. 8 and 9 the Peclet-number increases proportionally to the disc radius from the value 23 specified in Fig. 5 up to a value of about 117. From Figs. 3 and 4 it results that for this last value the liquid metal behaves behind the respective grid nearly like a fluid with a negligible thermal conductivity.

As was the intention by introducing the turbulence generating discs a measurable fluctuation intensity of about 3°C can be obtained in the centre of the plenum. For all further illustrative calculations presented in the following paragraphs a grid consisting of 19 discs with a radius of 9.6 mm has been chosen as optimum configuration.

Background temperature fluctuations

The nominal temperature profile at the bundle outlet is generally not flat. It is evident that then fluctuations exist without any cooling deficiency. To give an idea of the order of magnitude of these fluctuations, calculations have been executed for the situation illustrated in Fig. 10. A linear variation of the heat generation across the fuel element has been assumed causing a temperature difference of about 100°C along the diagonal AB. Profiles of T and σ_T have been represented in Fig. 11 for

different axial positions behind the bundle outlet. In spite of the use of a turbulence increasing grid the time average temperature profile decays rather slowly. This is due to the large distances over which heat has to be transported. As a consequence relatively large fluctuations characterized by an intensity of 10.2°C are recorded at the selected

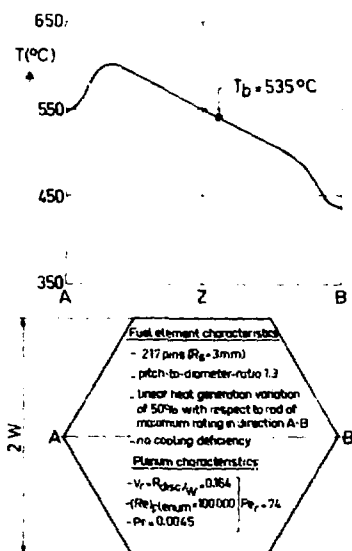


Fig. 10 : Definition of nominal time average temperature profile at the bundle outlet

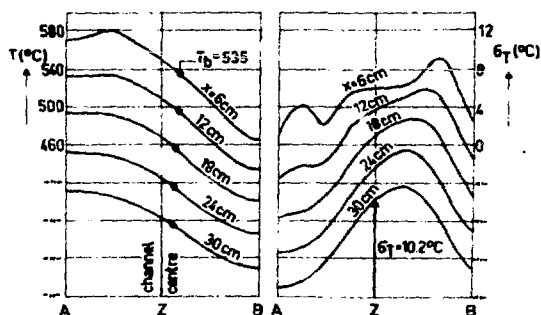


Fig. 11 : Time average temperature and fluctuation intensity profiles for the nominal situation defined in Fig. 10

probe position (30 cm behind the turbulence grid in the centre of the plenum). It is to be mentioned however, that the intensity of these "background" fluctuations decreases with a decrease in the variation of heat generation.

Signals caused by typical cooling anomalies

With respect to their configuration, their size and their location an infinite number of different blockage situations can be imagined. With the aim to show in a more schematical way the potential of an early detection method based on temperature fluctuation measurements, here a few typical examples have been selected, which are described in Figs. 12 to 14. It is assumed that within the extent of

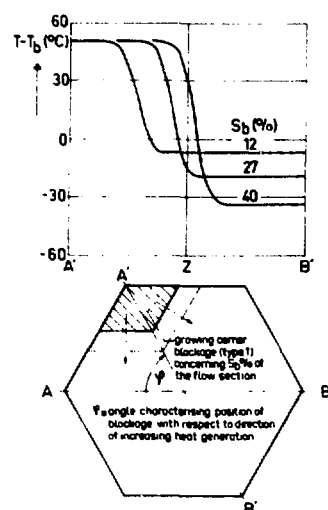


Fig. 12 : Definition of corner blockage type 1 and related temperature field at the bundle outlet

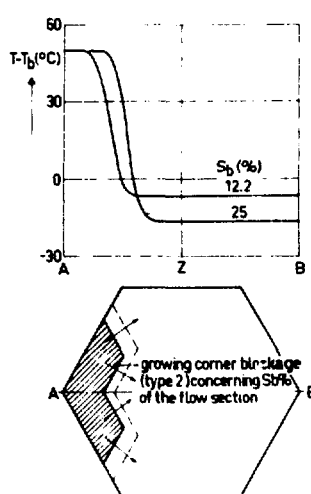


Fig. 13 : Definition of corner blockage type 2 and related temperature field at the bundle outlet

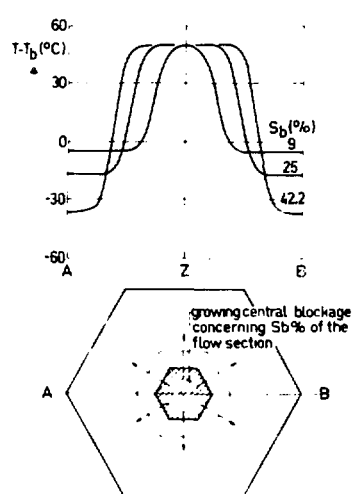


Fig. 14 : Definition of central blockage and related temperature field at the bundle outlet

the zones concerned, characterized by a percentage S_b of the flow section, a uniform overheating of 50°C with respect to the bulk temperature occurs, and that the resulting temperature perturbation profile decays slightly on its way to the bundle outlet. The perturbation temperature field pertaining to this location is superimposed on the nominal field described in Fig. 10. This procedure is illustrated in Fig. 15.

According to Ohlmer and Schwalm [12] in addition to the time average temperature T and the rms-value σ_T , the skewness factor F_s of the temperature amplitude distribution is a useful parameter for the detection of anomalous situations. F_s is defined by

$$F_s = \frac{1}{\sigma_T^3} \int_{-\infty}^{+\infty} T^3 P(T) dT \quad (22)$$

T in this relation being the difference from the local time average temperature. In Figs. 16 and 17 the significance of this parameter is illustrated. In Fig. 16 for a specified blockage the radial profiles of T , σ_T and F_s at $x = 30$ cm are shown. Three positions denoted with 1, 2 and 3 have been selected, for which the complete calculated amplitude distributions $P(T)$ are represented in Fig. 17. It is observed that the shape of $P(T)$ and consequently the related numerical value and the sign of F_s are strongly dependent on the characteristics of the time average temperature field in the vicinity of the position considered. F_s is positive or negative depending whether the position considered is closer to a low or to a high temperature level.

In Figs. 18 to 20 results are presented pertaining to the centre of the plenum at an axial distance of 30 cm behind the turbulence grid, where a measuring probe is supposed to be placed. In Fig. 18 the signals resulting from growing corner blockages are shown, curves "1" and "2" referring to locations in the hot and cold region of the fuel element respectively. The most significant fluctuation characteristic for this situation is the rms-value σ_T . Clear signals could be obtained distinguishing not only the extent but also the location of the damaged zone. Probably no clear distinction could be made between corner blockages of the same extent but of different shape, as can be seen from Fig. 19. There a comparison is made of signals caused by blockages growing preferably in radial and peripheral

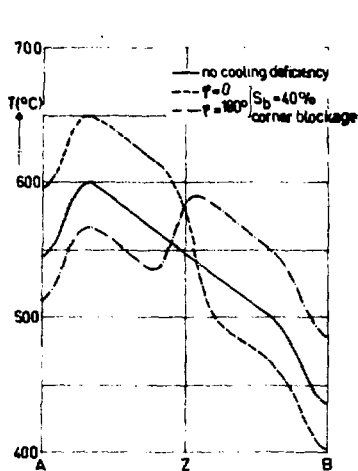


Fig. 15 : Examples for the superposition of perturbation and nominal temperature field at the bundle outlet

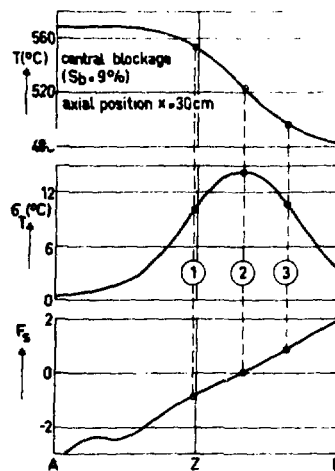


Fig. 16 : Profiles of time average temperature, fluctuation intensity and skewness factor at $x = 30$ cm for a specified blockage

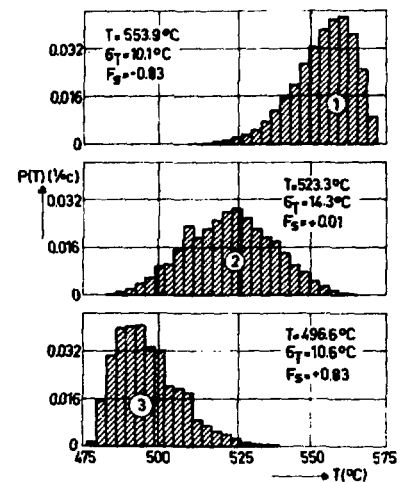


Fig. 17 : Temperature amplitude distribution for the positions specified in Fig. 16

direction. No significant difference can be observed. Fig. 20 shows the results obtained for growing central blockages. Here the importance of the different signal characteristics is inverse. No significant indication is obtained from σ_T , while T and F_s increase continuously with increasing extent of the damaged zone.

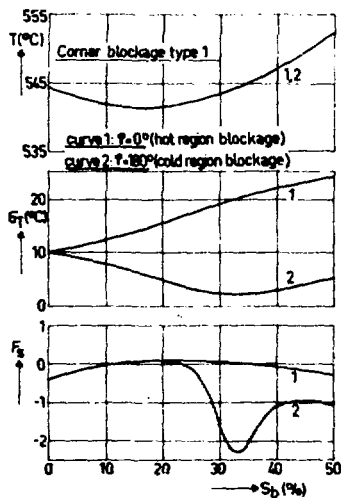


Fig. 18 : Signal characteristics at the supposed probe position caused by a growing corner blockage (type 1)

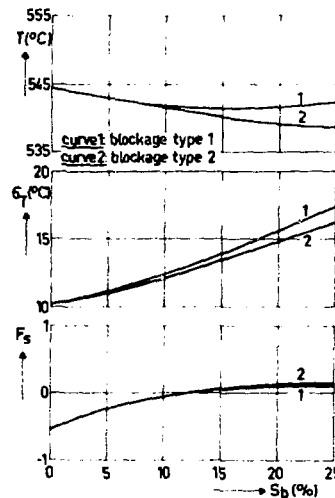


Fig. 19 : Signal characteristics at the supposed probe position caused by growing corner blockages of different shape

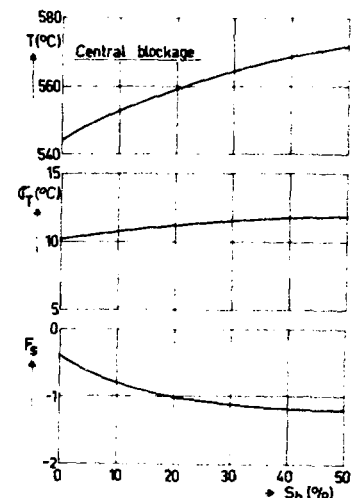


Fig. 20 : Signal characteristics at the supposed probe position caused by central blockages

CONCLUSIONS

Based on the results of this study one may come to the following conclusions:

The theoretical model presented for computing the temperature fluctuation characteristics provides a realistic description of the situation in the coolant plenum. A comparison of theoretical and experimental results shows a very satisfactory agreement. In this connection it is worth to point out

that the turbulence properties used in the model are based on the similarity existing between the situation in the coolant plenum and the well known situation in wind tunnels behind turbulence generating grids.

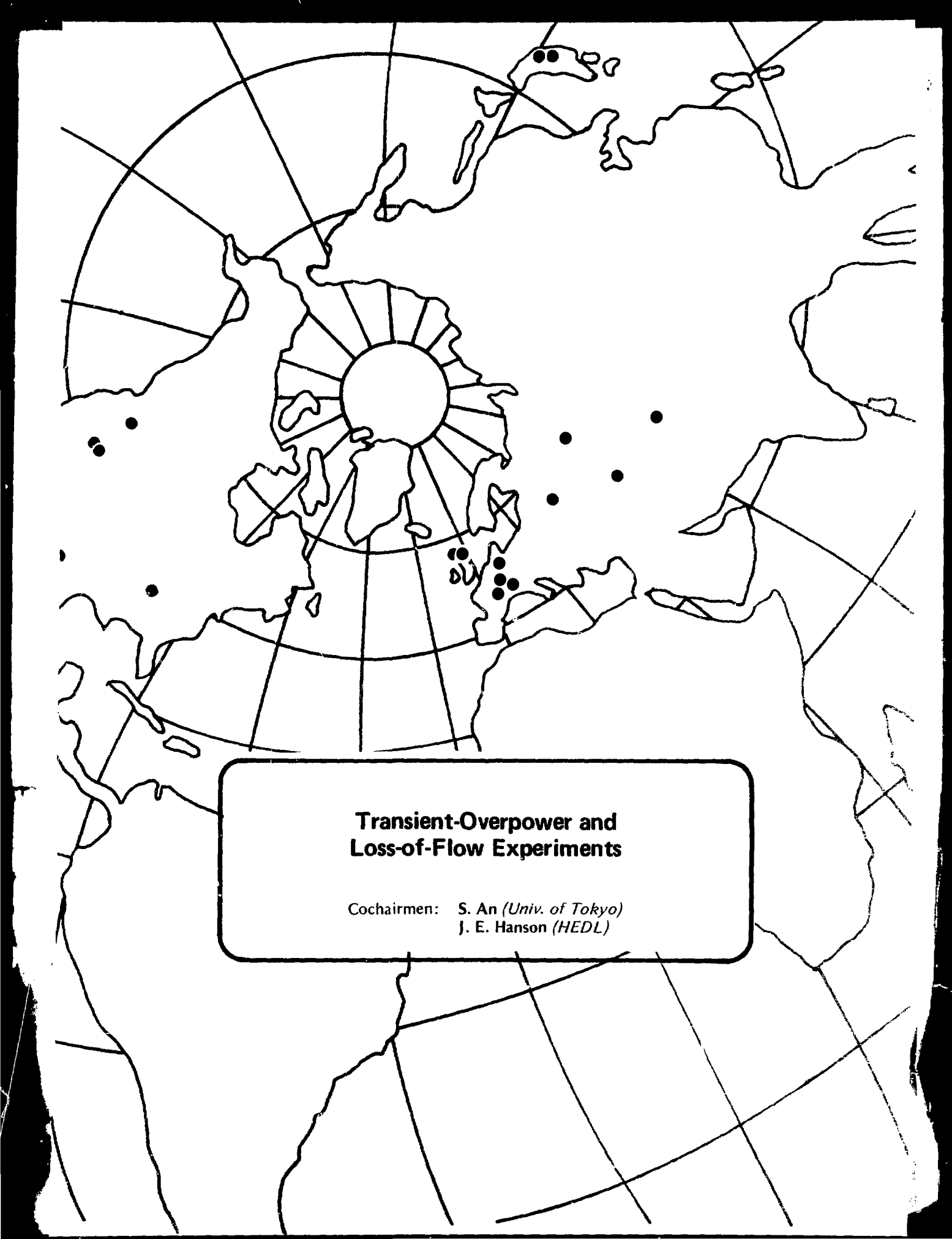
The knowledge of the inherent features of decaying turbulence allows it to indicate a possibility for optimising the signal intensity with respect to a minimum size of flow obstruction which has to be detected. It is emphasized that instead of the turbulence enhancing grid, made up of round discs, proposed in this study any other type of grid could be used, which furnishes the required performance. It may furthermore not be excluded that the cross section of the axial breeder element is designed as a turbulence enhancing grid, using a smaller number of pins with a greater diameter as those of which consist the fuel element.

From the application of the model to some typical cooling deficiency situations one may conclude that exploiting all the information contained in fluctuating temperature signals, a real chance exists to detect cooling anomalies in an early stage. For this it is however postulated that the nominal time average temperature profile at the bundle outlet and all factors, which may change this profile during the lifetime of the fuel element, are known for each subassembly.

ACKNOWLEDGEMENT: Dr. Greef and the management of CEGB are gratefully acknowledged for the permission to use the experimental data to be reported in ref. [9].

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- [10] Note that for the situations considered in this paper (calculations starting with a specified temperature profile at the bundle outlet) the bundle characteristics are used only for the determination of the turbulence properties in the plenum
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Transient-Overpower and Loss-of-Flow Experiments

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HEDL EXPERIMENTAL TRANSIENT OVERPOWER PROGRAM

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ABSTRACT

HEDL is conducting a series of experiments to evaluate the performance of Fast Flux Test Facility (FFTF) prototypic fuel pins up to the point of cladding breach. A primary objective of the program is to demonstrate the adequacy of fuel pin and Plant Protective System (PPS) designs for terminated transients. Transient tests of prototypic FFTF fuel pins previously irradiated in the Experimental Breeder Reactor-II (EBR-II) have demonstrated the adequacy of the PPS and fuel pin designs and indicate that a very substantial margin exists between PPS-terminated transients and that required to produce fuel pin cladding failure. Additional experiments are planned to extend the data base to high burnup, high fluence fuel pin specimens.

INTRODUCTION

HEDL is conducting an experimental transient testing program to evaluate the performance of FFTF prototypic fuel pins up to the point of cladding breach. Primary objectives of the program are to:

- Demonstrate adequacy of fuel pin and PPS designs for terminated transients,
- Determine fuel pin cladding failure thresholds,
- Define fuel pin cladding failure mechanisms, conditions and characteristics, and
- Develop analytic models for fuel pin transient performance prediction.

This paper summarizes results of the HEDL/Fuels Engineering Proof Test (HOP) and the transient overpower test (HUT) experiments conducted to date. Related papers by C. W. Hunter et al, F. E. Bard et al, and R. E. Raars describe cladding failure characteristics observed in recent Fuel Cladding Transient Tester (FCTT) laboratory experiments [1], advancements in analytic modeling [2], and empirical correlation of TREAT test data [3], respectively.

HOP SERIES

Transient tests of prototypic FFTF fuel pins previously irradiated in the

EBR-II are being conducted to evaluate fuel pin performance during postulated FFTF accident transients terminated by the PPS.

The design criteria for extremely unlikely faults (i.e., extremely low probability conditions that are not expected during the plant lifetime) is that in-place coolable core geometry be maintained. A hypothetical 3\$/sec reactivity insertion was defined as the limiting excursion for which the PPS must maintain the coolable geometry.

The fuel pins used in these tests operated at 33kW/m peak power to burnups ranging from 7 to 42 MWd/kg in the EBR-II. The fuel pins were subjected to shaped transients designed to obtain the thermal equivalent of a PPS-terminated FFTF hypothetical 3\$/sec transient in the Transient Reactor Test (TREAT) facility. Experiment results are summarized in Table I. Data analyses indicated the energy input to the fuel pins was within ~5% of desired for HOP 3-1A and 3-3B and ~25% greater than required for HOP 3-2A and 3-2B.

TABLE I

Summary of HOP Test Results

Experiment	Design Peak Temperature at Midplane, K			Actual Peak Temperature at Midplane, K			Peak Cladding Incremental Strain, %
	Fuel	Vol Avg	Cladding Mid	Fuel	Vol Avg	Cladding Mid	
HOP 3-3B		2023	823		2073	813	0
3-1A		2263	823		2253	863	0
3-2A		2223	923		2643	1073	0.4
3-2B		2103	903		2343	993	0.4

Fuel pin cladding did not breach even under the more severe HOP 3-2A and 3-2B test conditions. A comparison of pretransient and posttransient diameter measurements showed no detectable incremental cladding deformation of the HOP 3-1A and 3-3B pins and only 0.4% diameter increase for the HOP 3-2A and 3-2B pins. Figure 1 compares fuel microstructure at the axial midplane for the sibling (nontransient tested) pin and transient tested fuel pin for the HOP 3-3B test which is typical of the results obtained, i.e., effects of the transient on the fuel microstructure were not discernible.

Analyses of these experiments show that the PPS will not only maintain a coolable geometry as required for this limiting event but will prevent loss of cladding integrity. Adequacy of the PPS and fuel pin designs was thereby demonstrated to confine fuel pin transient overpower damage within the design criteria envelope [4].

HUT SERIES

The HUT series were designed to examine the regime of fuel behavior between PPS-terminated transients and cladding failure. This regime is relevant to characterization of failure thresholds and failure mechanisms and is extremely important to the development and correlation of suitable analytic models to be used in reactor safety analysis. The initial tests used fuel pins previously irradiated in the EBR-II at three power levels to evaluate the effect of steady-state power (i.e., fuel microstructure) on fuel pin cladding failure thresholds [5].

The fuel pins used in these tests operated at individual peak power levels

from ≈ 18 kW/m to ≈ 37 kW/m to a burnup of ≈ 50 MWd/kg. The fuel pins were subjected to shaped transients designed to obtain the thermal equivalent of FFTF hypothetical 50¢/sec and 3\$/sec transients not terminated by the PPS. The TREAT transients were terminated at the estimated time of fuel pin cladding failure. Five of the nine fuel pins achieved cladding failure. Results (calculated and observed) are summarized in Table II.

Of particular interest is the transient performance of the fuel pins irradiated at low power in the EBR-II, specifically HUT 5-5A and HUT 3-5A. A peak incremental cladding strain of $\approx 0.22\%$, and a fuel areal melt fraction of $\approx 30\%$ were observed in the HUT 5-5A experiment, but the fuel pin cladding did not fail. An incremental cladding strain of $\approx 0.6\%$ was observed for the HUT 3-5A experiment and the fuel pin cladding did not fail. A comparison of fuel microstructures at the axial midplane for the HUT 5-5A and HUT 3-5A fuel pins and their sibling pin are given in Figures 2 and 3. Results were contrary to what was expected of low power, "gassy" [6], fuel pin transient performance and indicate that low power fuel pins may not be as susceptible to failure as previously thought. Further studies are being conducted (i.e., analysis of other low power pin tests) to verify the observed responses of low power fuel pins and establish their failure mechanisms.

TABLE II
Summary of HUT Test Results

Test	Steady State Pin Power		T_{Clad} Max, K	Inc Cladding Strain, %		Pin Failed?	Areal Fuel Melt, Fraction %
	kW/m	Ramp Rate		Avg	Max		
HUT 5-3A	26.8	50¢/sec	1097	---	---	Yes	47*
HUT 5-5A	17.1	50¢/sec	1033	0.1	0.24	No	31
HUT 5-1A	37.8	50¢/sec	1116	0.22	0.55	No	55
HUT 3-3A	27.1	3\$/sec	1088	0.5	0.56	No	50
HUT 3-5A	18.6	3\$/sec	1130	0.5	0.6	No	0
HUT 5-5B	17.4	50¢/sec	1208	---	---	Yes	49*
HUT 3-1A	37.2	3\$/sec	1103	---	---	Yes	73*
HUT 3-5B	37.2	3\$/sec	1164	0.4	---	Yes	61*
HUT 5-1B	17.7	50¢/sec	1188	---	---	Yes	65*

*Calculated: at failure.

Evaluation of the HUT test results and initial analyses indicate that a very substantial margin exists between PPS-terminated transients and that required to produce fuel pin cladding failure. This is illustrated in Figure 3, and provides additional confirmation for the adequacy of fuel pin and PPS designs for terminated transients.

The results of the HUT Test Series were incorporated into a revised version of the empirical fuel pin failure threshold correlation model developed by Scott and Baars et al [5], which will be discussed in a subsequent paper by R. E. Baars [3]. This fuel pin failure correlation has been incorporated into the MELT-III [7] accident analysis code and its influence upon accident analysis will be discussed in a subsequent paper by N. P. Wilburn et al [8].

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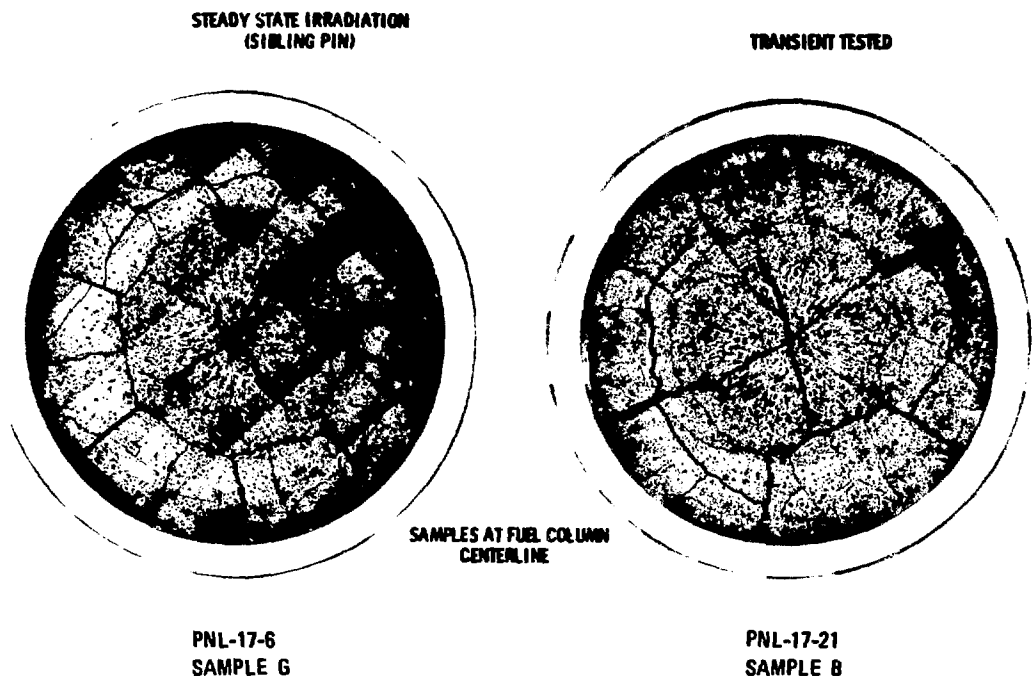
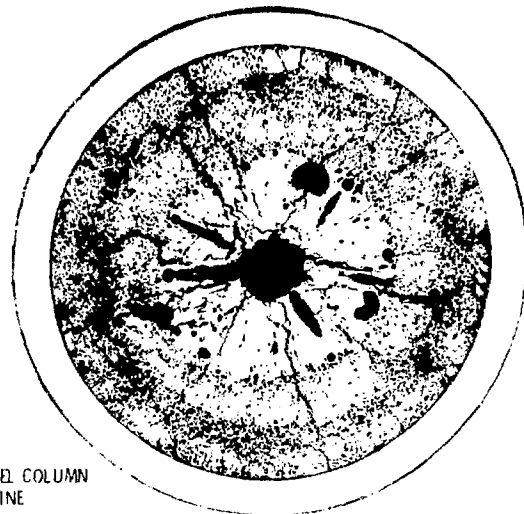
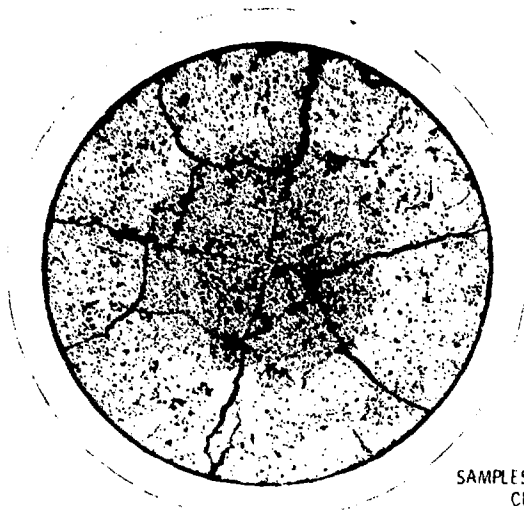


Fig. 1. Comparison of Fuel Microstructure at the Axial Midplane for the Sibling and Transient Tested Fuel Pins (HOP 3-3B).

STEADY STATE IRRADIATION
(SIBLING PIN)

TRANSIENT TESTED



SAMPLES AT FUEL COLUMN
CENTERLINE

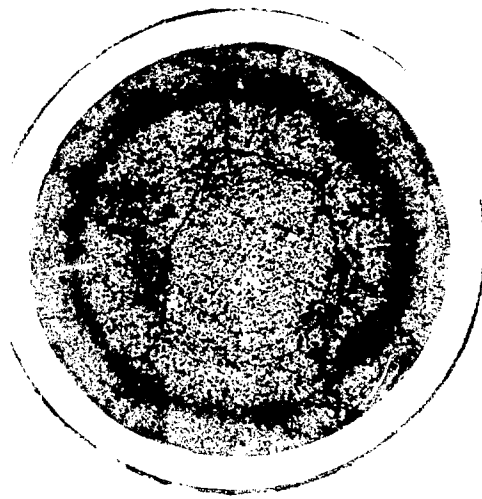
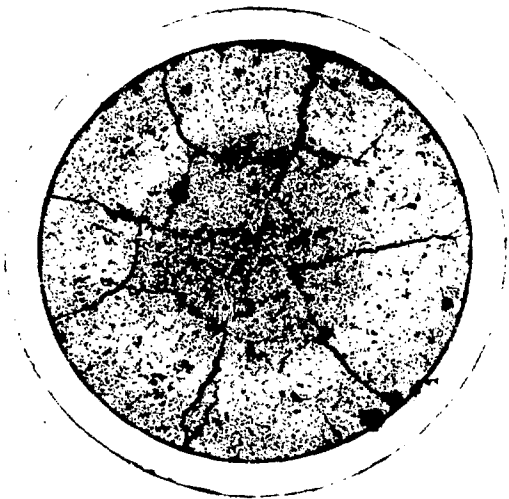
PNL 9-30
SAMPLE L

PNL 9-25
SAMPLE F

Fig. 2. Comparison of Fuel Microstructure at the Axial Midplane for The Sibling and Transient Tested Fuel Pins (HUT 5-5A).

STEADY STATE IRRADIATION

TRANSIENT TESTED

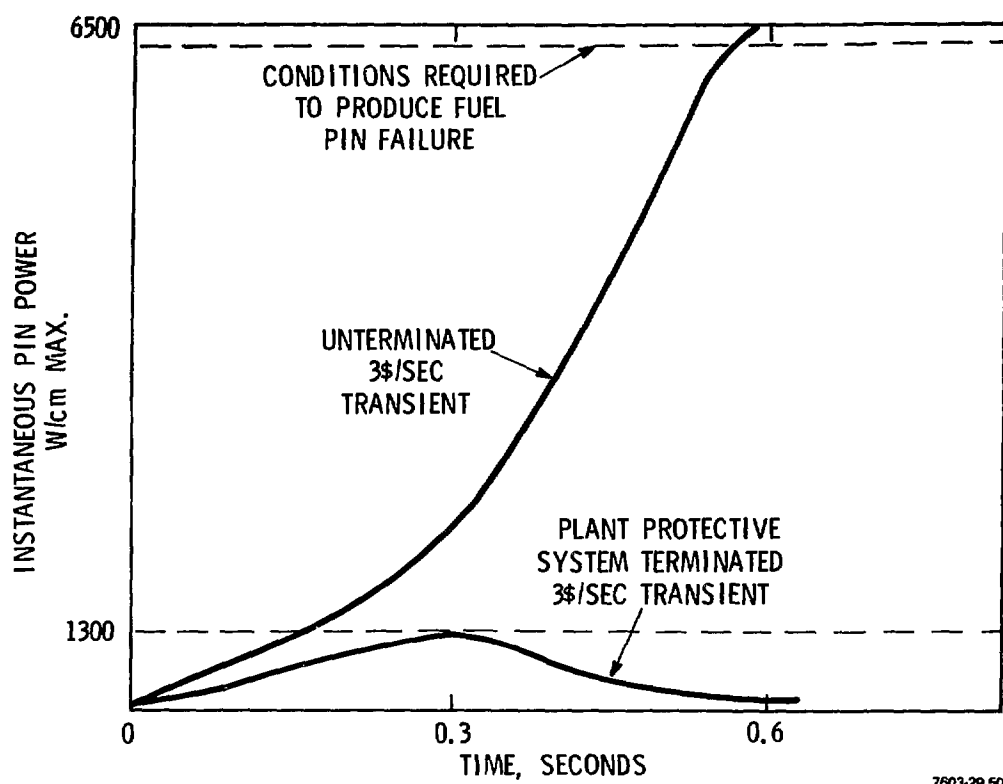


SAMPLES AT FUEL COLUMN
CENTERLINE

PNL 9-30
SAMPLE L

PNL 9-45
SAMPLE H

Fig. 3. Comparison of Fuel Microstructure at the Axial Midplane for the Sibling and Transient Tested Fuel Pins (HUT 3-5A).



7603-29,50

Fig. 4. Margin between PPS-terminated Transients and Fuel Pin Cladding Failure.

LMFBR PROMPT BURST EXCURSION (PBE) EXPERIMENTS
IN THE ANNULAR CORE PULSE REACTOR (ACPR)

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ABSTRACT

A series of in-pile experiments has been initiated to provide information on the energetics of LMFBR cores subjected to hypothetical superprompt-critical excursions. In these experiments, a single fuel pin in an instrumented piston-loaded autoclave is pulse heated into partial vaporization by the ACPR with initial reactor periods as low as 1.4 msec. Nine experiments have been performed with fresh unrestructured UO_2 fuel pins. The first three were conducted without sodium coolant. In the dry experiments, energy depositions up to 3200 J/gm were obtained resulting in low pressure histories, negligible energy conversion ratios, and large upward axial fuel motions within the cladding. With sodium, the energy depositions also ranged up to 3200 J/gm resulting in large sharp pressure events (60 MPa), very low energy conversion ratios (2.1×10^{-4}), and within-cladding axial fuel motions comparable to the dry capsule experiments.

INTRODUCTION

A series of in-pile experiments¹ has been initiated at Sandia Laboratories to provide information on the pressure generation and conversion of thermal energy into mechanical work in LMFBR cores subjected to hypothetical superprompt-critical excursions. These experiments are directed toward the third level of safety which concerns the postulated failure of the protective safety systems simultaneously with the accident they are intended to control. The potential consequences of such hypothetical accidents involving gross core failures must be understood in order to assess the adequacy of containment design.

An initial series of nine experiments using fresh UO_2 fuel pins with and without sodium coolant covering a range of energy depositions is reported in this paper. Subsequent experiments will include preirradiated mixed-oxide fuel, as well as carbide and nitride fuels.

ACPR DESCRIPTION

The ACPR² is a swimming pool reactor based on TRIGA technology with approximately 150 fuel elements 381 mm long using U-ZrH fuel with 20% enriched uranium. It can be operated in the steady-state mode or the pulse mode, with an energy release of 108 MJ and a peak power of 15,000 MW. The minimum initial reactor period is 1.3 msec. The central experiment cavity is 240 mm in diameter and extends vertically to the top of the tank. The pulse neutron fluence is approximately 3×10^{15} n/cm² with half of the fluence above 10 keV. The usefulness of the ACPR³ for fast reactor safety experiments has been examined previously.

EXPERIMENT DESCRIPTION

The experiment capsule is shown in Fig. 1 and accommodates fuel pins with column lengths of 343 mm (EBR-II length). The fuel pin is located in the lower section of the inner pressure vessel surrounded by an annulus of sodium or void followed by a fluted molybdenum liner (0.51 mm wall thickness) which simulates the geometry of six adjacent fuel pins and serves as a thermocouple mounting surface and as a refractory liner for the Inconel 718 pressure vessel (1.27 mm wall thickness). The flow area is 0.82 cm² with a condensation perimeter of 37.1 mm. The experiment contains approximately 55 grams of sodium. Surrounding the fuel pin housing is a beryllium heat sink wrapped by heater tapes and microquartz insulation. At the upper end of the inner pressure vessel is a tungsten alloy piston with a mass of 419 gms and a face area of 2.36 cm² attached to the armature of the linear motion transducer. The piston travel is 64 mm and displaces 15.1 cm³. The top of the piston is tapered and plastically deforms a sleeve in order to dissipate its kinetic energy. A rupture disc/receiver volume safety feature is included.

The diagnostic instrumentation consists of six grounded junction thermocouples of tantalum sheathed W-Re or stainless steel sheathed Ch-Al (1.02 mm O.D) spaced in pairs at the bottom, middle, and top of the fuel pin channel. The pressure transducers mounted at the top and bottom of the fuel pin holder are Kaman Sciences Corporation's Model KP 1911 high temperature pressure transducers rated at 34.0 MPa with a 46 KHz frequency response. The high temperature linear motion transducer is a variable reluctance device with a solid armature manufactured by Moxon, Inc. A scratch gage within the fuel pin cladding provides a passive monitor of the axial fuel motion.

The fuel pins were fabricated according to applicable RDT standards by Hanford Engineering Development Laboratory. FTR-type cladding and cross sectional dimensions are used. The fuel pin characteristics are summarized in Table 1.

RESULTS

Dry Capsule Series

The dry capsule experiments⁴ were given single reactor pulses starting with PBE-1E using a 14 percent enriched pin at lower pulse magnitudes and ending with PBE-3E using a 20 percent enriched pin

and a maximum size pulse. The measured results are summarized for both the dry capsule and sodium-in series in Table 2. The radially averaged peak energy deposition is determined by averaging the radial energy deposition at the peak axial location. The uncertainty in the energy deposition values is ± 10 percent.

In PBE-1E the fuel pellets retained their individual identity although extensive melting occurred. This is evident in the radiographs shown in Fig. 2. The cladding melted, ran down the column, and solidified in droplets. In PBE-2E only the pellets near the ends of the fuel pin retained their individual identity and some vapor was produced near the longitudinal center of the fuel column. The cladding in the lower end of the pin melted, flowed downward, and formed a solid plug near the bottom of the fuel pin. Immediately above this blockage were several large pieces of solidified urania. Annular urania debris remained in the upper part of the fuel pin. The debris was formed when the outer portions of the fuel column solidified because of heat transfer to the cladding and the molten center subsequently voided. No fuel was found above the original fuel pin location.

In PBE-3E the central area occupied by the fuel pin was largely voided and most of the fuel material formed a solid plug in the bottom of the autoclave. Some annular debris was located in the upper part of the fuel pin. No stainless steel blockage was evident in the radiograph. Some fine powder-like fuel debris was observed above the fuel pin. There was significant condensation and flow of urania on the walls of the molybdenum liner and there were numerous little droplets of stainless steel scattered along the length of the experiment attached to urania debris.

The measured pressures were low (< 0.6 MPa) and the ratio of the piston's kinetic energy to the deposited thermal energy was negligible. An important result which would produce significant negative reactivity in an LMFBR excursion was the measured upward axial fuel displacement within the cladding. This expansion occurs during the energy deposition time and prior to cladding failure. This conclusion is based on the acoustic relief time of the fuel column which is shorter than the energy deposition time which, in turn, is shorter than the radial thermal time constant of the fuel pin. In the first experiment, PBE-1E, where the peak energy deposition was slightly greater than the melting point, the axial expansion was about 2 percent. In PBE-2E and PBE-3E, the plenum spring was compressed to its solid height by an axial fuel expansion of 50 mm, or 15 percent of the fuel pin height. This motion is independent of sodium presence in the coolant channel.

Sodium-in Series

In the first sodium-in experiment, PBE-1S, subjected to a peak energy deposition of 1570 J/gm in a single pulse, the cladding did not fail and no sodium vaporization was detected. PBE-2S was a multiple pulse experiment similar in energy deposition to PBE-1S.

In PBE-3S, a 20 percent enriched pin was subjected to a multiple reactor pulse with 920 J/gm and 1450 J/gm in the first and second pulses, respectively. The cladding did not fail, but there was significant fuel melting and flow within the fuel column as seen in the radiograph (Fig. 2).

In PBE-4S, a 14 percent enriched pin was given a maximum single pulse depositing 2700 J/gm. The cladding failed producing a sharp pressure event followed by some pressure oscillations and a delayed event as shown in Fig. 3. The piston was fully displaced which can void 185 mm of flow channel length. Disassembly of the experiment revealed that the rupture disc had broken permitting the experiment contents to expand into the safety plenum. The peak pressures at the top and bottom of the capsule were 25.8 MPa and 41.4 MPa respectively. The bottom pressure transducer is located half as far from the pin failure location as the top transducer. The delayed pressure events are coincident with the failure of the thermocouple at the central location. The radiograph (Fig. 2) indicates that the central 150 mm of the capsule was voided with material condensed on the molybdenum liner and accumulations above and below the voided area. The cladding is well defined for a length of 80 mm at both the top and bottom of the originally fueled area which is now largely devoid of fuel. Calculations based on the thermodynamic state of the fuel at clad rupture indicate that the fuel can flash into the low pressure channel thereby voiding that portion of the cladding. The energy at failure given in Table 2 is a radial average and does not include peaking factors.

PBE-5S was a repeat of PBE-4S except that a stiffer rupture disc was used which did not break during the test. The pressure events as shown in Fig. 3 are similar to PBE-4S with peak pressures of 31.2 MPa and 41.5 MPa measured at the top and bottom respectively. Delayed events were observed on both pressure signals and the thermocouples at the center indicated that some cooling occurred at the same time. This may be caused by sodium re-entering the voided region. From the radiographs, it is evident that fuel is distributed nonuniformly in the upper flow channel accounting for the large difference in the results of the top thermocouples. The piston energy was 24J giving an energy conversion ratio of 2.1×10^{-4} . The radiographs for 4S and 5S are quite similar in terms of void length, plugging and empty well-defined cladding. The plenum spring in 5S is fully compressed, indicating large axial in-clad fuel motion.

In PBE-6S, the fuel pin enrichment was 20 percent yielding an energy deposition of 3200 J/gm for a maximum single pulse. The results are different from the previous experiments in that there was an early pressure event of 7.3 MPa measured at the bottom of the capsule followed five milliseconds later by gross pin failure and large pressure events. The peak pressures were 15.8 MPa and 59.6 MPa measured at the top and bottom of the capsule respectively. It is apparent that some blockage attenuated the pressures reaching the upper pressure transducer. The piston energy of 20 J was slightly less than in PBE-5S. The radiograph indicates significant voiding over a 250 mm length at the center with significant amounts of UO_2 frozen on the molybdenum liner. There are 20 to 30 mm lengths of voided cladding visible at the top and bottom of the fuel-pin and the plenum spring is fully compressed. Large amounts of fuel are in the flow channel above the fueled area.

Comparison with TREAT Test S-11

Some of the results from the ANL TREAT experiment, S-11⁵, are included in Table 2 for comparison. The average energy deposited at failure is greater in S-11 than PBE-4S/5S, although from the measured results, it is evident that the fuel was in a higher thermodynamic state (pressure and temperature) in PBE. Some of the difference is caused by heat transfer out of the fuel during the pulse and lower initial temperatures in S-11 and the uncertainty in the PBE dosimetry. Thermal stresses in the cladding at high heating rates can also contribute to earlier failure.⁶ Calculations for PBE indicate that temperature differences of 630K are achieved across the cladding. In the PBE experiments as in S-11 there is an initial pressurization which can be attributed to clad yielding prior to rupture followed by a rapid increase and quenching of pressure with some structure in the peak of the pulse. There are also some delayed pressure events which occur earlier in time in PBE. Sodium can reenter the voided area quicker since the specific energy deposition is less than half of that in S-11. The piston constraint in PBE is 2.8 times the constraint in S-11 which influences the measured pressures. The energy conversion ratio is higher in S-11. More fuel material is available to interact in S-11 since the fuel is uniformly heated axially while in PBE there is significant axial buckling.

In S-11, the pressure history was interpreted in terms of fuel vaporization. Given the error bounds on energy deposition in PBE and using radial peak-to-average values, the results of the S series and PBE series are consistent. Preliminary analysis indicates that the same general conclusions can be drawn regarding fuel vapor as the identity of the working fluid. These conclusions are reinforced if uncertainties in the uranium vapor equation-of-state are introduced.⁷

CONCLUSIONS

An initial series of experiments has been performed with fresh UO_2 under prompt burst excursion conditions. In the voided capsule experiments, very low pressures were observed resulting in negligible energy conversion ratios. Pre-failure in-clad upward axial fuel motions of 15% were measured. The implications are that in a fresh voided reactor subassembly under prompt critical conditions with energy releases comparable to these experiments, no work potential would exist and the in-clad axial fuel motion would contribute large negative reactivities.

With sodium in the experiments, large pressure events were observed, however, these resulted in low energy conversion ratios. The measured axial fuel motions were also 15%. The implications are that in a fresh reactor subassembly under similar energy depositions and rates, large pressures would be produced but resulting in very low work potential.

A comparison was made with the ANL S-11 experiment. The results are consistent indicating that fuel vapor pressure is the most likely candidate for the working fluid. However, more analysis and detailed modeling must be performed to substantiate this conclusion and to rule out other phenomena such as sodium liquid or vapor expansion. The influence of the relatively cold structure

in the experiments on vapor quenching needs to be examined as to its impact on energy conversion ratios. There is evidence that the fuel may flash to vapor on pin failure rapidly voiding the clad. This indicates that the axial failure location is critical for reactivity considerations. It is noted, however, that the large axial expansion of the fuel column within the clad occurs prior to pin failure.

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Table 1. Fuel Pin Characteristics

Fuel Material	UO ₂
Uranium Enrichment (%)	14-20
Fuel Mass (g)	64
Planar Smeared Density (%TD)	11.7
Fuel Pellet Diameter (mm)	4.93
Fuel Length (mm)	343.7
Fuel-Clad Bond	Helium
Plenum Length (mm)	90
Spring Solid Height (mm)	39.4
Cladding Material	316SST
Cladding Outside Dia. (mm)	5.83
Cladding Thickness (mm)	0.38
Wire Wrap Diameter (mm)	1.39

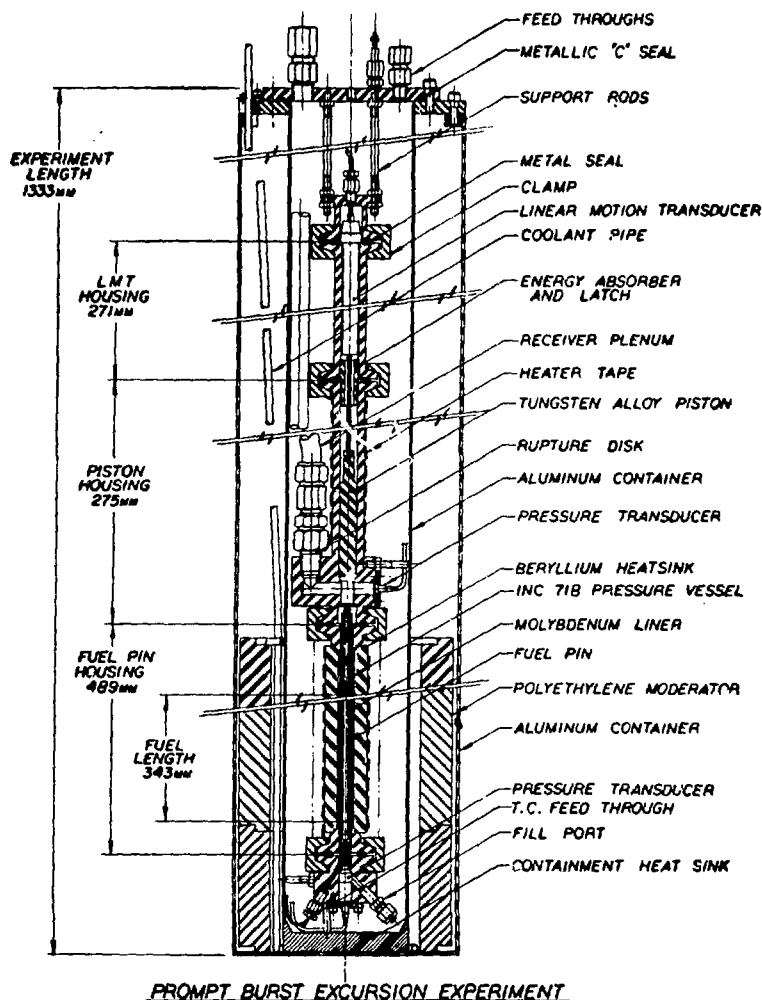


Figure 1. Schematic of Prompt Burst Experiment

Table 2: Summary of Measured Results

	PBE-1E	PBE-2E	PBE-3E	PBE-4S	PBE-5S	PBE-6S	S-11
Reactor Characteristics							
Pulse Mode	Single	Single	Single	Single	Single	Single	Single
Initial period (msec)	2.3	1.4	1.4	1.5	1.4	1.4	23
FWHM (msec)	7.3	4.7	4.8	4.9	4.8	4.8	104
Reactor Yield (MJ)	61	105	104	102	103	104	18000
Pulse Yield (MJ)	49	86	85	84	84	85	----
Initial Conditions							
Temperature (K)	420	650	770	770	770	770	403
Pressure (MPa)	0.14	0.24	0.28	0.28	0.28	0.28	0.15
Coolant	Void	Void	Void	Sodium	Sodium	Sodium	Sodium
Uranium Enrichment (%)	14	14	20	14	14	20	13
Results							
Radial Deposition	1.44	1.44	1.72	1.44	1.44	1.72	----
Profile (Peak/min.)							
Axial Deposition	1.24	1.18	1.12	1.12	1.12	1.12	1.05
Profile (Peak/ave)							
Radially Averaged Peak	1570	2700	3200	2700	2700	3200	7470
Energy Deposition (J/gm)							
Energy at Failure,	----	----	----	1910	1950	2380	2220
Radial Average (J/gm)							
Total Energy at Peak	----	120	150	109	113	132	66.5
Piston Momentum (KJ)							
Total Energy Deposited	81.2	147	184	147	147	184	202
(KJ)							
Piston Kinetic Energy(J)	0.0	2.2(-3)	0.33	5.6	24	20	60
Measured Energy	0.0	1.8(-8)	2.2(-6)	5.1(-5)	2.1(-4)	1.5(-4)	9.0(-4)
Conversion Ratio							
Peak Pressure (top) (MPa)	----	----	0.34	25.8	31.2	15.8	----
Peak Pressure (bottom)	0.27	0.48	0.58	41.4	41.5	59.6	15.0
(MPa)							
In-clad Axial Fuel	6	49	52	---	50	50	----
Expansion (mm)							

PROMPT BURST EXCURSION EXPERIMENTS
POST TEST RADIOGRAPHS



FIGURE 2. RADIOGRAPHS OF PBE SERIES

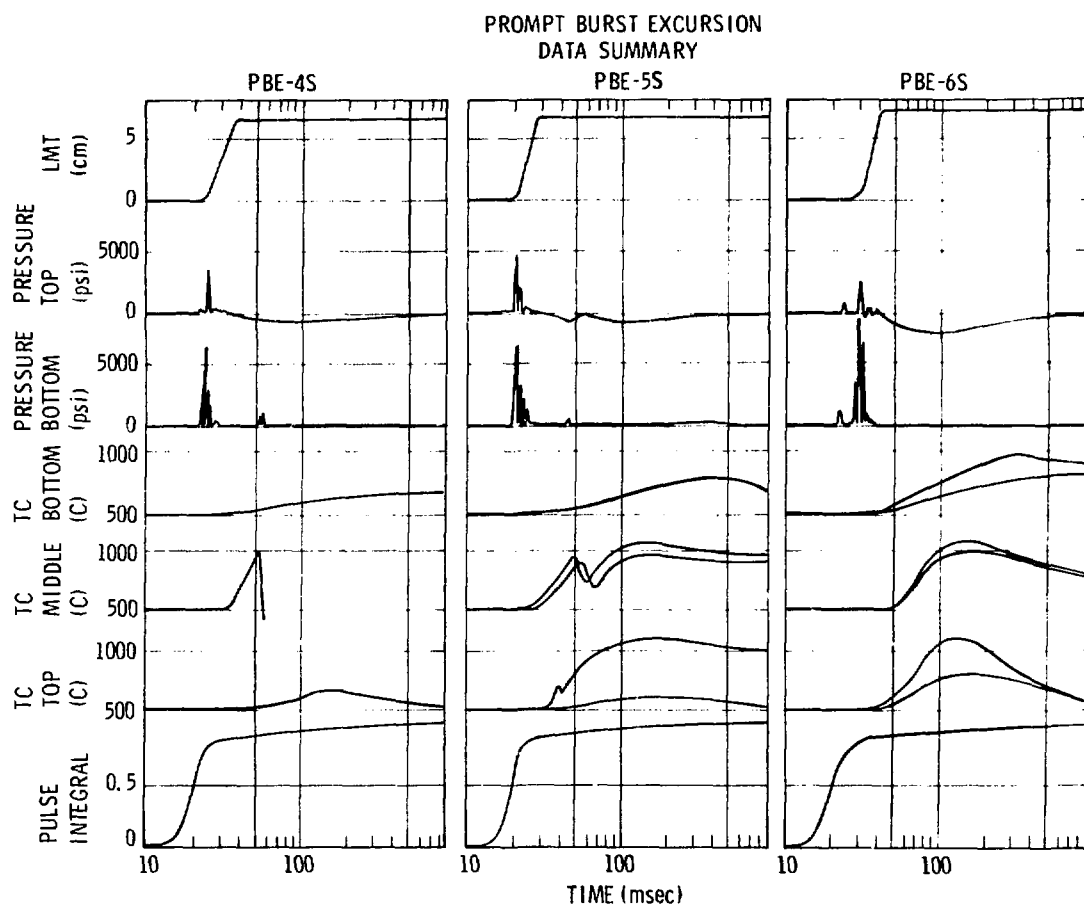


Fig. 3. Data Summary for PBE-4S, PBE-5S, and PBE-6S

LOSS-OF-COOLING EXPERIMENTS WITH SINGLE FUEL PINS

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ABSTRACT

This paper deals with some results of three series of Loss-of-Cooling experiments, namely with 1) fresh fuel pins, 2) pre-pressurized fresh fuel pins and 3) pre-irradiated fuel pins. These pins were tested in sodium filled capsules. Furthermore the Loss of Cooling (LOC) time and the type of the shroud tube (closed or with slots), were used as parameter. The results indicate that considerable clad melting can occur without leading to fuel failure. In cases fuel failure occurred this happened very gently. Preliminary results indicate that in case of equal energy input the extent of pin failure decreases by using a slotted shroud tube and pre-irradiated fuel pins. Considerable gas blanketing can occur if fuel pins with high internal pressure are subjected to a Loss of Cooling condition. The occurrence of a pressure pulse in case of an experiment with a low pressure, low burn-up fuel pin is most likely attributable to an FSI. So far no significant deformations of the capsule construction have been observed, so it can be concluded that the fuel failures were of a benign nature.

INTRODUCTION

The objective of this paper is to discuss some results obtained from a series of Loss-of-Cooling experiments performed in the framework of the DeBeNeLux SNR-300 project.

These experiments consist of the irradiation of single fuel pins in sodium-filled capsules and are carried out in the Poolside Facility of the High Flux Reactor at Petten.

A description of the experiments and the program has been given in Ref. /1/. Details of the capsule construction can be found in Ref. /2/.

To summarize, the loss-of-cooling condition is achieved by introducing a sudden interruption of the radial heat flow passing from the fuel pin and the surrounding stagnant sodium to the outer capsule construction. This is done by blowing away the NaK from the annulus which surrounds the sodium containment. The result is a temperature increase of the sodium, cladding and fuel of about 750C/sec. up to sodium boiling. The experiments known as LOC experiments, are terminated by a reactor scram either on clad failure or after a pre-set time (LOC time). The linear power is kept constant throughout the experiments at approximately 560 W/cm.

The fuel pins consist of 8% enriched, 91% TD UO₂ pellets 5.1 mm. in diameter, clad in stainless steel, Sandvik 12R/2HV (X8CrNiMoTiB 1515), with an inner diameter of 5.24 mm and a wall thickness of 0.38 mm.

The main objectives are to determine the failure modes, mechanisms, thresholds and consequences of fuel pin failures under simulated loss of coolant flow conditions. In addition, it is intended to investigate under which conditions fuel-cladding-sodium interactions will occur and to establish if such interactions lead to deformation of the surrounding stainless steel structure. Furthermore, attention is given to the release of fission gases as well as to fuel and cladding relocation. These phenomena are also potential failure propagation mechanisms.

The main parameters are:

1. Loss-of-Cooling (LOC) time, defined as the time between start of NaK-ejection and reactor scram.
2. the internal pressure of the fuel pin and
3. the burn-up of the pre-irradiated fuel pins.

Up to now, 14 LOC experiments have been carried out, of which seven were with fresh fuel pins without internal gas pressure, four with pre-pressurized fresh fuel pins, and three with pre-irradiated fuel pins. So far a max. burn-up of 20 Mwd/kg UO₂ was achieved. It was recognized from the start that the results of experiments with single fuel pins might be strongly influenced by the geometry used. In the first series of experiments the fuel pin was, for safety reasons, surrounded by a closed shroud tube. Later on, some capsules were equipped with a slotted shroud tube. This gives a better simulation of a pin in a fuel element.

EXPERIMENTAL RESULTS

A. Fresh fuel pins

The main parameter in this series of experiments is the LOC time. Although results of these experiments were reported earlier /1.3/, some of the conclusions will be repeated. In addition new information is presented.

It was observed /3/ that failure of fresh fuel pins is due to clad melting and occurs within about 1 second after the start of sodium boiling.

Clad melting over a length of 150 mm., without leading to disintegration of the fuel stack, has been observed in an experiment with a normalized LOC time (NLT) 1) of 7.6 sec. The maximum amount of central fuel melting was 22%.

Disintegration of the fuel stack was observed in an experiment (L04) with an NLT of 11.9 sec. The length of the clad melting was 190 mm. and the maximum amount of fuel melting 30%.

In the following experiment (L05), the LOC time was increased to 17.5 sec., corresponding with an NLT of 14.5 sec. A pressure pulse of 12 bar was measured 0.8 sec. before termination of the experiment. Most likely this pressure pulse was the result of a fuel-sodium thermal interaction (FSI) caused by some hot fuel particles dropping to the bottom of the capsule where liquid sodium was present. This is supported by a measured steep temperature increase of this sodium at the moment of the pulse.

- 1) Because the irradiation conditions differ somewhat with respect to (a) stationary sodium temperature, (b) linear power, and (c) rate of sodium temperature increase, the LOC times of the different experiments cannot be compared in a straight forward way. Therefore a normalized LOC time (NLT) was introduced based on normalized conditions /3/.

One more experiment (L08) with a pre-set LOC time of 17.5 sec. was carried out. The NLT in this case was 13.4 sec. which is 1.1 sec. less than the aforementioned experiment L05. A comparison of the results of both experiments is given in Refs. /3, 5, 6/.

The appearance of the fuel stack of experiment L08 was almost identical with that of fuel pin L05, i.e. fuel bulging and fuel ejection in the lower part of the fuel pin, while the upper part had become a thin-walled tube of UO₂. However, in this case no pressure pulse was recorded.

Fig. 1 shows four cross sections of experiment L08. Cross section A gives an impression of the macro structure at the top of the fuel stack. The maximum wall thickness of the remaining fuel at this location is only 0.7 mm. The amount of molten fuel approximated 64%.

Cross section B, shows the macro structure and corresponding autoradiograph at a location where molten fuel was ejected from the centre of the pin. About 50% of the fuel in this cross section melted. As can be observed the shroud tube also melted coming into contact with the sodium containment, without penetrating it.

The ejected fuel had flowed downwards remaining within the shroud tube. This is illustrated in cross section C, located 15 mm. below cross section B. The amount of molten fuel in cross section C was approximately 20%.

The presence of solidified stainless steel was observed in cracks and lenticular voids of the lowest pellet in the fuel stack. This is illustrated in axial cross section D, Fig. 1. The regions a and b are shown in more detail in Fig. 2. In region b, Fig. 2b, where the temperature was higher compared to region a, spherical metallic particles were observed. Close observations of these particles revealed the presence of a larger number of very small (1-5 μ) inclusions concentrated along the UO₂ grain boundaries. Micro analysis of the metallic inclusions proved that indeed the lenticular voids were filled with solidified canning material /4/. The largest spherical particles showed a depletion in Cr and Mn, most probably due to evaporation. The boiling points of Cr and Mn are 2480°C and 1900°C respectively. The main constituents of the very small inclusions were Cr, Mn and Fe /4/. From this it was concluded that these very small inclusions were formed from the larger spherical particles by an evaporation-condensation mechanism.

However, under the conditions considered, there is no indication of the cladding material present in the fuel causing fuel fragmentation.

B. Pre-Pressurized Fresh Fuel Pins.

In this series four experiments (L06, 07, 09 and 12) have been performed. Some of the experimental data are summarized in Table I. From the results of experiment L07, fuel pin pressure 76 bar, it was concluded /1,7/ that the canning failed just before the onset sodium boiling. Nevertheless considerable clad- and fuel melting was observed, Fig. 3. The macro structure illustrates meltthrough of the fuelstack at three different locations. A detail illustration of one of the openings through which the molten fuel has flowed out, is given in Fig. 3b. As is indicated by an arrow some solidified fuel is still present. The estimated amount of central fuel melting at the considered cross section, Fig. 3, was 40%.

These results can only be explained by assuming violent rupture of the canning. The subsequent rapid release of the gases caused partial blanketing of the fuel pin. Consequently in this region the temperature of the fuel pin increased much faster than before and ultimately resulted in considerable clad and fuel melting. At the moment of pin failure a pressure pulse of 160 bar was recorded in the sodium containment. This high pressure pulse is believed to be generated due to the sudden gas release from the fuel pin. During the release, the gas was heated by the hot fuel, cladding and sodium.

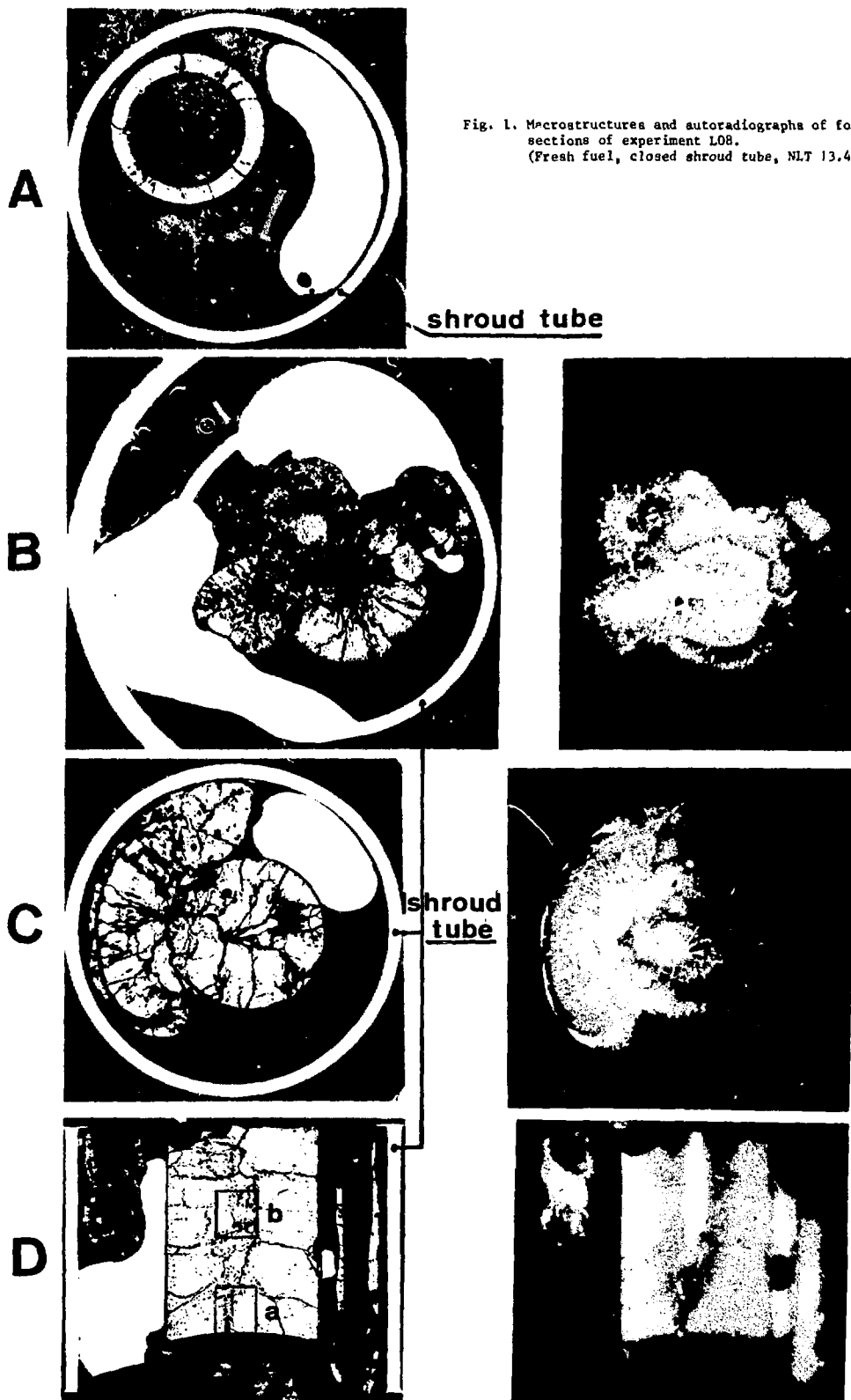


Fig. 1. Macrostructures and autoradiographs of four cross-sections of experiment L08.
(Fresh fuel, closed shroud tube, NLT 13.4. sec.).

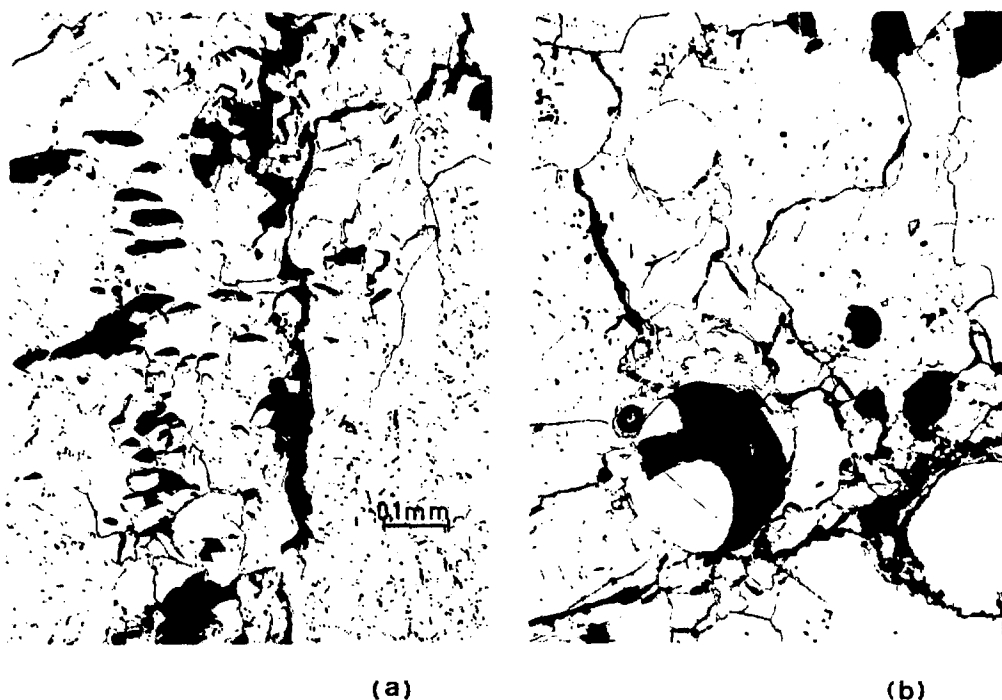


Fig. 2. Detailed view of cross section D, see Fig. 1, showing solidified canning material in the fuel. Region a. (a), Region b. (b).

TABLE I

Experimental data of pre-pressurized fuel pins.

Experiment no.		L06	L07	L12	L09
Initial pin pressure,	bar	75	76	76	36
Type of shroud tube		closed	closed	open	closed
Total LOC-time	sec.	7.4	7.1	10.9	8.9
Norm. LOC-time (NLT) ^{a)}	sec.	6.2	6.7	9.8	7.8
Pin failure at	sec.	-	6.4	10.0	7.6
Na-temperature at failure	°C	-	1070	1135	1120
Length of clad failure	mm	-	75	90	130
Max. amount of molten fuel	%	10	40	10	14
Fuel melt through		no	yes	no	no
Location of failure as per centage of the height of the fuel stack	%	-	30-60	60-95	30-85
Pressure Pulse-height	bar	-	160	160	80
-start	sec.	-	6.4	10.05	7.6
-peak	sec.	-	6.6	10.18	7.75

a) The times given are based on normalized conditions.

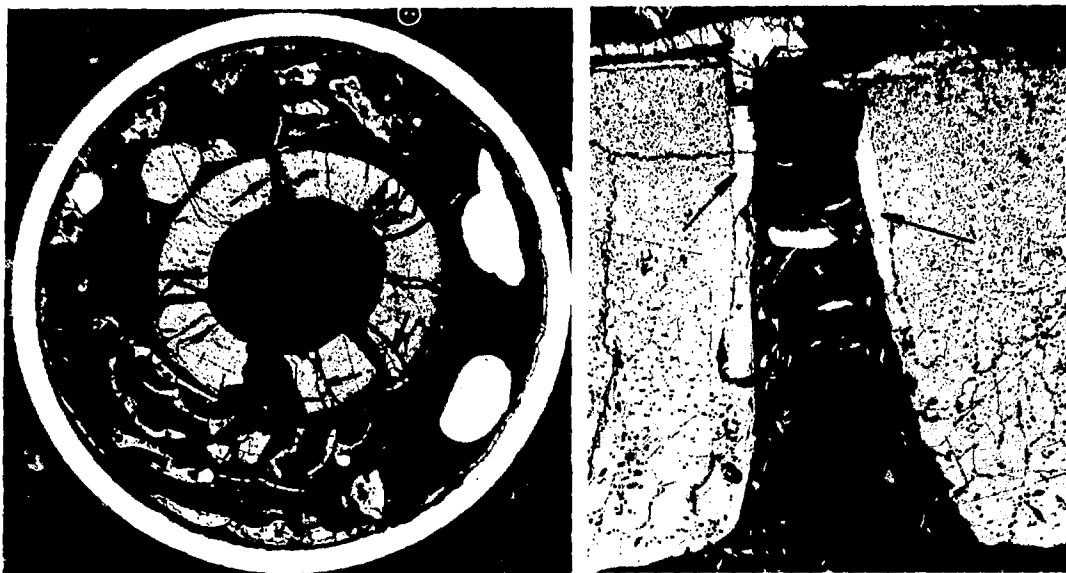


Fig. 3. Macrostructure (a) at the failure area of experiment L07, located at 45% of the height of the fuel stack. One of the openings through which molten fuel has flowed out is shown in more detail (b).

To determine the effect of the surrounding geometry on the potential of pin to pin failure propagation due to fission gas release, another experiment (L12) with a fuel pin pressure of 76 bar has been performed. The only difference was the shroud tube, which was slotted in case of experiment L12. The experimental data are also collected in Table I.

A comparison of the results of the experiments with a closed and slotted shroud tube, L07 and 12 respectively, has led to the following observations and conclusions.

1. The failure time of the fuel pin surrounded by a slotted tube (L12), was 3.6 sec. longer than of L07. The temperature measurements indicated that, in case of an open (slotted) shroud tube, sodium boiling started 3.2 sec. before pin failure. The time delay is rather long compared with previous experiments with a closed shroud tube. This can be explained by assuming dry-out did not occur. The different behaviour may be attributed to the use of an open shroud tube, which permits radial sodium flow through the slots. This can delay or even prevent dry-out. However, almost from the start of sodium boiling the temperature measurements of L12 indicate a continuous rise of the sodium temperature. Due to this temperature increase and the related expansion of the sodium, the pressure of the cover gas increased, which possibly resulted in suppression of the sodium boiling. This means that the time delay of about 3 sec. between the commencement of sodium boiling and pin failure cannot yet be looked upon as typical for the presence of a slotted shroud tube.
2. In case of gas release from the fuel pin this gas may also escape through the slots. Depending on the internal pin pressure this might reduce or prevent gas blanketing of the fuel pin. Although the length of clad failure (melting) did not differ significantly in the experiments L07 and L12, only in case of L07 (closed shroud tube) the fuel stack failed. As Table I indicates the maximum amount of central fuel melting differs also considerably. From these observations it can be concluded that during the experiment with

an open shroud tube (L12) the extent of gasblanketing was strongly reduced.

3. On fuel pin failure of both experiments a pressure pulse of 160 bar was recorded. It is believed that the nature of these pulses is the same. A pressure pulse of above 80 bar has been recorded during experiment L09, with a fuel pin pre-pressurized at 36 bar. Also in this case the peak pressure coincided with the reactor scram (Table I.) The shroud tube of this experiment was a closed one. The temperature measurements have indicated sodium boiling during 0.8 sec. before fuel pin failure. This is in accordance with observations of non pressurized fresh fuel pins /3/. The fuel pin (L09) failed at seven points between 30 and 85% of the height of the fuel stack. Based on the appearance of the canning at the failure spots, Fig. 4, the conclusion was that the fuel pin did not fail by violent rupture /1/.

Figure 4 illustrates four different views of the failure area of this fuel pin (L09). As can be seen, D shows a crack without melting, while the crack, shown in B was partially filled with molten clad material. Around the crack, illustrated in A, some clad material has melted away.

The given views of the failure spots are located at 38, 46, 68 and 80% of the fuel stack respectively. The occurrence of many cracks in the canning at different locations cannot be explained by a creep mechanism due to gas pressure. Therefore, from the results obtained so far it is concluded that failure of non-pressurized fuel pins, as well as of pins with internal pressures lower than approx. 40 bar, occurs by cracking of the canning due to differential expansion between fuel and cladding. Ultimately this leads to clad melting as a result of either an increased cladding temperature on contact with the fuel or of sodium vapour blanketing (dry-out). So far it cannot be said whether fuel-clad contact takes place before sodium boiling or vice versa.

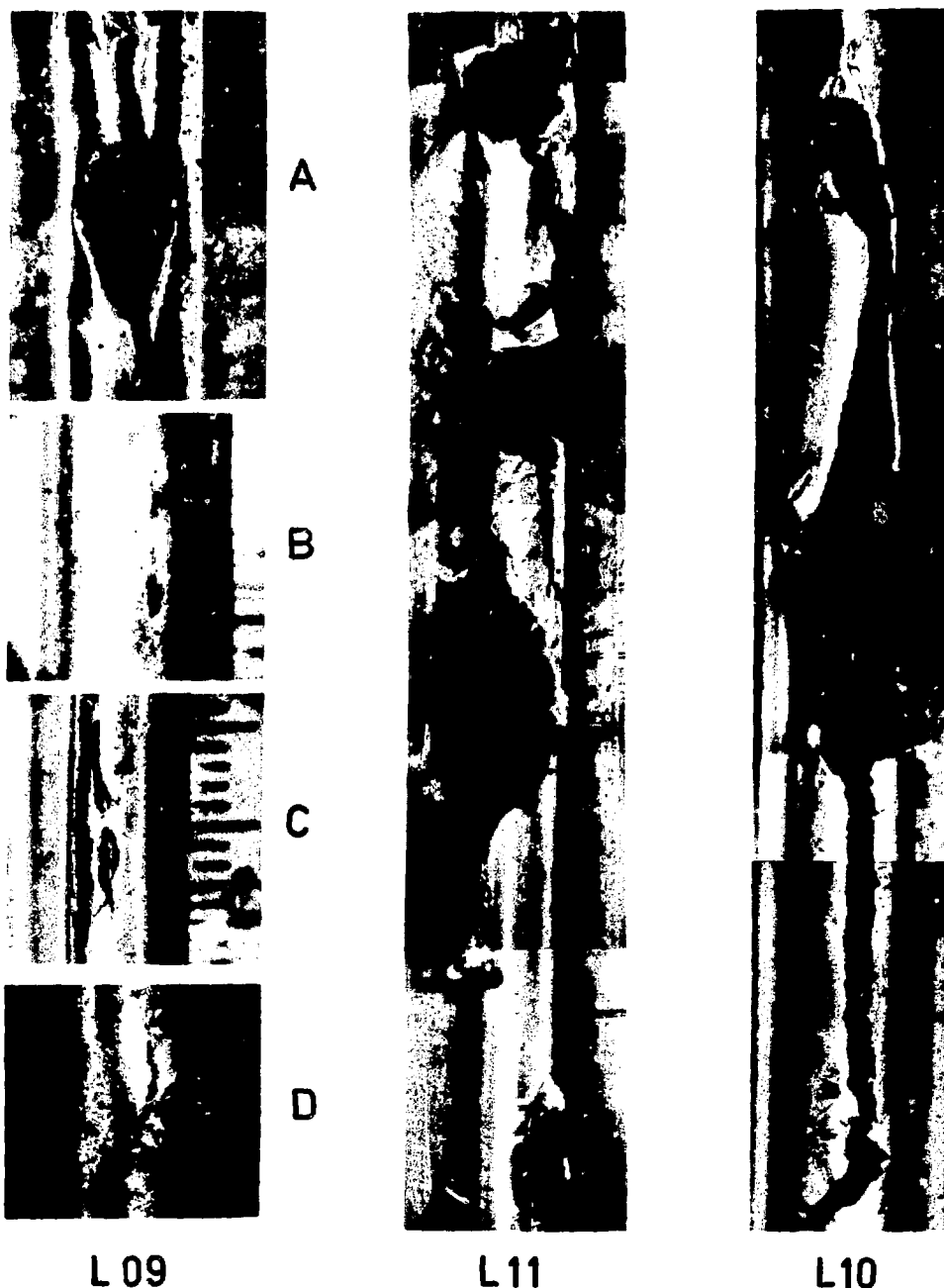
C. Pre-irradiated fuel pins.

The aim of this test series is to determine the influence of pre-irradiation, on the failure behaviour. So far three experiments (L10, L11 and L13) have been performed. Results of these experiments are summarized in Table II. The fuel pin of the first experiment (L11) was pre-irradiated at approximately 1% B.U. (about 10 Mwd/kg). The normalized LOC time (NLT) was 8.8 sec. The sodium boiling time, based on normalized conditions, was therefore $8.8 - 6.5 = 2.3$ sec. Failure of the fuel pin occurred after 8.2 sec. which means after a sodium boiling time of 1.7 sec.

The main difference between the first (L11) and the second (L10) experiment with a pre-irradiated fuel pin is the burn-up. As can be seen from Table II, the burn-up of fuel pin L10 is about twice as much as of L11. This table also shows that, although the failure time and the sodium boiling time are longer for experiment L10, the length of clad failure is considerably less compared with L11. The maximum amount of central fuel melting is almost equal in both experiments. The appearance of the failure area of fuel pin L10 and L11 is given in Fig. 4 and illustrates large and small cracks in the canning, together with locations of clad melting.

Comparing the experimental results of pre-irradiated and fresh fuel pins, tested at similar conditions (low internal pin pressure, short LOC times and a closed shroud tube) led to the following conclusions:

1. the mechanism and mode of clad failure of pre-irradiated fuel pins equals that of fresh fuel pins,
2. the extent of clad- and central fuel melting is less in case of pre-irradiated pins,
3. the presence of fission products on low burn-up fuel does not reduce the fuel failure threshold.



	L 09	L11	L10	
PIN PRESSURE	36	12	16	BAR
NLT	7.8	8.8	9.4	SEC
FAILURE TIME	7.6	8.2	8.6	SEC
BURN -UP	—	1.2	2.5	% FIMA

Fig.4 Views of the failure areas of fuel pins L09, L10 and L11.

The total LOC time of the third experiment with a pre-irradiated fuel pin (L13) was the same as of L05 and L08, being 17.5 sec. Contrary to the latter experiments, capsule L13 was equipped with a slotted shroud tube. The experimental data are also summarized in Table II. The NLT is estimated to be 16.2 sec. After a NLT of 7 sec., the pressure measurements indicated fuel pin failure. As at normalized conditions sodium boiling starts after 6.5 sec., this means pin failure occurred 0.5 sec. later. Compared to another experiment with an open shroud tube (L12) this seems rather fast. At 10.75 sec. a pressure pulse of around 90 bar, has been recorded. Post irradiation examination revealed the presence of some fuel and cladding particles down in the capsule to where the sodium was still in a liquid state. This pulse could therefore be attributed to a Fuel Sodium Interaction (FSI).

The neutron radiograph of capsule L13 showed fuel failure between 50 and 70% of the height of the fuel stack. This region is located much higher than in experiments L05 and L08. In these fuel pins fuel failure occurred at 5-30% and 15-45% respectively.

Fuel pin L13 remained intact up to approximately 30% of the height of the fuel stack.

TABLE II

Experimental data of pre-irradiated fuel pins.

Experiment no.		L10	L11	L13
Initial pin pressure	bar	16	12	6.4
Total LOC time	sec.	10.2	11.0	17.5
Norm. LOC time (NLT)	sec.	9.4	8.8	16.2
Type of shroud tube		closed	closed	open
Burn ² up (% Fima)	%	2.5	1.2	1.1
Time of failure	sec.	8.6	8.2	7.-
Na temperature at failure	°C	1100	1090	-
Length of clad failure	mm	75	130	160
Location of failure as percentage of the height of the fuel stack	%	70-100	40-92	30-95
Fuel stack failure		no	no	yes
Max. amount of molten fuel	%	13.5	12	65
Pressure pulse-height	bar	-	-	30
-start	sec.	-	-	10.37
-peak	sec.	-	-	10.75

To summarize, the results of the experiment L13 with pre-irradiated fuel, an open shroud tube and a long LOC time, revealed that;

1. the extent of clad failure (melting) is significantly less compared to experiments with fresh fuel and a closed shroud tube,
2. although the fuel stack failed seriously by bulging and flow out of molten fuel, only minor axial fuel relocation occurred.
3. from a detailed observation of the neutron radiograph and examination of various radial cross sections, it is concluded that despite rather close contact with molten fuel and canning, the shroud tube remained intact.

To evaluate whether the difference in failure behaviour can be attributed to the pre-irradiation or to the presence of a slotted tube, the experiments are being continued with two irradiations. The first one, L14, with fresh fuel and an open shroud tube. The second one, L16, will be pre-irradiated up to approx. 1% B.U. (like L13). The shroud tube around this fuel pin is of a closed type. The total LOC time of both experiments will be 17.5 sec.

SUMMARY

1. Considerable clad failure can occur without leading to fuel disintegration.
2. FSI only occurred during experiments with long LOC times.
3. The occurrence of an FSI is due to dropping of hot fuel down in the capsule.
4. No significant deformations of the inner capsule construction have been observed.
5. Molten fuel was not ejected violently.
6. Gasblanketing of the fuel pin only occurs at high internal pin pressures.
7. The use of an open shroud tube delays dry-out and decreases the extent of gasblanketing.
8. The failure of low burn-up fuel pins is not significantly influenced by the presence of fission products.
9. There are five to ten sec. between sodium boiling and disintegration of the fuel stack at the given heat rating.
10. No fuel or steel blockage in the blanket region was observed.

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RESULTS OF RECENT TOP AND LOF EXPERIMENTS IN TREAT

by

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ABSTRACT

Tests E8, L5, F1 and F2 were recent experiments performed at TREAT as a part of the LMFBF Safety Test Program. Test E8 employed seven pins with fuel which had an axial variation of its fuel microstructure more like FTR than in previous tests. Test E8 was the first overpower experiment with clear indication of an initial post-failure fuel dispersal. An analysis made with the PLUTO code indicates that the driving force for fuel expulsion to the coolant channel was fission gas. The subsequent fuel dispersal could be explained by fission-gas expansion and a very mild fuel-coolant interaction.

Posttest examination provided a plausible explanation for the initial radial swelling of fuel at constant power under the simulated loss-of-flow conditions in F1. The initial gross axial fuel movement in F1 was downward. The motion was however dispersive in Tests F2 and L5, both of which ended in power ramps simulating hypothetical overpowers under loss-of-flow conditions. The L5 posttest examination results showed some similarities to F1. Interpretation of the F1 and L5 posttest examination results may provide a scenario for initial fuel motion in LMFBF loss-of-flow accidents with voiding in the lead subassemblies at nominal to several times nominal power.

INTRODUCTION

This paper summarizes the results of Tests E8, L5, F1, and F2, the four most recent experiments conducted in TREAT on irradiated fuel in support of LMFBF safety evaluation. The experiments studied the behavior of fuel, cladding and coolant after fuel element failure in the initiating phases of the following hypothetical core disruptive accidents (HCDA): (1) transient overpower (TOP) leading to gross fuel movement just after clad failure (E8); (2) loss-of-flow (LOF) with delays of the order of seconds between clad failure and gross fuel movement (F1); and (3) overpower accidents driven by positive reactivity effects resulting from coolant, clad and fuel behavior under LOF conditions (L5, F2).

Table I summarizes the essential differences between the experiments with regard to test category, type of test vehicle and test cluster. Test E8 was a TOP experiment in which test conditions were established within the sodium environment of the Mark-II loop [1]. The test was conducted to obtain data for mechanistic accident analysis. Test L5 was the first mechanistic loss-of-flow

experiment with full-length, irradiated fuel conducted in the Mark-II loop. Tests F1 and F2 were phenomenological experiments investigating the behavior of fuel and cladding under idealized conditions with coolant dryout hypothesized for a specific point in the loss-of-flow accident scenario. Consequently, these tests were conducted in the absence of sodium.

Table I. Recent TREAT LMFBR Safety Tests with Irradiated Fast Reactor Fuels

Test	Test Category	Test Vehicle	Test Cluster
E8	Transient Overpower (TOP)	Mark IIC loop-flowing sodium	7 EBR-II-irradiated pins
F1	Loss-of-Flow (LOF)	Dry Capsule	1 EBR-II-irradiated pin with nuclear heated wall
L5	LOF-driven TOP	Mark IIC loop-flowing sodium	3 GTR-irradiated pins
F2	LOF-driven TOP	Dry Capsule	1 EBR-II-irradiated pin with nuclear heated wall

The most important highlights of the test objectives, results and conclusions are discussed in this paper. Interpretation of the test results is presented using: (1) the PLUTO [2] mechanistic analysis with E8; and (2) the phenomenological approach to explain fuel behavior for the F1 and F2 test conditions. A fuel behavior model based upon the F1 posttest examination is also shown to be plausible to explain both the L5 results and the results reported previously [3,4] for Tests L3 and L4. The PLUTO analysis of E8 is presented as a specific illustration of the development of a mechanistic model for LMFBR accident analysis.

Test E8 and the earlier Mark-II loop TOP tests with fresh fuel (E1, E2, H2 and E4) and irradiated fuel (E3, H3, H4, H5, E6, and E7) reported previously [5,6] represent a complete set of TOP experiments on FTR-type fuel. In addition, TOP experiment R9 was performed on full-length fresh fuel under prototypic LMFBR hydraulic conditions subsequent to E8 [7]. Test R9 deals with a 50¢/sec simulation analogous to H4 and H5.

Earlier Mark-II loss-of-flow experiments L2, L3, L4 [3,4] and experiments in the R-series [8] have demonstrated the differences in fuel motion behavior between unirradiated and irradiated fuels. From the fresh fuel tests, the initial fuel motion occurs by collapse of the fuel stack after the clad melts. The behavior for the irradiated fuel is apparently more complicated. For example, appreciable swelling for fuel above the solidus was observed in recent Tests F1 and L5 with irradiated fuel, an apparent consequence of the fission products entrained within the fuel microstructure. The test program therefore considers variations in fuel microstructure as an important test variable. Experiments have been conducted in which the fuel microstructure could be characterized from the preirradiation power level as being either "high-power-structure" or "low-power-structure". In addition, the fuel used in Test L5 possessed a microstructural variation along its 86-cm length. This variation represents a different physical variable from fuel, such as the L3 low-power and L4 high-power fuels, in which the microstructure was predominantly uniform along its entire length.

TEST FACILITY AND HARDWARE

All tests described in this paper were conducted in the Transient Reactor Test Facility (TREAT) [9]. To perform a typical test, two of the TREAT elements at the center of the reactor are removed to provide a 10-cm-by 20 cm space for

the experiment. Special slotted fuel and reflector elements provide a 60-cm-high viewing slot into the core center so that fuel motion can be monitored by a fast-neutron hodoscope [10]. The hodoscope provides a planar view of the test section by means of a collimator and array of fast-neutron detectors. Each channel of the hodoscope array covers a spatial element nominally 3.6-mm by 22.2-mm at the test region. A flexible, programmable TREAT control system provides a means of shaping the reactor power depositions in the test fuel. The following discussion deals with the most recent safety tests in greater detail.

TRANSIENT OVERPOWER TEST E8

In Test E8, seven mixed-oxide PNL-10 elements [11] from EBR-II subassembly X093 were subjected to a simulation of an LMFBR unprotected \$3 per sec reactivity insertion. The fuel elements for the test had been irradiated at a peak linear heating rate of about 30 kW/m (158 kW/kg) to a burnup of about 5 atom percent. These fuel elements therefore provide a representation of fuel with the microstructure of "low-power-structure" FTR-type fuel with a small central void.

The seven fuel elements were arranged on a triangular spacing in the test region of the Mark-IIIC loop. The E8 test region design employed a gas-filled region surrounding the six-fluted flow tube, as shown in Fig. 1, as an inset with the posttest neutron radiograph. This test section design was similar to the design for Test E7 [5]. The active 34-cm-long fuel region of the PNL-10 elements was filtered by 0.5-mm-thick dysprosium metal wrapped around the outside of the 50-mm-dia. test section. The initial sodium flow rate of about 575 cm³/sec (9.1 gpm) was provided by an annular linear induction pump of a design used in previous Mark-IIA tests. Using the average test region flow area of 1.72 cm², the coolant velocity in E8 was about 3.3 m/sec.

The TREAT transient, shown in Fig. 2, consisted of a 2-sec preheat at 44 kW/m (230 kW/kg) to simulate the peak stored energy per unit length in the hottest peripheral elements in the test cluster relative to that energy stored in a low-power FTR element undergoing a 3\$ per sec reactivity excursion. A power burst peaking at 2500 MW followed the preheat. The total energy release in the TREAT transient was 1180 MJ which deposited a total of at least 2.45 MJ/kg in the element in the cluster with the highest calibration factor. The initial period of 150 msec for the burst provided a burst width of about 325 msec (full width at half peak power).

Flowmeter and pressure-transducer data indicated that the first failure event occurred at an integrated TREAT power of 675 MJ (about 500 msec into the excursion) with a corresponding sample energy generation of 1.40 MJ/kg. At this point in the transient, COBRA computer calculations [12] indicated that about 50 percent of the fuel at the midplane of the two hottest peripheral elements had just reached the temperature of the solidus. Note, however, that this 50 percent estimate is sensitive to the calculational model. Small changes in input parameters can easily shift the result since the power generation rate is occurring rapidly on about a 150 msec period. Using this measure of the failure threshold, the E8 fuel failed approximately when expected based on previous TREAT tests with high-power structure fuel reported by Rothman *et al* [5]. After the first indication of failure, the inlet pressure transducer recorded several additional pressure pulses in the next 20 msec, but no pulse was larger than about 1.5 MPa (15 atm). The pressure transducer output at the outlet showed no pulses. Flowmeter data suggest that the test section voided rapidly and sodium could have completely vacated the test region in the 200 msec after the first failure indication. After the test, attempts to re-establish flow through the test region failed, implying that a flow blockage existed.

The neutron radiograph of the test section, containing the active fuel

column and upper fuel element structure, is shown in Fig. 1. The radiograph shows the failed fuel elements and indicates material movement well above the top of the fuel column. Using the radiograph as a guide, the loop was disassembled and dissected for metallographic examination.

From the examination to date, the following observations can be drawn by reference to Fig. 1. The region from the midplane to 76 mm below is essentially voided of either fuel or steel. The region from the midplane to the top of the original fuel column contains a mixture of failed fuel and steel. A closer examination of the region at the top of the original active fuel column is shown in Fig. 3. Figure 3 shows the mixture of fuel and steel, including the remnants of the flow tube. Insulator pellets shown in the figure had apparently dropped down from above. A steel barrier formed in some of the coolant channels just above the top of the fuel column. Yet, fuel, with a little steel entrained, solidified above in the region containing the reflector pieces. In addition, a quantity of fuel was found dispersed in the sodium above the top of the original fuel column. Molten fuel had apparently entered the coolant channel before very much clad had melted, consistent with the pressure transducer signals indicating that the element failures had been abrupt. The results appear to substantiate that a degree of sweepout of fuel is possible. The fuel sweepout mechanism and resultant decrease in reactivity have been postulated [13] as the means leading to termination of hypothetical overpower LMFBR accidents.

The general scenario of the events occurring in E8 is given in Table II. The results from the hodoscope are in apparent agreement with the sensor data and the posttest examination. The neutron hodoscope confirmed that several pins failed before the power peaked. Small quantities of fuel left the element and moved rapidly to the cooler reflector region above. Further sweepout ceased when apparently an upper blockage was formed. Fuel motion continued below the blockage. The first fuel motion detected by the hodoscope occurred at 7.219 sec, at 90% of maximum power. This time was about 15 msec after the first indications by pressure and flow sensors that an element had failed. If, as calculated, the outer radial portions of the fuel pellets were not yet molten, then the expelled material consisted mainly of molten fuel from inside the failing element, perhaps driven through a break by expanding fission gas. The expelled fuel moved rapidly upward and collected a few cm above the top of the original fuel column within 20-80 msec.

Upward sweepout from failed elements stopped after 80 msec by an apparent blockage, containing about 60 g of fuel, in the first 150 mm above the top of the original fuel column. The posttest examination confirmed that a stainless steel barrier had formed at the top of the original fuel column. Fuel motion continued within the original fuel zone. By the end of the transient, the 80-mm region just below the midplane was largely voided of fuel. The bulk of the fuel was between the midplane and the upper blockage. The final configuration seen by the hodoscope is consistent with the posttest neutron radiograph.

A slightly modified version of the PLUTO code [2] was used to analyze the early postfailure sodium and fuel motion (7.200-7.300 sec), providing additional insight into the early events. The version of PLUTO used for the E8 analysis models the Mark-II loop and pump and has the capability for resetting the fuel pin dynamics model to prefailure conditions at any time during the calculation. This represents a simplified treatment of noncoherent pin failures.

A parametric study was performed with a variation in the number and sequences of pin failures, initial pin pressure and melt fraction, fuel-coolant interaction (FCI) parameters, and gap sizes controlling the plenum fission-gas release. The best results obtained are shown in Figs. 4(a)-(d). The reasonable agreement was achieved by assuming the following for PLUTO: (1) A pin-hole type fission-gas release from one pin starting at 7.200 sec. The presence of

Table II. Scenario of Events in E8

Time, sec	Power Level, MW	Sensor*	Observation
<u>4.0 - 7.219</u>		H	No observable fuel motion.
4.4	115		Start of preheat plateau.
6.7	115		Power ramp begins.
7.204	2140	P, F	Beginning of observable pressure and flow changes.
		C	Calculated failure of first element.
7.212	2220	P	Pressure pulses recorded as first element ruptures.
<u>7.219 - 7.30</u>		H	Loss of fuel from pins, over extended axial region; lost fuel swept upward, to lodge above original fuel zone.
7.219	2270	H	Ca. 6 g of fuel leaves peripheral pin on east** side of bundle. Expelled fuel travels upward to region above fuel zone.
7.223	2300	P	Second set of pressure events recorded.
7.224	2310	C	Calculated failure of two pins.
7.228	2330	H	Ca. 18 g of fuel leaves west side of bundle, again moving rapidly to superstructure over fuel zone.
7.234	2360	P	Third set of pressure pulses recorded.
7.238	2400	F	Maximum exit flow rate (1.65 $\dot{V}$ /s); inlet flow crosses zero.
~7.270	~2535		Peak power reached.
<u>7.30 - 7.40</u>		H	Fuel moving about within original fuel zone.
7.30	2500	H	Upward sweepout greatly diminishes, as upper blockage is largely completed.
7.32	2470	H	Inner wall of flow tube penetrated on east side. (Penetrations of flow tube in line with hodoscope would be observable.)
7.34	2150	F	Inlet flow at maximum negative value (0.5 $\dot{V}$ /s).

* H: Hodoscope; P: Lower pressure sensor; F: Flow meters; C: Calculation (PLUTO)

** Orientation: Pump on south side of test zone; hodoscope to north.

this fission gas in the upper sodium slug attenuates pressure waves generated by later events and thus explains the lack of signals at the upper pressure transducer. (2) The failure of the cladding of one pin (rupture length 5 cm) was assumed to occur at 7.210 sec. This failure led to the first set of pressure pulses in Fig. 4(b). An initial pressure in the molten pin cavity of 11.8 MPa (120 atm) was used together with a void fraction of 4.5%. The pin cavity was assumed to encompass only 40 percent of the fuel area although more than 50% may have been at or above the solidus. This was done since none of the fuel had reached the liquidus at failure time and the outer molten fuel is believed to be very viscous. Moreover, a larger cavity leads to more fuel ejection from the pin, which is in disagreement with hodoscope data. (3) Additional failure of two pins was assumed to occur at 7.221 sec with a higher (23.6 MPa) fission-gas pressure than the first failed pin. This is due to the fact that the first failure probably involved fission-gas release to the coolant through a pin hole prior to ejection of any fuel (see (1) above). (4) The fuel injected into the channel was assumed to fragment instantaneously into particles of 0.01-cm-radius. Although the drag acting on these small particles is quite high, they

still do not move as rapidly as predicted by the hodoscope (see Fig. 4(c)). However, the Cho-Wright steady state FCI formulation used in PLUTO was modified such that the particle radius in the coefficient used was 0.0225 cm. This led to a rather mild FCI with a maximum sodium vapor pressure of 0.3 MPa which nevertheless contributed significantly to the initial voiding rate. (5) A plenum fission-gas release from one pin was indicated at 7.235 sec. The plenum gas prevents the upper sodium slug from losing its velocity much more rapidly than predicted by the experiment. Sodium vapor pressure cannot contribute much to the pressurization of a partially voided and rather cold plenum region ($\sim 860^\circ\text{K}$) because of strong condensation effects. (6) Two more pins were assumed to fail at 7.260 sec and a complete fuel blockage above the fuel section was then assumed to occur at 7.265 sec. Additional plenum-gas release below the blockage led to the pronounced lower flow reversal (see Fig. 4(a)). An uncertainty remaining is the question of whether a much higher ratio of fission gas to fuel was ejected through the clad ruptures or whether fission gas was released from fuel after its ejection into the channel. If either of these were the case no FCI or plenum-gas release may be necessary to explain the sodium voiding and fuel dispersal.

At any rate, the analysis shows that the PLUTO assumption of a "gas bottle" in the pin, which leads to fuel ejection at the clad rupture times is adequate for E8 type pins. Moreover, even fuel injected into a relatively slow coolant stream (~ 3.3 m/sec) is rapidly swept upwards. Thus, the PLUTO modeling seems adequate for predicting the early postfailure events. However, if gross voiding takes place fuel freezing phenomena become important. A new code PLUTO2 [14] which models these freezing phenomena is under development.

LOSS-OF-FLOW TEST L5

The L5 Test addresses postulated LMFBR loss-of-flow accident scenarios for which the interest lies in the potential for a reactivity reduction caused by fuel motion in voided subassemblies experiencing significant power increases. These power increases occur from reactivity insertions due to fuel slumping or sodium voiding elsewhere in the core. Therefore, the TREAT transient and Mark-IIIC loop flow coastdown subjected the test fuel to a mild overpower burst (a 800-msec initial period) peaking at 6 times the nominal fuel operating power of 36 kW/m after the test region was voided of sodium coolant, but at incipient clad melting.

The test cluster consisted of three HEDL-59 fuel elements with a 86-cm-long active fuel column, preirradiated in CETR at 41 kW/m to about 8 atom percent peak axial burnup. Although the HEDL-59 design [15] met most of the desired criteria for the test, it was necessary to reduce the fission-gas pressure in the irradiated fuel element plenum region to insure against premature clad failure [16]. Also, the thermal irradiation in CETR may have introduced conditions somewhat different from a fast-reactor irradiation. The desired axial power distribution for the test was achieved by means of dysprosium neutron filters of varying thickness placed along the outside of the 50-mm-dia. loop test region. A diagram of the fuel element arrangement as an inset with the posttest neutron radiograph is shown in Fig. 5. The TREAT power burst was timed to occur at incipient clad melting based upon pre-experiment predictions by the SAS2A code [17]. A Data-Trak controller regulated the pump power supply to achieve the desired linear coastdown. The TREAT reactor power level was set to heat the fuel initially at a peak linear heating rate of about 36 kW/m. The power burst was then designed to melt the fuel, but keep fuel temperature below 3500°C in order to limit fuel-vapor pressure below a few atmospheres.

Reactor power and the inlet flowmeter signal for the test are shown in Fig. 6. The flow coastdown was initiated about 4 sec after the reactor reached the 42-MW level. The first indication of boiling and flow reversal occurred about

4.6 sec later. The posttest radiograph results, shown in Fig. 5, indicate that the fuel, although apparently melted, had moved into a pattern of alternate regions of low and high density; apparently, gross slumping had not occurred.

Postmortem examination of the test section indicated that fuel had moved axially away from the original fuel column midplane forming plugs at the ends of the fuel column. However, the 86-cm region occupied by the original fuel column had not been completely voided of fuel at any elevation. The fuel was melted over nearly its entire length so that its identity as whole pellets was lost. Large scale motion of fuel was apparently preceded by melting and motion of clad. Solidified clad blocked the coolant flow channels both at the top and at the bottom of the original active fuel region. The flow tube was also destroyed by melting at several locations. No fuel material was found above the top of the active fuel column.

The results of test observations with the loop instrumentation and fast-neutron hodoscope are compared with SAS3A [18] calculations in Table III. Calculated voiding profiles are shown in Fig. 7. The gap conductance and sample-to-reactor power calibration factor values used in the SAS calculations were adjusted using the L5 heat balance test as a guide. The calculated sodium voiding preceded the observed voiding by about 1 sec. Analysis of the hodoscope data shows that the L5 fuel motion was not nearly as extensive as in previous loss-of-flow tests with irradiated fuel. The onset of major axial fuel motion occurred about 100 msec after peak power. The net motion was observed to be dispersive away from the midplane of the original active fuel column, with alternately dense and less dense regions axially.

Using the criterion for the initiation of fuel motion of 50% of the pellet area at, or above, the solidus, the SAS calculation indicated fuel motion inception before the scram. The hodoscope data indicated that the major fuel motion began just after the scram. The calculations indicate that the fuel had not exceeded its liquidus during the test.

The L5 Test results show that the net fuel behavior was initially dispersive away from the midplane. These results imply that fuel movements in voided subassemblies subjected to a mild power burst would reduce reactivity in an LMFBR HCDA. The dispersive driving force for L5 was probably entrained gaseous fission products since the fuel-vapor pressure generated was apparently too low. The L5 results might also have a bearing on the analysis of larger LMFBRs in which fuel motion in the lead subassemblies can occur at several times nominal power.

OBJECTIVES, DESIGN AND RESULTS OF THE F1,2 TESTS

The general objectives of tests F1,2 was to provide basic information for accident analysis on fuel element behavior under idealized conditions after coolant dryout hypothesized for loss-of-flow accident scenarios at power levels in the range of one to ten times design. Consequently these tests were conducted in the absence of sodium using a special dry capsule apparatus. Both tests were performed on irradiated fuel that was selected to be characteristic of "high-power-structure" FTR fuel, the first fuel to melt under hypothesized loss-of-flow conditions. Test F1 evaluated fuel motion of moderate burnup fuel at design power. The F2 test evaluated motion of low burnup fuel subjected to a power burst to represent an overpower accident driven by fuel compaction under hypothetical loss-of-flow conditions. Table IV summarizes certain properties of the F1,2 test fuels.

Fuel and fuel-boundary conditions in the F1 and F2 tests were achieved in TREAT in the special F-series dry test capsule. Each test used a single fuel element surrounded by a nuclear-heated wall. An axial view of the F1,2 test

Table III. Sequence of Events in TREAT Loss-of-Flow Test L5

Event	Time (sec)		
	Experiment Sensors	Fast Neutron Hodoscope	SAST Calculations
Reactor Control Program Initiated	0	0	0
Reactor Power Reached 42 MW (36 kW/m per element)	3.0	-	3.0
Flow Coastdown Initiated (initially 170 cm ³ /sec)	7.0	-	7.0
First Indication of Coolant Channel Voiding	11.6	-	10.9
Flow Reversal at Test Section Inlet	11.6	-	11.2
Power Burst Initiated at 390 MJ Energy Release	11.8	-	11.8
Incipient Clad Melting	-	-	12.3††
Flow Coastdown Ended (23 cm ³ /sec)	12.0	-	12.0
Localized Fuel Motion Observed*	-	12.1	-
Clad Melted	-	-	12.8
Clad Blockage Formed	**	-	12.9
Beginning of Fuel Melting	-	-	13.1
Peak Power Reached (240 MW)	13.4	-	13.4
Onset of Major Axial Fuel Motion	-	13.5	13.2
Reactor Scram Setting (600 MJ)	13.4	-	-

† Based on single element model using cluster average parameters.

†† 200 mm above midplane of fuel column.

* At about 125 mm above midplane of fuel column and also at 100 mm below midplane.

** Sensor data shows no clear indication of the time a clad blockage could have formed.

design is shown in Fig. 8, showing the position of the test fuel relative to the test instrumentation. At mid-axis of the fuel column, the fuel pin is surrounded radially by successive annular regions of a nuclear-heated wall, a thin molybdenum reflector, a stainless steel heat sink, a stainless steel capsule wall (the pressure barrier), and a steel wall. The test capsules were pressurized with a A-3% He mixture to 83.4 kPa at ambient temperature. The gas plenum of the F1 test element was punctured and back filled with Xe- 1% He to 83.4 kPa at ambient temperature.

The gas plenum of the F2 test element was not changed prior to the test. The test instrumentation consisted of two thermocouples, a pressure transducer and the fast-neutron hodoscope. Neutron filters were placed around the test region to achieve the desired axial and radial profiles within the test element.

F1 Test

The F1 test was conducted to evaluate the effect of fission products on motion of "high-power-structure" FTR fuel at design power. Therefore, the TREAT reactor was scrammed after complete melting of most of the fuel column occurred,

Table IV. F1,2 Fuel Description

Parameter	F1	F2
Irradiated Peak Power (kW/m)	39.4	38.1
EBR-II Peak Burnup (atom percent)	2.35	0.35
Fuel Column Length (mm)	343	343
Mid-Axis Fuel Microstructural Dimensions (mm)*		
Central Void Radius	0.41	0.66
Columnar Region Radius	1.65	2.06
Equiaxed Region Radius	1.73	2.21
Unrestructured Region Radius	2.54	2.51
Clad Dimensions (mm)		
Inner Radius	2.54	2.54
Outer Radius	2.92	2.92

* From destructive examination of sibling fuel elements.

but at fuel temperatures lower than those which could lead to fuel-vapor-pressure-driven dispersal.

Figure 9 summarizes the reactor power and capsule data before and just after the F1 test scram. Sample power was held at a constant level of 38 kW/m for 9.9 seconds. Unlike the subsequent F2 test, neither thermocouple was contacted by hot fuel or clad in the F1 test. The continuous rise in temperature during the test appears to be the result of capsule fill gas heating from the sample, hot fission gas released from the fuel, and gamma heating of the thermocouple sheath. The cause for the inflections in the temperature trace of the upper thermocouple cannot be readily identified, but may represent changes in convection patterns in the capsule fill gas as a result of shifts in hot fuel or cladding below the thermocouple. Note also that the time response of these thermocouples is about 30 msec in sodium, so that the time response in the helium environment of the F-series capsule is much longer. The gradual rise in capsule pressure response is apparently caused by heating of the test capsule fill gas. No sign of an abrupt or even a gradual release of fission gas into the capsule is apparent from the pressure transducer data. Evidently detection of fission-gas release from the element was masked by the presence of the much larger quantity of capsule fill gas.

Figure 10(a) is the posttest neutron radiograph of the F1 capsule. Pretest fuel column and heated wall axial positions are shown for reference. Examination of the radiograph shows that the heated wall contained the test fuel adequately, as can be judged by its undeformed appearance and almost total confinement of the fuel within the inner diameter of the heated wall. The only significant penetration of the heated wall, by molten fuel, occurred near the top of the original fuel column. The interpretation of the radiograph was aided by metallography performed on selected sections of the F1 test capsule. Final axial relocation of the fuel was downward, as evidenced by a 150-mm region voided of fuel above a 130-mm region of molten fuel, which extends radially to the inner diameter of the heated wall. Partially melted fuel segments exist above and below the regions of extensive axial relocation, with unmelted fuel pellets near the cooler end of each partially melted segment. A single unmelted fuel pellet at the top of the fuel column is the only fuel found above the original fuel column position.

Figures 11(a) and 11(b) show the second pellet and part of the third pellet from the top of the original fuel column, respectively. The location of these pellets is also indicated by the posttest neutron radiograph shown in Fig. 10(a).

Figure 11(a) shows the outside surface of the two pellets while Fig. 11(b) is a photomicrograph of a longitudinal surface containing the fuel pellet axis. The upper surface of the second pellet, and the interface between the second and third pellets are also indicated. The large axial variation in microstructure evident in Fig. 11(b) is due to the axial power shaping that resulted in about a 3% difference in power generation along one pellet length near the top of the fuel column. Hence at the test scram time, most of the region near the bottom of Fig. 11(b) is above the solidus while the region near the upper surface of the second pellet is not yet molten. The position of the fuel solidus isotherm, approximately located in Fig. 11(b), was inferred by examining the pores for angularity and assuming that rounded pores were above the solidus and angular pores were below the solidus. The significant feature shown in Figs. 11(a) or 11(b) is the large amount of bulk swelling, particularly near the bottom. The almost parallel outer surfaces of the third pellet suggest contact and realignment of these surfaces by the heated wall. Transient-induced fuel swelling in fuel below and above the solidus is shown in Fig. 11(b). It would be unlikely that fuel with a radially varying fission-product retention and transient thermal history would exhibit the same degree of swelling along its radius. Indeed, Fig. 11(a,b) shows that the swelling in fuel above the solidus was greater than swelling below the solidus, with the large cracks shown in Fig. 11(a) only at axial locations where some fuel had reached the solidus. The axial cracking pattern suggests that fuel below the solidus could not swell as fast as fuel above the solidus, so the difference in swelling was relieved by cracks in the unmelted fuel. Since no cracks were observed at axial levels where fuel had not yet reached the solidus, it is unlikely that the cracks were caused by transient-induced thermal shock. Figure 11(b) shows no evidence of transient-induced clad entrainment, although two large pores near the bottom of the figure were inadvertently filled with a mixture of metallic cutting debris and epoxy during specimen mounting. Unevenness in the central void shape near the bottom of Fig. 11(b) suggests that columnar fuel in this region had melted and started to run down the central void.

Interpretation of Fig. 11(b), plus consideration of the axial power distribution, provides a plausible fuel motion scenario for the F1 test. Gross swelling is initiated at or above the solidus temperatures in the highest power fuel near the center of the fuel column. The gross radial swelling can explain why unmelted portions in the upper half of the fuel column did not drop into the molten fuel region shown in the neutron radiograph. Evidently, the large amount of radial swelling was sufficient to cause the fuel to grip the heated wall so the upper half of the fuel column could not fall into the molten fuel region forming below it. Subsequent fission-product release from the fuel could relieve the mechanical gripping action at the fuel-heated wall interface, leading to the net compaction of fuel observed in the posttest neutron radiography.

F2 Test

Test F2 was conducted to evaluate the effect of fuel-vapor pressure on the fuel motion of restructured fuel. The test was planned so appreciable fuel vapor would be developed near the test scram time during a power burst without the effect of fission-product gases. Therefore, the fuel selected had no appreciable burnup.

Figure 12 summarizes the test capsule data during the test, including a short period after reactor scram. The F2 TREAT power transient consisted of about a 6.5 sec period at about 41 kW/m constant power followed by a burst to a power level about 11 times nominal. The initial period for the overpower was about 200 msec. The lower thermocouple burned out by contact with molten clad at the end of the constant power interval. The upper thermocouple burned out from contact with molten fuel that was ejected 410 msec after initiation of the power burst. The gradual rise in capsule pressure during the heatup interval is

caused by heating of the capsule fill gas and pin plenum gas. As in Test F1, there were no sharp increases in capsule pressure after cladding failure caused by release of retained fission gas from the fuel or pin plenum. The slight decrease in capsule pressure after the pressure peak is probably due to a negative radiation response for that particular pressure transducer. The F2 posttest neutron radiograph is shown in Fig. 10(b). The pretest fuel column and the posttest heated wall axial positions are located on the posttest neutron radiograph for reference. Interpretation of the radiograph is dependent upon incomplete posttest examination so that the interpretation is now still preliminary. However, a significant feature identified in Fig. 10(b) is a 165-mm-long region of once-molten fuel ejected to a position as high as 190-mm above the original fuel column. Most of the ejected fuel is lodged in a 100-mm-long annular fuel slug located between either the upper stainless steel heat sink or tantalum transition piece, and the insulator pellet region of the fuel pin. Proceeding downward from the ejected fuel are a 140-mm region voided of fuel, a 100-mm region partially voided of fuel, a 90-mm region of once-molten fuel extending radially outward to the inside of the heated wall, a 13-mm region partially voided of fuel, and a 13-mm region of partially melted fuel. Test-induced changes in the 330-mm-long heated wall are evident from its generally deformed shape. Melting of the UO_2 phase, radial deformation out to either the molybdenum reflector or the lower stainless steel heat sink, and test fuel leakage through the heated wall were observed in some parts of the 330-mm-long heated wall. Unlike the results for F1, the tantalum cup at the bottom is filled with stainless steel clad that had drained off the fuel column.

The initial fuel ejection event in the F2 test is the most important test result relevant to LMFBR safety evaluations. The hodoscope indicated that the fuel column was still erect at the end of the initial heating interval at constant power. Heat transfer calculations indicate that the peak axial fuel temperature was slightly below the solidus at the beginning of the power burst. The initial fuel ejection that occurred 410 msec after initiation of the power burst was detected by burnout of the upper thermocouple and by the hodoscope. The hodoscope detected the ejection as a large increase in the count rate of the top active row (row 2) in its detector array. Fig. 13 illustrates the geometry and the position of certain test instruments relative to the ejected fuel slug. The position of the ejected fuel in Fig. 13 was obtained from the posttest neutron radiograph, Fig. 10(b). The velocity of the ejected slug estimated from the hodoscope data is about 470 mm/sec. The low slug velocity infers that the pressure difference ($P_L - P_U$) that drove the ejected slug was also low. An estimate of P_L (of 152 kPa) was made based upon the pressure transducer data and simple hydraulic assumptions at the time of slug ejection. Heat transfer calculations indicate that the peak axial fuel temperature at the ejection time was about 3000°C. Less confidence is placed in the heat transfer calculation than the pressure estimate since the heat transfer calculation is more sensitive to changes in fuel geometry prior to the ejection.

A survey was made to determine what candidate constituents can generate a sufficient fuel slug driving pressure (152 kPa), which is assumed to correspond to the partial pressure of a gas or vapor in thermodynamic equilibrium with fuel at a temperature of about 3000°C, corresponding to the fuel hot spot in the F2 test. Four candidate species were considered to be fuel, cesium, xenon and cladding. Within the uncertainties of the the heat transfer calculations and the pressure estimate, none of the candidate species can be rejected as a cause of the ejection event solely on the basis of thermodynamic considerations. Fission products could have been released from the fuel before the ejection. There is also evidence that the stainless steel cladding drained off the fuel column before the ejection. However, the posttest examination revealed the presence of some stainless steel entrapped in the molten fuel. It is not certain that the steel entered the fuel before the ejection event. Examination of the capsule wall is in progress to determine what materials may have condensed

on the wall. Unfortunately both condensable fission products and fuel particles have been found. It therefore seems unlikely that the vapor or gaseous species responsible for the initial ejection event will be determined by experimental means.

While clad motion determination was not a primary goal in the F1 and F2 experiments, decided dissimilarities in clad behavior were noted during these two tests, which were similar except for fuel burnup up to the time of the F2 power burst. Response of the F2 lower thermocouple indicated clad draining into the bottom tantulum cup before the burst initiation. There was no indication of clad draining in the F1 test, even though about 42 percent more energy was generated at constant power in the F1 test compared to F2 at the time of the lower thermocouple burnout. Posttest examination indicates that some F1 cladding was trapped between the fuel and heated wall with molten clad intrusions into some fuel which was still below the solidus. Since no mechanical blockages to clad drainage were observed in the F1 posttest examination, one can hypothesize that some mechanism existed in that test which thermally isolated the clad from both the fuel and heated wall. This thermal isolation delayed heating of the F1 cladding relative to F2. The only apparent difference between these two tests is that the F1 fuel had a higher burnup than F2, 2.35 atom percent versus 0.35. Future investigation of differences in fission-product release, differential thermal expansion (or swelling) between the irradiated fuel and cladding, or clad-fuel wetting behavior might explain the differences in clad behavior for these two tests.

CONCLUSIONS

While certain conclusions can be drawn from the posttest examinations and data analyses, accident analysis models extend the area of understanding in important ways. For example, in Test E8 the initial postfailure coolant behavior could have been caused by either a fission-gas release and/or a mild fuel-coolant interaction. However, PLUTO analysis indicates that the most plausible explanation is an initial expulsion of a mixture of fission gas and fuel together with a very mild fuel-coolant interaction. Test E8 was the first TOP test in which the fuel motion was clearly dispersive. Initially, a small amount of fuel was swept above the original fuel column within 20 msec of the initial failure. Subsequent coolant perturbations and fuel movements followed until an upper blockage was formed at about 100 msec after failure. Further fuel movement within the original fuel zone continued beyond that time, but its usefulness for accident evaluation may be limited by the loss of test section geometry. However, the fuel and coolant behavior within the first tens of milliseconds after failure can be modeled with PLUTO.

Clad failure in Tests L5, F1 and F2 occurred when the clad was near its melting point. This was significantly different than the abrupt type of clad failure in Test E8 at a calculated clad temperature of 920°K. Due to the limitations in detector sensitivities under the particular test conditions for loss-of-flow Tests L5, F1 and F2, there were no indications of when clad failure had actually occurred. However, posttest examination of constant-power-test F1 provided a plausible fuel element behavior model, based on fuel swelling, to explain the fuel motion. Fission-product-induced swelling in the fuel above the solidus forced the unmelted outer fuel surface against the heated wall. The net fuel slumping observed in posttest examination of F1 could have been caused by a later release of fission products from the fuel above its solidus. The fission-product release relieved the driving force which had forced the fuel against the heated wall.

Note that the microstructure of fuel tested above the solidus in L5 under a loss-of-flow driven overpower (Fig. 11(c)) showed some gross radial swelling similar to the results in F1 at constant power (Fig. 11(b)). However, the gross

axial movement which followed was different. Both tests performed to date with irradiated fuel under LOF-driven overpower conditions, L5 and F2, were axially dispersive, even though the fuel in L5 was apparently only driven to partial melting. Fuel motion in Test F2 with low burnup fuel was even more dispersive. It is presently unclear from the current state of F2 posttest analysis whether the dispersive fuel motion was induced by fuel vapor, volatile fission products, fission product gases, clad vapor, or a combination of these driving forces.

The fuel dispersals in Tests L3 and L4 occurred in both tests several seconds after the initial axial motion. This type of dispersal did not occur in F1. Either F1 was terminated before a dispersal could develop or the dispersals were dependent upon such factors as the intimate mixing of fuel and stainless steel.

ACKNOWLEDGMENT

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Fig. 1. Test Cluster Cross-sectional View and Posttest Neutron Radiograph of Fuel Column Region of E8 Test Section

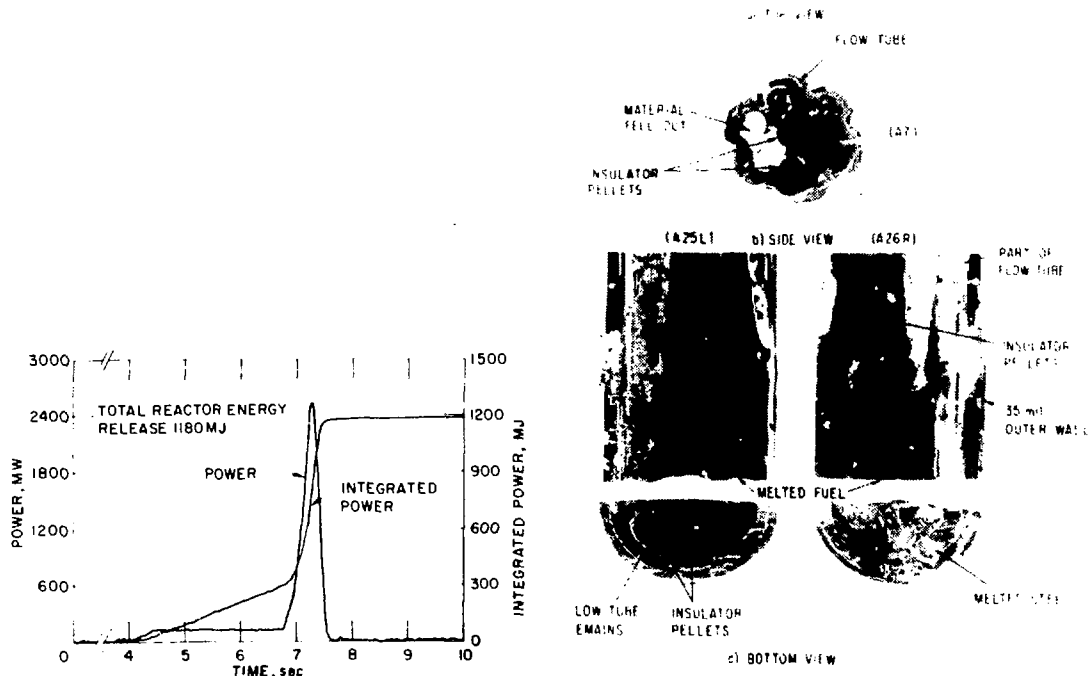


Fig. 2. TREAT Reactor Power and Energy Release for the E8 Transient Test

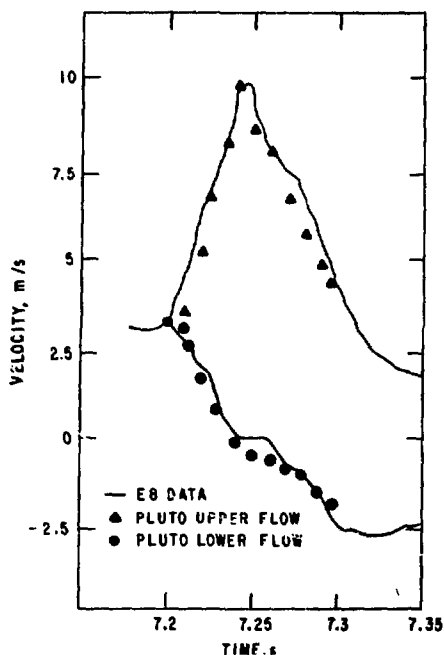


Fig. 4a. Experimental and PLUTO Calculated Flow Velocities

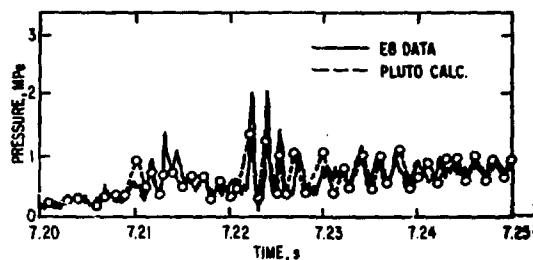


Fig. 4b. Experimental and PLUTO Calculated Inlet Pressures

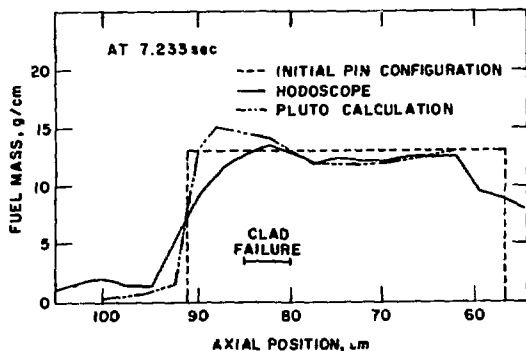


Fig. 4c. Hodoscope-determined and PLUTO-calculated Fuel Distributions (Top of Initial Fuel at 91 cm)

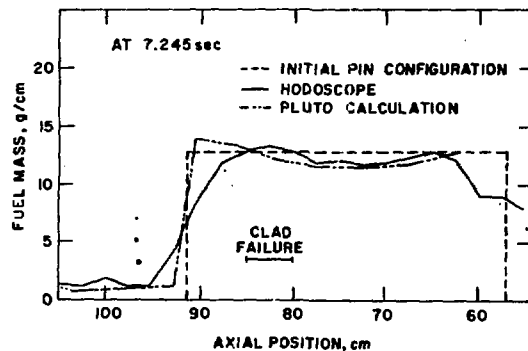


Fig. 4d. Hodoscope-determined and PLUTO-calculated Fuel Distributions (Top of Initial Fuel at 91 cm)

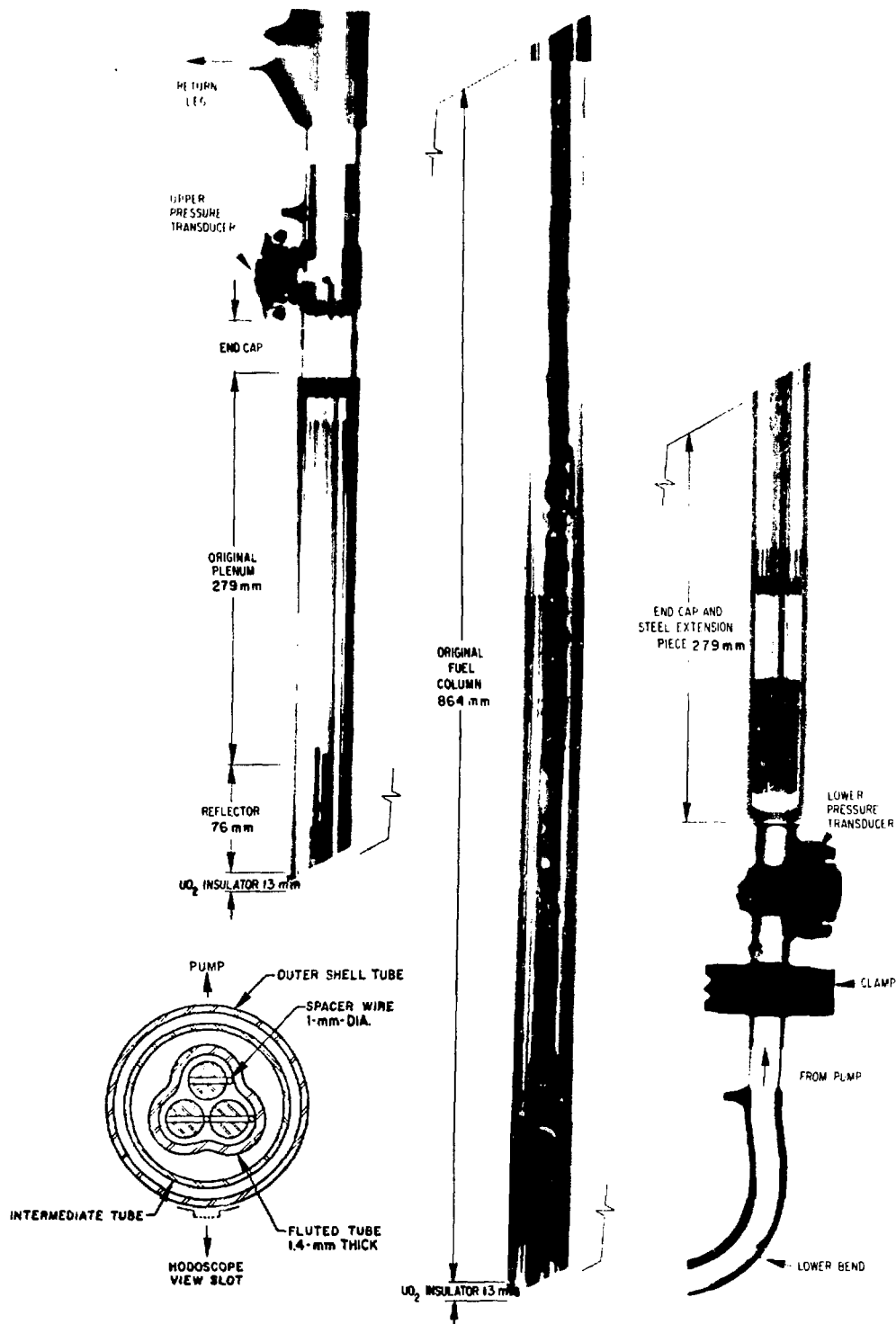


Fig. 5. Test Cluster Cross-sectional View and Posttest Neutron Radiograph of L5 Test Section

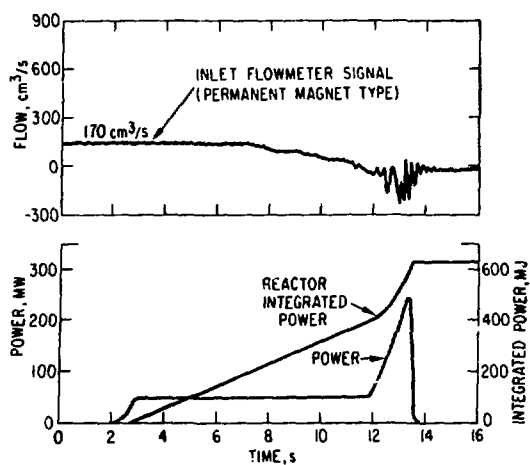


Fig. 6 TREAT Reactor Transient and Loop Inlet Flowmeter Data for L5 Test

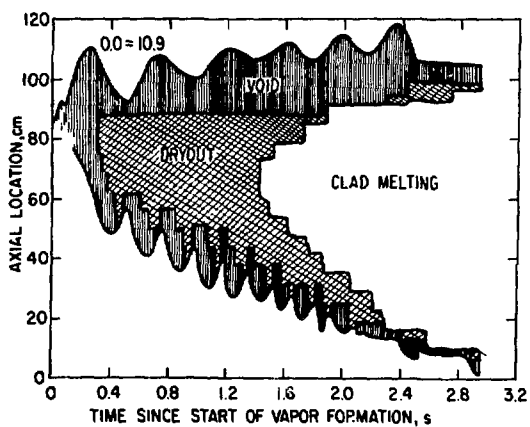


Fig. 7 Calculated Voiding Profiles for L5 Test

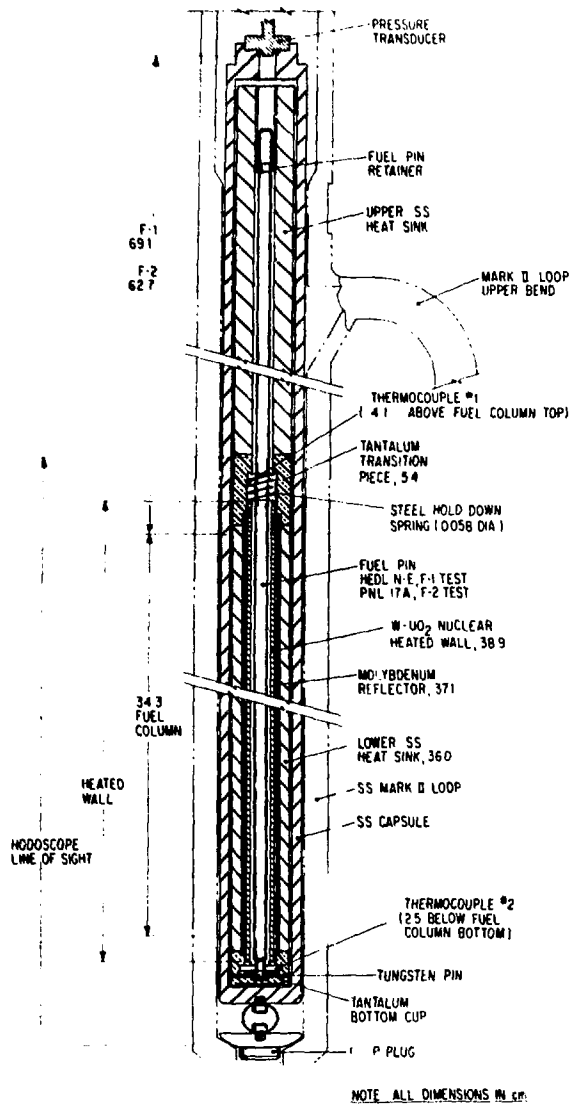


Fig. 8 F1,2 Test Capsule

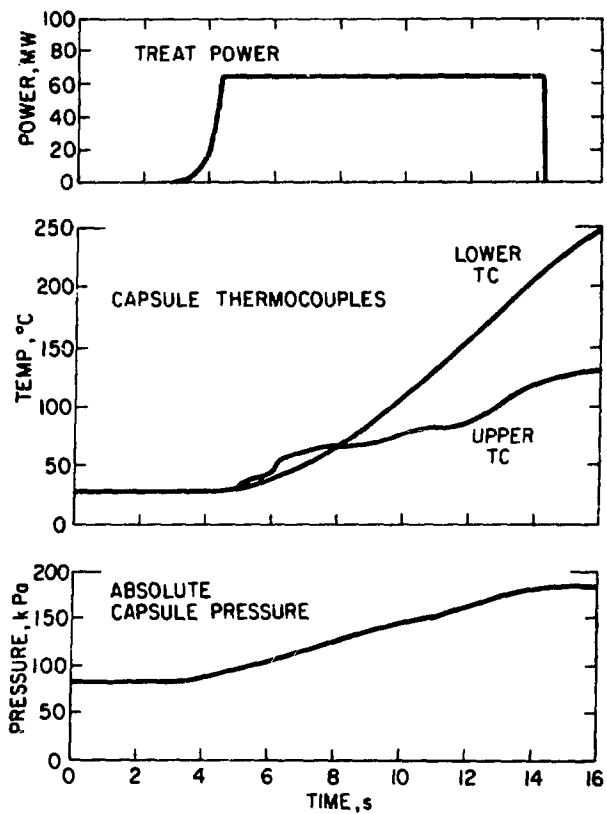


Fig. 9 F1 Test Data

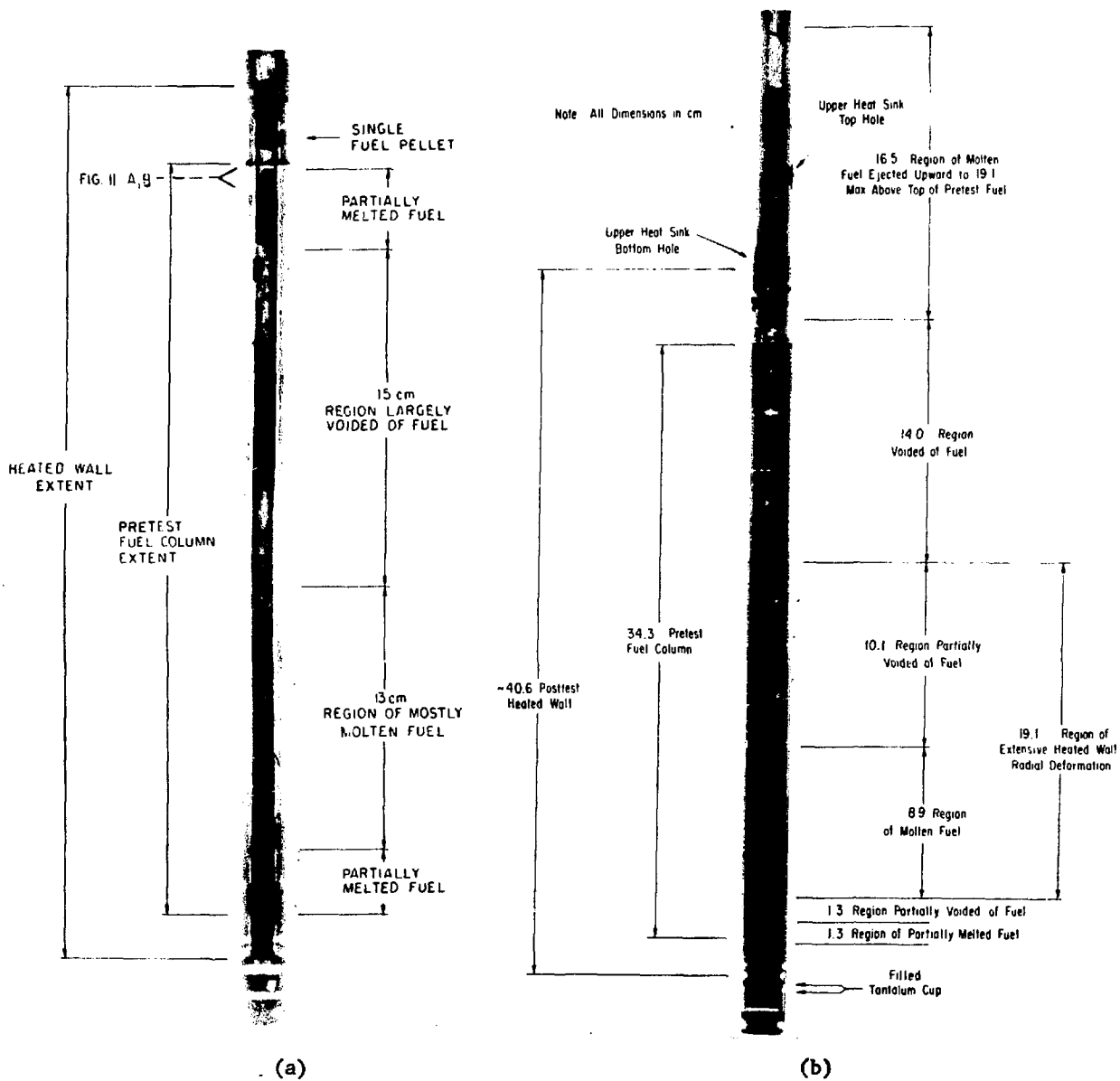
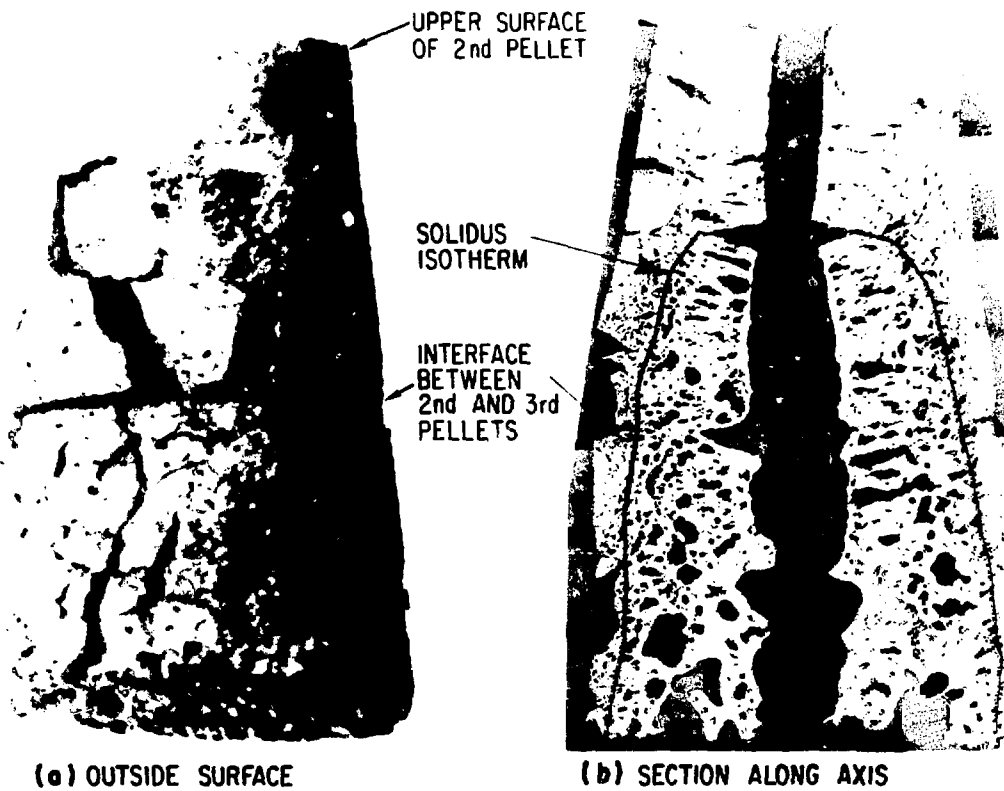


Fig. 10 F1, F2 Posttest Radiographs.
 (a) F1 Radiograph
 (b) F2 Radiograph

SWOLLEN F1 FUEL



(c) L5 SWOLLEN FUEL

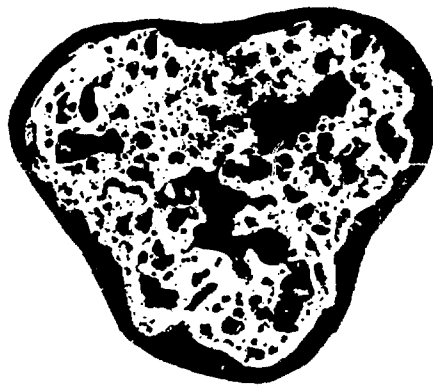


Fig. 11 Posttest Micrographs of Fuel Tested below the Liquidus in the F1 and L5 Experiments

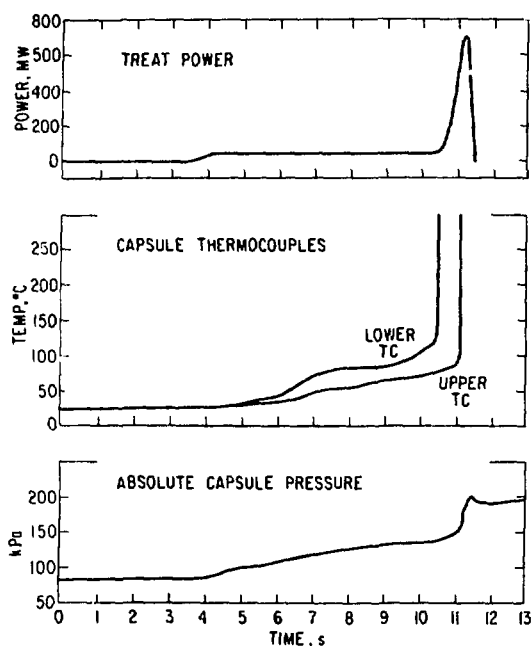
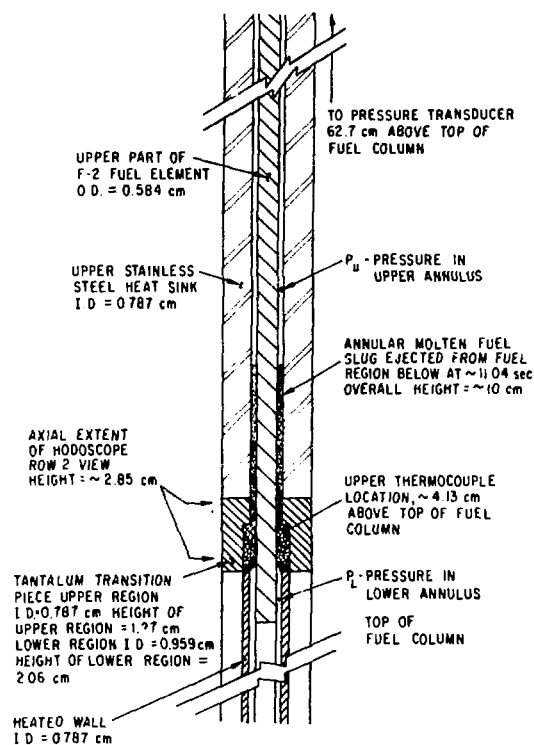


Fig. 12 F2 Reactor Power and Test Data Fig. 13 Schematic Representation of Once-molten F2 Ejected Slug



SUMMARY AND EVALUATION OF R-SERIES LOSS-OF-FLOW SAFETY TESTS IN TREAT

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ABSTRACT

A series of in-pile tests has been performed in which 7-pin bundles of FTR fuel pins (fresh UO_2) have been subjected to transient undercooling conditions representative of a hypothetical unprotected loss-of-flow (LOF) accident in FTR. The tests were undertaken to evaluate SAS modeling and code predictions of coolant voiding, molten cladding motion, and cladding blockage formations. Agreement with SAS predictions was found to be remarkably good. The absence of sodium superheat, sodium reentry, and fuel-sodium interactions was confirmed. Flooding of molten cladding by sodium vapor streaming was confirmed to result in an upper cladding blockage prior to subsequent formation of the lower blockage. The tests also revealed two areas where improvements in modeling were needed 1) to handle the additional effects upon coolant and cladding motion caused by plenum gas release, and 2) to improve the description of upward cladding motion and blockage formation which was found to be less substantial than predicted. These and related improvements suggested by the tests are being incorporated into SAS4A.

INTRODUCTION

Hypothetical accidents such as the unprotected loss-of-flow are currently analyzed by means of large computer codes which integrate models of physical phenomena into a description of an accident sequence for given initiating conditions. The R-series tests, together with complementing in- and out-of-pile testing programs, provide a data base with which the validity of code predictions may be assessed. The overall objective of the R-series LOF tests has been to support development of the SAS accident analysis code [1] in the areas of coolant dynamics and molten cladding behavior for the initiating phase of the hypothetical LOF accident under FTR-type conditions.

A new R-series test vehicle was developed for use at TREAT [2] which has the unique capabilities of accommodating full-size LMFBR fuel pins and providing the essential hydraulic and thermal features of an LMFBR (Figs. 1-3). Five 7-pin tests have been performed in which the test sequences were continued to varying degrees of cladding and fuel melting. Pretest analyses performed with SAS, using input conditions appropriate for these 7-pin TREAT tests, were used first as a basis for planning the test sequences (in particular, the times of test terminations), and later (Fig. 4) for comparisons with test results.

Evaluation of test data, post-test examinations and SAS comparisons have been completed to the extent that important findings for these 7-pin LOF tests can be summarized.

DESCRIPTIONS OF TESTS

All tests were conducted in a similar manner using the same basic apparatus and test section configurations. The 7-pin test sections, illustrated in Fig. 1, were designed to provide radially uniform power generation and temperature conditions, consistent with the SAS single-channel thermal-hydraulic model of a subassembly. Test conditions nominally represented the central region of a high power-to-flow subassembly in the FTR.

The purposes and key parameters of individual tests are listed in Table I. The only parameter varied in tests R4 through R6 was the time of termination of pin power relative to the onset of inlet flow reversal (FR). This gave three "snapshots" of debris configurations at varying stages of disruption. In particular, test R5 terminated early to key on the formation of cladding blockages. Tests R4 and R6 both terminated after considerable fuel melting had taken place. Test R7 was terminated by an energetic power pulse which was programmed to match the SAS-calculated power associated with fresh fuel slumping in FTR. The purpose of this test was to assess the motion of molten core materials (driven by fuel and steel vapor pressure) for a fuel bundle plugged at both the top and bottom by steel blockages. Test R8 was performed with pressurized fuel pins to examine the effect of pressurized, noncondensable gas release upon sodium voiding and upon formation of the steel blockages.

Initially, calibration experiments were performed to determine the test fuel power generation in terms of TREAT reactor power. Results are summarized in Table II and Fig. 3. All tests were begun with a preheat stage during which quasi-steady thermal and hydraulic conditions were established. Coolant velocity was about 7.2 m/s (23 fps), test fuel power was nominally 30 kw/pin, and the sodium inlet temperature was nominally 316°C (600°F). The inlet plenum pressure at full flow was about 8.5 atm and the outlet plenum pressure was constant at about 1.3 atm. The transient undercooling portion of the test was initiated by a controlled depressurization of the inlet plenum. The coastdown was programmed to be initially rapid (to conserve reactor run time), decreasing to about 40-50% of full flow in about 2 sec. Thereafter, the rate of coastdown was reduced to approximately match that of the FTR pump coastdown.

Test data, including sodium flowmeters, pressure transducers, thermocouples, acoustic microphone, and fuel failure detectors, were recorded on analog tape and subsequently processed by digital techniques. Information about fuel motion was obtained using the fast neutron hodoscope.

SUMMARY OF KEY TEST RESULTS

Results of tests R3 through R5 [3] and recent tests R6 through R8 [4-6] have been reported separately. Key test results are summarized in this section, including important variations caused by a) pressurized fuel pins, and b) a power burst at slumping initiation. A summary of parameters and events for the various tests is given in Table III.

The coolant flow coastdown induced undercooling, causing the coolant outlet temperature to increase toward its saturation temperature. Coolant boiling and subsequent voiding started at the top of the active fuel. The typical sequence showed local boiling occurring for about 1 sec. prior to the start of net voiding, and net voiding occurring for about 0.7 sec prior to inlet flow reversal. No significant sodium superheat was detected. Detailed OPERA measurements [7] have shown that initial void growth occurs as unidirectional annular flow in which sodium vapor fills the central region of the bundle while sodium flow continues through a peripheral annulus. The void growth continues upstream until vapor formation exceeds vapor condensation to the extent that pressurization leads to inlet flow reversal. This flow reversal (FR) occurred

after about 20 cm of voiding into the core for the R-series 7-pin tests [8]. Following FR, voiding continues as a bi-directional annular flow. Test data indicated the void fraction was about 85%, suggesting that the average sodium film thickness remaining on the cladding and structure was about 0.09 mm.

Test data in Fig. 5 show channel conditions during and following voiding. Key test events are indicated together with related SAS predictions. Time is measured from FR which provides a convenient reference between test and SAS.

Comparisons of the initial voiding profiles in Figs. 4 and 5 show good agreement except that the "chugging" oscillations have been smoothed out in Fig. 5. On the average, downward voiding through the core is limited by thermal and heat capacity considerations to the rate at which the cladding reaches the sodium saturation temperature [9]. Film dryout and subsequent cladding heatup (at 300-400 °C/s) followed shortly after sustained voiding (Fig. 4). Cladding failures, which began typically within 1 sec after FR, occurred near the tops of the active fuel at cladding temperatures of about 1315°C and 1200°C for the unpressurized and pressurized (R8) pins, respectively. The failures released plenum gas into the vapor-filled coolant channel, causing an enhanced displacement of the upper sodium slug. For the tests with unpressurized pins, this had no effect upon the thermally-controlled voiding of the lower slug (Fig. 5). However, when pressurized pins were used, the ensuing channel pressurization accelerated the lower slug downward out of the bundle. These test results differed from SAS which at that time did not account for the introduction of noncondensable plenum gas into the vapor-filled channel.

Test results have confirmed that sodium vapor streaming, calculated to be about 90 m/s, causes upward motion (flooding) of the cladding upon initial melting [10]. This steel was found frozen in the upper insulator pellet region in the form of a channel blockage (Fig. 5). Although the details of the blockage formation cannot be assessed, certain characterizations can be made: 1) it cut off vapor streaming to the extent that subsequent molten cladding moved downward, 2) it was much thinner (~0.3 cm) than predicted by SAS, 3) it formed a direct heat path from the pins to the flowtube which accelerated flowtube failure, 4) the blockage could be breached by 2 to 3 atm differential pressure, 5) the blockage formed an effective impediment to upward fuel/steel dispersal during a slumping-related power burst, and 6) the upper blockage may not form at all when channel pressurization effects, caused by failures of pressurized pins, cuts off vapor streaming during the time of initial cladding melting.

Within about 0.25 sec of its inception, cladding melting encompassed the entire top half of the fuel column. Thereafter, calculations indicate that the cladding melting progressed downward at about 18 cm/sec. Test data reveals little about the details of the molten cladding behavior during this stage, although presumably it drained downward at about the same rate as the melt front. As the molten cladding relocated, it left behind columns of fused, unmelted fuel pellets. A residue of molten steel appears to have adhered at surface cracks and pellet interfaces (Fig. 5). However, there is no consistent evidence that the liquid steel penetrated into the fuel; only in rare instances have pellet cross-sections revealed steel intrusions [11].

Inlet blockages were formed when the molten steel entered the lower insulator pellet and reflector region where it solidified. From posttest examinations, Fig. 6, the blockages extended 5 to 8 cm into the reflector region and ranged up to 20 cm in total thickness. These blockages trapped sodium below the bundle inlet and eliminated any possibility of reestablishing coolant flow. Only in the R5 early termination test was this blockage incomplete.

Both the upper and lower blockages were formed prior to significant fresh fuel melting. Upon subsequent melting, fuel drained into contact with a liquid pool of steel atop the lower blockage. While the evidence of fuel/steel mixing and dispersal under these conditions is interesting in itself, it is not relevant to the accident sequence since slumping-related reactivity effects would become dominant. This was treated in test R7 in which a power pulse deposited about 660 J/g when the hotspot melt fraction was about 50 to 90%. The resulting vaporization of fuel and steel drove core debris to the axial extremes of the

fuel region where the previously-formed steel blockages prevented expulsion. Photos of the debris are shown in Fig. 6. The 10-cm long clinker at the upper blockage was unique to this test and confirmed the SAS prediction that fuel would collect at the upper blockage.

The process of fresh fuel slumping at nominal power is characterized by pellets shown in Figs. 5 and 7. Pellets showing center melting of less than about 40% area fraction are typically undrained; at higher melt fractions, the central cavity is generally voided. Three possible mechanisms for fuel removal from an intact pellet stack are suggested in Fig. 5: 1) internal pressure may force the stack apart axially allowing the fuel to emerge through pellet interfaces; 2) internal pressure may cause lateral expulsion through a fault in the unmelted shell (as observed in DEH tests [12]); and 3) the pellet stack may buckle, permitting molten fuel to simply drain out of the center. There is evidence of molten fuel draining both through the inside of a pellet, Fig. 7, and on the outside surface of a stack, Fig. 5. In general, the fresh UO_2 pellets showed remarkable structural integrity until the center melting progressed completely to the surface.

COMPARISONS WITH SAS

The general agreement between SAS and the R-series tests in the areas of coolant and cladding behavior has been good [13]. The following variances have been identified and assessed.

1. The transition from local coolant boiling to flow reversal is slower in the tests than predicted by SAS. This is related both to heat losses and the complexity of the initial void growth. Recent analysis has indicated that the time and extent of upstream voiding prior to flow reversal would be significantly reduced in larger pin bundles. Hence the existing SAS model should satisfactorily describe such events at subassembly scale [8].

2. The need for current voiding models to handle the release of plenum fission gas into the coolant channel was identified. Test R8 demonstrated that channel pressurization upon initial cladding failures substantially accelerated sodium voiding and altered the subsequent motion of molten cladding compared to SAS predictions. Work is in progress to incorporate this into SAS4A [14].

3. While the formation of an upper cladding blockage by virtue of sodium vapor streaming was confirmed, the blockage was found to be much less substantive than predicted by SAS. An improved cladding motion model is presently under development for SAS4A [15].

4. Slumping of fresh UO_2 fuel was found to occur at a lesser melt fraction than the 1.0 value normally used for fresh fuel calculations in SAS. A more appropriate value appears to be about 0.5.

LIMITATIONS AND SIGNIFICANCE

In providing a data base for assessment of SAS modeling and code predictions, the R-series tests were both timely and successful. They do not in themselves provide a comprehensive validation of code development as it relates to any particular accident scenario, however. Limitations exist insofar as interpreting 7-pin test results with respect to SAS, and additionally in interpreting both the tests and SAS with respect to subassembly-scale events. While effective measures were taken to minimize heat loss from the 7-pin bundle, it was unavoidably larger per pin than encountered in a subassembly [16]. The principal effect was to lengthen the time scale of test events relative to subassembly events, particularly with regard to fuel melting and motion. This was accommodated in SAS by appropriately increasing the structure heat sink. Of greater limitation is the single channel nature of both SAS and the 7-pin tests in which interconnected channel effects brought about by intrasubassembly noncoherence cannot be assessed. In this regard, the one-dimensional cladding motion which produced the upper blockages in these 7-pin tests was enhanced relative to subassembly conditions. In general, the noncoherence effect is not expected to be large for sodium voiding at nominal power, but may be more significant in

terms of cladding and fuel motion. The combination of fresh UO_2 fuel and small bundle size unavoidably limits the prototypicity of these test results with respect to fuel motion. In balance, however, fuel motion follows voiding and cladding relocation for these FTR-type accident conditions and, in itself, has no bearing on these earlier events which were the focal points of the R-series tests. Certain aspects of fuel behavior were found to be of interest from a metalurgical and hodoscope diagnostics point of view.

A measure of the successfulness of the test series lies in the impact it has had upon modeling efforts. While confirming the overall validity of SAS predictions, the tests focused attention on areas of discrepancy and prompted further analytical study of such features as initial void growth, plenum gas release, cladding motion and freezing, and bundle heat losses. This has led to significant modeling improvements which are currently being implemented in updated versions of SAS.

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TABLE I

Summary of TREAT/R-series Tests Performed in
Support of FTR TUC Accident Analysis

<u>Test</u>	<u>No. Pins</u>	<u>Plenum Gas (STP)</u>	<u>Test Fuel Power/Termination</u>	<u>Purpose</u>
R3	1	He at 1 atm	10kw/ft flattop; 10.5 s after FR	R-series proof test
R4	7	He at 1 atm	10kw/ft flattop; 10.1 s after FR	Reference test: in- tegrated sequence at nominal power
R5	7	He at 1 atm	10kw/ft flattop; 4.4 s after FR	Early termination, cladding relocation test
R6	7	He at 1 atm	10kw/ft flattop; 8.2 s after FR	Hodoscope assessment of fresh UO_2 slumping
R7	7	He at 1 atm	10kw/ft flattop; 6.2 s after FR, 15x power burst	Vapor-driven fuel dispersal in presence of upper and lower cladding blockages
R8	7	3-Xe at 14.2 atm 4-Xe at 1 atm	10kw/ft flattop; 7.1 s after FR	Effect of pressurized plenum-gas release on coolant voiding and cladding reloca- tion

TABLE II

Summary of Calibration and Power Shape Data

<u>Parameter</u>	<u>R3</u>	<u>R4 - R7</u>	<u>R8</u>
No. of pins	1	7	7
Calibration factor; average of all pins, $\frac{\text{kw/pin}^a}{\text{MW}_{\text{TREAT}}}$	0.678	0.436	0.474
Axial power shape, max-to-avg. ^b	1.10	1.25	1.15
Radial power shape, max-to-min.			
a) center pin (20% enriched)		1.16	1.16
b) edge pin (14% enriched)		1.12	1.12
Pin-to-pin power ratios, max-to-avg.			
a) radial (center-to-edge)		1.20	1.20
b) azimuthal (edge pins)		1.01	1.01

^aBased upon 173.5 gm UO_2 per pin

^bAxial shapes for tests R4 through R7 were perturbed by presence of SST heat sink at lower one-third of active fuel; for R8, SST was replaced by graphite (see Fig. 3).

TABLE III

Summary of Key Events During
R-series FTR TUC Test Sequences

<u>Parameter</u>	<u>R3</u>	<u>R4</u>	<u>R5</u>	<u>R6</u>	<u>R7</u>	<u>R8</u>
Coolant inlet temperature, °C	304.0	327.0	338.0	324.0	316.0	310.0
Coolant velocity in test bundle, m/sec	7.05	7.15	7.32	7.56	7.59	7.15
Time of reactor power up, sec	2.8	3.0	3.2	3.5	3.5	3.5
Average power/pin at flattop, kw	29.0	28.3	28.3	28.3	27.9	31.8
Quasi-steady test section ΔT , °C	162.0	133.0	139.0	130.0	133.0	
Start of flow coastdown, sec	9.1	7.90	7.95	8.73	8.38	7.97
Supply tank (inlet plenum) pressure, atm	10.2	8.61	8.40	8.54	8.47	8.37
Fuel bundle inlet pressure, atm		6.88	6.67	6.81	6.74	6.64
Fuel bundle outlet pressure, atm	1.30	1.29	1.30	1.30	1.23	1.30
Onset of local boiling, sec	20.55	13.15	12.5	15.8	14.6	15.2
Onset of net voiding, sec		14.15	13.65	16.8	14.9	17.5
Coolant velocity, m/sec	1.8	1.83	1.83	1.70	1.37	2.1
Rate of flow decay, %/sec	12.0	12.0	12.0	12.0	28.0	11.0
Time of inlet flow reversal, sec	22.30	14.87	14.56	17.46	15.40	18.18
Elapsed time from FR to:						
i) onset of cladding failures (plenum gas release)	1.20	1.13	1.14	0.90	1.24	0.71
ii) upper cladding blockage	2.2	2.5	2.6	2.45	2.8	
iii) flowtube failure	3.57	3.14	3.29	3.00	3.73	0.79
iv) lower cladding blockage	5.85	5.20	6.20	4.35	6.6	2.49
v) onset of fuel motion				5.0	6.2	3.82
vi) power pulse					6.20	
vii) power termination	10.55	10.15	4.45	8.20	6.76	7.12
Complete upper blockage		yes	no	no	no	none
Complete lower blockage	yes	yes	no	yes	yes	yes

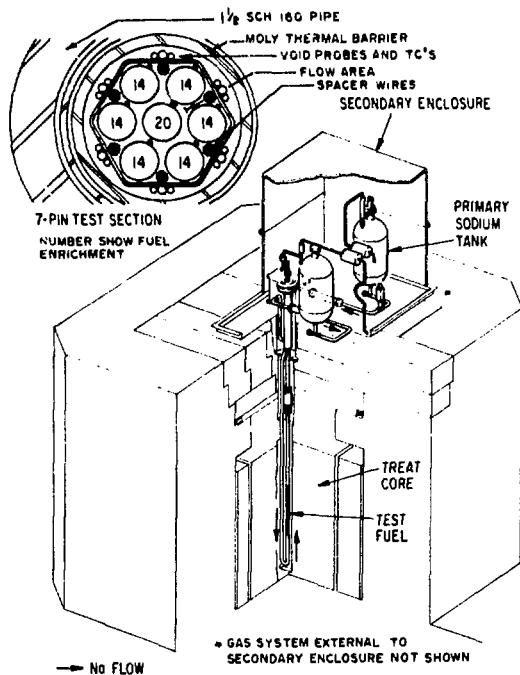


Figure 1
R-series Test Vehicle in TREAT and Cross Section of 7-pin Test Section Used For FTR TUC Tests R4 through R8

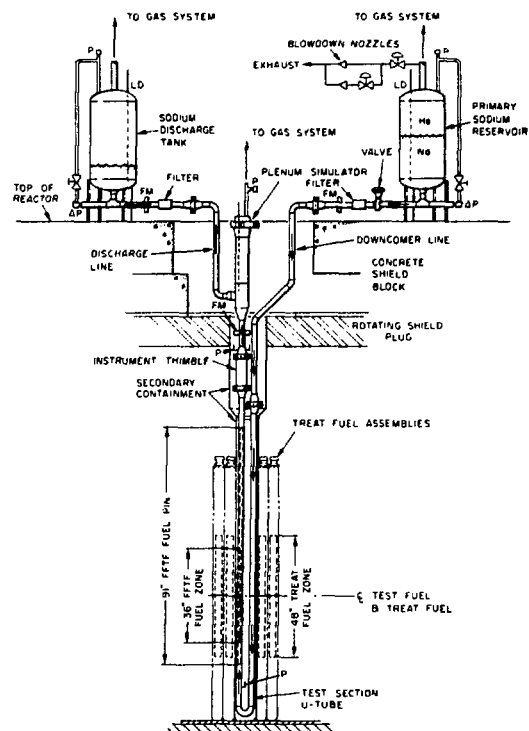


Figure 2
Schematic Diagram of R-series Apparatus Illustrating Basic Components and Instrumentation and Locating the Full-size FTR Fuel Elements in Relation to the TREAT Core

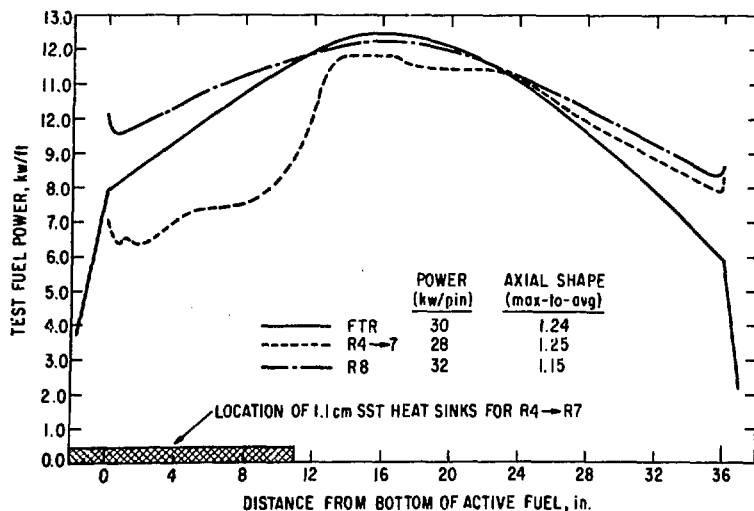


Figure 3
Axial Power Distributions of R-series Test Fuel in TREAT Compared with FTR Nominal Power

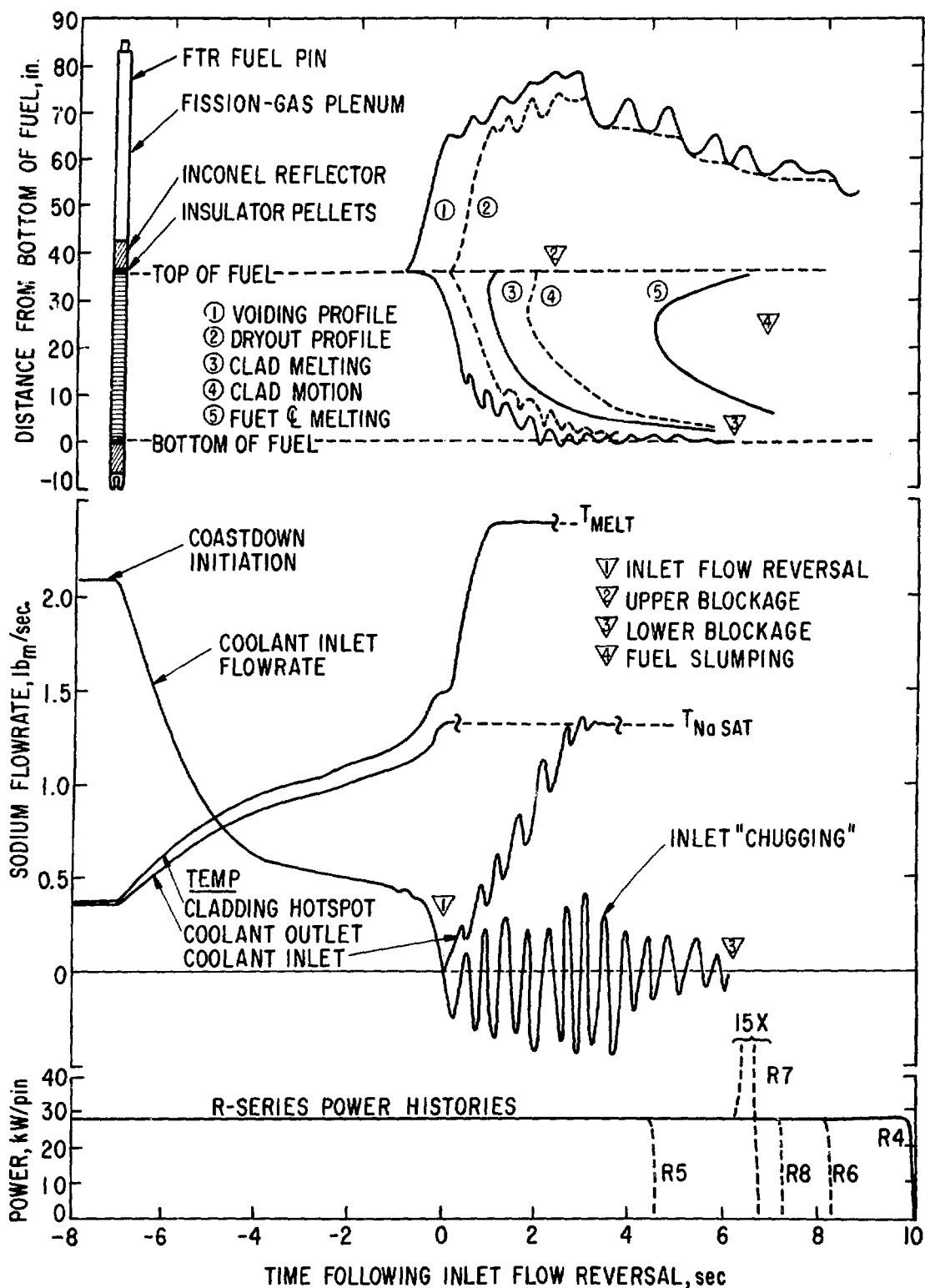
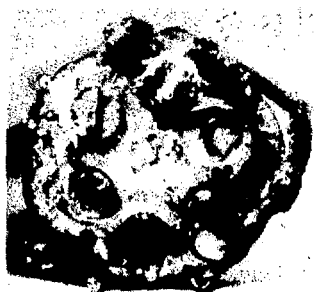


Figure 4. SAS3A R4 Analysis Case 1 (Low heat loss); 28 kW/pin. Power-time histories for 7-pin tests R4 through R8 are indicated.

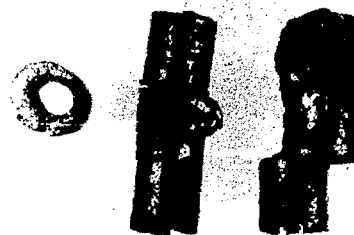
Pellet Stack following
Cladding Meltoff
(from R5)



Section of Lower Cladding
Blockage at Insulator
Pellet Region



Bottom View of Upper
Cladding Blockage



Pellets Showing Molten Fuel
Slumping (Unirradiated UO_2)

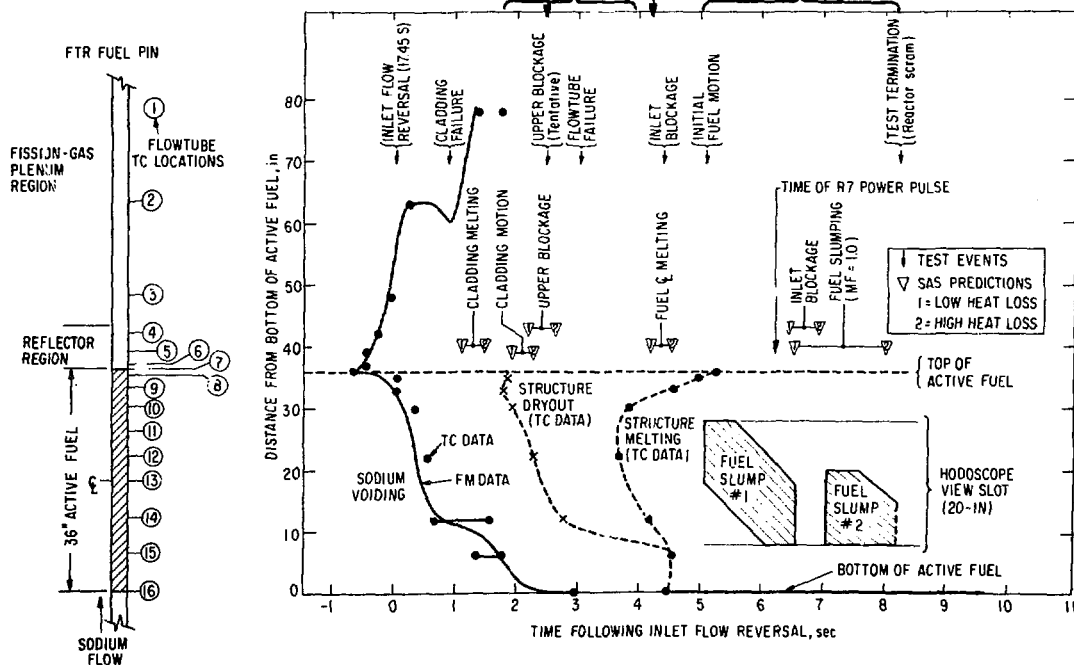


Figure 5. Composite of R6 Test Data Together with SAS Predictions
Illustrating Features of LOF Test at 28 kW/pin Flattop Power

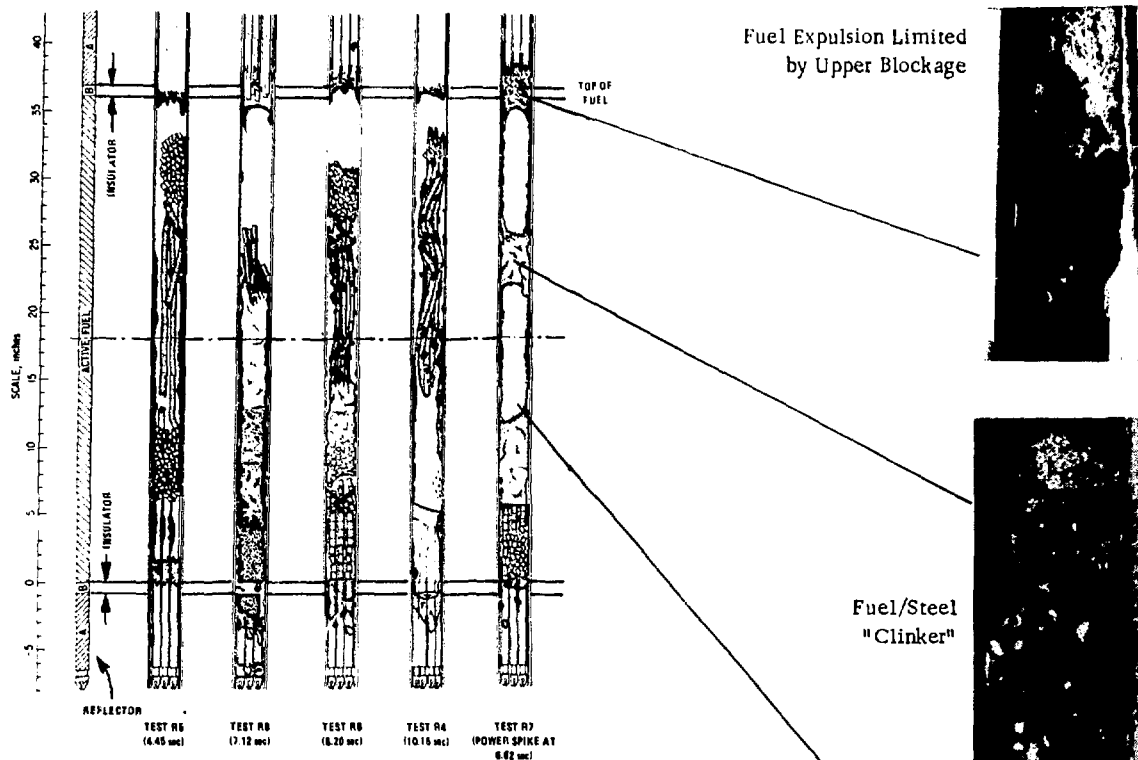


Figure 6

Illustration of Post-test Debris Configurations; Times in Parenthesis Indicate the Elapsed Times from Inlet Flow Reversal to Power Termination or Power Burst (R7).

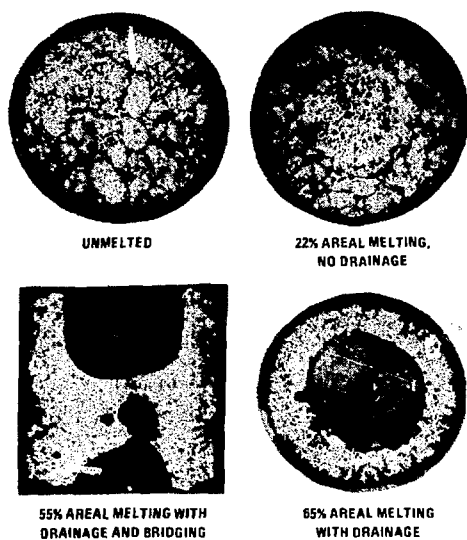


Figure 7

Sections through R5. Fresh UO_2 pellets showing variations in center melting.

ON THE SIMULATION OF LOF ACCIDENTS WITH DIRECTLY ELECTRICAL HEATED UO_2 PINS

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ABSTRACT

The behavior of directly electrical heated UO_2 pins has been investigated under loss of coolant conditions. Two types of hypothetical accidents have been simulated, first, a LOF accident without power excursion (LOF accident) and second, a LOF accident with subsequent power excursion (LOF-TOP accident). A high-speed film shows the sequence of events for two characteristic experiments. In consequence of the high-speed film analysis as well as the metallographical evaluation statements are given in respect of the cladding meltdown process, the fuel melt fraction and the energy input from the beginning of a power transient to the beginning of the molten fuel ejections.

INTRODUCTION

Let me state at the beginning of this paper that the failure of a single pin will have no severe consequences on the safety of a LMFBR if there is no possibility of a failure propagation from pin to pin or from fuel element to fuel element. Not only the possibility of this pin-to-pin propagation but also the probability of a simultaneous multiple pin failure make investigations on single pin behavior an essential instrument of understanding the entire failure phenomena.

To understand the phenomena of a single pin failure several parameters and initial conditions have to be known:

- First of all a set of characteristic pin failure criteria has to be defined
- Secondly the cause and the mechanism(s) of the failure have to be identified, investigated and theoretically determined
- Thirdly the effects of the failed pin on its neighbouring pins must be considered.

One of the possible methods to investigate these factors are out-of-pile experiments. In our program directly electrical heated (DEH-) UO_2 -pins are used to simulate reactor pins. The theoretical basis and the technical facilities along with the preliminarily experimental results of fuel and cladding behavior have already been reported [1]. The fuel response to electrical power transients is described by the code DYNAM [2].

EXPERIMENTAL RESULTS

The results which I will present here include the pin behavior during two types of simulated accidents:

- the A-type is a LOF-type accident without power excursion,
- the B-type is a LOF-type accident with subsequent power excursion.

Before describing the results in detail let me spend a few words on the experimental facility and set-up. Fig. 1 shows schematically our electrical pin burst facility (EPBF) along with its components. The UO_2 -pin (Pos. 2) is clamped between two Tungsten-electrodes (Pos. 3). The cooling medium is Helium which flows from the bottom to the top. A remotely handled quartz cylinder (Pos. 4) surrounding the UO_2 -pin assures proper cooling conditions. The electric heating system (Pos. 5) provides for a "controlled" and always reproducible power operation. This means that although UO_2 behaves like a "negative coefficient temperature"-resistor it is possible to operate the UO_2 -pins in a steady-state mode with the power fixed in advance.

The initial operating conditions are identical for the two experiments will be followed and chosen in such a way that they simulate the thermodynamic behavior of SNR-300 pins under normal operating conditions (Table I).

TABLE I

		EPBF-pin	SNR-300 pin
Rod power	W/cm	150	355
Central temperature	°C	2247°C	2247°C
Fuel outer temperature	°C	873°C	873°C
Cladding temperature	°C	580°C	585°C
Pellet diameter	mm	5.09	5.09
Gap fuel-cladding	mm	0.55	0.15

The following is a high-speed film showing the sequence of events of two typical experiments, the first being an A-type and the second being a B-type. The first part of the film (A-type accidents) shows the pin behavior in particular the cladding response of a DEH- UO_2 -pin to a coolant flow failure. The film begins during the experiment. The cladding has not yet failed, even though it is under considerably thermal stress. Start of clad-melting is not clearly defined, however, it can be observed at the upper third of the pin 20.8 s after accident initiation. Generally the propagation of the melting front has no regular pattern. Post-experiment analysis shows that the molten material accumulates at the lower end of the pin above the Tungsten electrode. It is important to mention that even intact unmolten parts of the cladding are loosened from the pin.

Fig. 2 shows the sequence of event graphically. Corresponding to the decrease in the coolant flow rate cladding temperatures increase up to the melting point.

In the second part (B-type accidents) the pin behavior is shown while undergoing an electrical power transient. The transient begins 0.8 s after flow-coast-down. The power increase is initiated by applying a higher voltage to the pin. The continued excursion with a time constant of about 100 ms following the voltage increase is caused by the negative temperature coefficient of the UO_2 -resistivity.

First ejections of molten material can be observed 1.6 s after transient-

initiation at the lower part of the pin. Additional ejections occur at the same point and at the middle of the rod and continue even during pin collapse. Observed clad melting occurs 2.6 s after initiation of the transient. Fig. 3 shows the sequence of events of this B-type accident. Begin of the ejections and clad-melting are indicated. Fig. 4 shows the rate of melting as a function of the rate of energy input during the electrical power excursion derived from the post-experiment analysis. The molten fuel fraction at the beginning of fuel ejections is about 25 %. The total transient energy input up to this part of the transient was 4.5 kJ which implies 265 J/gr UO_2 . This value corresponds to the statistical mean of all our B-type experiments [3_7].

SUMMARY

Results of our investigations performed up to now can be summarized as follows:

1. For all types of accidents simulated in our EPBF the cladding meltdown process indicates that idealized geometry assumptions of theoretical models currently in development have not been observed experimentally.
2. For LOF-accidents of the A-type the cladding meltdown process is initiated but post-experiment analysis has shown that fuel did not reach melting point temperatures anywhere.
3. For LOF-accidents of the B-type and power excursions with a time constant in the order of about 100 ms fuel melting and fuel ejections occur before clad melting starts.
4. The total input to the fuel in the time interval from the beginning of the transient up to the time when the first ejections are observed is for this simulated accident type 265 J/gr UO_2 .

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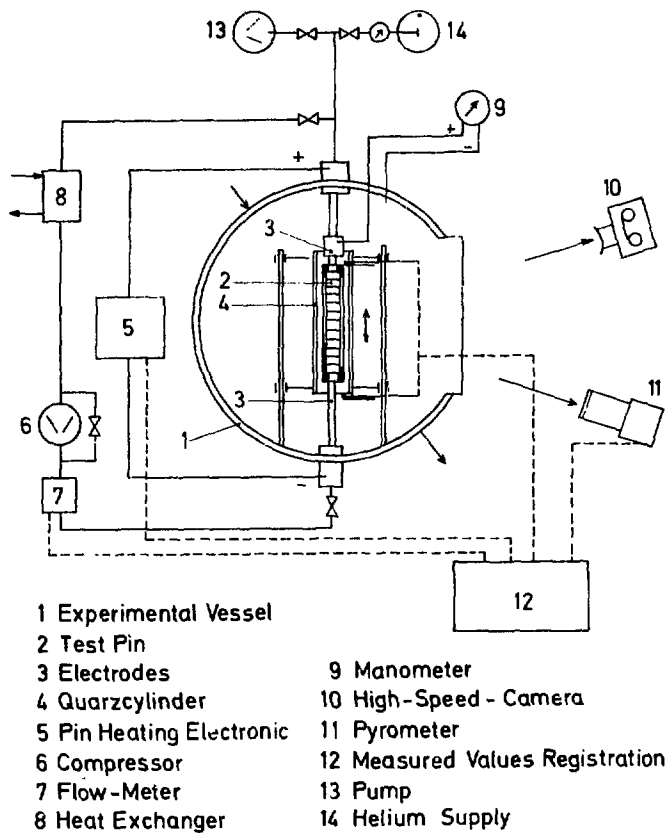


Fig. 1. EPBF Experimental Setup

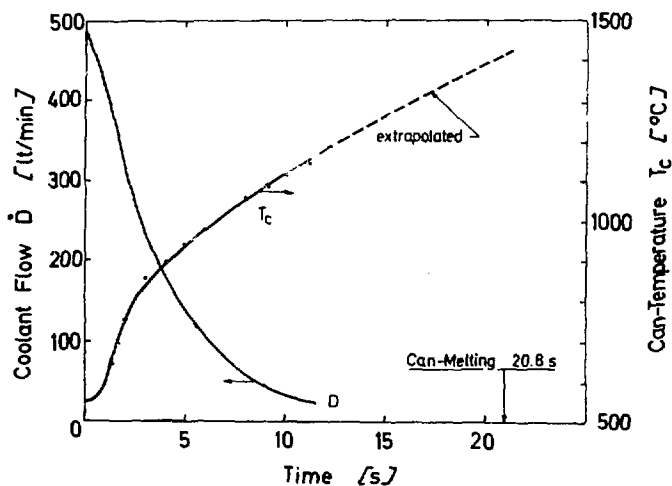


Fig. 2. Can Behavior of DEH-UO₂ Pins for LOF Accidents

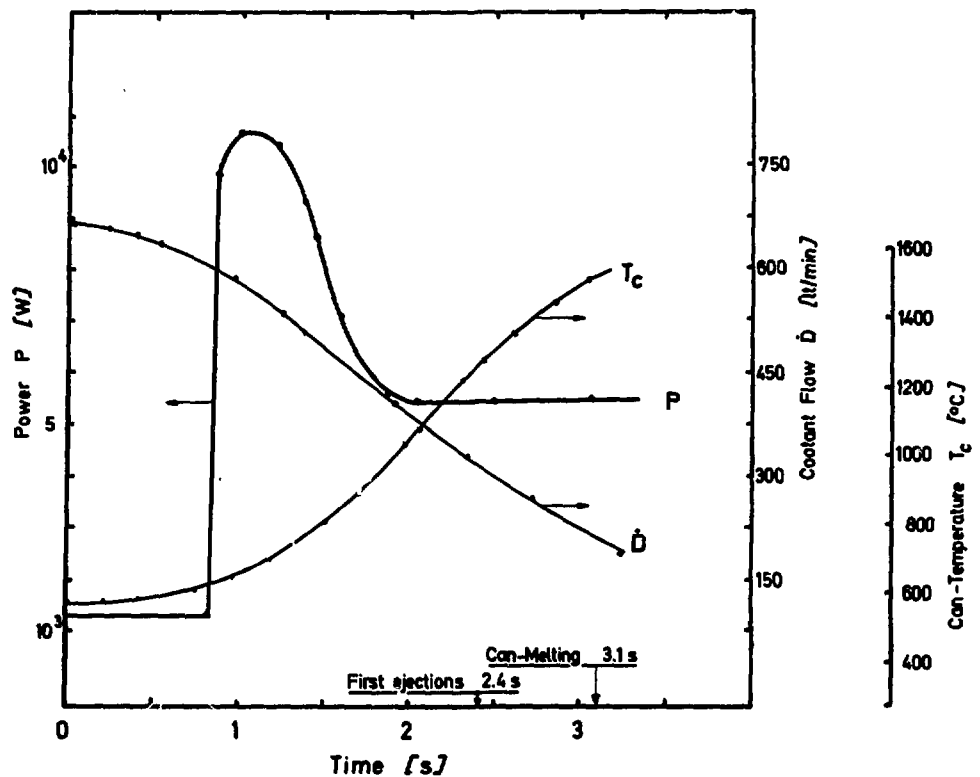


Fig. 3. History of Directly Electrical Heated UO_2 Pins for LOF-TOP Accidents

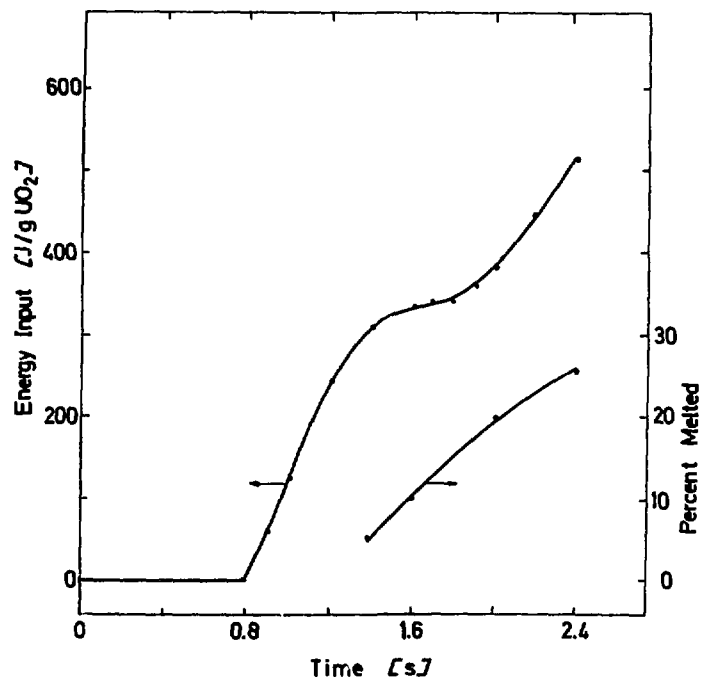


Fig. 4. Energy Input and Melt Fraction for LOF-TOP Accidents

OUT-OF-REACTOR EXPERIMENTAL STUDY OF FUEL-PIN FAILURE PHENOMENA

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ABSTRACT

Fundamental experiments have been performed with a direct-electrical-heating apparatus, on both unclad and quartz-clad UO_2 pellet stacks, to study the effect of a radial constraint on solid and molten-fuel motion during power transients. Results of simulated transient over-power experiments show that molten UO_2 can be quite mobile when the fuel centerline temperature exceeds the boiling point, i.e., fuel vapor pressures become a significant driving force for relocating molten fuel. For radially constrained pellet stacks, when an escape path was provided around the top pellet, significant upward axial fuel motion occurred prior to cladding rupture. Thus, the time sequence of events shows that potential exists for providing a negative reactivity-feedback effect, which would promote nuclear reactor safety. The data tend to support the existence of a "pressurized-bottle" effect, which was observed in high-speed movies.

INTRODUCTION

The occurrence of a core meltdown accident in an oxide-fuel liquid-metal fast-breeder reactor (LMFBR) is highly improbable. However, the potential consequences of core meltdown could be quite severe. Studies of fuel-pin failure phenomena that would precede core meltdown are important in evaluating the safety of an LMFBR. Therefore, a comprehensive out-of-reactor experimental study of fuel response to thermal transients was initiated in the Materials Science Division at Argonne National Laboratory (ANL).

Primary objectives of the program are to study (a) the mechanisms of oxide-fuel failure and (b) fuel motion that precedes cladding failure. The basic philosophy is to separate these phenomena and then study each of them directly out-of-reactor under controlled experimental conditions. This approach should result in a basic model for fuel motion and is complementary to the in-reactor analyst's objective of developing a broad model for fuel motion.

The purpose of the present paper is to expand considerably on previously summarized results of tests on unclad and clad, unirradiated UO_2 pellets [1]. These results serve as a guide for analysis and modeling of fuel-motion phenomena during hypothetical LMFBR accidents.

EXPERIMENTAL

The direct-electrical-heating (DEH) apparatus used in the present study of unirradiated UO_2 has been previously described in the literature [2,3]. In addition, the use of the apparatus has been extended to studies of irradiated fuel, and preliminary work has been reported [4].

The experimental procedure was as follows. A series of pellet stacks of equal length (3.3 cm), diameter (0.59 cm), and theoretical density (94%) was prepared. In the initial heat-up phase of all experiments, the power was increased so that a surface temperature of 1500 K was attained in 5 min. After an additional 2 min, which was used to achieve a steady-state temperature gradient, the transient phase of the experiment was initiated. Tests were performed on both unclad and quartz-clad UO_2 pellet stacks. The objectives of the experiments using unclad pellet stacks were to: (a) determine how unclad, unirradiated UO_2 fuel responds to transient temperatures and rapid heating rates and (b) gain fundamental insight into the principal driving forces responsible for gross fuel motion. The insight gained in testing unclad fuel was then applied to help explain the response of clad, unirradiated UO_2 to transient heating. In some of the experiments that utilized quartz cladding, the upper electrode-cladding diametral gap (which is analogous to the upper Inconel reflector-cladding diametral gap in-reactor) was varied from 0 to 0.23 cm. The objective of varying this gap was to study the effect of escape path size on upward axial molten-fuel motion. The expected differences between the steel cladding in a reactor and the quartz used in these experiments will be addressed in the next section.

A series of interrupted tests were run in which the time interval of the transient was preset with a timing device. High-speed movies were utilized to visually record fuel behavior during the transients. Fuel surface temperatures were measured continuously during the transient with noncontact infrared optical pyrometers, and data were recorded with a data-acquisition system that consists of a minicomputer with an analog-digital interface. Acoustic emission detection was used to monitor thermal-shock cracking of the unirradiated UO_2 pellets during power transients by clamping two acoustic-emission transducers to extensions attached to the upper and lower electrode holders of the DEH apparatus [5]. A linear variable differential transformer was used to constantly measure and record axial expansion of the fuel.

Comparison of Quartz and Stainless Steel Cladding

The quartz simulant cladding is not strictly prototypical of the stainless steel cladding normally used in-reactor. However, the quartz does permit direct visual observation of molten-fuel motion. In addition, by suitably varying the fuel-cladding gap, the extremely important fuel-cladding differential-expansion loading phenomenon is directly observable because the quartz fractures when the cladding load exceeds its fracture strength. Thus, at suitable gap sizes, which are easily varied, this phenomenon has been observed both in pretransient heat-ups and during transients with concomitant molten-fuel motion. For quartz-clad UO_2 , gap closure is accelerated (and therefore made more convenient to study) by two factors. First, the expansion of UO_2 is approximately an order of magnitude higher than that of quartz, and second, the UO_2 is heated to significantly higher temperatures.

Few high-temperature property data for quartz exist in the literature. However, it is known that quartz is unique in that it has a positive temperature dependence for strength and Young's modulus up to ~ 1473 K, which means that these values increase from room temperature up to 1473 K. At 1173 K, Young's modulus for Type 316 stainless steel and quartz are $\sim 17 \times 10^6$ and 12×10^6 psi (11.7×10^{10} and 8.3×10^{10} Pa), respectively. The melting points

for Type 316 stainless steel and quartz are ~ 1673 and 1940 K, respectively. Four-point bend tests of quartz rods show that plastic flow of quartz readily occurs at elevated temperatures and quartz has a fracture strength of 1064 kg/cm^2 at 1273 K, compared with $\sim 990 \text{ kg/cm}^2$ for annealed Type 316 stainless steel (the value for cold-worked Type 316 stainless steel is expected to be higher than that of quartz). Thus, the mechanical and physical properties of the two materials are similar at elevated temperatures and results similar to those obtained in DEH tests could be expected from in-reactor experiments.

Molten-fuel-motion Studies

During a highly improbable transient over-power (TOP) accident, coolant temperatures rise slowly; therefore, considerable fuel melting can occur before the fuel-pin cladding fails. The present study is addressed to the sequence of events that culminates in cladding failure during a TOP accident.

High-speed movies of experiments emphasize that the availability of escape paths is crucial when the response of radially constrained, unirradiated fuel in a mild TOP accident is considered. The escape paths available preceding the power transient are the fuel-cladding gap (for unrestructured fuel) and thermal-shock cracks in the fuel that result from the initial heat-up of the pellets. Figure 1 is a schematic that shows the relative extent of cracks formed during the pretransient heat-up, transient, and cooldown. The large acoustic signal during the pretransient heat-up shows that the largest cracks are formed during that period. Cracks were observed, heard, and detected by acoustic-emission sensors during the pretransient heat-up. Visual examination and posttest optical and scanning microscopy have shown that cracks form an important escape path for molten UO_2 . The present study has verified that, after initial cracking during the pretransient heat-up, additional cracking and crack growth can occur during rapid power transients, prior to gross melting.

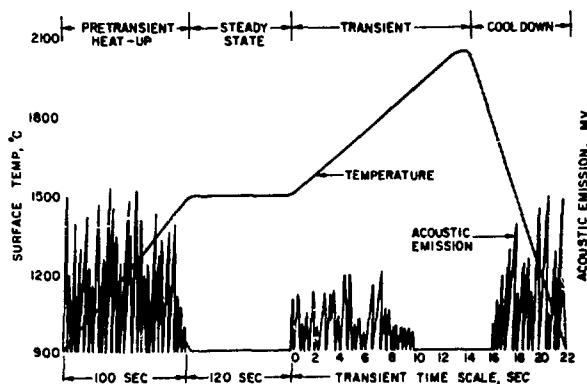


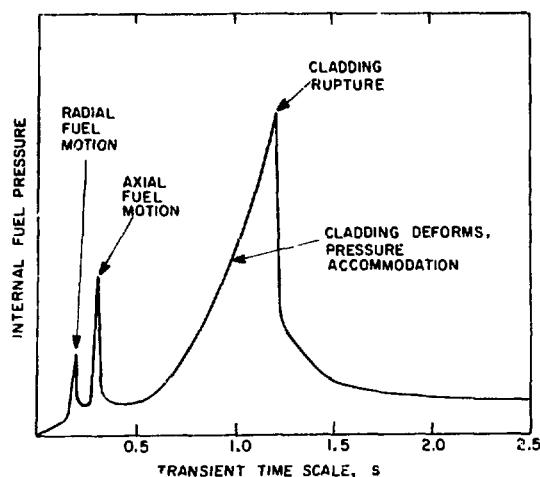
Fig. 1. Relative size of cracks formed before, during, and after a DEH transient.

Three stages of fuel motion will be described in general terms in order of occurrence, i.e., the radial fuel motion, axial fuel motion, and cladding-rupture stages, which have been observed routinely with high-speed photography. Table I lists the important experimental parameters at various stages of the tests. It should be emphasized that the UO_2 pellets tested were unrestructured (no central void), unirradiated (no retained fission gas), and clad with quartz tubing with a prototypic gap. Figure 2 is a qualitative plot inferred from observations of high-speed movies that shows the internal fuel-pressure increases which result in molten-fuel motion and pressure decreases which occur immediately following fuel motion. Figure 3 is a photographic sequence of quartz-clad UO_2 taken from high-speed movies (1000 frames/s) obtained during the transient phases of a test. Energy-input rate is an extremely important parameter when determining the time sequence of events [6]. In the present study, for an

TABLE I

Experimental Parameters at Various Stages for a Test Series^a

Test Stage	Time on Transient Time Scale (s)	Areal Melt Fraction (%)	Centerline Temperature (°K)	Surface Temperature (°K)	Axial Expansion ^b (mm)
Pretransient	0	0	~2370	~1500	0
Radial Fuel Motion	0.2	~8	~3130	~1550	0.2
Axial Fuel Motion	0.3	~15	~3680	~1600	0.3
Cladding Rupture	1.2	~40	~3680	~1800	0.4

^a Average energy-deposition rate = 675 J/g-s.^b Initial pellet stack length = 3.3 cm.Fig. 2. Qualitative plot of pressure buildup in molten-fuel region of a radially constrained, unirradiated UO₂ pellet stack tested in the DEH apparatus.

energy-deposition rate of ~675 J/g-s, which is approximately equivalent to a 50¢/s TREAT ramp radial fuel motion (the first stage, shown in Fig. 3a) was observed at ~0.2 s, with an ~8% areal melt fraction. The change in slope of the pressure curve reflects the increase in pressure due to melting. In Fig. 3a, the bright regions are areas in which molten fuel is flowing into the gap between the fuel and cladding. This is clearly observed in the movies. The molten fuel flows from the pellet centerline through cracks and pellet interfaces. The radial fuel motion was observed intermittently at various axial and radial locations until either upward axial fuel motion or cladding rupture occurred. The driving force for radial fuel motion is the ~10% volume increase when UO₂ melts [7]. The relocation of molten fuel to the fuel-cladding gap results in a slight decrease in internal pressure, which causes radial fuel motion to cease, and molten UO₂ solidifies in the fuel-cladding gap and cracks to seal this escape path.

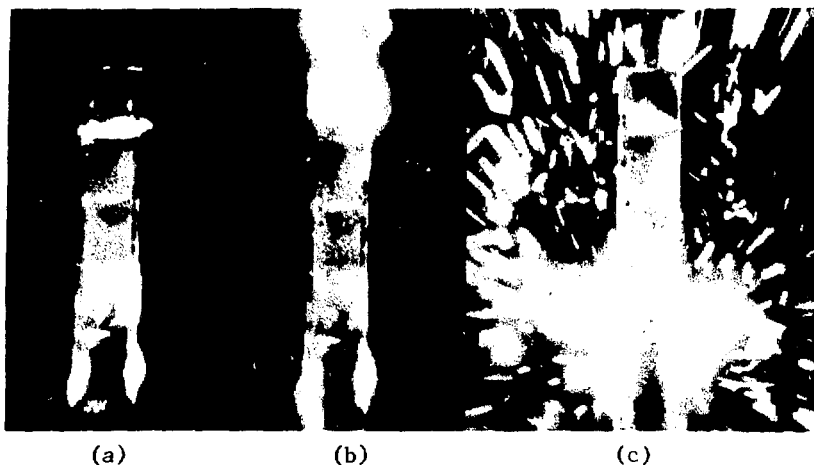


Fig. 3. Series of photographs taken from high-speed movies showing the response of radially constrained UO_2 to a power excursion.

Concomitant with the termination of radial fuel motion, the melt fraction increases rapidly, at an average rate of ~ 33 areal percent per second. The volume increase due to melting raises the internal pressure rapidly such that mildly explosive axial fuel motion occurs (Fig. 3b), with an $\sim 15\%$ areal melt fraction, in the upward direction at ~ 0.3 s. Since the gap and cracks around the bottom pellet were sealed in the radial fuel-motion stage of the experiment, axial fuel motion is directed upward through cracks in the top pellet, which is the last available escape path. Some of the fuel has solidified (bright regions at top), but some particles have escaped.

Photographs taken from high-speed movies of experiments performed on "unclad" fuel, tested at slower energy-deposition rates [8], show that shortly after radial fuel motion occurs (Fig. 4a), UO_2 vapors are formed (Fig. 4b). In addition, the temperature of liquid UO_2 at the fuel centerline was measured to be near the boiling point (~ 3700 K) at a pressure of 1 atm in the specimen chamber. The centerline temperature was measured using an optical pyrometer sighted into a drilled radial hole. In unclad fuel, the vapors are expelled with little or no internal pressure buildup because the pellets are not radially constrained by cladding, and free surfaces are available. For unconstrained UO_2 , an $\sim 10^4$ increase in volume occurs when liquid UO_2 vaporizes. For energetic gross fuel motion to occur in unclad, unirradiated UO_2 , the centerline temperature must exceed the fuel boiling point. The resultant fuel vapors internally pressurize the molten central region of the UO_2 pellets, which causes some squirting of molten-fuel droplets followed by gross molten-fuel release through cracks. If the UO_2 fuel-centerline temperature does not exceed the boiling point, the fuel will be contained by the solid outer region of UO_2 , which acts as a crucible for the inner molten region, even with melt fractions as high as 70 areal percent. The boiling point of UO_2 is only 550 K above the melting point. Thus, for the radially constrained fuel pellets described here, the upward-acting buoyant force of vaporized UO_2 is an important additional driving force to the already significant 10% increase in volume upon melting. This buoyant force drives the molten fuel upward, which is observed, and a rapid release of the vapors causes the mildly explosive effect observed.

To achieve better mechanistic understanding of the observed phenomena, a series of interrupted experiments was performed at various times up to and including cladding rupture.

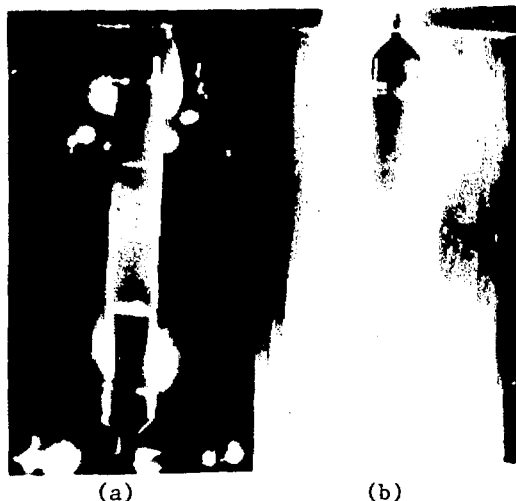


Fig. 4. Series of photographs taken from high-speed movies showing the response of unclad UO_2 to a power excursion.

Figure 5 is a photograph of a UO_2 pellet with the cladding removed, taken on a scanning-electron microscope, and corresponds to fuel appearance shortly after the axial fuel-motion stage. This figure shows a central void from which molten fuel has been ejected. The central void is surrounded by a crust that seals cracks from the central void. The crust is broken at two points where shutdown cracks were formed on cooling. Once-molten fuel is shown solidified in the wide crack and also in Fig. 6, which is an axial view of the same pellet.

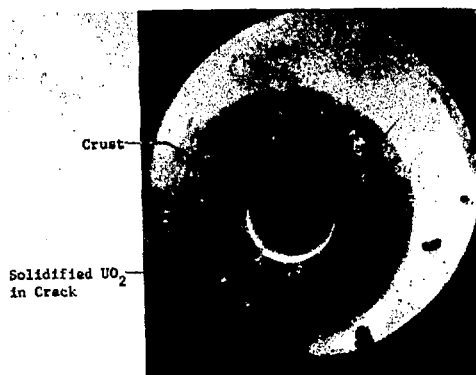


Fig. 5. Cross section of UO_2 pellet showing solidified UO_2 in crack and crust formed around central fusion zone following axial fuel motion. Mag. 14X.



Fig. 6. Axial view of UO_2 pellet showing solidified UO_2 in cracks. Mag. 17X.

It is postulated that the molten fuel escaped from the molten zone through the widest cracks and solidified in cracks and the fuel-cladding gap to form an effective seal. In addition, this relocation of molten fuel serves to effectively seal adjacent cracks by forcing the wedge-shape fuel segment against the adjacent wedge. This suggests the existence of a critical crack width, i.e., molten UO_2 fuel will not flow through a crack with an extremely narrow width. Once the cracks are sealed, the liquid-solid interface region is especially significant because it forms the walls of a so-called "pressurized bottle" that contains molten UO_2 , vaporized UO_2 , and heated pore gases, all of which transmit pressure to the cladding via the solid outer region of fuel to ultimately rupture the cladding.

To gain insight into the amount of UO_2 vapor required to cause molten-fuel relocation, a test in which the fuel was carefully weighed prior to and following the experiment was performed on an unclad UO_2 pellet stack. To eliminate molten-fuel ejection from the pellet stack, the power transient was preprogrammed to be mild. During the test, considerable UO_2 vapor ejection occurred, and approximately one-half of the vapors were collected on the nearly 2.5-cm-diam quartz chimney that enclosed the pellet stack. A few tiny molten particles were ejected during the test, but they were collected in a "catcher" immediately below the fuel stack. At ~ 0.2 s on the transient time scale, UO_2 vapor was expelled continuously, and ~ 0.05 g of vapor was lost during the 5.5-s mild transient. Although the collected vapors weighed only ~ 0.03 g, it was expected that some of the vapors had been swept out of the system by the flowing helium coolant gas.

After the mildly explosive axial fuel motion occurs in the DEH tests, a momentary void is created in the central fuel region, and the internal pressure again decreases. Concomitantly, molten fuel solidifies around the top pellet and in cracks in the top pellet to seal this second escape path. The continuing high rate of melting and associated fuel volume increase acts rapidly to fill the momentary void left by axial fuel relocation. With no escape paths available, the third stage, i.e., fuel motion that results in cladding rupture (Fig. 3c), occurs at ~ 1.2 s with an $\sim 40\%$ areal melt fraction. Figure 2 shows that the pressure buildup is slower in stage 3. This is a result of extensive cladding deformation that occurs locally to accommodate the large increase in internal pressure. The driving forces for this deformation are (a) significant increases in the temperature of the cladding, which enhances its ductility, (b) an increase in internal pressure due to fuel volume increase, vaporization of UO_2 , and heating of pore gases trapped in molten UO_2 , (c) a decrease in localized cladding thickness due to extensive deformation cladding strain varies from ~ 20 – 100% for various tests, (d) differential fuel-cladding thermal expansion, and (e) gravity force.

The above features act in concert to cause energetic cladding rupture. Immediately following rupture, the pressure decreases rapidly. These results tend to support the existence of the "pressurized bottle" effect observed in the high-speed movies. A similar effect has been postulated to explain TREAT results [9] and modeled in the BEHAVE-3 code [10].

Several recent tests have shown that energetic gross cladding rupture can occur even when the quartz cladding fractures during internal pressure pulsing before gross cladding failure. This is significant because one would expect fractures in the cladding to act as escape paths to vent UO_2 fuel vapors. However, the UO_2 vapors do not vent, probably because the solid outer region of fuel seals any potential path between the cladding and fuel vapors. It should be noted that the quartz cladding fractures are similar in appearance to the brittle fractures of radiation-embrittled stainless steel cladding.

The upward axial fuel-motion stage is quite interesting because it has potential importance in inherently safe fuel considerations. Results of experiments in which the upper electrode-cladding diametral gap was varied from 0 to 0.23 cm show that, for a large escape path (0.15 cm), more than 50% of the UO_2 fuel can be ejected upward. For a 0.005-cm electrode-cladding diametral gap, the upward axial fuel motion is eliminated, but a more energetic cladding rupture occurs after the radial fuel-motion stage.

Comparison with Mixed-oxide Fuel

The boiling point-melting point temperature differential ($T_v - T_m$) for $(\text{U}_{0.8}\text{Pu}_{0.2})\text{O}_2$ is only 383 K, compared with 550 K for UO_2 (Table II); therefore, similar fuel behavior could be expected under the same heating conditions.

Table II

Melting and Boiling Points of Oxide Fuels^a

Property	UO ₂	(U _{0.8} Pu _{0.2})O ₂
Melting Point (T _m)	3123	3040
Boiling Point (T _v)	3673	3423
T _v - T _m	550	383

^aFrom Ref. [11]. All value in °K.

However, fuel vapors would become significant for the mixed oxide at a lower temperature (≈167 K lower than for UO₂).

Comparison with In-reactor Irradiated Fuel

Unirradiated UO₂ pellets were used in these DEH tests, i.e., no fission products or fission gas was present, but it is reasonable to speculate on phenomena that might be observed for a high-burnup fuel which undergoes a similar energy-deposition rate. In this case, the fuel would have a large central void in the region of the core midplane and some retained fission gas. Axial escape paths would not be available in-reactor because of the greatly increased length of the solid pellet fuel column. Therefore, the assumption is made that only the cladding-rupture stage occurs when the fission gas is released from the fuel during the transient, which may magnify the pressurized-bottle effect. Low- and moderate-burnup fuels might be expected to respond in a manner similar to the unirradiated fuel tested in the present experiment, depending on the extent of fuel restructuring, gap closure, and axial freedom of motion in the fuel column.

Figure 7 shows the structure of some 0.51-cm-diam UO₂ pellets that were TOP tested in the TREAT reactor. The structure is similar in appearance to that of pellets tested in the DEH apparatus (e.g., Fig. 5). Evidence of axial fuel motion has been found in posttest examination of pellets from several TOP experiments performed in the TREAT reactor [9,12,13].

The in-reactor study performed by Hikido and Field [13] of the General Electric Co. (GE) is particularly appropriate for comparison because axial fuel motion was reported. The melt fraction at failure in the DEH study was ≈40%, whereas the areal melt fractions were ≈35% in the GE tests. The cladding ruptured in the GE fuel element composed entirely of solid pellets, whereas upward fuel relocation occurred without cladding failure in the fuel element with an annular fuel blanket. At the conclusion of the tests, the annular void was considerably larger in the DEH test (≈80 areal percent). This is explained by the fact that the DEH test was not terminated at cladding rupture. It is postulated that the cladding would not have breached in the DEH tests with such large quantities of molten fuel being relocated if nuclear fission heating was possible. However, in the DEH tests, cooling of the cladding surface with helium is not nearly as efficient as that of liquid sodium in-reactor. The calculated centerline and surface temperatures of the in-reactor tests, which cannot be measured directly, were ≈3320 and 1770 K, respectively, whereas analogous DEH temperatures, which can be measured directly, were ≈3675 and 1800 K. The GE centerline temperature was well below the boiling point of UO₂, but it is postulated in this report that fuel vapors present a large driving force for relocating molten fuel. However, the GE fuel was preirradiated so that retained fission gas could act as a driving force concomitant with the ≈9.6% volume expansion on melting and it would not be necessary to exceed the fuel boiling



Fig. 7. Cross section of pellets TOP tested in the TREAT reactor with similar appearance to DEH transient-tested pellets.

point to relocate molten fuel. Thus, the DEH tests produce results similar to in-reactor experimental results and have proven to be quite useful to modelers of in-reactor fuel-motion phenomena.

Application of Results to Fuel-pin Design

A reliable reactivity-feedback system to facilitate reactor shutdown during unsafe conditions is an essential part of any nuclear plant. On the basis of results reported in the present paper, it is proposed that the potential exists for providing a negative reactivity-feedback effect during TOP events which is internal to the fuel pin. Using several (~15) centimeters of annular pellets instead of solid pellets at the top of the fuel pins along with redesigned annular blanket or reflector pellets to provide escape paths, molten-fuel dispersal inside the pin would be enhanced. The upward fuel motion could act as a reactor shut-down mechanism prior to cladding rupture, by relocating molten fuel from high-reactivity-worth regions in the core to a low-reactivity-worth region (i.e., the plenum). An additional advantage of this design is that it is physically compatible with present fuel-pin design in that only minor modifications are required to achieve a completely independent, instantaneous negative reactivity-feedback effect.

An in-reactor test [13] has been performed with a single pin using annular blanket pellets, and axial fuel motion was observed similar to that of the present DEH results. In addition, an annular blanket or reflector was one of several proposals assessed for inherently safe fuel-element design in previous work [14]. Thus, the purpose of the present paper is to amplify or accent outstanding past work that has gone unheeded by fuel-element designers.

To verify the capabilities of the special fuel-pin design, the authors propose that tests be performed in-reactor using facilities with full-length, multipin capabilities. For example, future R-series experiments performed in the TREAT reactor on unirradiated fuel could use annular pellets in the upper region of the pins, with annular blanket or reflector pellets.

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HEDL EMPIRICAL CORRELATION OF FUEL PIN TOP FAILURE THRESHOLDS - STATUS 1976

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ABSTRACT

The Damage Parameter (DP) empirical correlation of fuel pin cladding failure thresholds for Transient Overpower (TOP) events has been revised and recorrelated to the results of twelve TREAT tests. The revised correlation (called the "Failure Potential" [FP] correlation) predicts failure times for the tests in the data base with an average uncertainty of 35 ms for 3\$/s tests and of 150 ms for 50¢/s tests.

INTRODUCTION

Tests of EBR-II irradiated fuel pins have been conducted in the TREAT facility under the Transient Overpower (TOP) experimental program [1, 2]. These pins consist of 13.5-inch (343 mm) long fuel columns, and possess fuel microstructures with relatively little axial variation. The TREAT tests have been viewed as tests of different portions of an FTR pin. Pending development of a mechanistic fuel pin transient code qualified against the TREAT tests, and its subsequent use to define fuel pin cladding failure thresholds for an FTR fuel pin, attempts have been made to develop empirical correlations of the fuel pin cladding failure thresholds as revealed in the TREAT tests.

The DP correlation, [3] currently programmed into the MELT-III code, [4] was such an empirical correlation. The DP correlation, which did not correlate failure thresholds of low power microstructure fuel pins, has been substantially revised and now satisfactorily correlates the original five tests plus an additional seven tests, including two tests of low power microstructure fuel pins.

REVISED CORRELATION

The revised correlation, hereafter called the "Failure Potential" (FP) correlation to distinguish it from the DP, is defined as follows:

$$FP = a \left(\frac{\Delta H}{Y} \right)_{\text{time avg}} t^b e^{cs} D^d$$

where:

FP = Failure Potential
H = Enthalpy (energy content per unit mass) upset of fuel in the noncolumnar grain region over that at steady-state operation, J/g
Y = Cladding burst strength as defined by FCTT tests [5] on unirradiated 20% CW 316 SS heated at a rate of 111 K/s KPa
t = Time into transient, seconds
s = Variable representing fuel structural features
= $0.91 \ln V_f - g/25.4$
g = Fuel-cladding radial gap at steady-state operation, mm
 V_f = Volume fraction of noncolumnar grain region of fuel
D = Available void volume, expressed as fraction of volume inside cladding not occupied by 100% dense fuel, relative to that for a nominal FFTF fuel pin
= $(1. - P_d)/0.15$
 P_d = Planar smear density = $T_d (R_f/R_{ci})^2$
 T_d = Pellet density, fraction of theoretical
 R_f = As-fabricated outer fuel pellet radius
 R_{ci} = As-fabricated inner cladding radius
a,b,c,d = Adjustable parameters
= 63.8, 0.09, 0.65, - 1.27, respectively.

All quantities except available void volume are determined on an axially local basis.

The values of the adjustable parameters were determined using linear regression techniques. The defining equation was solved for the variable $(\frac{\Delta H}{Y})$ time average which was then used as the dependent variable. The equation was linearized by taking the logarithm of both sides.

As with the DP, the FP is applied by calculating its value for several axial locations along the pin as the transient proceeds. Failure is presumed at the time and location of the first axial location to achieve an FP value of 1.0.

The value of the FP is correlated to a standard deviation of $\pm 18\%$ for the twelve-point data base. As an alternative statement of precision, the uncertainty in predicting time of failure averaged ~ 34 ms for 3\$/s tests, and ~ 150 ms for 50\$/s tests.

The data base for the correlation is identified in Table I, which also presents test steady-state characterizations. The FP values for the data base and other tests are shown in Figure 1 as a function of steady-state power.

DISCUSSION OF CORRELATION

The FP correlation is formulated with four fuel performance and characterization variables. The role and justification for including each is discussed following.

The composite variable, $\Delta H/Y$, provides a measure of the relative severity of conditions. The increase of fuel enthalpy (energy concentration), ΔH , represents the fuel loading processes, all of which stem (in the TOP event) from fuel thermal upset. The transient cladding burst strength, Y, represents the ability of the cladding to withstand the loading processes.

The condition-severity variable was time averaged because of evidence of

time- (or rate-) dependent processes in the TOP event. The time-integrated transient, t , was incorporated to allow fine tuning of the correlation for effects of time-dependent processes.

The variable, s , is also a composite, representing the fuel structural features of fuel-cladding radial gap and the volume fraction of the non-columnar grain region. The fuel-cladding gap at steady-state reflects the magnitude of thermal upset required for the fuel to begin loading the cladding. The volume fraction of the non-columnar grain region is directly related to the magnitude of the central void, and it may well be that is the reason for the influence of this variable. The central void provides space within which hot plastic fuel expansion can be accommodated. Both of these structural features would mitigate the severity of loading on the cladding, hence the variable, s , is never greater than one.

The available void volume, D , expressed relative to that of a nominal FFTF pin, reflects all of the void volume inside the cladding (as-fabricated porosity as well as the volume in the as-fabricated fuel-cladding gap). This variable represents the ultimate capability of the fuel pin to absorb fuel expansion of any sort. The variable provides a relative measure of differences between fuel pins due to design or fabrication processes. It was introduced to account for significant differences in that regard among the test fuel pins. Any combination of fuel-cladding gap or pellet density leading to a smear density greater than 85% results in an increase of failure potential; conversely, the failure potential is reduced for fuel pins with smear density less than 85%.

CORRELATION DATA BASE

All of the tests in the FP correlation data base were conducted in TREAT using shaped and filtered transients. That is, thermal neutron filters were employed to minimize the severe radial flux depression that would otherwise occur in the highly enriched EBR-II fuel pins in a thermal reactor, and to shape the axial flux profile to an approximation of that in EBR-II. A shaped transient is one in which preprogrammed rod movement provides a few seconds of constant power (called a flat top or preheat phase), then a power spike designed to simulate thermal aspects of an FFTF TOP event.

All HEDL tests (those prefixed by HUT- or HOP-) were conducted in static capsules. The flowing sodium coolant was simulated by a NaK annulus surrounded by a stainless steel heat sink. All ANL tests (those prefixed by H or E) were conducted in seven-pin bundles in flowing sodium in the MARK-II Loop. Tests E6, H4, [6] and H5 [7] consisted of a central preirradiated fuel pin surrounded by six fresh fuel pins. Tests E7 [6] and E8 [8] consisted of seven preirradiated fuel pins.

Test HUT 3-3A, in which no failure was observed, was included as part of the data base since the test pin is considered to be an incipient failure. The $\Delta D/D$ observed for this test was 0.6%, the most strain observed on a pre-irradiated TOP tested fuel pin without failure.

APPLICATION OF CORRELATION TO TREAT TESTS

Thermal transients were calculated for the HEDL tests using an adaptation of the ARGUS code [9] with the HEDL transient gap conductance model as applied in COBRA-3M [3] and MELT-III. Fuel-cladding gap closure was computed with volume-average-temperature-based differential-thermal expansion applied to the

cold gap as predicted by the SIEX code [10]. The power coupling (relation between TREAT facility power and fuel pin power) was adjusted as required to obtain a good match between predicted and observed axial-midplane inner heat-sink thermocouple traces for the spike transient. The predicted areal melt fractions (fraction of transverse fuel area above the solidus temperature) were then compared with the observed areal melt fractions where available. Agreement within a few percent was obtained in all cases.

Thermal transients were calculated for the ANL tests using the HEDL version [3] of the COBRA-3M code [11]. Close agreement between observed and predicted areal melt fractions was obtained for test H5, the only ANL test in the correlation data base for which a comparison was possible. Reasonable agreement between observed and measured coolant and duct temperatures were obtained for tests H4, H5 and E8.

Application of the FP correlation to the TREAT tests encountered a problem in that the steady-state fuel enthalpy frequently was reached much earlier than the start of the spike transient, which resulted in two quite different portions of the transient. The first portion of the transient was rather slow with conditions approaching steady-state; the second portion was characterized by an accelerating increase in severity of conditions. It was clear that the first portion of the transient would tend to dilute the second portion, artificially depressing the time-averaged condition-severity variable. For this reason, the FP values were calculated separately for the two parts of the transient in such cases, adding the two values to obtain the FP value for the test. This procedure may over-estimate the FP for tests in which a sizeable portion of the increment in conditions over steady-state was attained prior to start of the spike transient.

In application of the FP correlation to tests in which failure did not occur, the calculation was stopped when the peak value of the condition-severity variable was reached. Although failure on the downslope with fuel and cladding temperatures decreasing would not be impossible, and in fact occurred in at least one test in the data base (HUT 3-5B), it is felt that the FP correlation may over-estimate the importance of that type of operation.

CORRELATION LIMITATIONS

The correlation is subject to the following limitations:

- The ranges of data for tests against which the correlation was calibrated were 30 to 50 MWd/kg fuel burnup, 2.4 to 6.0×10^{22} nvt ($E > 0.1$ MEV) fluence; and 17 to 38 kW/m for linear power rating. As with any empirical correlation, extrapolation beyond the range of the data base is not recommended. The correlation is not applicable to unirradiated fuel pins.
- Application of the correlation to complex transients is not advisable (i.e., transients with erratically varying power multiplication periods, or cyclic behavior).
- The correlation appears to be valid for determining whether or not failure will occur. However, in such applications, the user should bear in mind that the standard deviation for FP values for the data base was $\pm 18\%$.
- Applications of the correlation to transients other than TOP events has not been established and should be avoided.

- The correlation has not been validated for other than nominal FFTF fuel pin geometry, cladding, or fuel composition.

TABLE I
Data Base Characterization
For Failure Potential Correlation

Test	Steady-State Axial Midplane Characterization			Transient Characterization			
	Power kW/m	Exposure MWd/kg	Fluence nvtx10 ⁻²²	Ramp Rate	Failure Time seconds ^a	Areal Melt %	Cladding Temp ^{°K} ^b
HUT 5-5B	16.9	44.8	5.4	50¢/s	4.80	49	1208
5-3A	27.5	49.9	4.0	50¢/s	3.20	47	1097
5-1B	38.3	47.0	4.1	50¢/s	3.68	65	1190
H4c	33.2	40.8	2.9	50¢/s	0.94	30	1029
H5c	28.8	31.1	2.5	50¢/s	1.90	25	1021
HUT 3-5B	18.1	48.1	6.0	3\$/s	0.80	61	1163
3-3Ad	28.1	51.2	4.2	3\$/s	0.58	53	1088
HOP 3-3C	27.7	29.8	2.4	3\$/s	0.80	34	1027
HUT 3-1A	38.3	47.0	4.1	3\$/s	0.70	73	1103
E6c	35.9	44.0	3.2	3\$/s	0.59	78	1202
E7c	31.9	39.5	2.9	3\$/s	0.40	32	988
E8c	27.9	30.5	4.1	3\$/s	0.50	69	1092

^a Time after start of spike transient

^b Quoted for location of failure if known; otherwise at ~80% of fuel column height

^c ANL tests; all other, HEDL static capsule tests

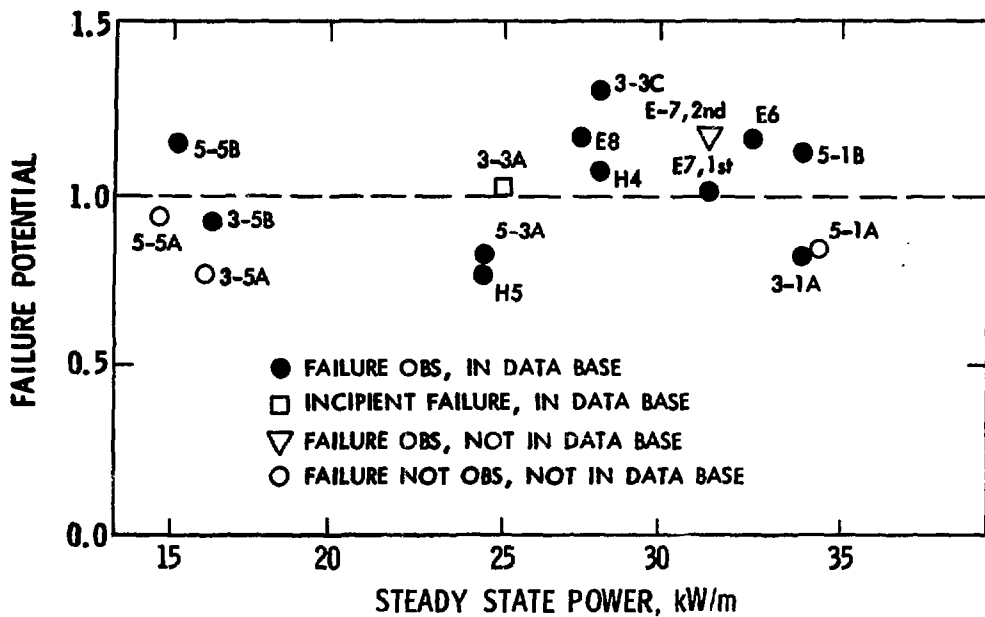
^d Cladding failure not observed; test pin considered to be on verge of failure

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Fig. 1. Failure Potential (FP) Values Are Shown as a Function of Local Steady-state Pin Power for the FP Data Base and Selected Other Tests.

CLADDING FAILURE IN TREAT OVERPOWER
EXPERIMENTS: A MECHANISTIC INTERPRETATION
AND ITS IMPLICATIONS FOR LMFBR SAFETY ANALYSIS

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ABSTRACT

Eleven TREAT overpower experiments have been analyzed using the BEHAVE-SST fuel rod mechanics program. The results suggest that mechanistic prediction of cladding failure under hypothetical overpower conditions is feasible. Differential fuel-cladding thermal expansion and fuel expansion upon melting appear to be the dominant loading mechanisms in the experiments analyzed. It was necessary to postulate a fission gas loading mechanism in only one experiment to account for the observed behavior. These results suggest that failure noncoherence under overpower conditions will depend primarily on rod-to-rod variations in fuel strength, cladding strength and the distribution of available volume within the fuel.

INTRODUCTION

Detailed mechanistic understanding of cladding loading and failure during hypothetical overpower transients is required to assess the response of an LMFBR core to accident conditions. Because cladding failure time and location affect many of the phenomena that dominate the early portion of the accident sequence, a major portion of the safety analysis effort at General Electric is designed to study the mechanisms that lead to cladding failure. The objective of this work is to identify and model the dominant cladding loading and failure mechanisms that act during overpower transients. Near-term analysis, to be described here, is intended to determine if current analytical models of differential fuel-cladding thermal expansion loading (including molten fuel expansion) are sufficient to explain the observed behavior of TREAT overpower experiments, or if fission-gas-induced loading mechanisms must be postulated.

DESCRIPTION OF ANALYSIS

The analysis of each TREAT experiment involved four major elements: 1) prediction of detailed temperature distributions in fuel and cladding, 2) mechanical analysis using the BEHAVE-SST fuel rod mechanics program [1], 3) cladding failure prediction using a Life Fraction damage function, and 4) comparison of calculated fuel rod behavior with observed behavior. A description of

each of these elements follows.

Detailed fuel and cladding temperature distributions for each experiment are obtained using the THTD (Transient Heat Transfer--Version D) program (2), which is a general-purpose heat transfer program that contains no internal fuel performance models and allows one to model experiment geometry in detail. A significant feature of the thermal analysis procedure is that fuel thermal properties, experiment heat deposition, and fuel-cladding gap conductance are adjusted as necessary to obtain agreement with thermocouple data and measured fuel melt radii. Because the program contains no physical models and requires extensive assumptions by the analyst to replace these models, it is not suitable for predicting temperatures when thermocouple data are not available. However, the flexibility afforded by THTD to model exact capsule geometry and thermocouple locations is believed to be essential in calculating valid experiment temperatures.

Accurate knowledge of the radial position of the thermocouples nearest the fuel rod has proven to be essential for correct prediction of cladding temperatures. An uncertainty of 10 mils (.25 mm) in thermocouple location has been shown to produce a cladding temperature uncertainty of approximately 50°F (28°C). Therefore, the THTD radial node structure is selected so that one node is centered on the thermocouple junction.

Recently the versatility of the THTD program has been used to allow molten fuel to relocate axially with time during the transient. This allows the analysis to reflect differences between the initial axial power profile (as measured during calibration transients) and the final axial power profile (measured by thermocouples during the test transient). Differences of approximately 30°C in calculated cladding temperatures were observed when molten fuel was allowed to relocate axially in experiment HUT 5-5B.

The second major element of each experiment analysis is a mechanical analysis using the BEHAVE-SST program. Fuel and cladding temperatures from THTD are provided to this program via tape. The BEHAVE-SST program predicts detailed fuel rod behavior prior to the transient. Cladding swelling, cladding creep, fuel restructuring, fission gas release, fuel cracking, and solid fission product fuel swelling are the major pretransient phenomena modeled.

For transient calculations, BEHAVE-SST models five axial sections, 20 radial fuel sections, and four radial cladding sections. The mechanical model is based upon a generalized plane strain treatment. The major cladding loading models included in the program are differential fuel-cladding thermal expansion, expansion of fuel upon melting, pressurization of the central void by fission gas (the "Gas Bottle" effect), and thermal stresses in the cladding. Fuel swelling caused by fission gas and grain boundary pressurization by fission gas are not modeled. Key elements of the molten fuel pressurization model are axial redistribution of molten fuel within the central void and axial movement of molten fuel into the axial blanket region when the top of the fuel column is molten.

The BEHAVE program models plastic fuel deformation and fission-enhanced thermal creep. Cladding deformation during a transient occurs by thermal creep and plasticity. At present, cladding plastic strain hardening is modeled using linear interpolation between yield strength and ultimate strength. The proportional limit, yield strength, ultimate strength, and uniform elongation were estimated by linear regression from high-strain-rate data for unirradiated 20% CW, Type-316 cladding. The strength correlations are functions of strain rate and temperature. This model is believed to overestimate cladding strength in the experiments analyzed, and a model describing plasticity in irradiated cladding is presently nearing completion.

Since differential fuel-cladding thermal expansion loading is a dominant factor in most experiments, some elaboration of the BEHAVE-SST models that represent accommodation of thermal expansion within the fuel rod is appropriate. When the fuel rod is fabricated, the volume available to accommodate thermal expansion is represented in BEHAVE-SST by the fuel-cladding gap, fuel porosity, a fuel central void (in annular fuel), and the dish volume of dished-end pellets. BEHAVE-SST redistributes this volume during pretransient irradiation and during the transient. As the fuel rod is brought up to power, fuel is assumed to crack and move outward to contact the cladding. Light fuel-cladding contact is maintained until differential thermal expansion or fuel swelling is large enough to close the cracks produced by the outward movement of fuel segments. The net effect of this early fuel relocation is to convert fuel-cladding gap volume into radial crack volume and central void volume. Continued irradiation reduces crack volume by fuel swelling and by fuel thermal expansion (if fuel temperature increases), and fuel porosity volume is created as cracks close. Fuel porosity migrates to the central void as pretransient irradiation proceeds.

During a transient, fuel thermal expansion is accommodated by crack volume and, to a lesser extent, by the central void. Fuel porosity is reduced by hot pressing when fuel temperature is greater than $\sim 1500^{\circ}\text{C}$, and porosity becomes part of the central void as fuel containing this volume becomes molten. The interaction of all these volume redistribution phenomena has proven to be a significant source of differences between the experiments analyzed to date.

As part of the mechanical analysis of each experiment, pretransient power and fuel-cladding gap conductance are adjusted in BEHAVE-SST to obtain agreement with measured fuel microstructural regions and pretransient fission gas retention in sibling fuel rods. The purpose of this procedure is to minimize the influence on transient calculations of errors in pretransient models. When these errors are minimized, the adequacy of transient fuel mechanics models is more readily apparent.

The third major component of the analysis procedure is the application of a Life Fraction Damage rule to predict cladding failure for each experiment. The purpose of this part of the analysis is to determine the feasibility of predicting failure using the Life Fraction Damage technique. In addition, the predicted time and axial location of failure, along with cladding deformation and failure appearance, are important quantities that can be compared with observed behavior to determine the validity of the mechanical analysis.

Cladding material property data for use in the damage calculation were obtained from Fuel Cladding Transient Tester (FCTT) experiments performed at Hanford Engineering Development Laboratory (HEDL) (3). Data for irradiated 20% CW, Type-316 cladding were put into an analytical form using the Larson-Miller Parameter (LMP), which is defined as

$$\text{LMP} = T \log t_f + 20.0 \quad (1)$$

where T = Absolute cladding temperature ($^{\circ}\text{R}$)
 t_f = Time to failure at constant stress and temperature (hr).

After deriving a value of LMP for each FCTT data point, the data were correlated as a function of the logarithm of cladding stress of the form

$$\text{LMP} = a \log \sigma + b \quad (2)$$

σ = Cladding hoop stress

a, b = Regression coefficients.

The correlation used for the present analysis is based upon data in the fluence range from 1.8×10^{22} n/cm² to 4.0×10^{22} n/cm². All data were obtained at a thermal ramp rate of 111°C/sec in the FCTT apparatus. The data include a range of irradiation temperatures and failure temperatures. As additional data become available, the correlation describing LMP will be refined to include explicit fluence, ramp rate and temperature dependencies.

Cladding failure for each experiment is evaluated using a Life-Fraction Damage function:

$$\text{Damage} = \sum_i \frac{\Delta t_i}{t_f(\sigma, T)} \quad (3)$$

where

Δt_i = a time interval within which stress and temperature can be considered constant.

t_f = time-to-failure in time interval Δt_i obtained by inserting BEHAVE-SST stresses and THTD temperatures into Equations 1 and 2.

Damage = 1.0 is defined as failure and the summation extends over the transient history.

The final element of each experiment analysis is to compare predicted behavior with measured data. Whenever possible, BEHAVE-SST predictions have been compared with observed failure time, axial failure location, cladding diameter change, and failure appearance. The results of these comparisons are presented below, along with brief descriptions of the experiments analyzed to date.

RESULTS

Eleven TREAT static-capsule overpower experiments have been analyzed (or partially analyzed) to date. Two of these are General Electric unirradiated experiments C4A and C4B {4}, while the remaining nine were irradiated in EBR-II prior to transient testing in TREAT. The irradiated experiments were General Electric experiments C4G and C4H and HEDL experiments PNL 2-1, PNL 2-11, HOP 3-2A, HOP 3-2B, HOP 3-3C, HUT 5-5A, and HUT 5-5B. This set of fuel rods, contains solid and annular fuel pellets, a range of transient heating rates, pre-transient linear powers from 5.5 kW/ft (180 W/cm) to 15.0 kW/ft (492 W/cm) and rods with maximum fuel melt fractions ranging from 0% to 90% of the cross-sectional area. Table I contains summary descriptions of these experiments.

As shown in Table I, all the experiments that were irradiated in EBR-II, except for HOP 3-2A and HOP 3-3C, were exposed to approximately the same burnup (40 to 45 MWD/Kg). The experiments listed in Table I are also similar with respect to transient heating rate; all the experiments, except HUT 5-5A and HUT 5-5B, had thermal ramp rates at least as high as those expected from a \$3/sec reactivity insertion rate in FTR. With these parameters excepted, the experiments considered in this study represent relatively diverse sets of fabrication parameters and operating conditions.

Table I also shows that a mixture of failed and unfailed experiments have been included in the analysis. Each of the unfailed experiments provides valuable information. Unfailed experiment C4G is very similar to failed experiment C4H, so the pair defines a threshold for failure. Unfailed experiment HUT 5-5A forms a similar pair with failed experiment HUT 5-5B. Unfailed experiments HOP 3-2A and HOP 3-2B are of value for a different reason. These fuel rods are

TABLE I

Summary Description of TREAT
Overpower Experiments Analyzed

Experiment Designation	Axial Peak Burnup (MWD/Kg)	Axial Peak Pretransient Cladding Temperature ($^{\circ}$ C)	Axial Peak Pretransient Power (w/cm)	Transient Fuel Melt Fraction
PNL 2-1	41.2	504	307	0.40 ^a
PNL 2-11	41.2	494	312	0.90 ^a
HOP 3-3C	35.0	493	344	0.40 ^a
HUT 5-5B	48.0	530	187	0.33 ^a
C4H	48.6	599	492	0.0 ^a
C4G	47.9	599	482	0.10
HOP 3-2A	21.4	723	384	0.03
HOP 3-2B	42.1	696	351	0.0
HUT 5-5A	46.5	537	182	0.32
C4A	0.0	-	-	0.63 ^a
C4B	0.0	-	-	0.79 ^a

^aFailed Experiment; melt values are calculated at failure time

among the few with significant cladding strain that did not fail. Therefore, they provide information that can be used to evaluate fuel rod mechanics models independently of the way failure is predicted.

Transient fuel melt fractions (i.e., the fraction of fuel cross sectional area above the solidus temperature) are shown in Table I for each experiment to provide an indication of the severity of each transient. For unfailed experiments, fuel melt values were measured near axial midplane during destructive examination of each fuel rod. Fuel melt values shown in Table I for failed experiments were calculated at the time and axial location of failure using the THTD program.

Several different cladding materials were used in fabricating these experiments. Solution annealed, Type-316 stainless steel cladding was used in fabricating the four General Electric tests (C4A, C4B, C4G, C4H), while 20% CW, Type-316 stainless steel was used in five of the irradiated experiments (HOP 3-3C, HUT 5-5A, HUT 5-5B, HOP 3-2A, and HOP 3-2B). The remaining two experiments (PNL 2-1 and PNL 2-11) were fabricated using solution annealed, Type-304 stainless steel. Despite these differences between cladding materials, only one set of properties was used to describe cladding plasticity in the analysis, and the properties that were used were derived from tensile data for unirradiated 20% CW, Type-316 stainless steel. As a result of this modeling assumption, strength values predicted by BEHAVE-SST for both irradiated cold-worked and irradiated annealed cladding are probably slightly too high. Strength values for unirradiated annealed material are significantly higher than they should be. However, it is believed to be reasonable to assume that the strength of cold worked material approaches the strength of annealed material after irradiation at the temperatures shown in Table I. Therefore, the use of a single model to describe

TABLE II

Comparison of Predicted and Observed Failure Times and Axial Locations for Experiments Analyzed

Experiment Designation	Failure Time (sec)		Axial Failure Location (cm)	
	Observed ^a	Predicted	Observed	Predicted
PNL 2-1	5.25- 5.30	5.24	13-18	12.7
PNL 2-11	11.50-11.55	10.62	28-32	28.9
HOP 3-3C	8.50- 8.55	8.47	14-18	16.8
HUT 5-5B	10.80-10.86	b	25-31	b
C4H	3.75- 3.80	3.80	23-32	27.9
C4G	No Failure	No Failure	-	-
HOP 3-2A	No Failure	No Failure	-	-
HOP 3-2B	No Failure	No Failure	-	-
HUT 5-5A	No Failure	No Failure	-	-
C4A	3.15- 3.20	No Failure	15-18	-
C4B	2.80- 2.85	No Failure	14-18	-

^aUncertainties are estimated from thermocouple data.

^bNot available in time for inclusion in this paper.

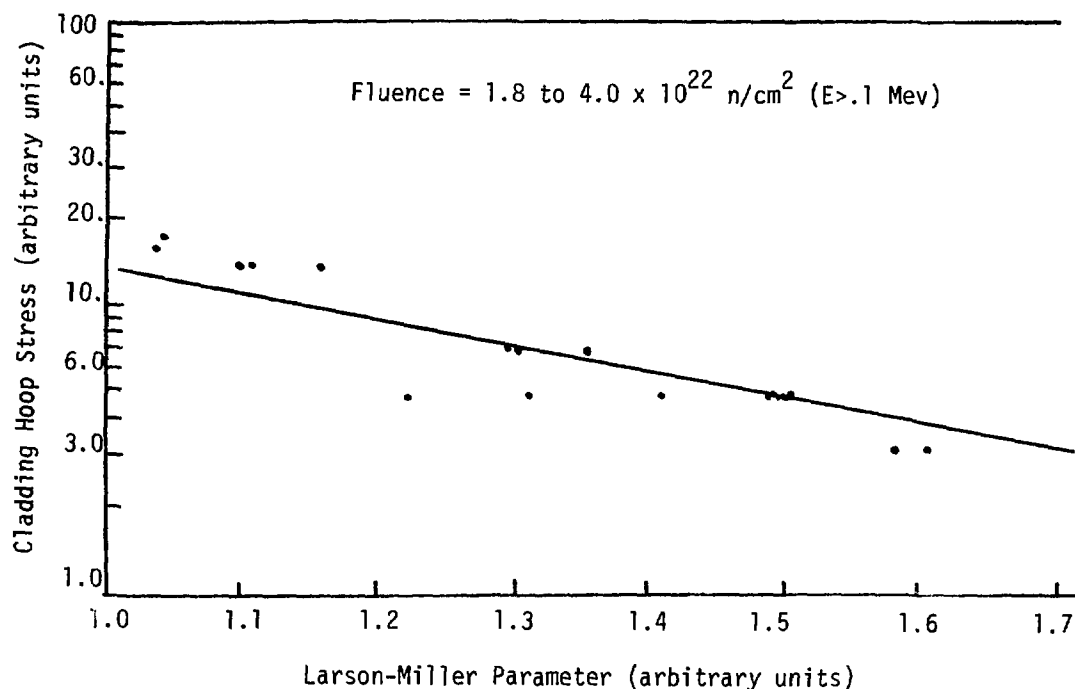
the strengths of several irradiated materials is believed to have been a valid approximation. This is discussed further later in this paper.

Table II summarizes BEHAVE-SST predictions for the time and axial location of cladding failure for each experiment. Observed failure times, which were inferred from thermocouple data, are also shown in Table II for comparison. Observed failure times are represented by a range of values for each experiment. This range corresponds to uncertainty in the time delay between cladding failure and the first indication of failure in thermocouple readings. Therefore, the latest time shown in the table for each experiment is the time at which a deviation in thermocouple data first became noticeable, and failure is known to have occurred earlier. Although the actual delay time between failure and the first thermocouple indication is unknown, the Table II estimate of 50 msec is believed to be a reasonable upper limit.

Predicted failure times for three of the four experiments that failed are within 0.08 seconds of the mean estimates of actual failure time. The predicted time of failure for the fourth irradiated experiment that failed (PNL 2-11) is clearly too early. In all irradiated experiments that did not experience failure, the analysis correctly predicts no failure. The predicted axial locations of failure are consistent with estimated failure locations for all failed experiments. However, this is partly due to the fact that actual failure locations are often difficult to determine and uncertainty is large. No failure was predicted for the two unirradiated fuel rods that were tested, even though these experiments actually failed. This was to be expected, since unirradiated solution annealed cladding is extremely ductile, and the mechanical properties used in the analysis are least applicable to this material.

The predicted failure times shown in Table II were determined using a correlation for the Larson-Miller Parameter of the form of Equation 2. However, the first coefficient (a) in Equation 2 was adjusted slightly from the value

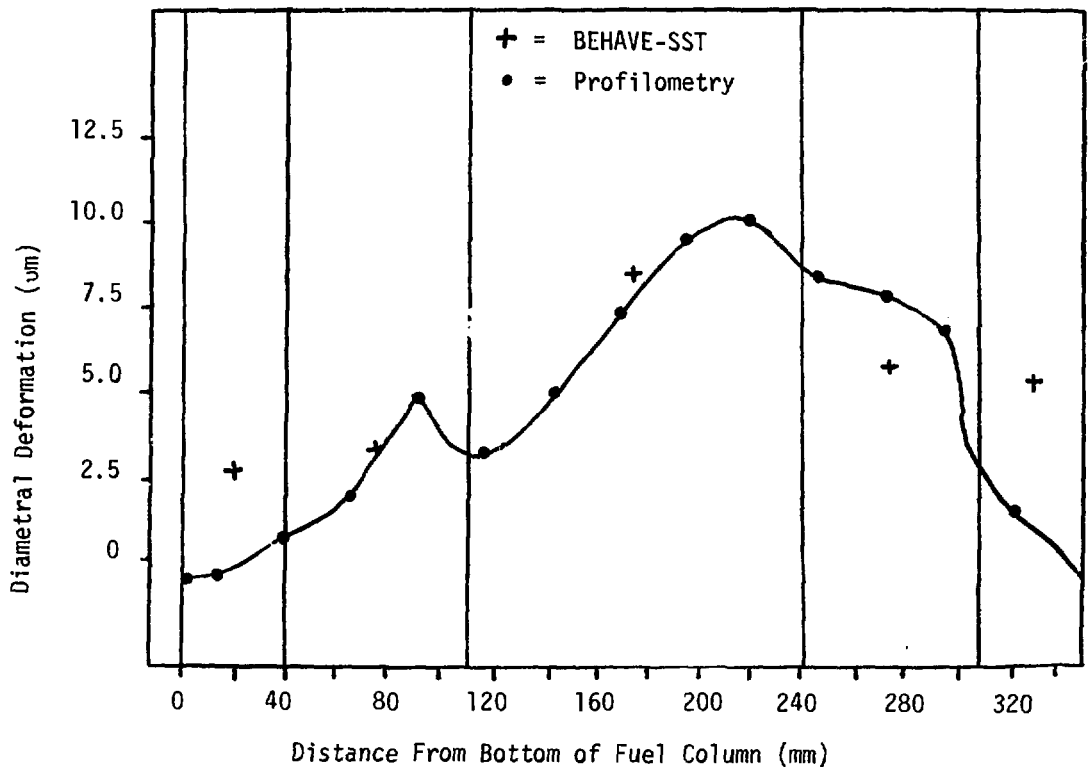
Figure 1
Comparison of BEHAVE-SST Correlation for Larson
Miller Parameter with Data for Irradiated Cladding
Tested at 111°C/sec in the HEDL FCTT Apparatus



determined by regression analysis of the FCTT data. The correspondence between the adjusted correlation and the data is shown in Figure 1. As Figure 1 indicates, the adjusted correlation falls within the spread of the FCTT data and is slightly higher than the median of these data. This is consistent with the fact that BEHAVE-SST predicts stresses that are slightly too high, and the failure correlation must predict failure at stress values that are correspondingly high.

The correspondence (shown in Figure 1) between the Larson-Miller Parameter (LMP) correlation used in BEHAVE-SST and the FCTT data provides a measure of the validity of the analysis. However, proper interpretation of these results requires some understanding of the strategy used to adjust the correlation. The LMP correlation was adjusted so that failed experiments were predicted to fail and unfailed experiments were predicted not to fail. This procedure yields larger deviations between observed and predicted failure times than would be obtained if agreement with observed failure times was the primary calibration criterion. If the LMP correlation were adjusted using this alternate criterion, the maximum difference between observed and predicted failure times would be reduced to 0.05 seconds (excluding PNL 2-11), experiment C4H would be predicted not to fail, and the correlation shown in Figure 1 would be slightly higher relative to the data. This illustrates the value of pairs of experiments that define a failure threshold; the LMP correlation could be adjusted over a relatively wide range and still remain within the scatter band in the FCTT data, but little adjustment is possible without losing the proper relationship between failure and nonfailure. Experiment HOP 3-2A provides a similar limitation against adjusting the LMP correlation downward; after a slight downward adjustment, this

Figure 2
Comparison of Experiment HOP 3-2A Post-Transient
Profilometry with BEHAVE-SST
Prediction of Cladding Deformation



unfailed experiment is predicted to fail.

The failure time and location results provide an indication of the adequacy of the combined thermal, mechanical, and damage function analyses. The BEHAVE-SST program has also been used to predict cladding inelastic strain in unfailed experiments HOP 3-2A and HOP 3-2B. This allows evaluation of BEHAVE-SST mechanics models independent of the LMP correlation or any other failure prediction method. Since the HOP 3-2A and HOP 3-2B fuel rods were irradiated in EBR-II at relatively high powers prior to transient testing, fission gas loading could be neglected in the analysis. Furthermore, since there was little fuel melting during the transient in either experiment, the adequacy of models involved in predicting differential fuel-cladding thermal expansion loading can be evaluated without interference due to other loading mechanisms.

Figure 2 compares BEHAVE-SST predictions of cladding diameter increase with post-transient profilometry measurements for experiment HOP 3-2A. Profilometry measurements are shown as a continuous function of axial position in Figure 2, while BEHAVE-SST is limited to five axial sections. Therefore, BEHAVE-SST results are shown at five discrete locations in Figure 2, which correspond to the midpoints of each section. Figure 2 shows very clearly that five axial sections are not sufficient to describe the local variations in cladding deformation. Despite this limitation, the agreement between observed and predicted cladding

deformation in the three middle axial sections is sufficient to validate BEHAVE-SST predictions. Agreement between predicted and measured deformation at the ends of the fuel column is not good. The data indicate deformation is significantly smaller at the ends of the fuel column than in adjacent sections, while BEHAVE-SST predicts little difference. The reason for this discrepancy is unknown, although it may be related to the way the radial fuel temperature profile is modified at the ends of the fuel column by double-thickness neutron flux filters. The experiment thermal analysis included this effect, but two-dimensional physics and thermal analyses are necessary and uncertainties are correspondingly larger. A similar analysis of experiment HOP 3-2B was also performed, and the correspondence between predicted and measured cladding deformation was very similar to that shown in Figure 2.

Although the cladding failure site is usually destroyed by molten fuel in TREAT experiments, the appearance of the failure is sometimes preserved well enough to provide information that can be compared with predictions. BEHAVE-SST predictions have been compared with failure characteristics for experiments C4H and PNL 2-1. The C4H failure was an axial crack approximately 2.5 cm in length, indicating that circumferential stress dominated cladding loading at the time of failure. The BEHAVE-SST analysis of C4H is in agreement with this evidence; circumferential stress was much larger than axial stress during the last half of the transient. Experiment PNL 2-1 appears to have failed under the influence of axial stresses, since the fuel rod broke completely into two pieces in a sudden manner. BEHAVE-SST predictions confirm this; axial stresses were much larger than circumferential stresses at most times (including the time of failure) during the transient test.

CONCLUSIONS

The results of this study suggest that mechanistic prediction of cladding failure during hypothetical overpower transient events is feasible using the BEHAVE-SST fuel rod mechanics program and a Life Fraction damage function. Feasibility has been demonstrated only for the conditions of experiments analyzed to date; analysis of additional experiments is required to extend the range of parameters for which feasibility has been demonstrated.

Radial and axial differential fuel-cladding thermal expansion had a very significant effect on fuel rod mechanical behavior in each of the irradiated experiments analyzed. Pressurization of the central void volume by expansion of fuel during melting was also a significant cladding loading mechanism in several experiments. Only in experiment HOP 3-3C was it necessary to postulate a loading mechanism in addition to thermal expansion and fuel phase change expansion. In this experiment, the central void region was assumed to be pressurized by fission gas released during the transient. While the HOP 3-3C analysis indicated that this loading mechanism is very likely to have been instrumental in causing the observed failure, there are a number of other effects that could have produced the same effect. Until additional experiments are found to require a loading mechanism related to fission gas, the need for modeling such mechanisms to predict failure during overpower events should be regarded as unproven.

Results obtained for unirradiated experiments C4A and C4B suggest that these experiments did not fail under the influence of mechanical loading alone. Use of correct cladding strength models (i.e., for unirradiated solution annealed stainless steel) in the analysis of these experiments would produce even lower values for Life Fraction damage than were obtained in the present analysis. It is concluded that failure in these experiments was caused by mechanical loading

of the cladding in conjunction with cladding overheating. Cladding overheating is believed to have been caused by contact between molten fuel and cladding.

Although encouraging results were obtained in this study using cladding strength properties for unirradiated 20% CW, Type-316 stainless steel for all experiments, this is not a generally valid practice. Apparently, good results were obtained only because irradiation-induced defect hardening occurred in the annealed material and some thermal recovery of cold work occurred in the cold worked material. As a result of these processes, differences between the various cladding materials apparently became small enough during pretransient irradiation to be obscured by other uncertainties in the analysis. Models describing the strength of irradiated 20% CW, Type-316 stainless steel are presently nearing completion, and these will be applied in future analyses.

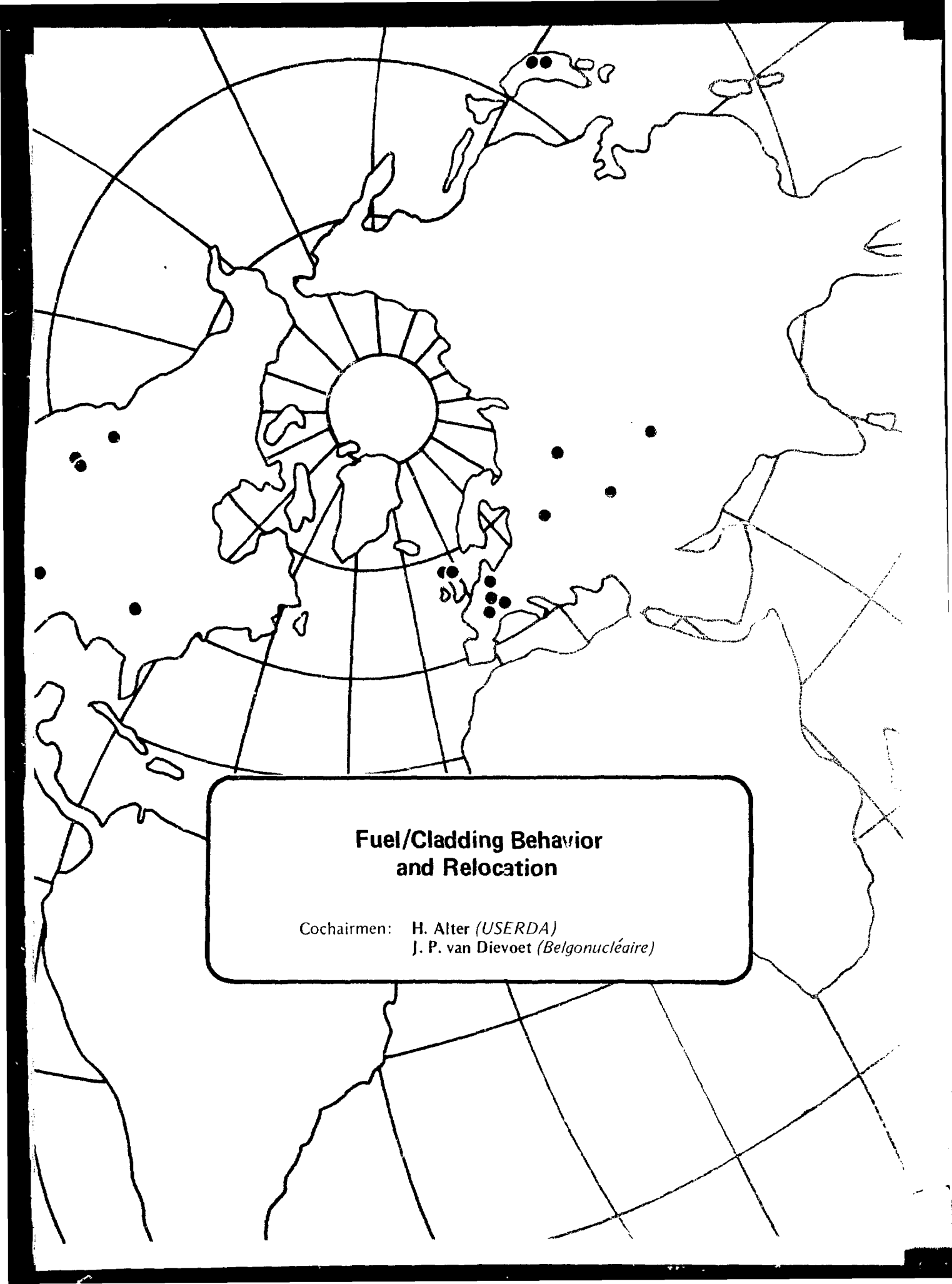
This study suggests that differential fuel-cladding thermal expansion dominates cladding loading and failure during overpower transients. Furthermore, the analysis suggests that fuel rods with large fuel-cladding gap volume, crack volume, and central void volume prior to the transient will be most resistant to failure. Based on these results, fuel rods with low power (<260 w/cm) and high burnup (80-100 MWD/Kg) may be most susceptible to cladding failure during an overpower event. This type of fuel rod has been observed to contain very little gap volume, crack volume, or central void volume after pretransient irradiation; the fuel appears to be of uniformly low density. In this situation, there is no volume readily available to accommodate fuel thermal expansion, and a relatively small increase in power above the pretransient level may produce significant cladding loading.

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Fuel/Cladding Behavior and Relocation

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CLADDING RELOCATION EXPERIMENTS

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INTRODUCTION

When analyzing the consequences of a hypothetical core meltdown accident in an LMFBR, the movement of molten clad material must be well understood for two reasons; (1) it has the potential for plugging the system as it leaves the core region, and (2) it is directly coupled to the neutronics of the system. To provide a detailed insight into this motion, small-scale single and ten pin tests have been conducted using Wood's metal to simulate the cladding material. In addition, a full length (FFTF) 28-pin triangular bundle experiment has been fabricated and is currently being calibrated.

The objective of these experiments is to study the behavior of molten cladding within a voided subassembly where sodium vapor streaming tends to counteract the drainage due to gravity. Under such conditions, the pressure difference across the fuel bundle could be considered constant at 50KPa, which would result in an upward sodium vapor velocity of approximately 80 M/sec. Available analyses on flooding of thin liquid films would predict that such films should experience this behavior. However, the data base for testing these available models did not include experiments with high density, high surface tension melts like stainless steel. The initial Wood's metal simulation experiments were constructed to specifically study the incipient flooding behavior in such systems and thereby, determine the applicability of available models to the reactor accident situation. The 28-pin experiments are constructed to provide a simulation of the overall cladding behavior including radial incoherence and upper and lower blanket heat sinks where freezing may occur.

SINGLE PIN EXPERIMENTS

The basic apparatus is illustrated in Fig. 1. Argon flows vertically upward through an annular channel and over the 20.3 cm-long section of 0.038 cm. Wood's metal cladding which is cast into an undercut region in the copper

*Work performed under the auspices of the U. S. Energy Research and Development Administration

tube and machined down to the outer diameter of the copper tubing. Vertically downward steam flow through the center of the test pin is the energy source to melt the Wood's metal. A large bypass and an inlet accumulator provide a good simulation of constant pressure drop across the test section. The basic instrumentation is test section inlet flow, pressure, and temperature and test-section pressure drop. High-speed movies (400 pps) provide a detailed look at the controlling two-phase phenomena.

Five incipient clad motion experiments were conducted in this single pin configuration and the results are given below.

TABLE I
Single Pin Clad Motion Experiments

Run	Gas Velocity M/sec	Cladding Motion
1	0	Drains
2	18.6	Definite flooding initially, but limited upward motion
3	10.4	Attempts to flood but no net transport downstream
4	41.8	Definite entrainment of molten clad
5	37.2	Flooding with entrainment of large drops of cladding

At a gas velocity of 10.4 M/sec, the clad attempts to flood, but the onset of flooding results in geometric distortion of the molten surface which increases the test section flow resistance. Under constant pressure drop conditions this causes the gas velocity to drop below the critical flooding value and the system drains. When the velocity is increased to 18.6 M/sec, definite flooding occurs. At even greater velocities, entrainment begins.

The incipient flooding criterion given in ref. 1 can be expressed as

$$U_g = 1.15 \left[\frac{\rho_l}{\rho_g} \frac{g\delta}{0.006 + 200 \delta^2} \right]^{1/2} \quad (1)$$

in terms of the critical gas velocity required. (The dimensional constant 200 has the units cm^{-2} .) This expression predicts a critical gas velocity of 10.2 M/sec for this Wood's metal-argon system, which is in excellent agreement with the experimental observations. Critical gas velocities for sustained flooding and entrainment² are given below in eqns. (2) and (3) respectively.

$$U_g = \frac{3^4 \sqrt{g\sigma(\rho_l - \rho_g)}}{\sqrt{\rho_g}} \quad (2)$$

$$U_g = \frac{3.7^4 \sqrt{g\sigma(\rho_l - \rho_g)}}{\sqrt{\rho_g}} \quad (3)$$

These velocities for the Wood's metal-argon system are 31.4 M/sec for sustained upward motion and 38.7 M/sec for entrainment. Both of these are in agreement with the photographic observations. Consequently, available correlations appear to give reliable indications of the behavior of these high density, high surface tension melts.

TEN PIN EXPERIMENTS

The single pin tests were extended to multipin geometries with transparent ten pin triangular test sections. The Wood's metal cladding on each 0.64 cm dia. pin was again 20.3 cm in length and 0.038 cm thick. Figure 2 is a schematic illustration of the basic apparatus which was very similar to the single pin system. The triangular configuration was chosen so that all sides of the bundle could be viewed with mirrors. Argon gas was again used to simulate the sodium vapor and the system instrumentation again consisted of gas flow rates, temperatures, and pressures. High speed movies (400 pps) were the principal data record for the experiments and the results are given for the four runs conducted in Table II.

TABLE II

Ten Pin Clad Motion Experiments

Run	Gas Velocity M/sec	Cladding Motion
1	9.2	Drains
2	18.3	Incipient flooding but quickly drains
3	30.5	Sustained flooding with large waves moving upward
4	48.8	Sustained flooding with high frequency waves and some direct entrainment

These multipin experiments also are in excellent agreement with the predicted velocities for incipient flooding, sustained flooding, and entrainment for these high density, high surface tension melts.

28-PIN EXPERIMENTS

Once the applicability of available models for describing the behavior of molten metal films had been established, the principal questions regarding molten clad behavior became the two-dimensional effects resulting from radial incoherence and the effect of clad refreezing as it moves vertically upward out of the core. An additional Wood's metal simulation experiment has been fabricated, in a transparent lucite test section, to specifically address these questions. The experimental apparatus is schematically illustrated in Fig. 3, along with the triangular test which is full length typical of FTR geometries. As with the single and ten pin test sections, the Wood's metal cladding is 0.038 cm thick, with a pin diameter of 0.64 cm, but in this test, the length of the Wood's metal region is increased to 91.4 cm. The simulated fuel pins are wrapped with 0.14 cm wires to maintain spacing. These wires

are made of Wood's metal in the core region and stainless steel outside of this location. Stainless steel tubing (0.64 cm dia.) was cold soldered to the Wood's metal coated copper tubing to form the inlet blanket region and fission gas plenum length.

The triangular configuration is specifically designed to make this test section behave as a one-sixth segment of a large LMFBF fuel bundle. Three specific additions were made to the standard triangular geometry.

- (1) The two lucite walls facing the mirrors have 0.16 cm diameter lucite rods attached to the walls between the simulated fuel pins over the entire length of the test section. This reduces the local hydraulic diameter and provides these wall subchannels with frictional pressure drop characteristics similar to subchannels within the central region of a fuel bundle. Consequently, these two faces, which can be viewed with the mirrors, should perform similar to these interior regions.
- (2) The apex at the junction of these two sides was filled with a 0.48 cm dia. "O" ring to eliminate high velocity gas streaming in this subchannel.
- (3) In order to provide a simulation of the radial incoherence in the time to clad melting, the seven (7) pins along the wall facing the camera have teflon plugs with small diameter holes (0.12 cm) inserted into the ends of the tubing which penetrate into the steam plenum. Since all the tubes are connected in parallel to the steam dome, these outer pins will have a reduced steam flow (approximately one-half of that of the other pins) and will experience an extended time to clad melting.

An additional very important design change was incorporated to provide a strong potential for freezing of the molten clad as it attempts to move either upward or downward from the simulated core. This was accomplished by inserting 0.3 cm ID by 0.4 cm O.D. glass tubing downward through the stainless steel tubing simulating the fission gas plenum region and upward through the stainless tubing which simulated the lower reflector. Both of these glass tubes terminate at Wood's metal. Consequently, the steam flows in through the upper glass tubing, then through the Wood's metal coated copper tubing, and finally out through the lower glass tubing. The stainless steel is thermally insulated from the steam and remains essentially at room temperature while the Wood's metal is heated to its 70°C melting point. Therefore, as the cladding attempts to move out of the core, it will encounter a very cold region, and thus, a strong potential for freezing.

In order to insure a constant pressure drop across the test section, a constant pressure relief valve was designed and incorporated into the system. This relief valve is a pneumatic cylinder with a weight which exactly balances the desired inlet pressure. An electric motor rotates the piston to eliminate any static friction problems associated with the initial movement of the piston. When the inlet pressure attempts to increase as the flow resistance increases during clad melting, the piston will move upward and uncover small vent holes. There are sufficient vents, if required, to exhaust twice the steady state gas flow.

The system instrumentation includes total system and test section inlet argon gas flow rates, inlet plenum pressure and other gas system pressures along with the steam supply pressure, and, as in the previous experiment, high speed movies of the melting process. Because of the extensive length of this test section, four or five cameras must be used in order to observe any detail of the flow structure.

Since argon gas is used to simulate the sodium vapor and Wood's metal is simulating the stainless steel cladding, it is important that the system is operated with parametric conditions which provide the same two-phase flow conditions. The incipient clad flooding velocity for sodium vapor over a stainless steel melt is about 16.4 M/sec using the criterion proposed in ref. 1. Vapor velocities required for sustained flooding and entrainment for such a system are 82 and 101 M/sec respectively. As mentioned in the introduction, a 50 KPa driving pressure across the core would provide a vapor velocity of 80 M/sec. Consequently, the melt should experience incipient flooding and perhaps sustained upward motion but no entrainment. The same driving pressure across the simulation test bundle provides an argon gas velocity of approximately 30 M/sec. With the critical velocities listed above for a Wood's metal-argon system this should produce incipient flooding and perhaps sustained upward motion but no entrainment. Consequently, the simulation experiment should provide an excellent insight into the flow structure and overall cladding behavior during this period in which significant two-dimensional and solidification effects are present.

NOMENCLATURE

- g - acceleration of gravity
- U_g - superficial gas velocity
- δ - film thickness
- ρ_g - density of gas or vapor
- ρ_l - density of liquid
- σ - liquid surface tension

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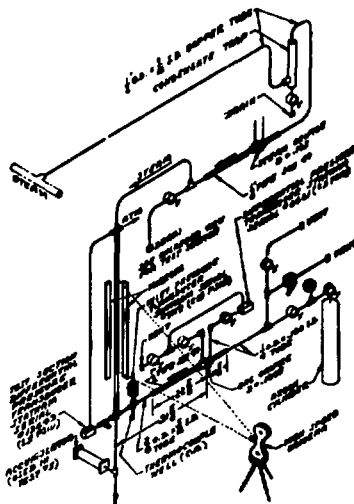


Fig. 1 Single Pin Apparatus

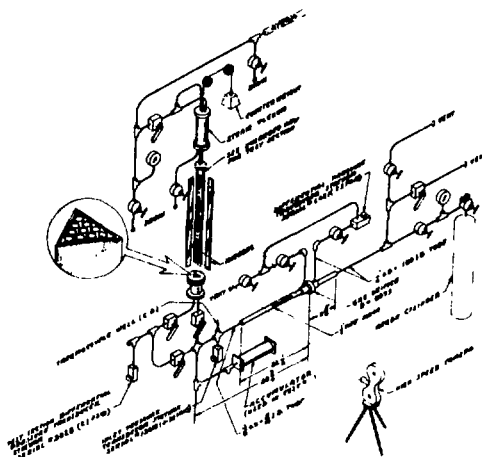


Fig. 2 Ten Pin Apparatus

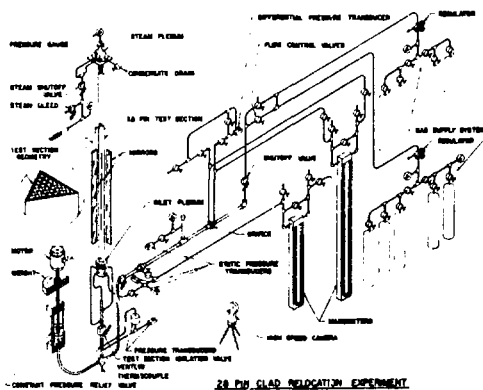


Fig. 3 Twenty-eight Pin Apparatus

CLAD RELOCATION DYNAMICS - THE PHYSICS AND ACCIDENT EVOLUTION IMPLICATIONS†

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ABSTRACT

Even under transient flow conditions the value $J_g^* = 1$ represents the threshold for upward film relocation. However, the two-phase frictional pressure drop multiplier is found to be substantially smaller than that predicted by the classical steady-state correlations for annular flow. Based on the values determined here new flow regime maps for clad relocation in LMFBR bundles under axial and radial melting incoherencies are presented. For large values of J_g^* upward relocation is usually accompanied by separation from the advancing film front and entrainment. The entrained mass proceeds at much higher velocities. Based on these findings and the calculated (by SAS) core pressure drop substantial clad relocation would be expected under LOFA conditions.

1. INTRODUCTION

The influence of clad relocation dynamics upon the mechanistic description of the postulated unprotected Loss-of-Flow Accident (LOFA) of a Liquid Metal Fast Breeder Reactor (LMFBR) was first evaluated for the Fast Flux Test Facility (FFTF) [1]. This evaluation was made within the framework of SAS-3A accident analysis code [2] by means of the clad relocation model contained in the module CLAZAS [3].

Although no significant departures in the reactivity and power transients were noted, these calculations led to the conclusion that thick steel plugs would be expected to form in the Inconel reflector region as the cladding, levitated by the upward streaming sodium vapor, froze in this cold region. These plugs would prevent fuel dispersal and thus the "transition phase" of the accident was formulated. Subsequent applications of SAS-CLAZAS to the LOFA analysis of the Clinch River Breeder Reactor Plant (CRBRP) indicated substantial aggravation of the overpower conditions (LOF-d-TOP) imposed by sodium voiding [4]. Almost concurrently with these assessments the first in-pile experimental evidence of upward clad relocation became available [5]. However,

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the predicted (by CLAZAS) upward relocation was substantially greater than that observed from the post-mortem examination. In view of the apparent accident sensitivity to clad relocation, this discrepancy has prompted a number of studies to better define the relevant mechanisms.

The problem was examined with the classical film-gas interaction models of annular two-phase flow, but with due consideration to the two-dimensional aspects of the flow produced by the "expected" radial (and axial) clad-melting incoherencies [6,7,8]. It was determined that this effect alone could reduce the clad velocities predicted by CLAZAS to: (a) "little net upward motion" [6], or (b) values reduced by more than a factor of four [9].

Subsequent accident analyses parametrics [10] indicated that for CRBRP the "sensitive" period for clad relocation reactivity feedbacks may have a duration of less than one second. However, the analytical models utilized in the above analyses are derived from steady-state experiments, and the experimental evidence under the general phenomenology of clad melting and relocation provides additional information along these lines.

2. ACCIDENT ANALYSIS ASPECTS

2.1 Computational Background

The influence of clad relocation upon the accident sequence has been examined by means of a series of parametric runs with SAS-3A. Here we will be primarily concerned with the role of cladding relocation rates in setting up the stage for the development of "LOF-d-TOP" conditions leading to pin failures in unvoided subassemblies. It is expected that within a range of parametric variations the likelihood of occurrence of these conditions can be reasonably assessed by the SAS system. At present, however, the mode and location of failure and subsequent interactions can not be reliably modeled. An "indication" of relative severity can, therefore, only be obtained by the amount of sodium (unvoided subassemblies) reactivity inventory and the power and reactivity increase rates at the time of pin failures.

Other important SAS-input variables that influence the outcome as defined above, are axial fuel expansion, and amount of fission gases available in SLUMPY [12] to disperse fuel. A fuel melt fraction (usually 50%) was taken as a criteria for pin failures, however, the results are not very sensitive to the precise value of this parameter. Axial expansion effects are controlled by specifying the fraction of the physical expansion utilized for reactivity feedback. The effect of fission gas dispersal is controlled by specifying two fractions of the fission gases that have been generated (depending on burnup). The one represents total amount eventually available for dispersal (after some delay), and the other represents the amount immediately available for dispersal. Cladding relocation can be controlled to values: (a) zero velocity, (b) CLAZAS prediction, (c) and outside specified constant upward velocity.

Other details of the SAS input utilized in these computations may be found elsewhere [13,14].

2.2 Results and Discussion

The combination of 100% axial expansion, 50% fission gases with 20% immediately available and zero clad velocity leads to complete core voiding without the occurrence of "LOF-d-TOP" conditions. This sequence has clearly avoided the potential for the so-called "direct" hydrodynamic disassembly. When in this combination the modest clad velocity of 80 cm/sec [9] (CLAZAS would predict ~240 cm/sec) is utilized in place of the immobile cladding assumption a radically different result emerges. Now "LOF-d-TOP" conditions are developed at 12.8914 seconds into the accident. At this time the power is 65.3 X SS, the reactivity is \$0.9390 and increasing at a rate of \$0.377/sec, and the core still contains \$0.90 of sodium reactivity. This comparison indicates that

even the most "favorable" choice of axial expansion and fission gases, when combined with an 80 cm/sec clad velocity, produces an "LOF-d-TOP" scenario. It is of interest, therefore, to more rigidly define the range of possible clad relocation velocities.

When a more "unfavorable" choice for axial expansion and fuel dispersal by fission gases is utilized, "LOF-d-TOP" conditions will occur with larger sodium inventory and higher ramp rates at the inception of pin failures. For example for 50% axial expansion feedback, 50% fission gases, (10% immediately available) and 80 cm/sec clad velocity, "LOF-d-TOP" is attained somewhat earlier, at 12.4056 seconds, at 160 X SS power, \$0.982 reactivity, \$0.93 sodium reactivity inventory, and \$0.812/sec reactivity rate. Reducing axial expansions to 0% further aggravates the situation. Now pin failures occur at 11.7664 seconds, at 158 X SS power, \$0.993 reactivity, \$1.41 sodium reactivity inventory and a reactivity insertion rate of \$2.2/sec.

If no immediate fuel dispersal is postulated (50% fission gases but 0% immediately available) in conjunction with 100% axial expansion and 80 cm/sec, the accident is prolonged somewhat, reaching "LOF-d-TOP" at 12.9334 seconds, with 105 X SS power, \$0.9834 reactivity, \$0.93 sodium inventory, but the reactivity rate now increases to \$12.1/sec.

The most "unfavorable" case would be 0% axial expansion (maximizes sodium core inventory) and 0% fission gases (maximizes reactivity rate). With 80 cm/sec this produces "LOF-d-TOP" at 11.7674 seconds, with power 163 X SS, \$0.9895 reactivity, \$1.4% sodium inventory, and a ramp rate of \$3/sec.

The cases that develop into "LOF-d-TOP" are in general "fast" and do not allow sufficient time even with the 80 cm/sec clad velocity to produce substantial plugging at the subassembly exit.

3. EXPERIMENTAL

3.1 Experimental Approach

Traditionally, flooding phenomena have been studied in counter-current two-phase flow systems [7,15]. In such systems co-current, upward flow may be induced for sufficiently high gas flow rates. However, this transition crosses the flooding region defined as the regime in which the liquid film has a nearly zero net velocity. At this point, large waves develop [7,15] and since the film is continuously fed it will grow in thickness. These phenomena are particularly important for small hydraulic diameter channels such as provided by the LMFBK rod bundles. Splashing, channel bridging, inlet and exit effects and other such phenomena induce a chaotic flow field with much increased pressure drop requirements. It is important to determine, starting with a zero velocity, non-fed liquid film, which suddenly experiences the gas drag, whether a similar flow regime would evolve.

Crucial to the evaluation of the clad film velocity is the gas velocity and the momentum coupling between the gas and the liquid at their interface. For incoherent radial melting, they also determine the amount of flow diversion into the dry subchannels. Since these processes depend, in turn, upon the two-phase flow pattern, due consideration must be given to the highly transient (developing) nature of the particular problem at hand. Information on this subject is presently lacking.

The two areas outlined above indicate the need for fundamental level experiments to measure and observe the detailed behavior of film/gas interaction under transient conditions. Clearly, a flat plate (two dimensional) film geometry is ideal for such purposes and was selected for this work. "Simulant" rod bundle experiments are carried out elsewhere [16,17] and it is expected that these two sets of experimental programs will be complimentary to each other. Since in-pile experiments are presently limited to small bundles and they primarily produce final clad disposition data from post-mortem

examination, and since sufficiently prototypic out-of-pile tests are not possible, it is important to study the phenomena from various angles of "simulation" ranging from the most fundamental to the most simulation-oriented ones.

Applications of such out-of-pile information to accident analysis must keep in perspective all possible complications and random variations present in the real system. An example of such complications is presented by the plenum fission gas release. Although it is difficult to precisely quantify the impedance to gas flow present in the pin upper blanket region, it is estimated that it would be substantial and it might promote a prolonged release, of the order of perhaps one second, and thus interfere to some extent with the clad melting and relocation process. It does not appear likely, however, that it will enhance the upward relocation velocities.

The problem of clad relocation has always been examined both experimental [7,15] and computationally [3,6] as one of liquid film flow. It has always been assumed that molten clad will wet solid fuel, until recently, when the possibility of non-wetting behavior was brought up [18]. Special consideration to this problem is given in sections 3.3.1 and 3.3.2.

The wire-wrap effects have been evaluated earlier as insignificant [11]. However, pin spacers would be quite influential in disrupting the continuity of the flow and providing the basis for local freezing and plug formation.

3.2 Experimental Facilities and Procedures

As already mentioned, a flat plate geometry was selected for the test section. It is shown schematically in Figure 1. The Woods alloy is used as the cladding simulant. It is cast as a 0.015 in. film on the base plate. Special flux treatment is necessary to establish a good bond, which is essential for a fast and uniform melting. The base plate can vary in material and thickness. A thin, 0.0015 in. steel plate was used for investigating quantitatively local film thickness transients based on the method of electrical resistivity. In this case voltage taps were placed on the back side separating the test section in eight regions. A thicker, 0.020 in. brass plate was utilized for handling convenience for all runs that did not involve detailed film thickness measurements. In all cases a 8-12 Kw infrared lamp bank provided fast and reproducible melting by irradiating the back side of the base plate. By shielding the irradiation from a portion of the base plate the melting pattern and extent of downstream freezing could be controlled.

With the side spacers the hydraulic diameter can be adjusted to any value. It was set at 0.4 cm for the experiments reported here. The channel is completed by the front plate made out of 1/4 in. acrylic sheet for easy observation and replacement. This window has four pressure taps positioned as indicated so that the pressure drop over the whole test section, as well as over the predominantly liquid portion may be obtained simultaneously.

The overall experimental set up shown in Figure 2 indicates the test section location. The air stream originates from two high pressure, high capacity tanks, and enters the apparatus at room temperature, after filtration, through a control valve. A very large amount of bypass is provided to achieve a constant pressure at the inlet to the test section throughout the experimental run. The exit section consisted of a straight, 1 in. copper tubing which also served as a flow meter. Smooth transitions were provided for the inlet and outlet section of the test section.

All pressure measurements were obtained with Validyne Model aDP-15 and Pace Engineering Model P90D strain gage transducers, and Validyne Model CD-101-3CT carrier demodulators, and were recorded on a Honeywell Model 1508C Oscillograph simultaneously with eight voltage signals corresponding to eight equal sections of the test section. These latter signals responded to a constant current power supply, Sorensen Q-Nobatron Model QRB20-1.5, and variations of local resistivity due to film thickness changes and temperature changes. Two simultaneous high speed (175-250 ips) motion pictures were obtained using HYCAMs Model 41-0004 covering the overall film behavior

and the local structure (telephoto lenses).

A run was initiated by establishing the required flow rate and switching on the lamp bank for a specified period of time (usually 3.5 secs). The visicorder and the movie cameras were switched on-off automatically.

3.3 Experimental Results

3.3.1 Scope of Experimental Conditions

It has been shown earlier that a convenient formulation of the clad relocation problem may be obtained in terms of the dimensionless gas velocity, J_g^* [7]. The experimental range of this quantity covered in this work was from 1.38 to 3.80 corresponding to velocities of 100 ft/sec and 270 ft/sec respectively. The bulk of the data was obtained with 0.015 in "clad" film, heated downstream region (to avoid plug formation), and coherent melting of the whole film (no radial melting incoherencies present). Parametric variations covered the effects of film thickness (0.010, 0.015, 0.030 in), unheated downstream region, non-wetting downstream region, and radial melting incoherencies.

3.3.2 Qualitative Trends

Both crucial aspects of the experimental technique, namely, rapid and uniform film melting and constant inlet pressure, performed satisfactorily. In most cases the film was completely molten prior to motion initiation and the pressure at the inlet normally would not build by more than 15%.

A value of J_g^* close to one, in agreement with previous data [8], appeared to mark the threshold for upward motion. For J_g^* values greater than 1.4 upward motion was clearly observed. This tendency for upward motion increased with J_g^* . The whole event was over in 0.5 to 1 sec, either due to film depletion by advancement and/or entrainment for high J_g^* values, or by local splashing and freezing on the window for J_g^* values near one.

The lamps were usually left on for ~3.5 secs, allowing the portion of the film remaining on the original location to freeze. In certain cases with $J_g^* = 1$, that happened not to splash, nearly all the original film mass was recovered in this form.

3.3.3 Gas/Liquid Momentum Transfer

A typical pressure drop trace is shown in Figure 3. It may be seen that upon melting the pressure drop concentrates due to two-phase friction, upon the film region, while simultaneously the flow through the system decreases to accommodate the increased pressure drop requirements. Based upon these measurements the two-phase pressure drop multipliers, ϕ_g^2 , were reduced. They were consistently found in the range of 3 to 5. No systematic trend with J_g^* was observed, and for the majority of cases they ranged between 3 and 4. These measurements are complicated by transient events and transducer output oscillation due to waves, entrainment, etc. and should be interpreted, at this time, as order of magnitude estimates. A clear departure, close to one order of magnitude, from steady-state correlation is indicated by these data. It may be attributed to transient wave development properties. Indeed the motion pictures indicate that instead of the usual roll-waves typical of fully developed annular two-phase flow, we observe "microscopic" waves that appear like minute rapidly moving pebbles on the liquid surface. When J_g^* is near one, local thickening of the film may occur leading to wave growth and splashing. When J_g^* is significantly larger than one, these small waves are not given the opportunity to grow. The film moves uniformly, and becomes thinner further decreasing the tendency to produce large waves and splash.

3.3.4 Film Thickness Transients

The visicorder voltage tap traces (a typical one shown in Figure 4) were manually digitized at intervals of 0.25 seconds and analyzed with the help of the computer. This analysis had to account for resistivity changes with temperature. This was accomplished by determining the heatup rate from the pre-melt voltage history (film thickness known), and for a check, at latter times

the voltage drop changes in the upper regions not yet covered with film (for small J_g^* 's). Additional checks were provided by runs for which no net film movement was observed in the motion picture records, as well as by checking, for all runs analyzed, the final film disposition (post-test examination) against that deduced from the voltage signal analysis. Typical results, showing the film thickness variation along the test section for various time instances, are shown in Figure 5 for run NTS-23 with $J_g^* = 2.76$. In these plots the thicknesses are normalized to the initial solid film thickness. From these plots also an indication of experimental and data-reduction errors can be obtained. The time-wise variation of the film thickness in any region can also be visualized. Two such typical plots for the same run (NTS-23), one for a region under the original film location and one for a downstream initially film-free region are depicted in Figure 6. More detailed deductions from these results is presented in the full report [19]. At this time the observation is made that for the range of J_g^* 's examined film velocities do not exceed a few inches per second.

The film thickness analysis discussed above can also produce local entrainment rates. A qualitative picture can be obtained from Figure 5. Quantitative results are presented elsewhere [19].

As may be seen from Figure 4 a manual data reduction technique as utilized here is cumbersome and can not capture the fine structure of transient film profile. An automated system is now in preparation.

3.3.5 Entrainment

Two types of film entrainment were observed. The first preferably called splashing is a result of large wave development and channel gap bridging. It is observed at low values of J_g^* and produces splashing both just above or slightly downstream of the original film location. The latter mode is preferred as J_g^* values increase above one. The second mode of entrainment, is produced by peeling off the top of waves in the neighborhood of the advancing film front, and it is typical of high J_g^* (>2.0) values. These waves disintegrate into droplets which move at very high velocities and little deposition on the test section window. Droplet velocities in the range of 100-300 cm/sec were observed, and they increase with J_g^* .

A good deal of the entrainment process seems to depend on the wetting properties of the downstream region. By flux-treating this region it was possible to reduce entrainment and promote uniform film advancement (at velocity of a few inches per second). Untreated test sections, on the other hand, lead to entrainment of the entire mass advancing upwards. It appears that non-wetting results to a local film accumulation and break-off at high gas velocities.

For a value of $J_g^* = 2.8$ a typical post-test film-mass disposition was as follows: 30% remained in original location, 30% moved out of the test section as droplets, and 25% was deposited on the window and 15% was advanced on the downstream (to the film) region.

Thinner films are more difficult to move and entrain. For example, Figure 7 (a and b), shows the final disposition for two runs with $J_g^* = 1.8$. Part (a) had an original thickness of 0.010 in and part (b) had one of 0.015 in. The difference in upward motion is clearly seen.

3.36 Incoherency Effects

The radial incoherency, or air flow diversion aspects were studied by covering only one third of the channel width with the film. If the steady-state two-phase multiplier was applicable these effects would be quite significant [6,7]. However, no apparent difference, from the full-width films was observed. Both for J_g^* of 1.7 and 2.8, the film front moved upwards with velocity of ~ 10 cm/sec. At $J_g^* = 1.7$, $\sim 22\%$ of the film remained in the original location, while at $J_g^* = 2.8$ only $\sim 10\%$ did. Nearly 14% was lost on droplets, and about 50% was solidified upon the downstream region. Figure 7(c) depicts the "post-mortem" of run NTS-31 ($J_g^* = 2.8$, $V \sim 200$ ft/sec).

In view of the results of the previous sections the behavior observed here

may be anticipated. The very small two-phase multiplier yields a small amount of flow diversion. The high velocity air is now capable to shear and entrain the liquid film inducing a substantial upward relocation.

4. ANALYTICAL

4.1 Flow Regime Maps

The two-phase pressure drop multipliers desired here can now be utilized to update the flow regime maps for U.S. LMFBR subassembly presented earlier [7]. Applying the same techniques, for two values of the multiplier (2 and 8) encompassing the range determined here, the maps of Figure 8 (a) and (b) were obtained. For comparison the earlier map for the same void fraction of $X = 0.76$ is also presented in Figure 8(c). In these figures $\tilde{A}_w$ and $\tilde{L}_w$ are the values of radial and axial melting incoherence. The dimensionless gas velocity J_g^* is based on the local superficial gas velocity within the two-phase flow region of the subassembly and is made dimensionless by $(gD_H \rho_g)^{1/2}$. The value of J_g^* may be read off for any multiple, m , of the minimum pressure gradient corresponding to the liquid sodium head.

As expected, as the multiplier decreases the sensitivity of J_g^* upon the degree of melting incoherence decreases, and the corresponding values of J_g^* increase. This is quite fortunate since the analysis of clad velocity will not have to depend upon the very complicated details of early transient heatup and two-dimensional voiding [20] and clad dryout phenomena. It is also noted that even for $m = 1$ the whole range of possible incoherence would imply upward relocation ($J_g^* > 1$).

4.2 Assessment of Cladding Velocities

A most important quantity in assessing clad relocation potential in an LMFBR during LOF conditions is the core pressure drop. Due to continued voiding it is not unlikely that a lower plenum pressure build up will occur. This pressurization can be calculated by a SAS module called PRIMAR and can yield values of m , at the time of clad melting for example in CRBRP analyses, 2 to 8. From the flow regime maps of Figure 8 (a) and (b), we deduce J_g^* values in the range of 2 to 6.

Under such conditions substantial upward clad relocation and substantial entrainment would be expected. In view of the quantitative observations reported in sections 3.3.2 to 3.3.6, however, it appears that still a reactivity feedback corresponding to all molten clad moving at 80 cm/sec would represent a reasonable upper bound of the expected values.

5. CONCLUSIONS

Even under transient flow conditions the value $J_g^* = 1$ represents the threshold for upward film relocation. However, the two-phase frictional pressure drop multiplier was found to be substantially smaller than that predicted by the classical steady-state correlations for annular flow. Based on the values determined here new flow regime maps for clad relocation in LMFBR bundles under axial and radial melting incoherencies were presented. For large values of J_g^* , upward relocation is usually accompanied by separation from the advancing film front and entrainment. The entrainment mass proceeds at much higher velocities. Based on these findings and the calculated (by SAS) core pressure drop substantial clad relocation would be expected under LOFA conditions. It does not appear likely, however, that the reactivity feedback would exceed that calculated by clad motion of the whole molten film at 80 cm/sec.

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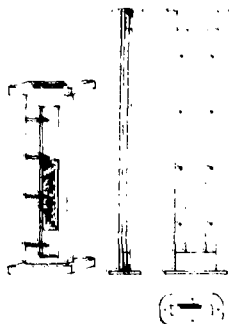


Fig. 1. Test section for 2-D Wood's alloy runs.

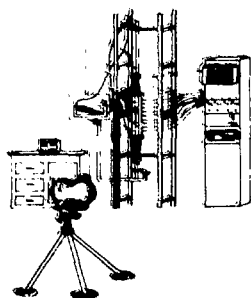


Fig. 2. Experimental apparatus showing test section location.

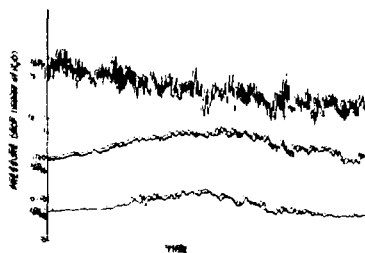


Fig. 3. Pressure drop vs. time for $J_g^* = 1.76$. ΔP_g = "flow rate" pressure drop. ΔP_{ts} , ΔP_{tsf} = test section pressure drop.

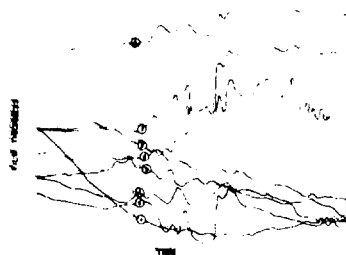


Fig. 4. Voltage drop for each region vs. time at $J_g^* = 1.76$.

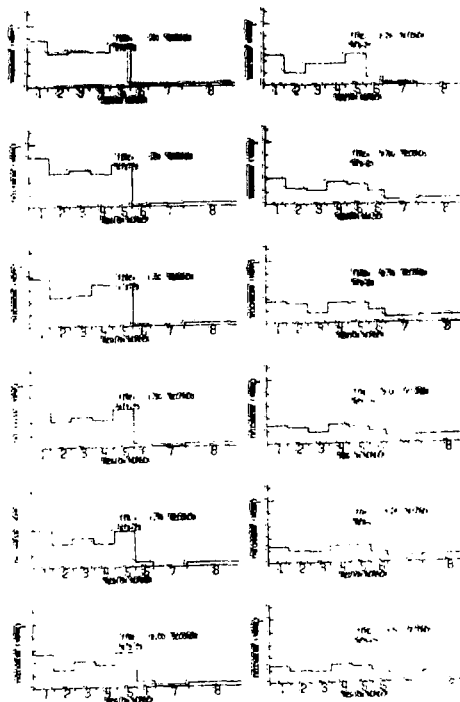


Fig. 5. Film thickness at various times for each region for $J_g^* = 2.76$.

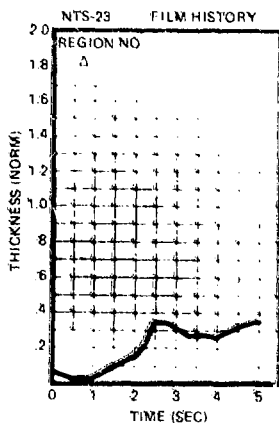


Fig. 6a. Normalized film thickness vs. time for a region under the original film for $J_g^* = 2.76$.

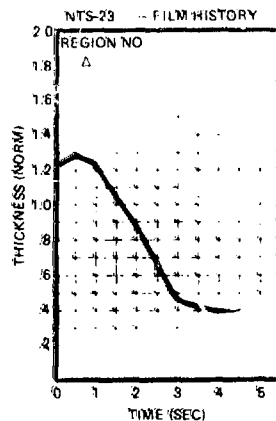


Fig. 6b. Normalized film thickness for a region downstream of the original film for $J_g^* = 2.76$.



Fig. 7. Final disposition at $J_g^* = 1.8$ for original film thicknesses of (a) .010 in., (b) .015 in., and (c) .015 in.

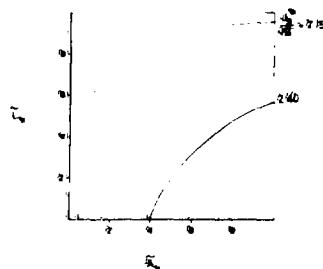


Fig. 8a. Flow regime map for $\alpha = 80$ and $\phi_g^2 = 2.0$

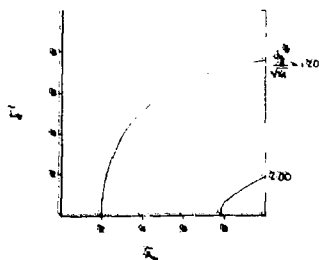


Fig. 8b. Flow regime map for $\alpha = .80$ and $\phi_g^2 = 8.0$

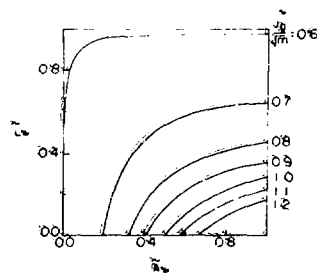


Fig. 8c. Flow regime map for $\alpha = .80$ and $\phi_g^2 = 37$.

FUEL DISPERSAL EXPERIMENTS WITH SIMULANT FLUIDS

by

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ABSTRACT

Boiling pool stability experiments have been conducted for open and "bottled up" systems to ascertain the dispersive characteristics of such systems. These studies were performed in a microwave oven with water as the boiling liquid. High speed movies demonstrate that open pools are dispersive at very low vapor production rates and that "bottled up" pools, as a result of upward heat sinks created by the pressurization, are also dispersive at vapor production rates equivalent to 2% of nominal reactor power.

POOL BOIL-UP EXPERIMENTS

INTRODUCTION

The sequence of events following the hypothetical core meltdown accident in a liquid-metal fast breeder reactor would begin with the single phase temperature increase of the sodium coolant, and include sodium voiding, clad melting and relocation, and finally fuel melting and boiling of a molten fuel pool. The dispersive characteristics of this boiling mixture of fuel and steel is a basic concern in the assessment of the accident progression and consequences since this determines whether or not fuel slumping and compaction can occur, and hence, whether or not strong reactivity events can be induced by the motion of the fuel pool.

Little work has been done in the area of pool boil-up for violent boiling systems with internal heat generation which has a system of real interest. A great deal of work has been done in both boiling systems from a flat heated surface and pool stability for gas-liquid systems in which the gas is injected

*Work performed under the auspices of the U. S. Energy Research and Development Administration

through a fixed horizontal surface. For these types of systems, the stability of the two-phased flow regime has been quantified experimentally and analytically in terms of a ratio of the dynamic and surface tension forces. This ratio is generally given as

$$K = \frac{U^* \sqrt{\rho_c}}{v \sqrt{g(\rho_H - \rho_L)}} \quad (1)$$

where the parameter K is commonly referred to as the Kutateladze number. This parameter can be used to characterize the stability of a given flow process. The following table, which is taken from reference 1, shows the value of the parameter for various flow regimes as well as which phase represents a continuous media for the given flow regime.

The experiments described in this paper, were carried out to; (1) investigate the relevance of this stability parameter for systems with internal heat generation, and (2) to study the behavior of such boiling systems when the entire system becomes a sealed, constant volume container. This latter configuration is the type of behavior one would expect from a molten fuel pool attempting to move outward from the core region through cold steel structure. The principal question to be addressed under these types of circumstances is whether or not the fuel would remain as a monotonically dispersive system.

TABLE I
Stability Parameter K

<u>Nature of Process</u>	<u>K</u>
Breakdown of Bubbly Flow	0.3; $\rho_c = \rho_H$
Breakdown of Churn Turbulent Flow-Drop Fluidization of a Heavier Fluid By A Lighter Fluid	0.14; $\rho_c = \rho_L$
Flooding Of Liquid Films	3; $\rho_c = \rho_L$
Entrainment Of Liquid Films	3.7; $\rho_c = \rho_L$

EXPERIMENTAL APPARATUS PROCEDURE

The basic apparatus, which is shown in Fig. 1, for these unique experiments was a large microwave oven capable of operating at both one (1) and two (2) kilowatt power levels. This oven was modified by replacing the darkened glass in the oven door and the stainless steel screen, and by mounting high intensity quartz lamps inside of the oven so that high quality, high speed motion pictures could be taken of the boiling pool. Additional modifications included a transport cylinder and plug so that the test sections could be sealed during the test and gas jets around the oven window to minimize any condensation and obstruction of the viewing port.

The tests were taken under two (2) different conditions. The first series of experiments were conducted with the system at one atmosphere and the top of the boiling vessel open to the environment. These tests were conducted

as steady state experiments to determine the relevance of the Kutateladze stability criteria on the systems with internal heat generation. The second series of tests were conducted by heating the coolant (water) up to the saturation point, applying power at the one (1) and two (2) levels to cause the pool to boil up to the top of the test section at which time, the container was sealed with a plug and the pool behavior was monitored by high speed movies and pressure instrumentation. These experiments were conducted specifically to examine the pool stability under bottled up conditions.

The system power level for a given test section configuration was determined by measuring the time required to heat the system from room temperature to saturation at one atmosphere. This power level was assumed to remain constant during boiling of the pool. The rate of pressurization was monitored by a pressure transducer connected to a port in the top plug. All high speed movies were taken at a framing rate of one hundred frames per second.

EXPERIMENTAL RESULTS

The first set of experiments to demonstrate boiling pool stability in a constant pressure system were carried out in a smaller microwave oven which had a maximum power output of 600 watts. Small, neutrally buoyant plastic spheres were "seeded" in the water coolant to insure ample bulk nucleation sites. Experimental results were taken at varying power levels with different vessel diameters to obtain a wide range of superficial vapor velocities as listed in Table II. These results are illustrated in Fig. 2 in terms of steady state average void fraction developed by the boiled-up pool as a function of the superficial vapor velocity at the top of the pool. Since the system was saturated at one atmosphere, the superficial vapor velocity at the top of the pool can be evaluated from energy and mass balances and is given by

$$U_v = \dot{Q}_v / (A h_{fg}) \quad (2)$$

Figure 2 shows a tremendous increase in steady state void fraction for a vapor velocity of approximately 0.05 M/sec. At this velocity, the system undergoes a marked transition from normal bubbly flow to the churn turbulent regime, and the void fractions dramatically increase from standard bubbly flow void fractions of 10 - 20% to values approaching 90%. Flow regime stability calculations predict that the onset of the churn turbulent regime should occur at a velocity of 0.047 M/sec, which is in excellent agreement with the experimental measurements.

Similar calculations for oxide fuel predict the onset of churn turbulent flow at a superficial velocity of 0.05 M/sec. For a completely vented, unpressurized system, the superficial fuel vapor velocity at the top of the pool is approximately 250 M/sec. Consequently, one would expect a boiling fuel pool to be monotonically dispersive under such conditions, even at very low power levels.

In an actual reactor system, one would not expect that a dispersing pool would remain at constant pressure since the movement of molten steel and fuel into regions containing cold steel structure and perhaps sodium should result in solidification of the fuel and steel. Depending upon the amount of steel and fuel available, this solidification may provide a rather strong coherent plug as evidenced by the fuel solidification experiments reported in ref. 2. Consequently, from a reactor safety point of view, one must assume that complete plugging of these relief passages would occur, which then leaves the

TABLE 11

Boiling Pool Data
Ambient Pressure

Test Section Power (W)	Vessel Diameter M	Volume of Liquid L	Vapor Superficial Velocity M/sec	Average Steady State Void Fraction α
605	0.120	0.500	0.040	0.29
516	0.100	0.050	0.048	0.54
470	0.086	0.050	0.058	0.75
481	0.075	0.050	0.080	0.80
487	0.065	0.050	0.109	0.80
548	0.053	0.050	0.181	0.86
571	0.046	0.050	0.244	0.84
491	0.038	0.050	0.318	0.91
582	0.025	0.050	0.845	0.91
419	0.020	0.050	1.006	0.91
293	0.015	0.020	1.227	0.87
267	0.013	0.008	1.537	0.88
115	0.010	0.005	1.079	0.93
182	0.008	0.009	2.917	0.95

molten boiling pool in a constant volume, bottled up condition. Once such a configuration is established, pressurization of the system will be initiated, and during this pressurization, axial and radial heat sinks play an extremely important role in determining the pool stability. This can be illustrated by first considering the adiabatic pressurization rate of a boiling pool. An energy balance between the increase of sensible heat with the vapor and liquid phases, the amount of additional vapor formed to provide the pressurization, and the energy generated within the pool can be expressed as

$$\dot{Q} = m_l c_l \frac{dT}{dt} + m_v c_v \frac{dT}{dt} + h_{fg} \dot{m}_v \quad (3)$$

If systems with void fractions of approximately 50% are considered, which is the range of interest for core dispersal behavior, then the sensible heat of the vapor is negligible compared to that of the liquid. If the vapor is assumed to be a perfect gas, then

$$\dot{m}_v = m_v \left(\frac{1}{P} \frac{dP}{dt} - \frac{1}{T} \frac{dT}{dt} \right) \quad (4)$$

Substitution of the Clausius-Clapeyron relation into eqn. 4 yields,

$$\dot{m}_v = \frac{m_v}{T} \left[\frac{h_{fg}}{P v_{fg}} - 1 \right] \frac{dT}{dt} \quad (5)$$

Equation 5 can then be substituted into eqn. 3 to give an expression for the instantaneous rate

$$\dot{Q} = \left\{ m_l c_l + \frac{h_{fg} m_v}{T} \left(\frac{h_{fg}}{P v_{fg}} - 1 \right) \right\} \frac{dT}{dt} \quad (6)$$

of temperature rise. Assuming thermodynamic equilibrium enables one to evaluate the pressure rise rate. For a reactor system at full power, this initial rate is approximately 0.45 MPa/sec as compared to 0.012 MPa/sec for microwave oven test sections. The superficial velocities of the vapor required to produce these pressurizations are 0.53 and 0.01 M/sec respectively. Consequently, the equivalent reactor power level studied in the microwave oven was approximately 2% of nominal. It is also noteworthy that adiabatic superficial velocity in the simulation is a factor of five less than that required to establish churn-turbulent flow.

Self-pressurization experiments were conducted by plugging the vessel when the boiled up pool reached the top. In the movies which accompany the presentation of this paper, it is obvious that the pool continues in a boiled up state even though pressurization is occurring. How can a pool be "boiled up" if the adiabatic superficial vapor velocity is less than the critical value for churn-turbulent flow? The answer is axial and radial heat sinks. For the adiabatic case, of the 1500 J/sec deposited within the pool, only 22.6 J/sec are required for vapor production. Therefore, a small axial heat loss can increase the vapor production rate many times. To illustrate this, assume a bottled up configuration as shown in Fig. 3 with an initial average void fraction of 0.50. Consider the only heat sinks to be the sensible heat of the surrounding structure and assume thermal equilibrium exists between the structure and the pool. Equation 6 is then modified to include the structure.

$$\dot{Q} = \left\{ m_x c_\ell + m_s c_s + \frac{h_{fg} m_v}{T} \left(\frac{h_{fg}}{P v_{fg}} - 1 \right) \right\} \frac{dT}{dt} \quad (7)$$

The addition of the structure decreases the rate of thermal escalation and thus the rate of pressurization. However, the energy required for the sensible heat in the upper half of the container must be supplied by condensing vapor. Therefore, the true vapor superficial velocity within the pool is due to that required for the upward heat sink and that required for pressurization.

$$U_v = \frac{v_v}{A h_{fg}} \left\{ \frac{m_s c_s}{2} + \frac{h_{fg} m_v}{T} \left(\frac{h_{fg}}{P v_{fg}} - 1 \right) \right\} \frac{dT}{dt} \quad (8)$$

If this axial heat sink is included for the glass sensible heat in the microwave oven apparatus, a superficial vapor velocity of 0.07 M/sec is calculated. This would mean that churn-turbulent flow should begin at approximately 70% of all liquid height. This is also in excellent agreement with high speed movie observations. Figure 4 shows a comparison between the predicted (eqn. 7) and measured pressurization rates, and it is seen that there is good agreement with this simple prediction.

The role of the upward heat sink is to increase the superficial vapor velocity within the pool. This was demonstrated by mounting a teflon cylinder, which had been cooled to liquid nitrogen temperatures, at the upper end of the cylinder. With this accentuated upward heat sink, the onset to churn-turbulent flow occurred at about 20% of the all liquid height. This would correspond to superficial vapor velocity of approximately 0.25 M/sec. In a reactor system, the high pressurization rates will force all surrounding structure, both solid and liquid, to appear highly subcooled, which will promote large axial heat sinks, principally in the upward direction. These large heat sinks will limit the pressurization rate and eventually equilibrium will be established between the internal energy generation and the heat losses. However, at equilibrium, essentially all the energy generated is in the form of vapor production, and the pool is even more dispersive. The relative importance of these initial heat sinks can be estimated through eqn. 8. If it is assumed that the sensible heat of the subassembly can walls and a thin plug (0.003M) of steel at the top of the core are the only axial heat sinks, the maximum superficial vapor velocity for full power conditions would be approximately 17 M/sec as compared to the 0.53 M/sec adiabatic value. Consequently, the large initial pressurization rates under core disassembly conditions force the surrounding structure to appear highly subcooled, which in turn, requires large vapor flow rates to maintain the temperature escalation rate. This fact, along with the large adiabatic superficial vapor velocities of such pools leaves little doubt that boiling fuel pools will be monotonically dispersive for full and decay power levels, even under "bottled up" conditions.

NOMENCLATURE

A - cross sectional area
 c_l - specific heat of liquid
 c_s - specific heat of structure
 c_v - specific heat of vapor (constant volume)
 D - pool diameter
 g - acceleration of gravity
 h_{fg} - latent heat of vaporization
 K - Kutateladze number
 m_l - mass of liquid
 m_s - mass of structure
 m_v - mass of vapor
 $\dot{m}_v$ - mass flow rate of vapor
 P - pressure
 p - power level
 $\dot{Q}$ - energy generation rate
 T - temperature
 t - time
 Γ_v - superficial vapor velocity
 Γ_v^* - critical superficial vapor velocity
 v_v - vapor specific volume
 v_{fg} - specific volume increase at vaporization
 x_0 - initial average pool void fraction
 ρ - density, continuous phase
 ρ_H - density, heavy phase
 ρ_L - density, light phase
 σ - surface tension

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2. M. Epstein, et al., "Fuel Freezing Behavior," presented at the Internal Meeting on Fast Reactor Safety and Related Physics, Chicago (Oct. 1976).

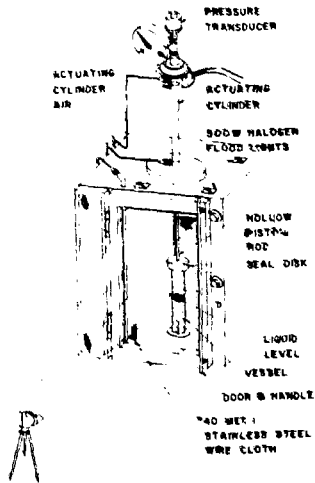


Fig. 1 Experimental Apparatus

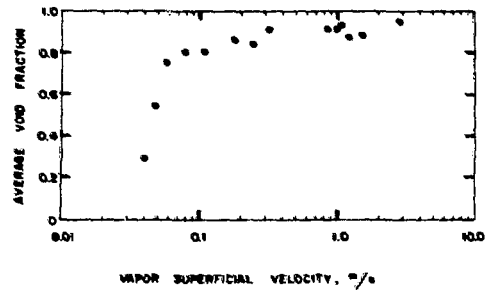


Fig. 2 Atmospheric Pressure Data

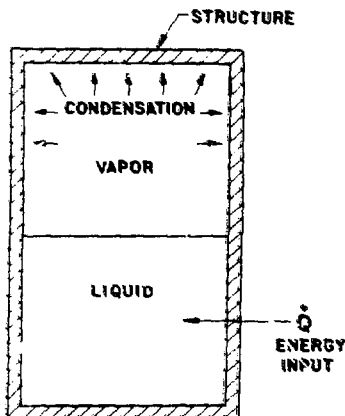


Fig. 3 Constant Volume System

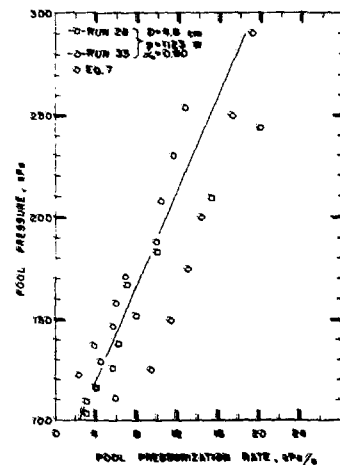


Fig. 4 Pool Pressurization Data

MODELING THE RESPONSE OF FAST REACTOR FUEL TO ACCIDENT TRANSIENTS

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ABSTRACT

Progress in modeling the response of fast reactor fuel to accident transients is reported in four areas: fission gas release from grains and intragranular fuel swelling, fission gas redistribution and internal pressurization in solid fuel, fuel cracking models, and fuel plasticity modeling. Applications of model in the first two areas to comparison of the dispersive potential of fuel in a CRBR hypothetical unprotected loss-of-flow accident with the dispersive potential of fuel in recent experiments are presented. It is concluded that the reactor case presents a greater dispersive potential.

INTRODUCTION

Recent analyses of hypothetical unprotected transient loss-of-flow (LOF) and transient overpower (TOP) accidents performed in support of fast reactor licensing efforts have indicated that fuel response during the early phases of an accident sequence can have a significant impact on accident energetics. While a number of specific questions arise in analysis of fuel response, two major issues appear relative to the energetics of the initiating phase. These issues, simply stated, are early fuel dispersal during a LOF accident, and the response of clad fuel pins to power transients. The latter item includes time and location of cladding failure and subsequent fuel motion.

Early fuel dispersal - a dispersive motion of the fuel in the first sub-assemblies in which conditions permitting fuel motion are realized - appears to be of particular importance in situations in which moderate rates of sodium void reactivity addition are realized. In such cases, early fuel dispersal can result in an energetically benign initiating phase, leading to meltdown into a transition phase. However, if reactivity feedback from early fuel dispersal is insufficient, reactor power will continue to rise as voiding proceeds, leading to the potential for fuel pin failures in unvoided subassemblies. Such failures are potentially autocatalytic. Understanding of the dynamics of fuel dispersal is necessary to identify allowable rates of reactivity addition so that appropriate design measures can be taken. It should be noted that, if the void reactivity addition rate is small or negative, such as is the case in the FTR, early fuel dispersal is not necessary to achieve a non-energetic initiating phase.

Response of clad fuel pins to power transients arises as a major issue in two contexts. One of these, the so-called "LOF-driven TOP," may come about if early fuel dispersal does not terminate the reactor power transient resulting from void reactivity addition. Fuel pin failure in unvoided subassemblies may have the potential to produce autocatalytic reactivity additions if sodium is removed from the core without compensating reactivity removal due to fuel motion. The other context in which response of cladding is a major issue is that of the classical TOP accident, in which the time and location of failure largely determine the initiating phase energetics. Regardless of the context, one needs to understand the mechanisms producing loading on the cladding before and after initial failure, the response of cladding to the imposed loads, and the fuel motion before and after cladding failure.

Rational study of the phenomenology of fuel response to accident transients, and early fuel dispersal in particular, requires definition of a set of relevant technical issues. One such set includes:

- 1) the quantity and distribution of fission-gases and volatile fission products at transient initiation,
- 2) Redistribution of gases and vapors during a transient prior to fuel pin disruption,
- 3) The mechanical response of fuel and cladding to pressure gradients and external forces, including the mode and timing of fuel pin disruption,
- 4) Energy and momentum interchange between gases, vapors, and fuel following disruption,

In the present paper, the status of our concepts and modeling, particularly relevant to items 2) and 3) above, will be discussed, and several results of interest will be presented.

MODELING

Fission-gas Release and Intragranular Swelling

Understanding of the behavior of fission gases and effects of these gases on fuel behavior is fundamental to understanding the response of fuel to an accident transient. Intragranular fission gases can cause swelling of the fuel grains due to bubble coalescence prior to melting, as well as provide a considerable pressure source for dispersal of molten fuel. Intergranular fission gases can cause breakup of solid fuel due to collection of bubbles on grain boundaries and can provide the pressure gradients necessary to disperse fuel particles after breakup. Intergranular gases may also contribute to gross swelling of fuel under certain conditions. Gases released into connected porosity can directly load the cladding, as well as cause indirect loading through swelling and molten fuel pressurization mechanisms.

Modeling of transient intragranular fission-gas behavior is based on the hypothesis that, during an accident transient, gas that is normally present in unstructured fuel as a supersaturated solution of individual gas atoms will precipitate rapidly into bubbles, which migrate, coalesce, and move to grain boundaries. Grain boundary gas can also escape to grain-edge porosity, where it provides a source of internal pressurization of fuel. The FRAS code [1] is used to carry out a probabilistic analysis of bubble coalescence during the transient, the principal result of which is the variation of bubble size and density distribution with time. Bubble size is important because the reduced surface-energy restraint for larger bubbles leads to swelling, and the larger bubbles have lower mobility leading to slower release of gas to grain boundaries. Surface diffusion is assumed to be the dominant bubble migration mechanism, and biased migration up the temperature gradient is found to dominate over random migration in most cases. In the present FRAS code, all bubbles are assumed to be in equilibrium with surface tension and hydrostatic pressure forces.

Based on FRAS code calculations, a generalized fission-gas release and swelling correlation has been developed which is expected to greatly facilitate detailed calculation of fission gas release and swelling in accident-analysis codes such as SAS [2]. The correlation, called PFRAS [3], is based on dimensionless parameters describing the bubble mobility and a dimensionless time. Results calculated with PFRAS agree well with FRAS results over wide ranges of the important variables: temperature, temperature gradient, initial gas concentration, grain size, and hydrostatic pressure. Parametric studies indicate that grain size is a very important variable, both to gas release and swelling. Hydrostatic pressure is important to swelling, but much less important to gas release.

Recent analytical and experimental results indicate that the assumption of instantaneous bubble equilibration will require modification for fast transients, and may be inaccurate for large bubbles even in slow transients. Figure 1 shows the variation of relaxation time based on volume diffusion, for deviation of product bubble radius from the equilibrium radius following coalescence of two identical bubbles. Note that for large bubbles (100 nm), the relaxation times are of the order of a second even near the melting point of fuel. Smaller bubbles show much shorter relaxation times. The implication is that swelling will be overpredicted by FRAS because it is controlled primarily by large bubbles.

Since significant fuel swelling can result from large bubbles on grain boundaries, the FRAS code has been extended to a FRAS-2 version that includes modeling of the grain boundary bubble size-density distribution to predict grain-boundary swelling. Coverage of grain boundaries with gas is also being considered in connection with effects of grain-boundary bubbles on mechanical response of the fuel. Such gas bubbles reduce the tensile load bearing capability of the fuel and, with high gas pressure, may lead to grain boundary separation and promote fuel breakup and dispersal.

Fission-gas Redistribution and Internal Pressurization

Release of fission gas from fuel grains makes the gas available to produce fuel dispersive forces or loading of cladding. An estimate of these gradients and loadings is required, along with an estimate of the quantities of gas in grains, in porosity, and lost from the system prior to disruption. As a first approximation to the problem of gas redistribution, a model based on the theory of flow in a porous medium has been constructed and implemented in a code called POROS. In this model, fission gas is assumed to be released directly from grains into a network of connected porosity according to the FRAS model. Holdup on grain boundaries is not considered. It can be shown that Darcy's law describes the gas flow for conditions of interest in fuel response analysis. The perfect-gas equation-of-state is used. Boundary conditions at the outer radius of the fuel may be a prescribed pressure, simulating an open gap or absence of cladding, or no flow, simulating a closed gap. At the inner boundary, the central void pressure is used if a central void exists; otherwise, a zero gradient at the centerline is assumed. A multi-axial-segment version of the model is available, with the axial segments coupled through a central void or molten fuel and through the external boundary condition. At present, the code does not provide for change in porosity volume during a transient, it is assumed that all porosity is connected, and the temperature history is obtained from the results of other codes or from experimental data.

Appropriate values of permeability of fuel to gas flow present a difficulty in quantitative analysis. In many cases of interest, the nominal fuel densities are close to "zero-permeability" values. However, if zero permeability is assumed, pressure buildup in the pores is very rapid as soon as gas release becomes appreciable. This pressure, plus externally applied stresses, should rapidly open up paths for gas redistribution. For the present calculations, permeability values extrapolated from data of Graham [4] are employed;

the extrapolated values have at least order-of-magnitude uncertainties.

The central void, if present, is assumed to have a uniform temperature and pressure at all times. All fuel at or above the fuel melting temperature is considered fully and instantly melted due to the curve-fit nature of the temperature history. Upon melting, the volume occupied by the melted fuel is increased by an amount determined from both the mass of fuel melted (accounting for porosity) and the change in density of the fuel upon melting. The volume occupied by the gas in the central void is just the total volume of the void minus that of the liquid fuel. It is assumed that the gas within the void is at the same temperature as the molten fuel and its pressure (i.e., the void pressure) is obtained from the perfect-gas law.

Fuel Cracking

Modeling of the response of fuel to accident transients requires consideration of cracking of the fuel, since in many cases of interest the fuel will behave in a brittle manner. Predictions of pellet deflections and resultant cladding loads depend significantly on whether or not cracking is considered. Cracks also provide a means by which molten fuel can escape from the central region of a pin, as well as providing a path for gas to escape. Removal of fuel and gas from the central region of the fuel alters the pressure history in the central zone. Thus, predictions are needed of the number and depth of cracks, and the effects of cracks on fuel-pellet deflections. Radial cracks are of most immediate interest.

Several approaches to modeling of cracks have been reported, including modification of elastic properties and setting the principal stress perpendicular to cracks to zero. To provide a means of assessing the suitability of these approaches and to examine crack propagation in a pellet, a model of cracked fuel which examines the behavior of wedge-shaped elastic segments, bounded on the inside by a ductile zone and on the outside by cladding, has been developed. This model is depicted in Fig. 2. Cracks partially traverse the elastic zone along radii bounding the wedge, and no normal or shear stress is permitted at cracks. Examination of the stresses around the tip of the crack allows prediction of crack propagation. Analysis of stresses within the wedge allows prediction of new cracks. The solution technique employs boundary collocation, and is suitable for repetitive calculation.

One result obtained from this model is shown in Fig. 3, where the maximum and minimum deflections at the outer radius and total crack-opening displacement are indicated as a function of the included angle of the wedge. One sees that, even for as few as 8 cracks (45° included angle), the deflections differ only slightly from the values for an infinite number of cracks. This result suggests that the simple one-dimensional crack model that sets the stress perpendicular to cracks equal to zero may provide a reasonable approximation of the pellet deflections for many cases of interest.

Calculations of stress intensity factors for crack propagation are shown in Fig. 4. At any time during a transient, intersection of these curves with the critical stress intensity factor curve provides a criterion for crack propagation. Results obtained thus far suggest that stable crack propagation may be a suitable model for many cases of interest to accident analysis where pretransient macrocracks exist. That is, fuel having a tensile principal stress in excess of the fracture strength can be considered to be cracked, but cracks would not propagate unstably into lower stress regions.

Fuel Plasticity

In most of the transients of interest, fuel will undergo a temperature change from the normal operating range to melting. At the high temperatures involved, time-dependent fuel deformation and dilatation mechanisms probably occur at very high rates, even in the context of rapid heating. In addition,

the creep and hot-pressing equations thought to apply are highly non-linear in stress. Special numerical techniques are therefore needed to determine the mechanical response of the fuel.

The usual method for combining time-dependent strains with elastic strains in the mechanical analysis of fuel is to assume that during a given time increment, the time dependent strains accumulate at constant stress. At the end of the time increment, elastic strains are superposed with the total plastic strain such that the equilibrium equations and the boundary conditions are satisfied.

Although this algorithm may be suitable for steady-state calculations, difficulties arise in applications to safety-related analysis. During an accident transient, stresses in the hotter regions of the fuel relax on a time-scale considerably shorter than the time-scale of the transient. As the fuel approaches melting temperatures, this creep-relaxation time approaches zero. Unless time steps are chosen smaller than the relaxation time of the hottest fuel, numerical instabilities result from the calculation of unrealistically large creep strain increments. This difficulty has been overcome in some fuel-pin models by simply assuming that the fuel above a certain temperature is always in a relaxed state of hydrostatic stress. However, although it is generally true that such a hydrostatic zone does exist in the fuel at any given time during a transient, elasto-plastic calculations using a general purpose finite element code have shown that the zone boundary depends both on temperature and heating rate. Fuel that has completely relaxed during steady state may respond elastically during a rapid accident transient.

An alternative to calculating plastic strains by assuming that the stress remains constant over a given time increment is to make use of the fact that, even during rapid stress relaxation, total strain changes in the fuel are small. The LIFE fuel performance code [5] makes use of this algorithm. An initial guess for the total strain increments is made assuming that the strain rate is constant over the time interval. The plastic components of the strain are then determined from a finite difference approximation of the constitutive equations along with a Newton-Raphson solution of the resulting nonlinear equations. These plastic strains are used as initial strain contributions in an equivalent elastic problem that satisfies the equilibrium equations and the boundary conditions. The resulting total strains form the next approximation in an iteration scheme.

A second alternative has recently been developed which appears promising for application to transient modeling. The change in the total strain is again assumed to be small over the time interval of interest. However, instead of choosing the strain path as one of constant strain-rate, the path is assumed to consist of two segments. Over one segment the strain is held constant and time varied, while over the other segment, time is held constant and the strain is varied. The fuel response over the first part of the path is determined by an exact integration of the constitutive equations, while the response to the second part of the path is necessarily elastic and can be determined from existing solutions such that at the end of the time interval, the equilibrium equations and the boundary conditions are satisfied. Although this staircase approximation to the strain path is probably less accurate than the linear approximation, it does have the advantage of direct integration of the constitutive equations without the need for solving nonlinear equations or for iteration.

Figure 5 shows a comparison of finite-element calculations (curves) with predictions using the simple algorithm outlined above (symbols). The particular application is for a carbide fuel pin subjected to a steady-state temperature distribution for 10 hours followed by an overpower transient with a period τ of 60 seconds. Even though large time steps were used in the simplified approach, agreement of results for both stresses and displacements is excellent.

APPLICATIONS TO ASSESSMENT OF POTENTIAL FOR EARLY FUEL DISPERSAL

It is useful to postulate a set of requirements for early fuel dispersal. These appear to be:

- 1) Pressure gradients relative to the region into which fuel must move must be available to disrupt fuel as suitable conditions obtain. These pressure gradients must arise from sources internal to the fuel pin, such as fission and fill gases in the central void and porosity due to steady-state irradiation, fission gas retained in grains or released to grain boundaries or porosity during the transient, and fission product vapors from volatile species.
- 2) The pressure sources must not be dissipated in non-dispersive fuel motion. Large scale swelling may dissipate the dispersive potential of the intergranular gas. Equilibration of intragranular gas bubbles would minimize the immediate dispersive potential from this source.
- 3) In order to continue the dispersal process after pin disruption, dispersive forces must be maintained. Thus, excessive net gas separation from the fuel during the early stages of dispersal must not occur, unless other dispersive forces such as sodium vapor streaming can become effective.

There appear to be three modes of disruption of irradiated fuel pins which can be postulated based on test results. These modes are: 1) large-scale swelling of fuel at elevated temperature, 2) "chunk" breakup of the solid shell surrounding a molten core, and 3) breakup of solid fuel into small particles due to grain boundary separations.

Large-scale swelling of irradiated fuel would be favored by high fuel temperatures, long times at temperature, and lack of mechanical constraint. Swelling can come about through the action of both intragranular and intergranular gas bubbles. The rate of swelling depends on the rate at which gas bubbles coalesce due to random or biased migration, the rate at which bubbles grow towards equilibrium size after coalescence, and the rate at which the fuel can deform to accommodate pressures in porosity. These rates tend to increase markedly as temperatures approach the melting point. The requirement for lack of mechanical constraint implies a uniform high temperature in the fuel or extensive cracking of cold fuel. If sufficient bubble coalescence and/or unbalanced pressure due to non-equilibrium bubbles is attained, a breakup into particles by transgranular fracture or grain disruption may occur.

Breakup into chunks of a solid fuel shell surrounding a molten core would be expected in cases in which steep radial gradients exist. As fuel melting starts, an internal pressure will be generated which will tend to move solid fuel radially. If radial cracks exist linking the molten core with the ambient, molten fuel can be ejected through the cracks to partially relieve the pressure. However, if no cracks exist or cracks are plugged by once-molten fuel, pressure can build up and eventually rupture the solid shell, with radial motion of solid and molten fuel.

Breakup of solid fuel into small particles due to grain boundary separation would be favored by rapid gas release from grains to grain boundaries producing high pressure gradients in connected porosity, and by high pressures in grain boundary bubbles. Relatively high fuel density and low permeability to gas flow are favorable conditions. The time-temperature relationship must be such that massive swelling is not produced.

Application of the fuel response models in the FRAS and POROUS codes allows a comparative evaluation of the potential for dispersive fuel motion from intragranular and intergranular gas, respectively. For illustration, five cases have been examined: two calculated voiding-driven power transients in the CRBR, corresponding to two different representations of the reactor in SAS input, and three thermal histories calculated for TREAT experiments, tests L5 [6], F1 [7], and F2 [8]. Test L5 was a loop simulation of a loss-of-flow

accident with irradiated fuel and a power excursion after sodium boiling. Test F1 was a dry capsule test using irradiated fuel run at constant power, while F2 used fresh fuel and incorporated a power burst. In all cases, the calculations are one-dimensional, examining the axial location of maximum temperature. The temperature histories in unstructured fuel are shown in Fig. 6, and radial temperature distributions at the time of initial melting are shown in Fig. 7. (Note that the power transient in F2 occurred after the time under consideration and is not reflected here.)

Results of FRAS calculations are given in Table I. The predicted gas release fraction is relatively low in each case, suggesting that intragranular gas should play an important role in fuel disruption and dispersal. (Note that the quantity of gas available in F2 is an order of magnitude less than in the other cases.) Equilibrium fuel swelling is predicted to be large in all cases, although there is a factor of three spread between extreme values. The calculated value is an upper limit based on instantaneous equilibration, and is an index to the amount of bubble coalescence to be expected. That is, the higher the swelling values, the more coalescences taking place and the larger the quantity of gas per bubble. The mean bubble radius in the final distribution is indicated, from which the final equilibrium pressure can be estimated.

TABLE I. FRAS Analysis of Dispersal Potential

Case	Grain Dia., μm	Release Fraction	Swelling, %	$\bar{R}$, nm	t^* , sec	$T(t^*)$, $^{\circ}\text{C}$	Relax Frac.
CR/N	10	.201	187	273	.11	2380	.09
CR/A	10	.237	246	380	.49	2280	.29
F1	8	.228	122	211	.88	2640	.88
F2	8	.651	6	109	.05	2750	.13
L5	10	.195	95	159	.09	2450	.07

Taken at face value, these results suggest that large-scale swelling would tend to occur (except in F2) and dissipate the dispersive potential. However, consideration of the data in Fig. 1 and the relevant thermal histories reveals that large-scale swelling should not be expected in all cases. The value t^* in Table I is the elapsed time available between achievement of a mean bubble radius of 100 nm and fuel melting, and is the time available for equilibration of bubbles. The value $T(t^*)$ indicates the temperature at which 100 nm mean bubble size is attained. Finally, the fractional relaxation of bubble radius is indicated as an index to the extent of equilibration of the product bubble resulting from coalescence of two 100 nm bubbles at the time t^* .

Quantitative interpretation of these results in terms of dispersive forces is not yet possible. However, a reasonable postulate is that a combination of large equilibrium swelling and small relaxation of bubbles would produce a dispersive system. The large swelling reflects many bubble coalescences while the small relaxation fraction indicates that most of the coalesced bubbles are out of equilibrium, and thus represent a potential dispersive force. On this basis, it appears that both of the reactor cases, labeled CR/N and CR/A, and the L5 experiment have greater dispersal potential than the F1 and F2 experiments. In general, experimental results are consistent with this postulate.

The preceding discussion focused on behavior of intragranular gas bubbles, and effects of these bubbles in causing large-scale swelling or unbalanced

forces within grains due to unequilibrated bubbles. However intragranular bubbles probably will not cause grain boundary separation, nor will they promote breakup into chunks. Such phenomena are more likely to be the result of intergranular gases and the forces resulting from their presence.

Application of the POROUS code analysis allows comparison of different transients and fuel samples with respect to pressure gradients available from intergranular gas. Figure 8 shows the radial distributions of pressure within the assumed porosity network and the central cavity for the various reactor and experiment cases considered above. Table II gives certain input variables of importance. A consistent set of assumptions regarding porosity and permeability were used in the cases presented. Values used were: porosity of 4.4% in columnar fuel and 11.8% in equiaxed and unrestructured fuel, with permeability of 1×10^{-14} mm² in columnar fuel and 5×10^{-11} mm² in equiaxed and unrestructured fuel. Permeability values are subject to very large uncertainties. Furthermore, the effects of cracks have not been considered. Thus, the present results should be regarded as comparative in nature.

TABLE II. Parameters of POROUS Calculation

	CR/N	CR/A	L5	F2	F1
Initial Pressure (MPa)	1.49	1.83	0.101	0.111	0.099
C_0 (10^{20} atoms/cc)	1.38	1.44	1.00	0.193	1.00
Normalized Radius of Equiaxed Fuel	0.655	0.725	0.862	0.795	0.655
Time to First Fuel Melting (sec)	11.5	15.0	11.0	11.0 ^a	13.0

^aMelting not accounted for.

Data for each of the curves in Fig. 8 were calculated by assuming the pin cross section to be at a uniform pressure and have the steady-state temperature distribution at time zero. During the early part of the transient, a constant pressure equal to the initial pressure was maintained at the pin outer radius. When cladding melting was indicated, the boundary pressure p_0 was changed to the ambient value. Figure 8 was plotted from data taken as the centerline reached melting. Both reactor cases show high pressures in the central void and the columnar fuel, which largely reflects constant-volume heating of the gas originally in these regions. The low permeability assumed for columnar fuel essentially permits no gas redistribution in these regions. Pressures in the equiaxed and unrestructured fuel are lower and reflect competition between gas release to the porosity from grains and loss from the porosity to the ambient. Higher pressure in the CR/N case occurs because gas release is more rapid, although smaller in total amount. Since no gas is assumed to be present in equiaxed fuel grains, a local pressure minimum is sometimes found because of slow diffusion from unrestructured fuel. Data for both L5 and F1 show the effect of fission-gas release from grains alone. In these cases, the initial pressure was low enough that appreciable central void pressures are not realized until melting begins. However, gas release is rapid enough to produce larger pressures in the unrestructured and adjacent equiaxed fuel. Test F2 was analyzed assuming no fission-gas release from grains, so the pressures reflect only heating of the original fill gas.

Interpretation of these results in terms of pin disruption modes leads to the conclusion that, because of the higher initial pressure, both CRBR cases

show potential for disruption by chunk breakup well in excess of that in the experiments. As melting starts, the central void pressure in the other cases will build up, but not so rapidly. Because of the uniform high temperatures in the F1 and F2 cases (see Fig. 7), one would not expect pressures as high as those computed since the constant volume assumption is almost certainly violated. (Of course, high pressure could result from rapid heating in F2 after the time of interest here.) Since disruption by particle breakup due to grain-boundary separation should be favored by pressure gradients near the outer radius, the CR/N case would have the most potential for such disruption.

The results presented here illustrate an important modeling application--assessment of the extent to which certain experiments simulate reactor conditions. Overall, one must conclude that the tests considered here are not good simulations of the reactor cases in question, and that the reactor should produce a more dispersive set of conditions. Thus, we have a potential for early fuel dispersal leading to limited initiating phase energetics for the CRBR unprotected loss-of-flow accident.

ACKNOWLEDGMENT

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FIGURES

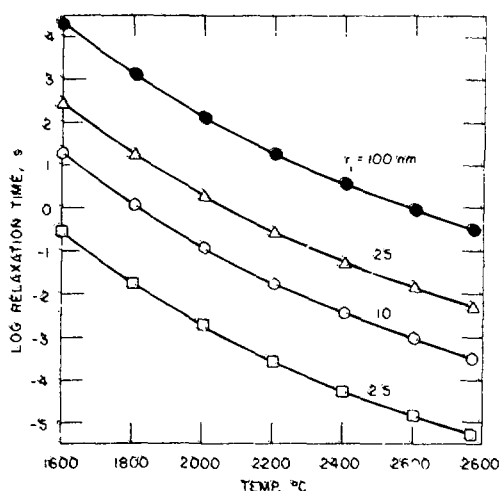


Fig. 1. Bubble relaxation times.

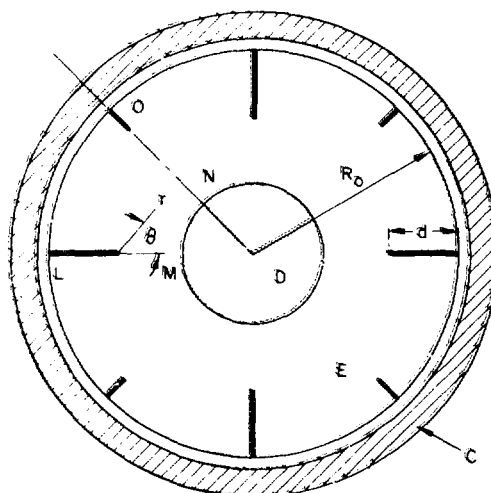


Fig. 2. Fuel-pin crack geometry.

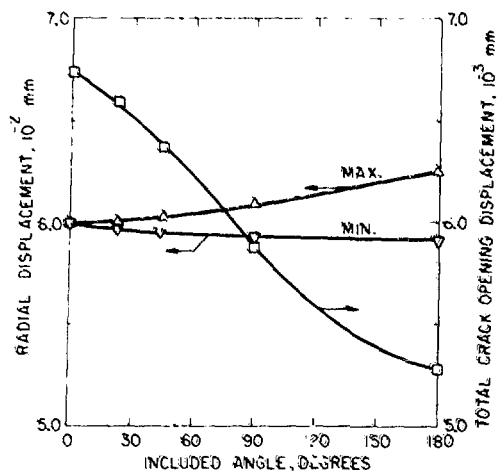


Fig. 3. Cracked fuel displacements.

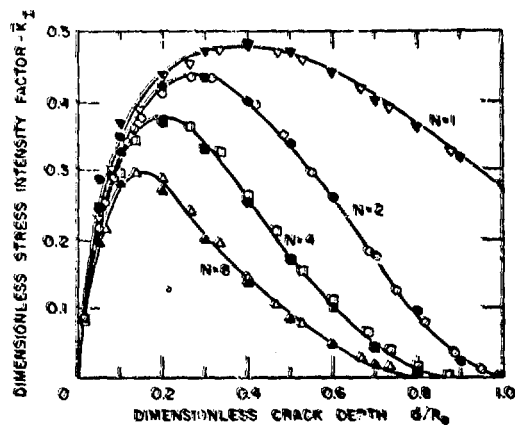


Fig. 4. Stress intensity factors for N cracks.

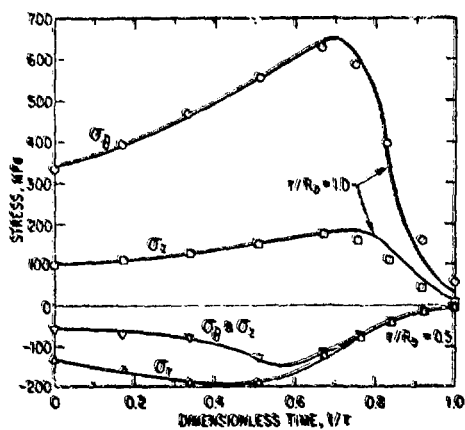


Fig. 5. Transient fuel stresses.

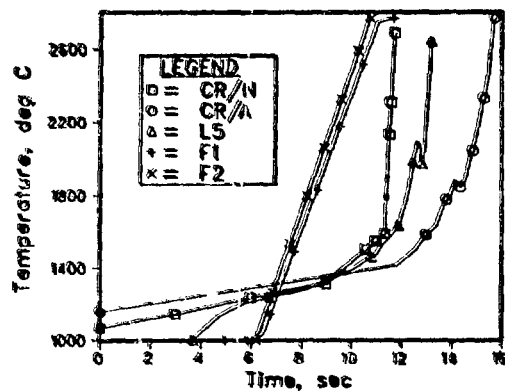


Fig. 6. Thermal histories.

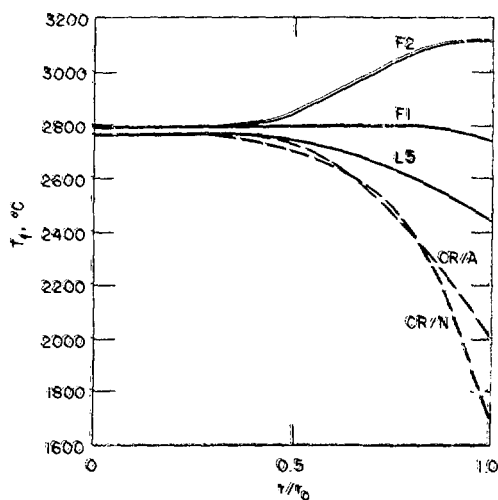


Fig. 7. Radial temperature distributions.

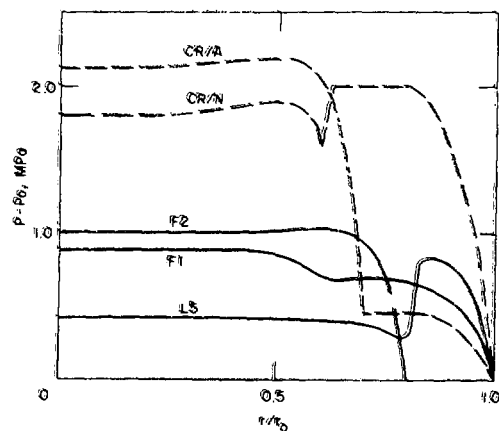


Fig. 8. Radial pressure distributions.

TRANSIENT UNDERCOOLING ANALYSIS OF THE DISPERSAL MECHANISM TEST

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ABSTRACT

The initiating phase response of the proposed Dispersal Mechanism Test to an unprotected loss-of-flow initiator is studied with the SAS3A accident analysis code. This is done in order to study the question of whether or not the initiating phase would have a high probability of evolving into the gradual core meltdown/boilup transition phase scenario which this proposed test is intended to examine. It is found that the initiating phase could well terminate with a power burst driven by the slumping of fresh fuel. This has led to a search for core design changes which would tend to mitigate any such burst.

INTRODUCTION

The purpose of this work is to examine the initiating phase response of a small fast reactor to a postulated loss-of-flow accident. The essential tool used in this analysis is the SAS3A [1] accident analysis code. The transient calculations are used to provide guidelines for an appropriate design of the experimental facility that could help in the understanding of accident phenomena that cannot be treated by present accident analysis codes or observed in present experimental facilities. The phenomena of concern are those which govern the behavior of a growing molten/boiling fuel/steel region in a voided and disrupted LMFBR core.

Recent analysis [2], based on heat transfer and two-phase flow stability criteria in heat generating pools, suggests that the internally heated oxide fuel-steel mixture found in an already-disrupted core is not likely to recompact. Confirmation of this phenomenon requires a minimum portion of a representative reactor core which contains molten fuel and steel. For this reason an experiment consisting of a small self-driven reactor in which a loss of flow occurs has been proposed [3].

The following sections describe some of the design parameters of the reactor design, parametric SAS3A cases that are believed to provide a conservative bound to the true transient behavior, and concluding remarks concerning the usefulness and applicability of this study.

DESIGN OF THE DMT

To establish feasibility of the test it is necessary that the core will operate under prototypic LMFBR conditions. To accomplish this, the current concept requires seven subassemblies, chosen to be structurally identical to the CRBR subassemblies. The seven highly enriched UO_2 subassemblies are surrounded by a blanket and controlled by a suitable reflector. Since it is expected [3] that fuel dispersal will occur even at low power levels, a few days of full power operation are required to assure sufficient decay heat.

Voiding a sodium-cooled core hardens the spectrum and increases the neutron leakage. Because the leakage is dominant in a small core a negative void coefficient of reactivity will result. To introduce a prototypic positive void coefficient a small amount of Lithium is introduced into the coolant. This does not significantly change the thermo-physical properties of the coolant. To provide negative Doppler feedback, some power is produced in the blanket region of the reactor. Table I lists some of the design parameters at full power operation that resulted from reactor physics and thermal-hydraulic calculations.

THE SAS3A MODEL

A four-channel SAS3A model is employed for the transient analysis. The discrete channel structure, illustrated in Fig. 1, coincides with fuel enrichment discontinuities, different pin size regions, and radial subassembly wall boundaries. An iterative sequence of calculations between r-z neutron diffusion and the thermal-hydraulic (SAS3A) steady-state calculations are carried out using the mapping techniques discussed in Ref. 4. This iteration yields a consistent neutronic and thermal-hydraulic description of the core at steady state operating conditions. Once the converged steady state agreement is achieved, a first-order perturbation approximation is used to determine spatially distributed reactivity feedback coefficients.

LOSS-OF-FLOW INITIATING PHASE ANALYSIS

Use of the SAS accident analysis code provides a deterministic description of the transient behavior. Despite the uncertainties present in the phenomenological models, it is believed that examination of several limiting cases bounds the actual response of the proposed DMT to the loss-of-flow initiator.

The SAS3A cases whose descriptions follow were generated by making different assumptions within available SAS3A models regarding the nature of the interactions between fuel, steel and sodium vapor in a voided, disrupted LMFBR subassembly. All cases examined here start from the same steady state conditions and use the same flow coastdown. For convenience, these cases are summarized in two parts: (a) a complete description of the base case, followed by (b) a description of several parametric cases starting from an already-voided core.

(a) Upon initiation of pump coastdown the first changes in reactivity and power are due to fuel and coolant thermal expansion, and negative Doppler feedback. The blanket region (Channel 4) provides approximately 90% of the over-all Doppler coefficient. Until the time of boiling inception, the negative Doppler dominates and keeps the power level below nominal value. Vapor bubble formation first occurs in the inner core (Channel 1) at the boundary between the active core and upper axial blanket. Later stages of coolant

boiling in the remainder of the core are characterized by mild oscillations in power and reactivity as voiding spreads through the core and axial blanket. Positive void reactivity (never exceeding +50¢) and reduced heat transfer from fuel result in rapid clad dryout and subsequent melting of clad. The melting of clad begins just below the upper-blanket-core boundary. The next stage of the initiating phase is governed by clad relocation and the associated reactivity. The forces acting on the molten clad are the following: friction between clad and streaming sodium vapor, friction with solid fuel or solid clad, gravity, and the coolant vapor pressure drop. Upward clad motion is initiated in the central subassembly upon satisfaction of flooding criteria. Approximately one second after the initiation of clad motion, 1/3 of the core cladding in the central subassembly is found refrozen in the upper axial blanket, indicating the formation of steel blockages. After this event, due to the absence of upward streaming vapor, the remainder of the core cladding drains down under gravity and creates steel blockages at the bottom of the core region. The entire core experiences such clad relocation pattern, and the net reactivity associated with this motion is never below -55¢, thus having approximately equal but opposite sign to the coolant void reactivity. Fuel melting begins before the final formation of the steel blockages are complete. Once the melt fraction of the fuel reaches the assumed value of 0.8, the one-dimensional compressible hydrodynamics model, SLUMPY [5], is used to determine fuel motion. The compressible region is assumed to be a homogeneous mixture of fuel and steel containing 7% of clad steel. Coupling between coolant vapor pressures and the slumped (compressible) region is assumed. Fuel collapse in the outer core region produces a prompt critical excursion and initiates fuel slumping in the inner core. Finally, as the fuel temperatures in the outer core reach fuel boiling temperature, fuel vapor dispersal results in large negative reactivity insertion, indicating neutronic shutdown. Due to the time constants for heat transfer to steel, the duration of the prompt critical burst ($\sim 6 \times 10^{-3}$ sec) did not allow for earlier dispersal of fuel by steel vapor. The energy release during this excursion is calculated by SAS3A to be 320 MJ.

(b) Since voiding of the core does not result in prompt critical conditions and the associated reactivity oscillations are observed to be somewhat similar to a larger core with positive void coefficient, further investigations of potential DMT response in an already-voided core are made by varying assumptions regarding interactions and modes of fuel, clad, and sodium vapor dynamics. The assumptions involve the following phenomena: (i) restrictions on clad relocation, (ii) variation of the fuel melt fraction to initiate the formation of a compressible (slumped) region, (iii) coupling between slumped fuel and coolant vapor, (iv) the amount of steel in the steel-fuel mixture of the slumped fuel. Tables II and III list the assumptions and the character of the prompt-critical bursts that resulted in the five cases considered. The detailed scenarios predicted by SAS3A for these parametric cases do vary somewhat. Restraining clad motion, and thus eliminating its negative reactivity contribution, enhances early fuel melting and motion. Variations in the melt fraction required to initiate fuel motion and in the assumed mode of fuel-sodium vapor interaction result in fuel motion ranging from a slow plastic fuel collapse to more dispersive motion. However, all five cases eventually result in a prompt critical burst initiated by a small amount of fuel compaction which terminates due to fuel-vapor-induced fuel dispersal. The heat transfer time constants do not allow enough time to vaporize the mixed-in steel during prompt critical reactivity levels, and no significant steel vapor pressures exist during slumping. The energy releases during the fuel motion range from 320 MJ to 650 MJ, and time scales associated with these energy releases range from 2.4 to 7.5 ms. Fig. 2 illustrates a typical power and reactivity history during the entire transient.

On the basis of the SAS3A calculations described above, it can be seen that the combination of the small Doppler coefficient, the small highly-enriched core, and the coherent behavior inherent in a seven-subassembly core result in the DMT core design just analyzed being quite sensitive to initial fuel motion. Because such initial motion may well be non-dispersive in fresh fuel, it is difficult to preclude the occurrence of the burst which SAS3A predicts. Such a mode of initiating phase termination would not be prototypic of the transition phase entry modes predicted by SAS3A for reactors like FTR and CRBR.

Two possible core design modifications suggest themselves from study of the SAS3A calculations. More voiding, cladding relocation and fuel motion incoherence could be added by utilizing a larger number of smaller subassemblies orificed and enriched to add both power and power-to-flow incoherence. Also, modifications could be made to increase the magnitude of the Doppler reactivity. Because of a desire to use prototypic subassembly sizes in the DMT, the first possible design change is not considered further in this paper. The next section describes the SAS3A-predicted transients that result in a modified design which has lower core fuel enrichments and a larger Doppler coefficient.

PRELIMINARY TRANSIENT ANALYSIS OF THE MODIFIED DESIGN

Reactor physics computations predict that by (i) adding BeO to the radial blanket, (ii) increasing the size of the radial blanket, and (iii) reducing the ^{235}U content of the core, the Doppler coefficient is increased by a factor of three and the core fuel worths decreased by 10%. A series of SAS3A loss-of-flow cases have been run for a DMT design based on the above design changes, using the same range of assumptions as were applied to the reference design analysis. Prior to fuel motion initiation, only slight differences are observed between the predicted behavior of the reference and modified designs. In the modified design, fuel motion, once initiated, again dominates the reactivity history. However, because of the lower fuel worths, higher degree of noncoherence and the somewhat more sluggish behavior brought about by the larger Doppler coefficient, there is time for steel vapor to be produced in the slumping channels in sufficient quantities to prevent prompt criticality if it is assumed that some small fraction (7%) of the cladding remains on the fuel column and can mix intimately with the fuel upon fuel motion initiation. On the other hand, a more conservative assumption of no fuel/steel mixing in SAS3A results in the same type of power burst terminated by fuel-vapor-induced dispersal as is predicted for the reference design. Thus, although some of the sensitivity has been removed in the modified design, the non-prototypic fuel motion sensitivity is still sufficiently significant in the modified design to potentially jeopardize the success of the proposed experiment.

CONCLUSIONS

(a) Limitations of this SAS3A Study

The SAS3A code has evolved into a very large and complex code comprised of a number of detailed models. A substantial effort has been devoted toward providing experimental verification of these models. A general agreement has been established in certain areas between SAS models and isolated small-scale experiments. For example: (i) sodium boiling inception and liquid slug oscillations after core voiding are found to agree with previous experimental results [6]; (ii) melting and relocation of molten cladding has been verified by the TREAT R4 experiment [7], where formation of clad blockages above and

below the core were observed; (iii) collapse and dispersal of fresh oxide fuel has been observed consistently in the TREAT-L series experiments [8]. However, in applying SAS3A to the analysis of a reactor like the DMT, significant uncertainties remain which limit the usefulness of its predictions. For the DMT, the most significant uncertainties lie in the areas of clad blockage formation, the degree to which these would limit sodium vapor flow rates, the interaction of sodium vapor with the unclad and possibly slumping fuel column, and the possibility of fuel/steel mixing leading to a steel-vapor-induced fuel dispersal.

(b) Implications of this Study on the DMT Experiment

In view of the modeling limitations and uncertainties, SAS3A certainly cannot provide a single definitive loss-of-flow scenario for the DMT. However, it is quite useful in pointing out areas of sensitivity in the scenario, as has been discussed, and can provide a reasonable bound on potential energetics. The SAS3A analyses have identified initial fuel motion as being the most important phenomena in the DMT loss-of-flow scenario. If initial fuel motion is non-dispersive (as it could well be with fresh fuel) and if a sodium-vapor-induced or steel-vapor-induced fuel dispersal is not quickly initiated, it is difficult to absolutely preclude the occurrence of a relatively energetic burst as the mode of initiating phase termination.

Such extreme sensitivity to the details of early fuel motion is, of course, unique to a small highly-enriched reactor like the DMT. Certainly, the loss-of-flow scenarios predicted by SAS3A for the FTR and CRBR do not exhibit this sensitivity. Because the occurrence of such a burst would make it less likely that the desired transition phase would ensue, the reference design is probably unsatisfactory. The preliminary SAS3A analyses discussed above indicate that design modifications can be made which lessen the fuel motion sensitivity. Should interest in the proposed DMT continue to exist, work on further enhancing the Doppler and reducing this sensitivity could result in a satisfactory design.

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TABLE I

Design Parameters (reference design)

Total Reactor Power	50 MW
Coolant Flow Rate	630 gm/cm ² sec
Peak Linear Power	360 watts/cm
Peak Fuel Temperature	2040°C
Coolant Inlet Temperature	316°C
Coolant Temperature Rise	166°C
Li Concentration in Coolant	14%
Enrichments (U235%)	
Inner Core	78.6
Outer Core	92.5
Radial Blanket	8.0
Effective Neutron Lifetime	2×10^{-7} sec
Doppler Coefficient	-0.00175
Void Reactivity	+0.50\$

TABLE II

Transient Fuel Motion Assumptions

<u>Assumption</u>	<u>Case Number</u>				
	1	2	3	4	5
clad motion	yes	yes	no	no	yes
% of clad steel in fuel/steel mixture	7	0	70	0	7
fuel melt fraction to *initiate fuel motion	0.8	0.1	0.8	0.1	0.7
interaction of slumped fuel with coolant vapor	no	yes	yes	no	no

TABLE III

Summary of Transient Results

Event	Case Number					
	1	2	3	4	5	1 ^a *
Initiation of fuel motion (sec.)	22.7	18.4	19.1	17.7	22.0	19.1
Normalized power at fuel slumping (P/P ₀)	0.530	0.695	1.657	1.550	0.669	0.882
Reactivity at slumping (β)	-0.343	-0.269	+0.244	+0.253	-0.205	-0.308
Prompt-critical time, t_0 (sec.)	22.93	15.55	19.43	17.80	22.12	-----
Ramp rate at t_0 (β /sec.)	18	16	30	13	24	-----
Peak power level (P/P ₀ x 10 ⁻³)	4.97	7.76	15.3	6.02	11.2	0.003
Total energy release (joules x 10 ⁻⁹)	1.374	1.485	1.705	1.470	1.605	1.006
Burst energy (joules x 10 ⁻⁹)	0.320	0.565	0.638	0.520	0.579	-----
Burst duration (sec. x 10 ⁻³)	5.3	2.4	4.0	7.5	6.0	-----

*Case 1^a corresponds to the improved design with identical assumptions as in Case 1.

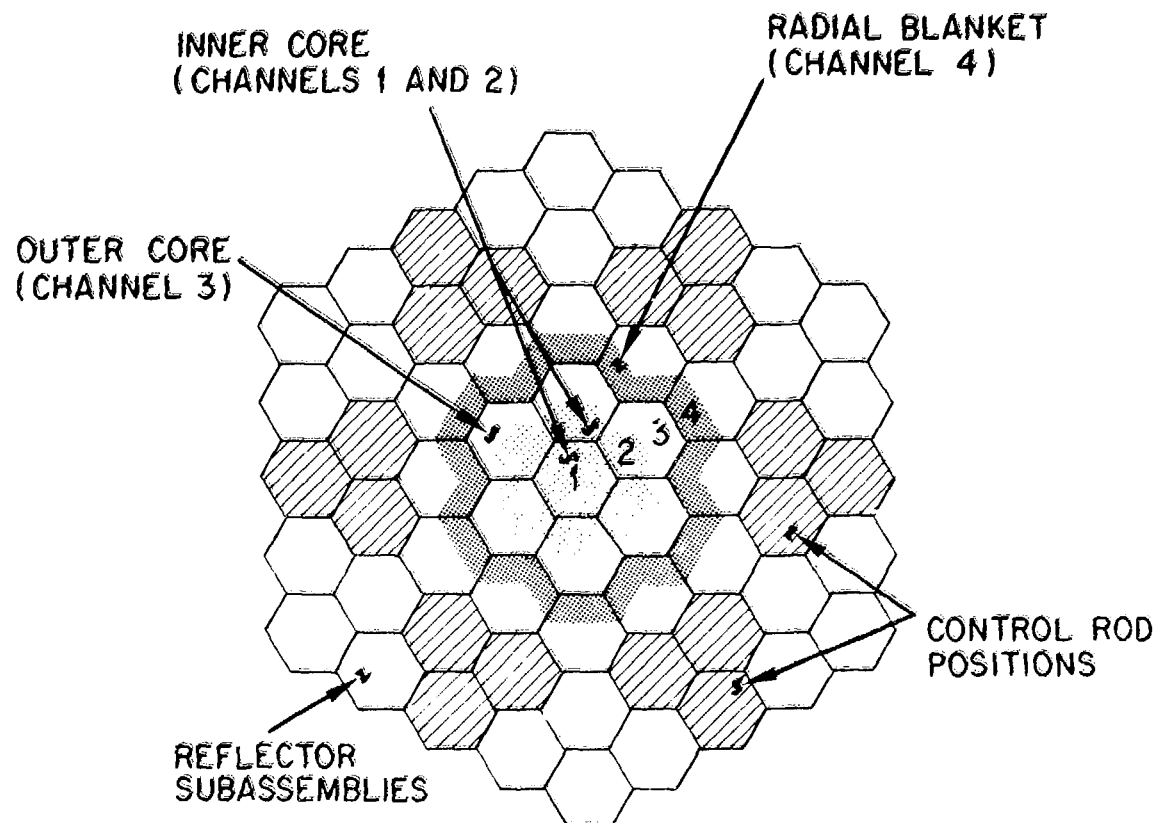


Fig. 1. The SAS3A channel structure of the DMT

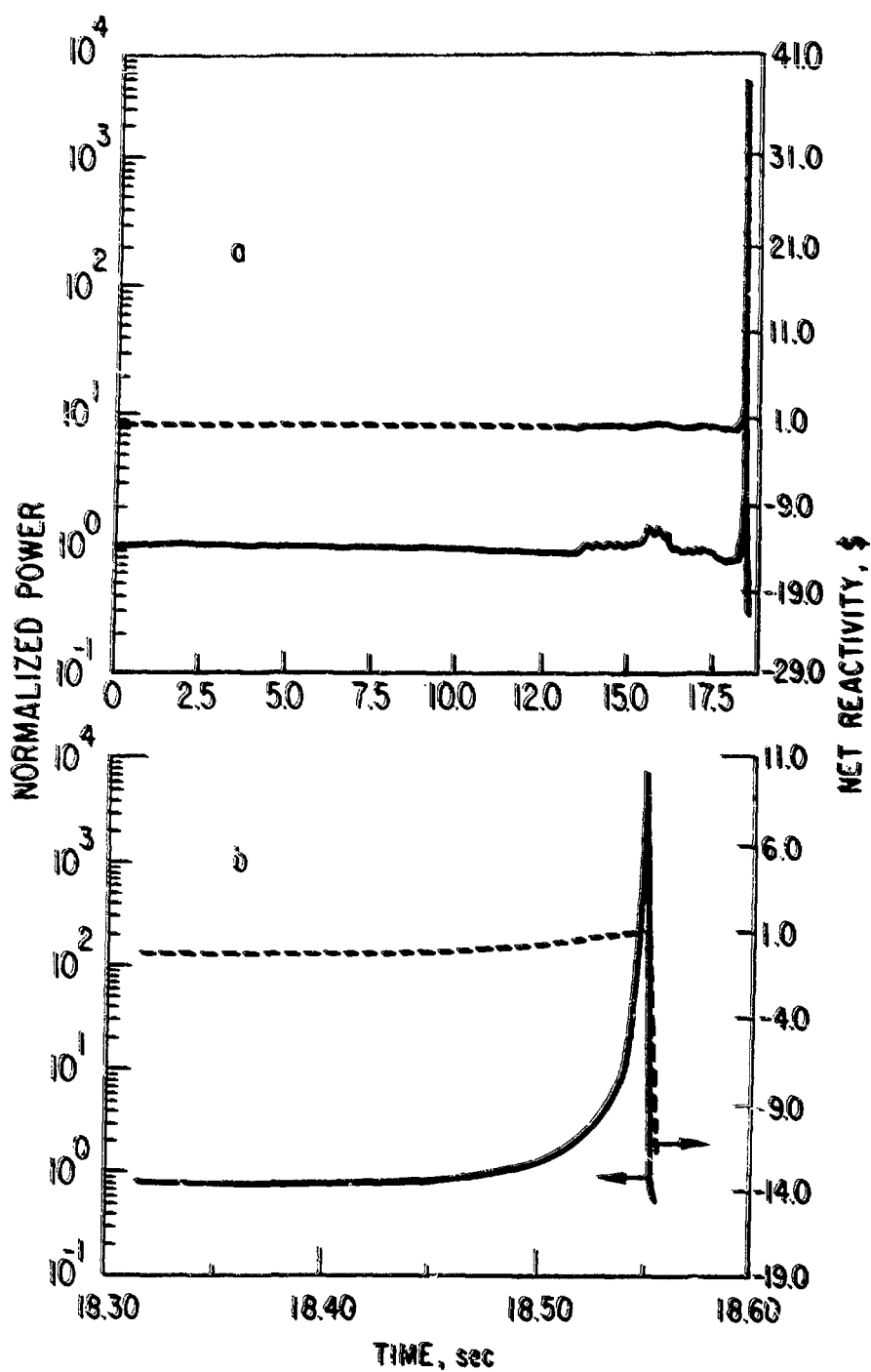


Fig. 2.
Typical power and reactivity during (a) the entire initiating phase and (b) the final prompt critical burst (reference design).

EXPERIMENTS ON PRESSURE-DRIVEN FUEL COMPACTION WITH REACTOR MATERIALS

by

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ABSTRACT

Pressure driven, fuel-recompaction experiments have been conducted with reactor materials in 37-pin wire wrapped bundles. The experiments have been performed with the bundle full, half full, and empty of sodium coolant. Measurements and post-test examination of the bundles shows that the sodium coolant had no significant effect on the final disposition of the fuel. Consequently, no fuel recompaction forces were observed.

INTRODUCTION

Following a postulated unprotected, pump coastdown in a liquid metal fast breeder reactor, a scenario of events would describe (a) coolant voiding of the reactor subassemblies, (b) clad melting and relocation within the subassembly, and (c) fuel melting and subsequent fuel ejections. During such an ejection or dispersal, one must consider the possibility that molten uranium dioxide could travel into the upper gas plenum region of the pin bundle where it could contact, and perhaps entrain, limited amounts of liquid sodium. Under such conditions, it is theoretically possible that the sodium could vaporize and provide significant local pressurization to reverse the direction of the fuel and drive it back into the core region. If such motions are physically reasonable, they would play an important role in analyzing the reactivity ramp rates within the boiling liquid pool and the resultant system energetics.

In order to provide guidance in the understanding of such complex phenomena, small scale experiments were carried out with reactor materials. Molten uranium dioxide was generated by a thermite type reaction between metallic uranium and molybdenum trioxide and was then injected into a 37-pin wire wrapped pin bundles. In the first experiment, the test bundle had just been partially drained of its sodium coolant leaving a thin film of sodium still

*Work performed under the auspices of the U. S. Energy Research and Development Administration

wetting the pin surfaces and the wire wraps. The second test was conducted with a completely dry bundle.

EXPERIMENTAL APPARATUS

An isometric view of the experimental apparatus is shown in Fig. 1. The thermite injector was located directly over the 37-pin bundle which was coupled to a sodium reservoir and gas plenum region in a "U" tube configuration. Primary instrumentation for this experiment consisted of temperature measurements throughout the facility, piezoelectric pressure transducers at four locations on the test bundle, strain gauge pressure transducers on both the thermite injector and the gas plenum region of the "U" tube (Fig. 2), and voltage taps along the length of the hexagonal test bundle (Fig. 2). Such measurements give a good profile of the initial conditions prior to the injection and sodium movement resulting from these pressures. External trace heaters were provided to increase the bulk sodium temperature to the desired level for a given test.

As shown in Fig. 1, the pins were 0.584 cm OD x 0.038 cm wall tubing and each pin was wrapped with an 0.142 cm diameter wire wrap over its entire length. The ends of the tubes which were exposed to the injector were open, and the opposite ends were sealed with 0.584 cm diameter steel plugs 2.5 cm long. Each pin was then welded into a grid at the bottom of the bundle. The thickness of the hexagonal can was 0.103 cm.

SEPARATE EFFECTS INJECTOR TESTS

The thermite process, which was used to generate the molten ceramic fuel, is an exothermic reaction between metallic uranium and molybdenum trioxide which produces UO_2 and metallic molybdenum plus a sufficient amount of energy to increase the temperature of the reaction products to approximately 3470°K. The thermite mixture was ignited by a small nichrome heating wire. The proportions of the various components used in this study are given in Table I along with the free volume, mixture volume, and resulting void fraction within the injector housing. The uranium powder used in these tests contained approximately 10% by weight of UN which released N_2 during the reaction. This was verified by injecting the reaction products into a closed containment and sampling the gaseous products. One atmosphere of argon cover gas was also in the injector prior to injection. Consequently, this would also provide additional pressurization as the combustion progressed.

Several separate effects tests, including the gas sampling tests, were conducted to determine the completeness of the thermite reaction and the flow configuration of the exhausting products. In one of these tests, three tungsten element filament bulbs were calibrated with an optical pyrometer for filament temperatures of 3070, 3270, and 3470°K. These bulbs were then located in close proximity to the exhaust of the injector and the ejection was recorded on high speed (1000 fps), daylight, photographic film. This enabled a comparison between the calibrated temperatures of the tungsten element bulbs and the ejected reaction products. These tests show that the mixture temperature was approximately 3470°K and that the exhausting products rapidly expand as they exit from the injector throat which indicates a compressible behavior. This compressible behavior is a result of a high pressure, two-phase, liquid-gas, state of the reaction products in the injector. The calculation for the two-phase, two-component critical flow rate, which should be experienced by such a system, shows that systems with initial pressures of approximately 50 bars, as measured in this experiment, will certainly experience a compressible flow limitation. This is in agreement with the observations of these separate

effects tests.

The metallic molybdenum, which is a by-product of the thermite reaction, provides a simulation of the high temperature, molten stainless steel that would be present in a reactor environment under these postulated accident conditions.

TABLE 1
Injector Characteristics

	Component	Mass, gm	Gram Moles
	UN	48	0.19
	U	432	1.82
	MoO ₃	200	1.39
Fuse	U	5	0.02
	CrO ₃	2	0.02
		687	

$0.19 \text{ UN} + 1.84 \text{ U} + 1.39 \text{ MoO}_3 + 0.02 \text{ CrO}_3$

$2.07 \text{ UO}_2 + 1.39 \text{ Mo} + 0.02 \text{ Cr} + 0.09 \text{ O}_2 + 0.1 \text{ N}_2$

Free Volume - 68 cc

Mixture Volume - 68 cc

Void Fraction - 0.75

TEST CONDITIONS

The sodium within the "U" tube and the hex can were heated to approximately 920°K with the system trace heaters. During this heat up period, which required about five hours, the entire system was under an argon gas blanket at approximately 0.1 MPa. At various times during the heat up period, sodium was partially drained from the test bundle into the supply tank and then pumped back into the bundle in order to check the sequencing and integrity of the system voltage taps. At a sodium temperature of 920°K, the power input from the trace heaters equaled the heat losses of the entire system. Sodium was then added from the dump tank until the leak detector on the overflow system indicated that the test bundle was full. The valve in the fill line was closed, the dump tank was evacuated, and the fill line was reopened to allow partial drainage of the sodium within the bundle.

EXPERIMENTAL RESULTS--UPI NO. 1

Figure 3 shows the measured injector pressure just prior to the injection, which was approximately 5 MPa, and the rapid blowdown after rupture of the diaphragm. The time scale was established as zero time being coincident with the beginning of injection and it is common for all figures. This pressure level was in good agreement with the measured behavior in the separate effects discussed above, and consequently, one should expect similar fuel temperatures and flow patterns during the injection.

The results of the piezoelectric transducers are illustrated in Fig. 4. All three transducers record the rapid pressurization to approximately 6.9 MPa at about 7.5 msec into the event. This pressurization is sustained for about 500 msec and then quickly relieved. The rates of pressure rise and decay are indicative of impact pressures resulting from the impingement of the two-phase molten stream upon the sodium interface. This is confirmed by the results of the voltage taps shown in Fig. 5. It will be recalled that the end of the pins which faced the injector were open and allowed fuel to pass down through the inside of the pins. Since UO_2 is electrically conductive in its molten state, this transport path would result in a decrease in the local electrical resistance and a resulting decrease in the voltage tap output. As fuel flowed through the coolant channels, it destroyed the sodium film which coated the pins and wire wraps and resulted in a net increase in the voltage tap output. These two behaviors allowed a resolution of fuel motion both inside and outside of the pins, and this behavior is graphically illustrated in Fig. 5. As shown in this figure, the initial sodium interface, is approximately 17 cm from the injector. The voltage tap measurements indicate that the molten reaction products contact the sodium interface at approximately 7.5 msec after injection which is in excellent agreement with the pressure transducer measurements and interpretation of impact pressurization.

Impact pressurization can also be calculated if the density and velocity of the reaction products are known just prior to contact with the sodium interface. The injector itself is schematically represented in Fig. 6 along with the mixture properties just prior to injection. As illustrated, the measured pressure was 5.2 MPa, the temperature 3470°K, and the average void fraction 0.75. Because of the argon fill gas and the nitrogen released during the combustion the reaction products present a high temperature, high pressure, two-component, two-phase, liquid-gas system. The flow rate, and consequently the expansion, from the injector is thus limited by the compressible behavior of the mixture. A simple analysis, similar to that presented in ref. 1, shows that the critical pressure ratio for this two-component system is expressed by the following transcendental equation

$$\gamma \frac{\gamma+1}{\gamma} = \frac{2}{\gamma} \left\{ \frac{\frac{1-\alpha_o}{\alpha_o}(1-\eta) - \ln \eta}{\left[\frac{1-\alpha_o}{\alpha_o} + \frac{1}{\eta} \right]^2} \right\} \quad (1)$$

Since N_2 and O_2 are the most prevalent gases, it will be assumed that the isentropic exponent, γ , is equal to 1.4 for the resulting mixture of gases. The trial and error solution of this equation shows the critical pressure ratio to be approximately 0.4, which corresponds to a throat pressure of 2.1 MPa, a void fraction of 0.85, and a mixture velocity of 48 m/sec. After the injector throat, the expansion is unbounded and the one-dimensional velocity change, as the system depressurizes to ambient pressure, can be calculated by

$$u - u_e = \frac{p_e - p_{\text{atm}}}{G_e} \quad (2)$$

After this expansion the local conditions are a pressure of 0.1 MPa, a void fraction of 0.98, and a mixture velocity of 75 m/sec. In this void fraction regime, the system is undoubtedly in a mist flow regime and the sonic velocity for such a two-phase regime can be expressed as²

$$C_{TP} = C_g \sqrt{\frac{2\alpha}{1+\alpha}} \quad (3)$$

This corresponds to a local sonic velocity of approximately 656 m/sec. The impact pressure upon contact can be calculated by

$$\Delta P = (1 - \alpha) \rho_l C_{TP} u \quad (4)$$

This calculation, with the parameters described above, results in a pressure of 9.8 MPa which is in good agreement with the measured values given the uncertainties of calculating the two-phase behavior of this jet. To illustrate these uncertainties, a void fraction of 0.986 as opposed to 0.980, results in a predicted impact pressure of 6.9 MPa which is the measured value. If the measured fuel velocity from Fig. 5 is used in Eq. 4, with a void fraction of 0.98, the calculated impact pressure is 5.2 MPa. These calculations demonstrate that impact pressures, equal to the measured values, are possible and probable in this system.

A final check which can be made on the existence of impact pressures is their duration. These pressures should be sustained during the time it takes a compression wave to travel to a free interface and the rarefaction wave to return. The distance from the impact location to the free interface on the other side of the manometer configuration is 66 cm. Assuming the velocity of sound in the sodium is 2300 m/sec, the duration time should be 600 μ sec which is in good agreement with the measured duration times seen by the piezoelectric transducers.

Consequently, the existence of the impact pressure has been demonstrated by three independent mechanisms, the voltage taps, the injector compressible characteristics, and the time for acoustic relief in the sodium liquid and these produce consistent results with the measured pressures. The second impact at approximately 10.5 msec into the transient results in a slight additional acceleration of the sodium interface leaving the test bundle.

The pressure in the gas space on the other side of the "U" tube configuration is shown in Fig. 7 for the first 70 msec into the transient which includes the largest pressurization by far in this gaseous zone. Given this pressurization measurement, the work done on the gas can be evaluated from

$$W = \int_{P_1}^{P_2} P dV = \frac{-P_1 V_1}{\gamma - 1} \left[\left(\frac{P_2}{P_1} \right)^{\frac{\gamma-1}{\gamma}} - 1 \right] \quad (5)$$

The cover gas in the "U" tube was argon which has an isentropic exponent of 1.6. The cross-sectional area of this gas space is 21.7 cm² and its initial length is 31.8 cm or an initial volume of 689 cm³. Assuming an isentropic path for the gas, the measured pressurization results in 180 J of work done on the gas, which except for a small amount of dissipation, is equal to the mechanical energy delivered to the sodium coolant by the impacting fuel. If on the other hand, we evaluate the kinetic energy of the fuel upon contact using the measured velocities illustrated in Fig. 7,

$$K.E. = \frac{1}{2} m v_1^2 \quad (6)$$

it is found that the reaction products have a kinetic energy of 0.8 J/g. For an injection quantity of 0.5 kg this yields an energy of 400 J, or over twice the work done on the gas space. The conclusion from this measurement is that the sodium did not even absorb the initial kinetic energy of the fuel stream. That remaining energy was dissipated in frictional and ablative irreversible losses on the fuel pin surface.

DETAILED VOLTAGE TAP RESULTS

Figure 8 illustrates the calculated void fractions within voltage tap increments 5-6, 8-9, 9-10, and 10-11 as a function of time into the transient. It is seen that the original behavior following impact of the fuel on the sodium interface is to quickly void the channel to a void fraction of approximately 98%, and this increment remains voided until the gas plenum is pressurized and the sodium is forced back into the bundle. For this case, reentry of sodium occurs at approximately 83 msec and local cooling is provided for approximately 15 msec before the second voiding occurs. The coolant can be observed to enter the bottom of the bundle (E10-11), proceed up the bundle to the approximate region of final disposition of the fuel (E5-6), and the resulting expulsion from this point back out of the pin bundle with subsequent reentry approximately 80 msec later. The essential point of these measurements is that the sodium motion is quite benign and is the result of comparatively slow quenching of the reaction products and melted cladding. This second expulsion provides a very small pressurization of this gas plenum which lasts for approximately 70 msec and is relieved by movement of the sodium back into the pin bundle. The frequency of the small amplitude oscillations which follow can be predicted by assuming that this gas space oscillates as a simple Helmholtz cavity.

PHYSICAL EXAMINATION OF THE TEST ASSEMBLY

Following a test, the system was disassembled and the test section was removed for examination. There was very little attack of the hexcan wall but the fuel pin cladding in the upper regions had been completely melted. It is interesting to note that the cross-sectional area required for the free-expansion of the fuel jet as illustrated in Fig. 6 would have a diameter of approximately 4 cm which is the internal dimension of the hexcan bundle. Therefore, this high temperature mist would expand and encompass all of the 37 pins but not really provide much heat source to the outer wall since the outer expansion of this jet would be the coldest and lowest density portion of the expansion. While qualitative, this observation is certainly in agreement with the experimental results. The fuel basically solidified in the central region of the pin bundle which is certainly in agreement with the voltage tap observations.

CONCLUSIONS OF UP1 TEST #1

Given the results discussed above, the principal conclusions of the tests were:

1. Pressurizations measured by the piezoelectric transducers were the result of impact pressures between the fuel mixture and the sodium coolant.
2. Mechanical work done by the fuel on the sodium was less than half the initial kinetic energy of the fuel stream itself.
3. The sodium coolant played a negligible role in determining the final disposition of the fuel.
4. No forces capable of reversing the flow of the fuel and recompacting it into the core region were observed.
5. The only apparent role of the sodium coolant was the final quenching of the molten fuel-clad mixture.

UPPER PLENUM INJECTION TEST #2

Since the conclusion of the first upper plenum injection test was that the sodium played no major role in the final disposition of the fuel, which implicitly means that the coolant could not provide significant pressures for fuel recompaction, it was decided to run an identical test with no sodium present. The facility was basically the same as that shown in Fig. 1 with all the sodium apparatus removed. The final disposition of the thermite mixture in UPI #2 is almost identical to that of UPI #1, which confirms that the sodium does not play a significant role in determining the final disposition of the reaction products.

UPPER PLENUM INJECTION TEST #3

A test was conducted with reactor materials in which the initial sodium level was at the top of the pin bundle. Detailed results of this experiment are currently being analyzed in the same manner as UPI test #1. However, the post-test examination of the bundle showed that the final fuel disposition was the same as that observed in the first two experiments.

SIMULANT FLUID EXPERIMENTS

As discussed above, the thermite mixture used in these tests release a considerable amount of gas. Consequently, the flow regime of the exhausting reaction products was one of a high temperature mist. Simulant fluid tests are underway with Freon-11 and mineral oil and Freon-22 and mineral oil to study the importance of the initial fuel flow regime. With these systems, the initial flow regime can be varied from all liquid, to bubbly, and finally to mist flows.

NOMENCLATURE

c_g - speed of sound, gas

c_{TP} - speed of sound, two-phase mixture

G_e - mass flow rate per unit area, exit

m - mass

P_{atm} - atmospheric pressure
 P_e - pressure, exit
 ΔP - pressure difference
 u - velocity
 u_e - velocity, exit
 u_f - velocity, fuel
 V - volume
 W - work
 α - void fraction
 α_0 - void fraction, initial
 γ - isentropic coefficient
 η - critical pressure ratio
 ρ_l - density, liquid

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1. R. E. Henry and H. K. Fauske, "The Two-Phase Critical Flow of One-Component Mixtures in Nozzles, Orifices, and Short Tubes," Trans. ASME, J. Heat Transfer, 93-C, 179 (1971).
2. R. E. Henry, M. A. Grolmes, and H. K. Fauske, "Pressure-Pulse Propagation in Two-Phase, One- and Two-Component Mixtures," ANL-7792 (1971).

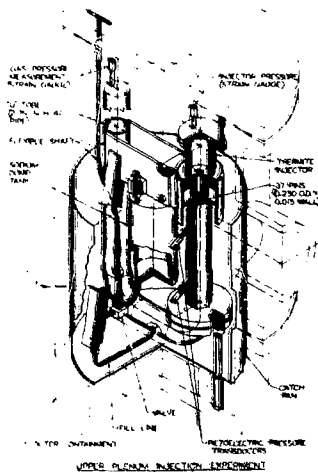


Fig. 1 Experimental Apparatus

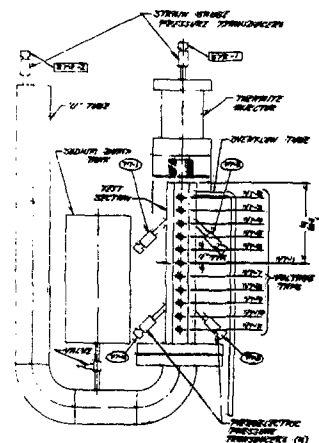


Fig. 2
Pressure and Sodium Void
Instrumentation--Upper Plenum
Injection Experiment No. 1

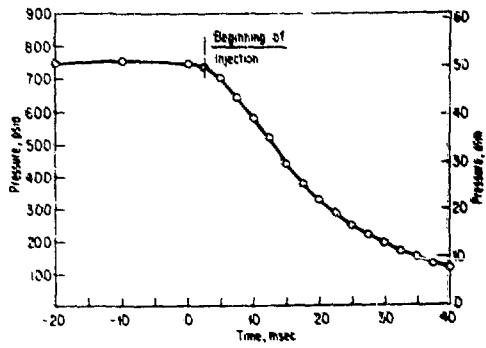


Fig. 3 Measured Injector Pressure

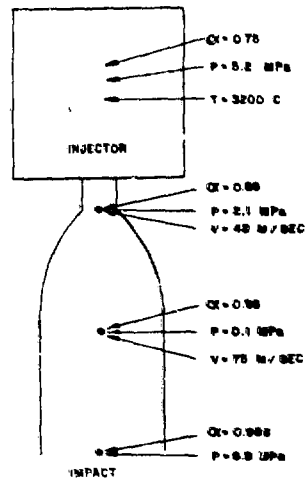


Fig. 6 Injector Schematic

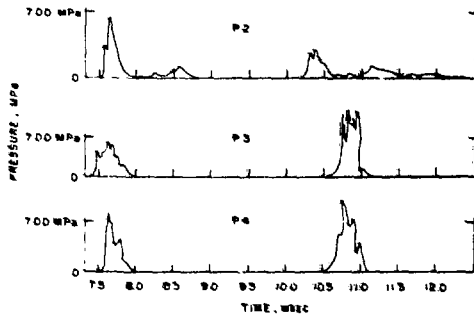


Fig. 4

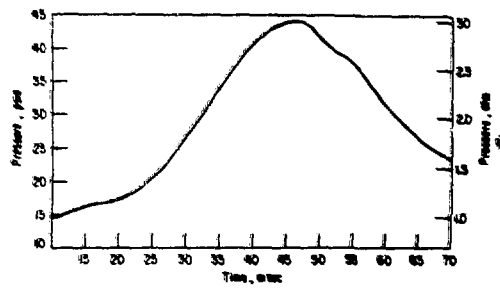


Fig. 7 Measured Gas Space Pressure

Piezoelectric Pressure Measurements

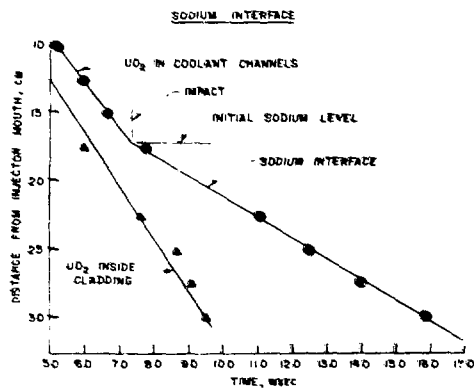


Fig. 5 Sodium Interface Behavior

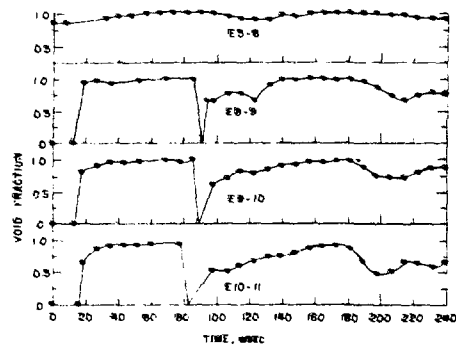


Fig. 8 Composite Voltage Tape Record

EQUATIONS OF STATE FOR LMFBR MATERIALS*

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ABSTRACT

Tabular equations of state are constructed for use by the SIMMER code in its investigations of LMFBR safety in hypothetical core disruptive accidents. This paper describes a correction to a sodium equation of state reported earlier and presents the revised tabular data. Need for further work on the melting transition is suggested.

Tabular equations of state for sodium, steel (iron), and fuel (UO_2) are constructed for use by the SIMMER [1] computer code by a treatment, somewhat similar to a virial expansion, that we have termed an "explicit cohesion" expansion. Traditionally, in a dilute system the deviation of pressure from the ideal value is described through the virial coefficients $B(T)$, $C(T)$, etc., in some expression equivalent to

$$P(\eta, T) = RT \left[\rho_0 \eta + B(T)(\rho_0 \eta)^2 + C(T)(\rho_0 \eta)^3 + \dots \right], \quad (1)$$

where the compression $\eta = \rho/\rho_0$ is the ratio of density to the normal zero-temperature density of the solid and R is the gas constant divided by molecular weight. In using Eq. (1) one makes no use of valuable information he may possess as to the cohesive energy of the solid and, to some approximation, the thermal properties of the condensed material.

In our approach we gather terms representing the average net interaction among atoms at zero temperature into a "cold pressure" function $P_c(\eta)$ and write

$$P(\eta, T) = P_c(\eta) + RT \left[\rho_0 \eta + b(T)\eta^2 + c(T)\eta^3 \right]. \quad (2)$$

*Work performed under the auspices of the Nuclear Regulatory Commission and the United States Energy Research and Development Administration.

We take the cold pressure to be given basically by the modified Morse expression [2]

$$p_c(\eta) = a\eta^{2/3} \left(\frac{b_r}{r} e^{b_r \eta} - e^{b_a \eta} \right),$$

where $\eta = 1 - r^{-1/3}$. The parameters a , b_r , and b_a are obtained from the experimental bulk modulus and solutions of the Thomas-Fermi-Dirac equation or, as in the case of sodium [3], from a variation of the statistical atom theory with less than the full value of exchange energy. We demand in determining these parameters that the energy of separation at zero degrees, i.e., the cohesive energy

$$E_{\text{coh}} = \frac{1}{\rho_0} \int_1^0 p_c(\eta') \frac{d\eta'}{\eta'^2}$$

be equal to the experimental heat of solidification from the vapor. Equation (2) is to apply throughout the regime $\eta \leq 1$ for those portions of the isotherm that are thermodynamically accessible. For each isotherm, $b(T)$ and $c(T)$ are determined from an evaluation of $P(T)$ and $\partial P(T)/\partial \eta$ for a Debye crystal (modified as noted below) at normal solid density. Pressure isotherms calculated by Eq. (2) have a Van der Waal form below an implied critical temperature.

It is instructive to consider Eq. (2) from another point of view. The Born-Oppenheimer principle [4] justifies the calculation of the electronic energy of a solid as a function of separation of the nuclei while considering the nuclei stationary. Once the potential due to the electronic charge is obtained, calculation of vibrational energy levels of the nuclei in this potential can be performed. If we associate $P_c(\eta)$ with the change in value of the electronic energy with change of volume, this is simply the "pressure" due to the electronic charge distribution -- negative because the net interaction with all other atoms in the sample is one of attraction. The remaining terms in Eq. (2) then constitute a method of interpolating on the pressure due to the ions as they proceed from being highly bound (at the lower temperatures) at crystal density to being completely free to translate at very low density.

In Fig. 1 we depict for sodium the magnitude of electron pressure as a function of compression. At the lowest compression indicated on the graph the pressure is varying approximately as η^3 , which corresponds to Van der Waals attraction, but the curvature demonstrates that the interaction as modeled is actually falling off more rapidly. This is not correct, and it leads to the prediction on our model of a saturated vapor density that is too small at low temperature.

In Fig. 2 we show for sodium the variation of ion pressure with compression as calculated by our model and compare to the ideal gas. We note in particular that in the neighborhood of the critical point ($\eta \approx 0.1$) the deviation is quite large.

Below the critical temperature a Maxwell equal-area construction, carried out numerically for each isotherm, yields an estimate of the vapor pressure. In general for metals we find that the calculated value, over several orders of magnitude, differs from the experimental estimate by less than a factor of 2 or

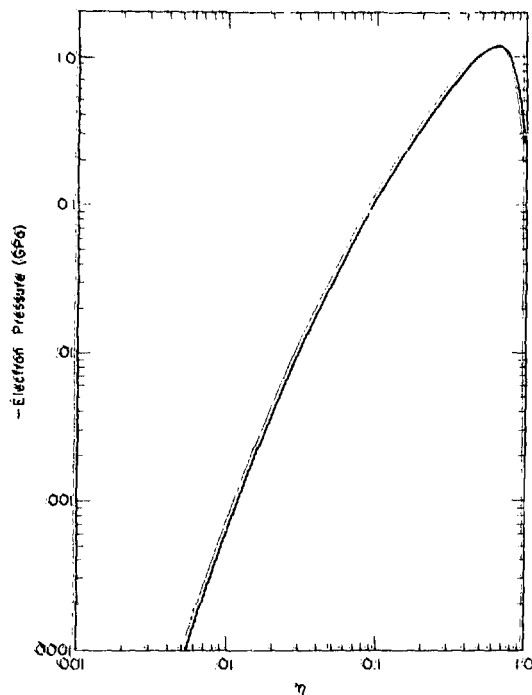


Fig. 1. Absolute value of the electron pressure as a function of compression for sodium.

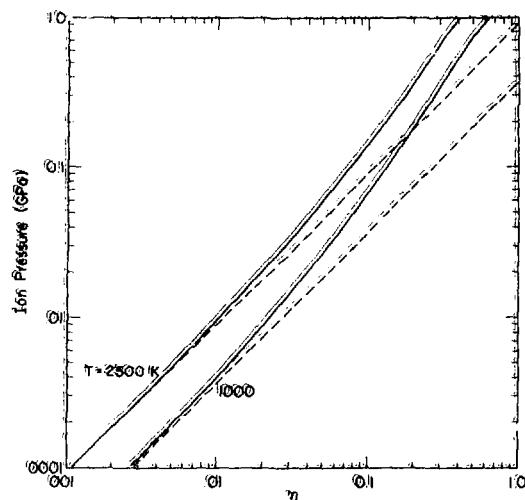


Fig. 2. Ion pressure (solid curves) as a function of compression for sodium. Dashed curves are for the corresponding ideal gas.

3. Thus we can have some confidence in the calculated isotherms and in particular apply them to the calculation of evaporation and condensation rates. Of course it is the experimental estimate of vapor pressure that is represented in the tables, and the saturation points are the intersections of the experimental pressure with the calculated isotherm.

In the earlier work on sodium, cited above, the energy at normal solid density was taken as that of a Debye solid of characteristic temperature Θ_D :

$$E_\theta = 3RD_e(x)T, \quad (3)$$

where $x = \Theta_D/T$ and $D_e(x)$ is the Debye energy function. Thermal pressure in the solid is then related to E_θ through the Grüneisen ratio, Γ , by the expression

$$P_\theta = \eta_0 \Gamma E_\theta;$$

at $\eta = 1$ the normal value of the Grüneisen parameter is given by

$$\Gamma_0 = \frac{\alpha B}{\rho_0 c_p},$$

where α is the volume coefficient of expansion, B_s is the adiabatic bulk modulus, and c_p is the heat capacity at constant pressure.

Equation (3) furnishes the constant of integration that is needed to evaluate the energy at each temperature and density point. Along each isotherm we integrate the basic thermodynamic relation

$$P = \rho_o \eta^2 \left(\frac{\partial E}{\partial \eta} \right)_T + T \left(\frac{\partial P}{\partial T} \right)_\eta \quad (4)$$

In the earlier work we observed that although the pressure is calculated to approach the ideal gas value at large volume the thermal energy, i.e., the total internal energy as obtained by integration of Eq. (4) less the energy of separation, is in obvious error in not approaching $\frac{3}{2} RT$ as density approaches zero. In subsequent work [5] we have corrected the energy by allowing a temperature variation of the Grüneisen parameter.

Since even at normal solid density the thermal energy at sufficiently high temperature must approach $\frac{3}{2} RT$ (ignoring ionization) we rewrite Eq. (3) as

$$E_\theta = 3RD_e(x)T\phi_o(T) \quad ,$$

where

$$\phi_o(T) = \left[\frac{\Gamma_o(T)}{\Gamma_o(o)} \right]^\alpha \quad ,$$

with the subscript "o" denoting evaluation at $\eta = 1$. We choose α so that as $T \rightarrow \infty$, $\phi_o(T) \rightarrow \frac{1}{2}$. At each value of temperature Γ_o is found such that the correct energy at vanishing density is obtained through integration of Eq. (4). Within this general framework we adjust the cold curve and other parameters at our disposal for each material to produce the best agreement possible with whatever experimental data are available.

In Fig. 3 we show isotherms for sodium as the temperature approaches the critical value. The Van der Waals loops are shown as they extend inside the saturation curve to the spinodal points, i.e., the local maxima and minima representing the limiting points that can be reached under conditions of nonequilibrium between vapor and liquid. Points on a calculated isotherm between the two spinodals are thermodynamically inaccessible.

We have utilized a fit to the vapor pressure, P_v , of the form

$$\ln P_v = A + \frac{B}{T} + C \ln T \quad ,$$

and determined the parameters by fitting to experimental results at 500 K and 1000 K [6] and to the recently-reported critical point determination of Bhise and Bonilla [7].

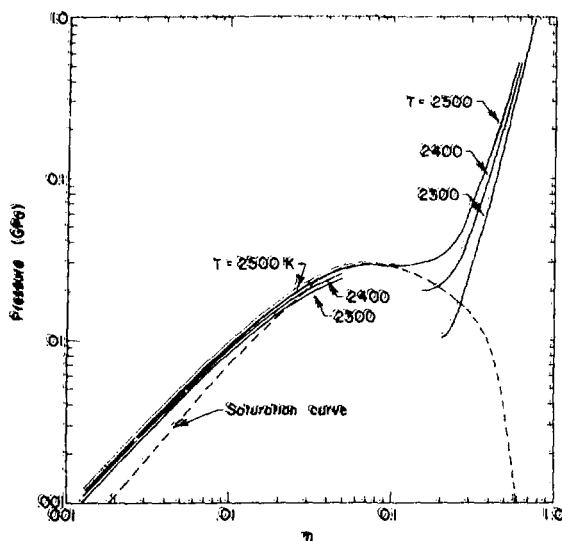


Fig. 3. Calculated pressure isotherms for sodium.

In using the aforementioned fit to vapor pressure we find reasonable consistency between the predicted critical point and the measured point, and correspondingly the assumed vapor pressures fall within the calculated Van der Waals loop until we approach quite near the predicted critical temperature. However, some weaknesses of the scheme are apparent as discussed below.

Table I displays the input parameters used in this revision. All input is the same as used in Ref. 3 except that the cohesive energy is artificially increased from 26.04 to 26.38 kcal/mol to force agreement with measured enthalpy on the saturation curve at $T = 700$ K (Fig. 4). Unfortunately, this disrupts the good agreement with calculated liquid density that was reported in Ref. 5. Figure 5 illustrates the difficulty. In the solid region the calculated density is in excellent agreement with the measured value, but the calculated liquid density is poor. Clearly we must revise our procedure to add the heat of fusion correctly by integrating through the volume change on each isotherm.

Table II compares our calculated critical data with the recent experiments [7]. Table III contains the pressure and energy tabulation.

We have constructed preliminary iron and uranium dioxide tables through these same techniques. The electron equation of state for UO_2 is obviously a strong function of temperature as the interacting electronic charge distribution depends on the species present in the vapor.

TABLE I

Input Parameters for Sodium Equation of State		
Atomic weight	A	= 22.9898
Density at 0 K	ρ_0	= 1.011 g/cm ³
Bulk modulus	B_0	= 7.70 GPa
Binding energy	E_{coh}	= 26.38 kcal/mol
Grüneisen ratio	γ_G	= 1.17
Debye temperature	θ_0	= 158 K

TABLE II

Calculated Critical Constants and Experimental Results		
	Calculated	Experiment
Temperature (K)	2500.-2525.	2504.
Pressure (GPa)	0.0290	0.0253
Density (g/cm ³)	0.10	0.135

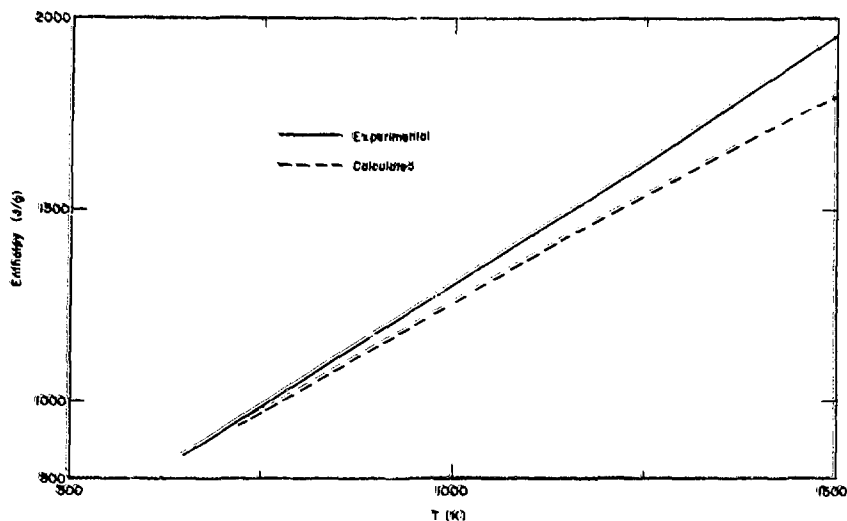


Fig. 4. Calculated and experimental enthalpy on the liquid saturation curve for sodium.

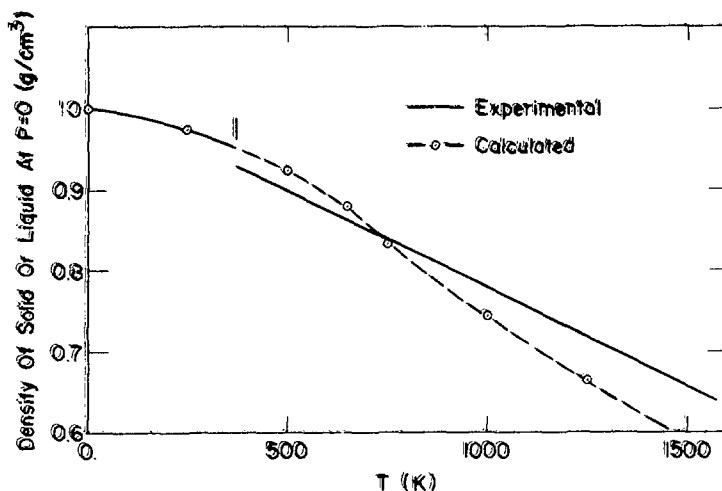


Fig. 5. Calculated and experimental density of solid or liquid. The melting point is indicated by the vertical mark.

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TABLE III

Sodium Equation of State

COMPRESSION	T = 650 K			T = 750 K			T = 1000 K			T = 1250 K		
	PRESSURE (GPA)	ENERGY (KJ/G)		PRESSURE (GPA)	ENERGY (KJ/G)		PRESSURE (GPA)	ENERGY (KJ/G)		PRESSURE (GPA)	ENERGY (KJ/G)	
.00001	2.377E+06	5.157E+00		2.742E+06	5.211E+00		3.657E+06	5.347E+00		4.571E+06	5.482E+00	
.00002	4.754E+06	5.157E+00		5.485E+06	5.211E+00		7.314E+06	5.347E+00		9.142E+06	5.482E+00	
.00005	1.189E+05	5.157E+00		1.372E+05	5.211E+00		1.829E+05	5.347E+00		2.286E+05	5.482E+00	
.00010	2.379E+05	5.156E+00		2.745E+05	5.211E+00		3.660E+05	5.347E+00		4.574E+05	5.482E+00	
.00020	4.761E+05	5.156E+00		5.495E+05	5.211E+00		7.326E+05	5.347E+00		9.156E+05	5.482E+00	
.00050	1.193E+04	5.155E+00		1.377E+04	5.211E+00		1.837E+04	5.347E+00		2.295E+04	5.482E+00	
.00100	2.396E+04	5.154E+00		2.767E+04	5.210E+00		3.692E+04	5.347E+00		4.609E+04	5.482E+00	
.00200	4.816E+04	5.150E+00		5.570E+04	5.209E+00		7.432E+04	5.347E+00		9.261E+04	5.482E+00	
.00500	1.173E+03	5.136E+00		1.370E+03	5.262E+00		1.848E+03	5.347E+00		2.318E+03	5.482E+00	
.01000	1.964E+03	5.098E+00		2.307E+03	5.176E+00		3.384E+03	5.329E+00		4.350E+03	5.472E+00	
.02000										6.733E+03	5.407E+00	
.05000												
.10000												
.15000												
.20000												
.25000												
.30000												
.35000												
.40000												
.45000												
.50000												
.55000	6.427E+01	2.153E+04		5.145E+01	1.644E+00		-3.343E+01	2.079E+00		-2.222E+01	2.286E+00	
.60000	6.362E+01	1.601E+04		4.927E+01	1.406E+00		-3.324E+01	1.885E+00		-2.233E+01	2.094E+00	
.65000	6.013E+01	1.636E+00		4.435E+01	1.424E+00		-3.126E+01	1.714E+00		-2.016E+01	1.927E+00	
.70000	5.340E+01	1.419E+00		3.624E+01	1.274E+00		-2.655E+01	1.566E+00		-1.630E+01	1.781E+00	
.75000	4.301E+01	1.228E+00		2.468E+01	1.141E+00		-1.941E+01	1.438E+00		-1.019E+01	1.657E+00	
.80000	2.855E+01	1.043E+00		1.72E+02	1.033E+00		-9.273E+02	1.333E+00		1.007E+01	1.552E+00	
.85000	9.576E+02	9.235E+01		9.172E+02	1.033E+00		4.189E+02	1.243E+00		2.043E+01	1.466E+00	
.90000	1.432E+01	8.091E+01		1.065E+01	9.422E+00		2.133E+01	1.174E+00		0.933E+01	1.398E+00	
.95000	4.354E+01	7.196E+01		3.518E+01	8.745E+01		4.249E+01	1.124E+00		0.521E+01	1.314E+00	
1.00000	7.861E+01	6.509E+01		9.993E+01	8.277E+01		9.838E+01	1.077E+00		9.147E+01	1.208E+00	
							1.337E+00	1.077E+00		1.222E+00	1.299E+00	
										1.578E+00	1.315E+00	

TABLE III (cont.)

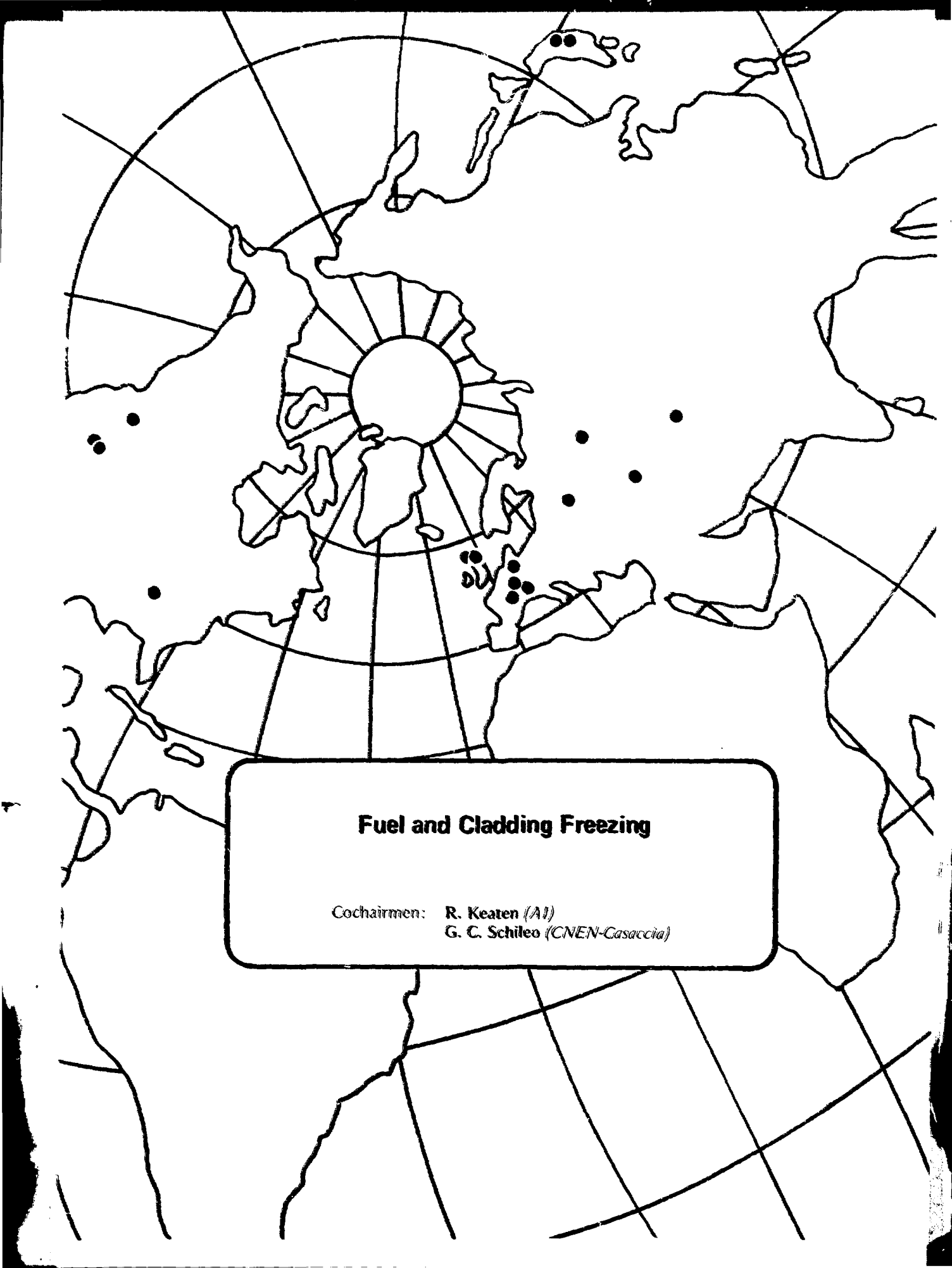
COMPRESSION	T = 1500 K			T = 1750 K			T = 2000 K			T = 2250 K		
	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)
.00001	5.465E-06	5.618E+00	6.399E-06	5.753E+00	7.313E-06	5.889E+00	8.227E-06	6.025E+00				
.00002	1.097E-05	5.618E+00	1.260E-05	5.753E+00	1.463E-05	5.889E+00	1.646E-05	6.025E+00				
.00005	2.743E-05	5.618E+00	3.230E-05	5.753E+00	3.658E-05	5.889E+00	4.115E-05	6.025E+00				
.00010	5.489E-05	5.618E+00	6.433E-05	5.753E+00	7.318E-05	5.889E+00	8.232E-05	6.025E+00				
.00020	1.099E-04	5.618E+00	1.282E-04	5.753E+00	1.464E-04	5.889E+00	1.647E-04	6.025E+00				
.00050	2.753E-04	5.618E+00	3.211E-04	5.753E+00	3.669E-04	5.889E+00	4.126E-04	6.025E+00				
.00100	5.527E-04	5.618E+00	6.444E-04	5.753E+00	7.361E-04	5.889E+00	8.278E-04	6.025E+00				
.00200	1.112E-03	5.618E+00	1.297E-03	5.753E+00	1.481E-03	5.889E+00	1.665E-03	6.025E+00				
.00500	2.785E-03	5.618E+00	3.250E-03	5.753E+00	3.714E-03	5.889E+00	4.179E-03	6.025E+00				
.01000	5.304E-03	5.618E+00	6.209E-03	5.752E+00	7.193E-03	5.887E+00	8.138E-03	6.022E+00				
.02000	8.687E-03	5.554E+00	1.066E-02	5.695E+00	1.259E-02	5.829E+00	1.454E-02	5.963E+00				
.05000							2.275E-02	5.687E+00				
.10000												
.15000												
.20000												
.25000												
.30000												
.35000	-1.421E-01	2.506E+00	-8.172E-02	2.601E+00	-3.317E-02	3.437E+00	4.613E-03	3.803E+00				
.40000	-1.412E-01	2.309E+00	-6.954E-02	2.415E+00	-2.268E-02	2.822E+00	5.842E-03	3.509E+00				
.45000	-1.260E-01	2.139E+00	-4.276E-02	2.255E+00	3.843E-02	2.632E+00	7.214E-02	3.256E+00				
.50000	-9.374E-02	1.992E+00	9.199E-02	2.117E+00	9.321E-02	2.328E+00	1.215E-01	2.846E+00				
.55000	-4.198E-02	1.865E+00	6.393E-02	2.004E+00	1.671E-01	2.209E+00	1.877E-01	2.538E+00				
.60000	3.272E-02	1.759E+00	1.488E-01	1.903E+00	2.625E-01	2.109E+00	2.728E-01	2.416E+00				
.65000	1.311E-01	1.671E+00	2.591E-01	1.823E+00	3.818E-01	2.028E+00	5.085E-01	2.231E+00				
.70000	2.581E-01	1.601E+00	3.947E-01	1.761E+00	5.275E-01	1.954E+00	6.637E-01	2.166E+00				
.75000	4.159E-01	1.548E+00	5.611E-01	1.715E+00	7.023E-01	1.916E+00	8.471E-01	2.117E+00				
.80000	6.076E-01	1.512E+00	7.603E-01	1.687E+00	9.087E-01	1.886E+00	1.061E+00	2.085E+00				
.85000	8.361E-01	1.493E+00	9.951E-01	1.673E+00	1.149E+00	1.871E+00	1.308E+00	2.068E+00				
.90000	1.104E+00	1.489E+00	1.268E+00	1.676E+00	1.427E+00	1.871E+00	1.590E+00	2.068E+00				
.95000	1.416E+00	1.524E+00	1.583E+00	1.693E+00	1.744E+00	1.889E+00	1.910E+00	2.063E+00				
1.00000	1.773E+00	1.528E+00	1.941E+00	1.725E+00	2.104E+00	1.919E+00	2.271E+00	2.113E+00				

TABLE III (cont.)

COMPRESSION	T = 2500 K			T = 2750 K			T = 3000 K			T = 3250 K		
	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)
.00001	9.141E-06	6.115E+00	1.006E-05	6.296E+00	1.007E-05	6.432E+00	1.007E-05	6.432E+00	1.100E-05	6.567E+00	1.100E-05	6.567E+00
.00002	1.828E-05	6.115E+00	2.011E-05	6.296E+00	2.019E-05	6.432E+00	2.019E-05	6.432E+00	2.377E-05	6.567E+00	2.377E-05	6.567E+00
.00005	4.572E-05	6.115E+00	5.029E-05	6.296E+00	5.466E-05	6.432E+00	5.466E-05	6.432E+00	5.943E-05	6.567E+00	5.943E-05	6.567E+00
.00010	9.146E-05	6.115E+00	1.006E-04	6.296E+00	1.098E-04	6.432E+00	1.098E-04	6.432E+00	1.109E-04	6.567E+00	1.109E-04	6.567E+00
.00020	1.833E-04	6.115E+00	2.013E-04	6.296E+00	2.196E-04	6.432E+00	2.196E-04	6.432E+00	2.379E-04	6.567E+00	2.379E-04	6.567E+00
.00050	4.584E-04	6.115E+00	5.042E-04	6.296E+00	5.500E-04	6.432E+00	5.500E-04	6.432E+00	5.950E-04	6.567E+00	5.950E-04	6.567E+00
.00100	9.195E-04	6.115E+00	1.011E-03	6.296E+00	1.103E-03	6.432E+00	1.103E-03	6.432E+00	1.195E-03	6.567E+00	1.195E-03	6.567E+00
.00200	1.849E-03	6.115E+00	2.033E-03	6.296E+00	2.217E-03	6.432E+00	2.217E-03	6.432E+00	2.402E-03	6.567E+00	2.402E-03	6.567E+00
.00500	4.644E-03	6.115E+00	5.110E-03	6.296E+00	5.577E-03	6.432E+00	5.577E-03	6.432E+00	6.043E-03	6.567E+00	6.043E-03	6.567E+00
.01000	9.081E-03	6.115E+00	1.003E-02	6.205E+00	1.099E-02	6.432E+00	1.099E-02	6.432E+00	1.194E-02	6.567E+00	1.194E-02	6.567E+00
.02000	1.848E-02	6.065E+00	1.846E-02	6.218E+00	2.044E-02	6.356E+00	2.044E-02	6.356E+00	2.242E-02	6.494E+00	2.242E-02	6.494E+00
.05000	2.802E-02	5.805E+00	3.350E-02	5.919E+00	3.901E-02	6.059E+00	3.901E-02	6.059E+00	4.450E-02	6.200E+00	4.450E-02	6.200E+00
.10000	2.812E-02	5.360E+00	4.075E-02	5.400E+00	5.350E-02	5.554E+00	5.350E-02	5.554E+00	6.610E-02	5.699E+00	6.610E-02	5.699E+00
.15000			4.894E-02	4.965E+00	7.046E-02	5.136E+00	7.046E-02	5.136E+00	9.179E-02	5.266E+00	9.179E-02	5.266E+00
.20000	3.276E-02	3.727E+00	6.389E-02	4.586E+00	9.546E-02	4.741E+00	9.546E-02	4.741E+00	1.267E-01	4.895E+00	1.267E-01	4.895E+00
.25000	4.340E-02	3.457E+00	8.545E-02	4.257E+00	1.202E-01	4.417E+00	1.202E-01	4.417E+00	1.704E-01	4.575E+00	1.704E-01	4.575E+00
.30000	6.373E-02	3.226E+00	1.175E-01	3.970E+00	1.722E-01	4.134E+00	1.722E-01	4.134E+00	2.263E-01	4.297E+00	2.263E-01	4.297E+00
.35000	9.571E-02	3.028E+00	1.619E-01	3.719E+00	2.722E-01	3.667E+00	2.722E-01	3.667E+00	2.958E-01	4.054E+00	2.958E-01	4.054E+00
.40000	1.401E-01	2.858E+00	2.199E-01	3.497E+00	3.804E-01	3.669E+00	3.804E-01	3.669E+00	3.796E-01	3.840E+00	3.796E-01	3.840E+00
.45000	2.013E-01	2.713E+00	2.932E-01	3.303E+00	3.869E-01	3.479E+00	3.869E-01	3.479E+00	4.794E-01	3.653E+00	4.794E-01	3.653E+00
.50000	2.763E-01	2.589E+00	3.832E-01	3.133E+00	4.902E-01	3.312E+00	4.902E-01	3.312E+00	5.950E-01	3.440E+00	5.950E-01	3.440E+00
.55000	3.740E-01	2.486E+00	4.917E-01	2.905E+00	6.117E-01	3.167E+00	6.117E-01	3.167E+00	7.371E-01	3.349E+00	7.371E-01	3.349E+00
.60000	4.920E-01	2.401E+00	6.204E-01	2.858E+00	7.530E-01	3.044E+00	7.530E-01	3.044E+00	8.038E-01	3.220E+00	8.038E-01	3.220E+00
.65000	6.296E-01	2.334E+00	7.712E-01	2.751E+00	9.150E-01	2.940E+00	9.150E-01	2.940E+00	1.059E+00	3.127E+00	1.059E+00	3.127E+00
.70000	7.938E-01	2.283E+00	9.462E-01	2.663E+00	1.102E+00	2.855E+00	1.102E+00	2.855E+00	1.255E+00	3.045E+00	1.255E+00	3.045E+00
.75000	9.851E-01	2.249E+00	1.147E+00	2.593E+00	1.313E+00	2.788E+00	1.313E+00	2.788E+00	1.476E+00	2.981E+00	1.476E+00	2.981E+00
.80000	1.276E+00	2.230E+00	1.377E+00	2.542E+00	1.551E+00	2.738E+00	1.551E+00	2.738E+00	1.723E+00	2.934E+00	1.723E+00	2.934E+00
.85000	1.459E+00	2.226E+00	1.636E+00	2.507E+00	1.810E+00	2.706E+00	1.810E+00	2.706E+00	1.996E+00	2.904E+00	1.996E+00	2.904E+00
.90000	1.745E+00	2.237E+00	1.920E+00	2.497E+00	2.115E+00	2.691E+00	2.115E+00	2.691E+00	2.294E+00	2.891E+00	2.294E+00	2.891E+00
.95000	2.066E+00	2.262E+00	2.255E+00	2.497E+00	2.445E+00	2.693E+00	2.445E+00	2.693E+00	2.632E+00	2.895E+00	2.632E+00	2.895E+00
1.00000	2.439E+00	2.302E+00	2.610E+00	2.500E+00	2.809E+00	2.711E+00	2.809E+00	2.711E+00	2.940E+00	2.915E+00	2.940E+00	2.915E+00

TABLE III (cont.)

COMPRESSION	T = 3500 K			T = 3750 K			T = 4000 K			T = 4250 K		
	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)	PRESSURE (GPA)	ENERGY (KJ/G)
.00001	1.284E+05	6.703E+00	1.371E+05	6.838E+00	1.463E+05	6.974E+00	1.554E+05	7.110E+00	1.645E+05	7.246E+00	1.736E+05	7.381E+00
.00002	2.560E+05	6.703E+00	2.745E+05	6.838E+00	2.925E+05	6.974E+00	3.105E+05	7.110E+00	3.285E+05	7.246E+00	3.465E+05	7.381E+00
.00005	6.400E+05	6.703E+00	6.857E+05	6.838E+00	7.315E+05	6.974E+00	7.772E+05	7.110E+00	8.229E+05	7.246E+00	8.686E+05	7.381E+00
.00010	1.280E+06	6.703E+00	1.372E+06	6.838E+00	1.463E+06	6.974E+00	1.554E+06	7.110E+00	1.645E+06	7.246E+00	1.736E+06	7.381E+00
.00020	2.562E+06	6.703E+00	2.745E+06	6.838E+00	2.925E+06	6.974E+00	3.105E+06	7.110E+00	3.285E+06	7.246E+00	3.465E+06	7.381E+00
.00050	6.416E+06	6.703E+00	6.874E+06	6.838E+00	7.332E+06	6.974E+00	7.790E+06	7.110E+00	8.248E+06	7.246E+00	8.706E+06	7.381E+00
.00100	1.287E+07	6.703E+00	1.378E+07	6.838E+00	1.470E+07	6.974E+00	1.562E+07	7.110E+00	1.654E+07	7.246E+00	1.746E+07	7.381E+00
.00200	2.586E+07	6.703E+00	2.770E+07	6.838E+00	2.954E+07	6.974E+00	3.139E+07	7.110E+00	3.323E+07	7.246E+00	3.507E+07	7.381E+00
.00500	6.510E+07	6.703E+00	6.976E+07	6.838E+00	7.442E+07	6.974E+00	7.908E+07	7.110E+00	8.374E+07	7.246E+00	8.840E+07	7.381E+00
.01000	1.289E+08	6.631E+00	1.384E+08	6.832E+00	1.479E+08	6.968E+00	1.574E+08	7.105E+00	1.669E+08	7.241E+00	1.764E+08	7.376E+00
.02000	2.439E+08	6.631E+00	2.637E+08	6.769E+00	2.834E+08	6.906E+00	3.030E+08	7.044E+00	3.226E+08	7.183E+00	3.422E+08	7.315E+00
.05000	4.997E+08	6.340E+00	5.542E+08	6.481E+00	6.086E+08	6.621E+00	6.629E+08	6.761E+00	7.173E+08	6.892E+00	7.717E+08	7.000E+00
.10000	7.878E+08	5.845E+00	9.131E+08	5.997E+00	1.038E+09	6.134E+00	1.162E+09	6.279E+00	1.286E+09	6.418E+00	1.410E+09	6.563E+00
.15000	1.134E+09	5.416E+00	1.340E+09	5.565E+00	1.549E+09	5.714E+00	1.757E+09	5.863E+00	1.965E+09	6.012E+00	2.173E+09	6.156E+00
.20000	1.577E+09	5.049E+00	1.885E+09	5.273E+00	2.190E+09	5.356E+00	2.493E+09	5.510E+00	2.796E+09	5.654E+00	3.099E+09	5.798E+00
.25000	2.123E+09	4.734E+00	2.538E+09	4.892E+00	2.949E+09	5.044E+00	3.357E+09	5.206E+00	3.760E+09	5.348E+00	4.163E+09	5.492E+00
.30000	2.799E+09	4.467E+00	3.329E+09	4.621E+00	3.854E+09	4.783E+00	4.375E+09	4.944E+00	4.896E+09	5.042E+00	5.417E+09	5.186E+00
.35000	3.616E+09	4.222E+00	4.268E+09	4.386E+00	4.913E+09	4.551E+00	5.553E+09	4.715E+00	6.191E+09	4.809E+00	6.829E+09	4.903E+00
.40000	4.583E+09	4.010E+00	5.363E+09	4.179E+00	6.129E+09	4.348E+00	6.891E+09	4.516E+00	7.673E+09	4.616E+00	8.455E+09	4.720E+00
.45000	5.738E+09	3.827E+00	6.612E+09	3.999E+00	7.576E+09	4.171E+00	8.392E+09	4.303E+00	9.214E+09	4.419E+00	1.003E+10	4.526E+00
.50000	7.020E+09	3.667E+00	8.031E+09	3.843E+00	9.051E+09	4.010E+00	1.006E+10	4.084E+00	1.107E+10	4.189E+00	1.208E+10	4.299E+00
.55000	8.472E+09	3.529E+00	9.624E+09	3.708E+00	1.077E+10	3.887E+00	1.190E+10	3.956E+00	1.372E+10	4.057E+00	1.547E+10	4.166E+00
.60000	1.013E+10	3.412E+00	1.140E+10	3.594E+00	1.267E+10	3.775E+00	1.392E+10	3.956E+00	1.524E+10	4.057E+00	1.656E+10	4.266E+00
.65000	1.199E+10	3.313E+00	1.338E+10	3.499E+00	1.475E+10	3.683E+00	1.611E+10	3.866E+00	1.739E+10	4.057E+00	1.868E+10	4.366E+00
.70000	1.407E+10	3.234E+00	1.556E+10	3.422E+00	1.744E+10	3.608E+00	1.850E+10	3.794E+00	2.019E+10	4.057E+00	2.198E+10	4.466E+00
.75000	1.637E+10	3.172E+00	1.796E+10	3.362E+00	1.953E+10	3.552E+00	2.129E+10	3.747E+00	2.348E+10	4.057E+00	2.577E+10	4.566E+00
.80000	1.892E+10	3.128E+00	2.059E+10	3.320E+00	2.225E+10	3.512E+00	2.388E+10	3.702E+00	2.647E+10	4.057E+00	2.906E+10	4.666E+00
.85000	2.173E+10	3.101E+00	2.347E+10	3.295E+00	2.519E+10	3.489E+00	2.689E+10	3.681E+00	2.909E+10	4.057E+00	3.180E+10	4.766E+00
.90000	2.480E+10	3.090E+00	2.660E+10	3.286E+00	2.837E+10	3.482E+00	3.012E+10	3.677E+00	3.280E+10	4.057E+00	3.608E+10	4.866E+00
.95000	2.817E+10	3.095E+00	2.999E+10	3.294E+00	3.180E+10	3.491E+00	3.350E+10	3.688E+00	3.722E+10	4.057E+00	4.036E+10	4.966E+00
1.00000	3.184E+10	3.111E+00	3.367E+10	3.317E+00	3.549E+10	3.516E+00	3.722E+10	3.714E+00	4.036E+10	4.057E+00	4.410E+10	5.066E+00



Fuel and Cladding Freezing

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A STUDY OF FUEL FREEZING AND CHANNEL PLUGGING DURING HYPOTHETICAL OVERPOWER TRANSIENTS*

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ABSTRACT

In the present work a mechanistic approach has been used to determine the events following pin failure which may lead to fuel freezing and channel plugging. The location of the impact of the fuel particle with a wire wrap and the induced impact stresses in the particle are determined. A fuel particle is assumed to plate out at the impact location, if the induced impact stress exceeds the maximum fracture or yield stress. Knowing the distribution of the particles around the failure location, the amount and axial distance from the point of failure are determined. The effect on fuel plate-out of such variables as the rate and amount of fuel injection, fuel particle size, fission heating rate and presence of fission gas has been studied parametrically.

The above modelling is applied to determine the amount and location of fuel plate out in CRBR under hypothetical slow and fast transients.

INTRODUCTION

The study of Core Disruptive Accidents (CDA's) has evolved from the Bethe-Tait [1] disassembly type analysis towards a refined, mechanistic sequence to describe the events taking place. This sequence has been split into several phases for computational ease and is based upon the dominant physical considerations during the sequence. For the Loss-of Flow sequence, a "transition phase" has been added to examine potential recriticality during the transition between initial core disruption and long term coolability [2,3]. For the transient overpower sequence, research has centered upon the pre-disassembly phase and the transition to core disruption, should hydrodynamic disassembly occur, or upon the transition to nuclear shutdown, should hydraulic sweepout of fuel occur.

In transient overpower studies, the events leading to rupture of fuel pins, ejection of molten fuel into the coolant channel, movement of fuel and coolant in the interaction zone and the physical state of the interaction zone have been considered to a great length [4,5,6]. However, in these studies the initial fuel/coolant interaction is studied qualitatively and little attention is given to the physical state of the ejected fuel particles. The fuel particles are allowed to fragment at a certain rate and are then simply assumed to be swept out of the coolant channels. The sweepout of the fuel from the active core introduces a strong negative reactivity component which makes the reactor sub-critical. Thus, under the fuel sweepout assumption, most of the earlier

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studies conclude the over-power transient accidents to be benign.

However, many of the TREAT experiments, in particular the tests H-4 and H-5 [7,8], have yielded substantial fuel plugging following pin failure. Generally, it has been argued that this may be due to non proto-typical thermal hydraulics and neutronics of the TREAT tests. Nevertheless, some of the subsequent studies [9-11] have parametrically considered the effect of fuel sweep-out on reactivity and heat transfer. For example, in Reference 9, the fuel in the coolant channel is allowed to continually freeze at a given rate and with a prescribed skewed distribution, while in Reference 10, a given fraction of the fuel element is allowed to plug completely. In the Clinch River Breeder Reactor Plant PSAR [12], the fuel particles are allowed to form porous plugs in the axial blanket and it is shown that the core can be cooled even if 90% of the flow area is occupied by these plugs. However, the treatment given in the PSAR to the formation of these plugs and the pressure loss through these plugs can be questioned on several counts, e.g., size of the particles, size of the plug and the porosity of the plug, etc.

At present, in-core fuel freezing or plugging of the channels has either been completely ignored or has been treated in a parametric manner. The possibility of the formation of fuel plugs near the upper plane of the active core is of significant concern as these plugs can be a source of increased reactivity coupled with deficient cooling due to increased resistance to the flow of the coolant through the channels. Under these circumstances an over-power transient accident can lead to a transient under-cooling accident with possibility of melting, slumping of the core and a hydrodynamic disassembly.

In the present work, a mechanistic approach has been used to determine the events following pin failure which may lead to fuel freezing and channel plugging. In a sense then, this work is a first attempt at mechanistically defining a transition phase, should it exist, between post failure pin behavior and long term coolability. From such a transition phase, considerations such as fuel motion reactivity effects, long term coolability and possible secondary criticality can be analyzed.

DESCRIPTION OF THE MODEL

The model used in this paper to predict fuel freezing and channel plugging is the same as that developed in Reference 13. In this model the location and size of the opening in the pin, the time of injection, the rate of injection of molten fuel into the coolant channel and the initial thermal hydraulic conditions in the channel are assumed to be known. For this investigation they were supplied by the HOPE computer code [6]. The fuel is assumed to be injected smoothly into the interaction zone and no direct splashing of fuel on the neighboring pins is considered. The size of fuel particles is also not known apriori as it significantly depends on the nature of the fuel/coolant interaction. Thus, the fuel particle size is also treated as an input parameter.

Physical State of the Fuel Particle

After injection of fuel into the coolant channel, the physical state of the fuel and the coolant in the interaction zone is determined by solving energy and momentum equations simultaneously. The solidification model for a molten fuel particle is shown in Figure 1. The transient one dimensional conduction equation with variable physical properties is written for both liquid and solid phases as*

$$c_f \rho_f \frac{\partial T_f}{\partial t} = \frac{1}{r^2} \frac{\partial}{\partial r} \left(k_f r^2 \frac{\partial T_f}{\partial r} \right) + \dot{q} \quad (1)$$

*Symbols not explained in the text are defined in the Nomenclature Section.

The initial condition for equation (1) is

$$T_f(r, 0) = T_m \quad (2)$$

The boundary conditions at the liquid/solid interface are:

$$T_f(r_1) = T_m \text{ at } r = r_1(t) \quad (3)$$

and

$$k_f^s \frac{\partial T_f^s}{\partial r} - k_f^l \frac{\partial T_f^l}{\partial r} = \rho_f^l h_{fs} \frac{dr_1}{dt} \text{ at } r = r_1(t) \quad (4)$$

At the external surface the boundary condition is

$$-k_f^s \frac{\partial T_f^s}{\partial r} = h_c (T_f^s - T_c) \text{ at } r = R \quad (5)$$

In equation (5), the convective heat transfer coefficient, h_c , and the temperature, T_c , of the coolant depend on time and are coupled to the physical state of the coolant. At the time of fuel injection into the channel, the sodium is highly subcooled and an immediate direct contact of liquid sodium is expected to occur [14], so h_c is calculated by using the forced convection heat transfer correlation similar to that proposed in reference 15,

$$h_c = \frac{k_c}{R} [1 + 0.185 \text{ Re}^{0.6} \text{ Pr}^{0.5}] \quad (6)$$

When the interaction zone expands and sodium voiding occurs or if fission gas is also present, the heat transfer coefficient is calculated by assuming that both liquid and vapor phases contact the fuel surface in proportion to their volumes in the interaction zone.

The acceleration of the particle in the vertical direction is governed by the difference in drag and gravity forces and is given as

$$m_f \frac{dv_f}{dt} = F_D - F_g \quad (7)$$

where

$$F_D = \frac{\pi R^2}{2} \rho_c (V_c - V_f) |V_c - V_f| \epsilon^N C_D \text{ and } F_g = m_f g - 4/5 \pi R^3 \rho_c g \quad (8)$$

The distance in the axial direction travelled by the particle starting from its position of injection into the coolant channel is given by

$$S = \int_0^t V_f dt \quad (9)$$

The cross flow velocity of the particle depends on the coolant cross flow velocity and the slip between the coolant and the particles. As seen from Figure 2, for no slip between the wire wrap and the coolant, the cross-flow velocity of the coolant can be written as [16]

$$V_{ct} = \frac{\tau p}{L} V_c \cos \left(\frac{2\pi}{L} z \right) \quad (10)$$

Presently data are lacking for slip between a particle and its carrier moving in a helical path. Assuming the particle velocity to be half of the carrier velocity as in the case of particles moving in horizontal pipelines, the cross-flow velocity of the fuel particle becomes

$$v_{ft} = \frac{\pi n}{2L} v_c \cos\left(\frac{2\pi}{L} z\right) \quad (11)$$

Physical State of the Coolant in the Interaction Zone

The heat transfer from the fuel particles results in a rapid increase in the internal energy of the coolant in the interaction zone and the interaction zone expands doing work on the overlying and underlying sodium. Assuming that the overlying and underlying sodium does not exchange heat and mass with the interaction zone and the heat transfer from the pin walls to the interaction zone is small in comparison to that from the fuel; the energy equation and the equation for the change of enthalpy with change of state can be written as

$$\frac{dH_c}{dt} = \dot{q}_f + v_c \frac{dP_c}{dt} = c_c \frac{dT_c}{dt} + v_c [1 - \alpha_{pc} T_c] \frac{dP_c}{dt} \quad (12)$$

For a two phase mixture (boiling or fission gas), the equation for change of enthalpy becomes

$$\frac{dH_c}{dt} = x \frac{dH_v}{dt} + (1-x) \frac{dH_l}{dt} + h_{lv} \frac{dx}{dt} \quad (13)$$

The equations of state for single phase and two phase mixture are written as

$$\frac{dv_c}{dt} = v_c \alpha_{pc} \frac{dT_c}{dt} - v_c \beta_{pc} \frac{dP_c}{dt} = x \frac{dv_v}{dt} + (1-x) \frac{dv_l}{dt} + v_{lv} \frac{dx}{dt} \quad (14)$$

The momentum equations for the upper interface of the interaction zone is written as

$$\frac{dz_{up}}{dt} = \frac{-gL}{C} + \frac{p_c - p_t}{\rho_c C} \quad t < t_{ac} \quad (15)$$

$$\frac{dp}{dt} = g + \frac{p_c - p_t}{\rho_c (z_{top} - z_{up})} - \frac{U_{up} |U_{up}| f}{2 Wh} \quad t > t_{ac} \quad (16)$$

where t_{ac} is the acoustic time, $t_{ac} = \frac{L}{C}$; L is the height of the coolant channel and C is the speed of sound in sodium. Similar equations can be written for the lower interface. The equation of constraint for the interaction zone can be written as

$$\frac{d(m_{fg} v_{fg})}{dt} + m_c \frac{dv_c}{dt} = A_{up} U_{up} - A_d U_d \quad (17)$$

Fuel Freezing and Plugging Mechanism

The freezing of fuel and plugging of channels can occur if the fuel is in a molten state when it contacts the upper cooler pin walls or if thin solid crust of the semi-solidified particle breaks open because of impact stresses as the particle hits a pin or wire wrap. The impact of the particle with a

wire wrap is possible, because of the slip between the particle and the main stream, and the inability of the particle to follow rapid changes in tangential velocity. If the impact stress is large enough, it may cause cracking of the outer shell and in turn ejection and freezing of molten fuel on the wire wraps.

The maximum force on a fuel particle due to an impact is obtained from reference [17], as

$$F_{\max} = n \eta_{\max}^{3/2} \quad (18)$$

where

$$n = 1 / \left[(\beta_1 + \beta_2)^2 \frac{3 \pi^2}{16 R} \right]^{1/2}; \quad \beta_1 = \frac{1 - \nu_f^2}{\pi E_f} \quad \text{and} \quad \beta_2 = \frac{1 - \nu_{st}^2}{\pi E_{st}} \quad (19)$$

and

$$\eta_{\max} = \left(\frac{5}{4} \frac{v_f^2 m_f}{n} \right)^{2/5} \quad (20)$$

The maximum impact area is expressed as

$$A_{\max} = \pi^{1/3} \left[(\beta_1 + \beta_2) F_{\max} \frac{3 \pi^2}{16} \right]^{2/3} \quad (21)$$

Knowing, $F_{\max}$ and $A_{\max}$, the maximum impact stresses can be calculated as follows:

maximum shearing stress,

$$\tau_{\max} = \frac{3}{4} \frac{F_{\max}}{\pi A_{\max}} \left[\frac{1 - 2\nu}{2} + \frac{2}{9} (1 + \nu) \sqrt{2(1 + \nu)} \right] \quad (22)$$

maximum tensile stress,

$$\sigma_t = \frac{(1 - 2\nu)}{2} \frac{F_{\max}}{\pi A_{\max}} \quad (23)$$

Equations (1-5) for the physical state of the fuel particle, equation (7) for axial velocity of the particle and equations (12-19) for the physical state of the interaction zone were solved simultaneously by using an iterative implicit finite difference scheme. The moving fuel freezing front was made stationary by using a transformation. The details of the numerical analysis are given in reference 13. Knowing the cross flow velocity of the coolant and the particle, the trajectory of the particle in the coolant channel was traced. If the particle hit the wire wrap and the impact stress exceeded the yield or fracture stress for the particle, the fuel particle was assumed to stick to the wire-wrap and freeze in place.

DISCUSSION OF RESULTS

Two cases representing slow transients in a middle of life fuel pin and a fast transient in a fresh pin were analyzed using the geometrical configuration, thermal hydraulics and neutronics of CRBR. The thermal hydraulic characteristics of the coolant channel at the time of pin rupture and the other data related to pin failure was obtained from the HOPF [6] code. The conditions at pin failure, as predicted by the HOPF code, and as used in the fuel freezing model are listed in Table 1.

The pressure and temperature histories of the coolant in the interaction zone following pin failure, under a reactivity ramp of 2.4 cents/sec and when fuel particles of diameter 250 μm (—) and 1000 μm (---) are injected into the coolant channel is shown in Figure 3. The heat transfer from the fuel to the coolant causes the pressure in the interaction zone to increase. For bigger particles, the rate of pressure rise prior to boiling is lower because of smaller heat transfer area per unit volume of the fuel. The maximum pressure in the interaction zone reaches to about 76 atmospheres, 1.5×10^{-5} seconds after 250 μm diameter particles are assumed to be injected into the coolant channel. Thereafter, the interaction zone expands with boiling occurring at 2.8×10^{-5} seconds. Just after boiling begins, the pressure in the interaction zone increases again because of increased voids. However, soon the lower heat transfer rate and inertial expansion of the interaction cause the pressure to decrease. The boiling in the interaction zone when 1000 μm particles are assumed to be injected is delayed and initiates only after 5.2×10^{-5} seconds. The pressure in the voided interaction zone reaches a maximum value of 185 atmospheres but rapidly drops due to expansion of the interaction zone.

The surface temperature, the radial location of the solidification front and axial location of the fuel particles are plotted in Figure 4. The smaller fuel particle completely solidifies after 6×10^{-5} seconds, whereas, the solid crust of the bigger particle is only 0.2 mm thick after 4.5×10^{-2} seconds. The upward movement of the bigger particle is slow due to its increased weight. The particle even drops down a little when the drag of the voided interaction zone is smaller than the weight of the particles.

Figure 5 shows the trajectories of the fuel particles as a function of their angular position in the plane of pin failure. The 1000 μm diameter particles, irrespective of their angular position, will hit the wire wrap in semi-molten state within 0.04 seconds, and will fracture as a result of induced impact stresses. The smaller particles injected at angular position 0 and $-\pi/2$ may hit the wire wrap in semi-molten state and will contribute to some in channel fuel freezing.

From the above results, if the particles are assumed to freeze in place, it can be concluded that the larger particles ($d = 1000 \mu\text{m}$) will contribute significantly to in-channel, in-core fuel freezing. The small fuel particles ($d = 250 \mu\text{m}$) generally solidify very early in their passage through the coolant channel and thus will have lesser probability of freezing in the channels. The solidification and in-channel movement histories of particles of other sizes can similarly be obtained.

The axial distance through which a particle may hit the wire wrap in a semi-molten state and fracture due to impact stress is plotted in Figure 6, for different particle sizes. Certain fraction of particles smaller than 250 μm will freeze prior to hitting a wire wrap. This fraction of particles will probably be swept out of the coolant channels, provided the particles do not settle in the fuel pin wire wrap corners. The larger particles ($d > 250 \mu\text{m}$) will in all probability hit a wire wrap prior to complete solidification.

To obtain the amount and location of fuel plateout, the particles at the time of injection are arbitrarily assumed to have a distribution as listed in Table 2. In reality the size distribution will strongly depend on the interplay of such parameters as size of the fuel pin opening, initial temperatures of fuel and sodium and energetics of the fuel coolant interaction, etc. Using the information contained in Figure 6 and the size distribution

as given in Table 2, and assuming that the particles are spread out uniformly along the pin periphery, the amount of fuel plateout as a function of the axial distance from the point of failure can be obtained. The results of these calculations are plotted in Figure 7. These results show that for the present example about 75% of the injected fuel will plate out within 30 cm from the point of pin failure provided enough cooling capability exists on the impacted surface (in this case it is the wire wrap).

The amount of fuel plate-out predicted by the present model should be considered conservative, as all the fuel hitting the wire-wrap is assumed to lead to channel plugging and no allowance for the fraction of fuel not sticking to the wire wrap is made. Also, re-melting of fuel particles sticking to the wire-wrap has not been considered by the present model.

CONCLUSIONS

A mechanistic model to study fuel freezing and channel plugging subsequent to an overpower accident in a LMFBR has been developed. The model can be used to predict the amount and location of fuel plate-out in the coolant channels.

The present model, when applied to CRBR, predicts that about 75% of the injected fuel will plate out within 30 cm from the point of pin failure. The amount of fuel plate out predicted by this model should be considered conservative, as the fuel hitting the wire wrap is assumed to freeze in place and no allowance for the fraction of fuel not sticking to the wire wrap is made.

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NOMENCLATURE

C_D	Drag Coefficient = $24/Re$ for $Re < 2$; $18.5/Re^{2.3}$ for $2 < Re < 500$; 0.44 for $500 < Re < 2 \times 10^5$	
c	Specific Heat	
H	Specific Enthalpy	
h	Latent Heat	
h_c	Convective Heat Transfer Coefficient	
k	Thermal Conductivity	
N	Exponent used in equation (8) = $9.4 \left[\frac{(1+0.15Re^{0.687})}{(1+0.253Re^{0.687})} \right]$	
P	Pressure	
p	Pitch of Fuel Pins	
$\dot{q}$	Fuel Fission Heating Rate, W/cm^3	
R	Radius of Fuel Particle	
r	Radius	
r_l	Radius of Liquid/Solid Interface	
Re	Reynolds number, $2R v_c - v_f \rho_c \epsilon / \mu_c$	
T	temperature	
t	Time	
U	Interface velocity	
V	Velocity	
v	Specific volume	
x	Quality	
Z	Distance in vertical direction	

Super Scripts

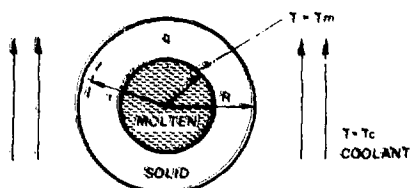
s	Solid fuel
l	Liquid fuel

Sub Scripts

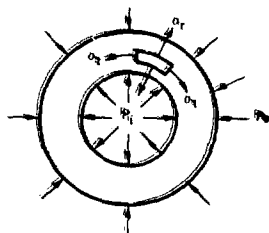
c	Coolant
f	Fuel
f_g	Fission gas
ls	Liquid-solid
lv	Liquid-vapor
m	Melting temperature

Greek Letters

α	Void fraction
α_{pc}	Coefficient of isobaric expansion of the coolant
β_{Tc}	Coefficient of isothermal compression of the coolant
ϵ	Volume fraction of the coolant in the interaction zone
μ	Viscosity
ρ	Density



(a) SOLIDIFICATION MODEL



(b) INDUCED STRESS MODEL

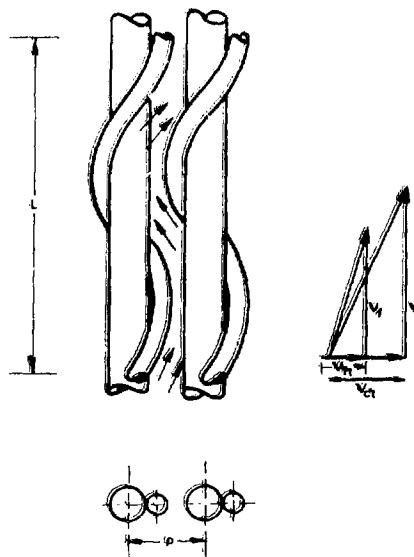


Fig. 1. Models for Solidification and Induced Thermal Stresses

Fig. 2. Geometric Arrangement of Fuel Pin and Wire Wrap

TABLE 1. HOPE Code Predicted Conditions at Failure

	2.4 cwns/sec 5% atom burnup	1.00 dollar/sec 0 % atom burnup
Failure height	~ 109.6 cm	109.6 cm
Initial temperature of FCI Zone	1361°K	1362°K
Initial pressure of FCI	5 atm	5 atm
Density of coolant above FCI Zone	.72 gm/c.c	.75 gm/c.c
Density of coolant below FCI Zone	.82 gm/c.c	.807 gm/c.c
Initial velocity of the upper interface of FCI	763.54 cm/sec	637.89 cm/sec
Initial velocity of the lower interface of FCI	786.6 cm/sec	822.73 cm/sec
Mass of fuel injected into FCI Zone	$= \frac{80}{.07} (\text{time}) \text{ gm}$ For time < .07 sec = 80 gm for time ≥ .07 sec	$= \frac{30}{.08} (\text{time}) \text{ gm}$ For time < .08 sec = 30 gm for time ≥ .08 sec
Mass of fission gas injected into FCI Zone	$= \frac{.0307}{.07} (\text{time}) \text{ gm}$ for time < .07 sec = .0307 gm for time ≥ .07 sec [3.6 × 5 (time) - 220 (time) ²]	$= \frac{.0007}{.08} (\text{time}) \text{ gm}$ for time < .08 sec = .0007 gm for time ≥ .08 sec = [26.5 - 25.5 (time)] × 808 $\frac{\text{W}}{\text{c.c}}$
Rate of heat generation for fuel particles	~ 808 W/c.c for time < .1 sec = 1535 W/c.c for time ≥ .1 sec	for time < .0237 sec = 808 W/c.c for time ≥ .0237 sec
Hydraulic diameter of coolant channel	.3894 cm	
Fuel pin height	284.48 cm (includes fission gas upper blanket, active core, lower blanket)	
Pressure at top of fuel pin	~ 1.5 atm	
		Pressure at bottom of fuel pin ~ 8 atm

TABLE 2. Assumed Particle Size Distribution

Percentage	Percentage of Total Fuel Ejected
0 - 100 μm	40%
100 - 250 μm	30%
250 - 1000 μm	30%

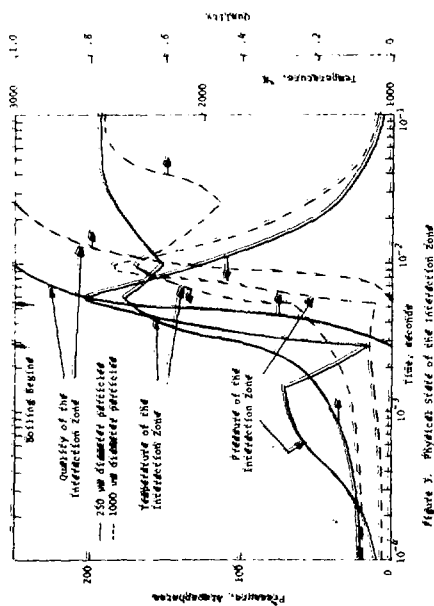


Figure 3. Physical State of the Interdication Zone

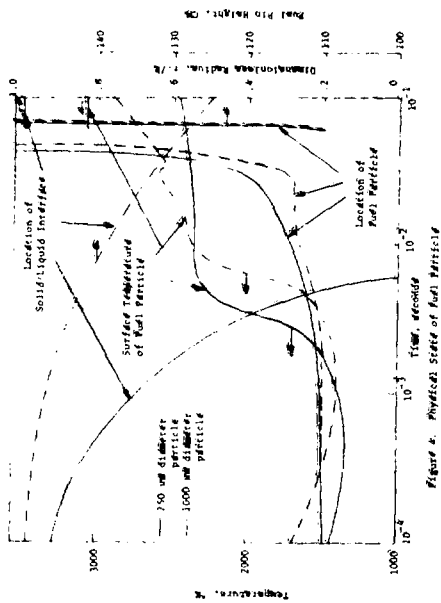


Figure 4. Physical State of Fuel Particle

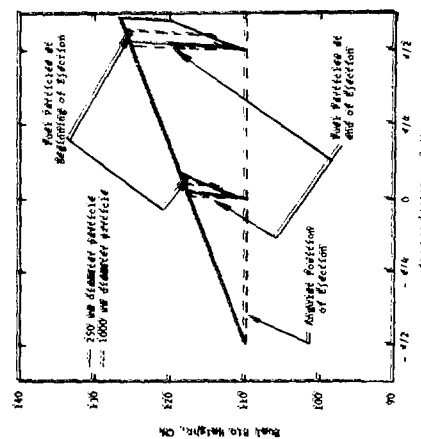


Figure 5. Trajectory of Fuel Particle

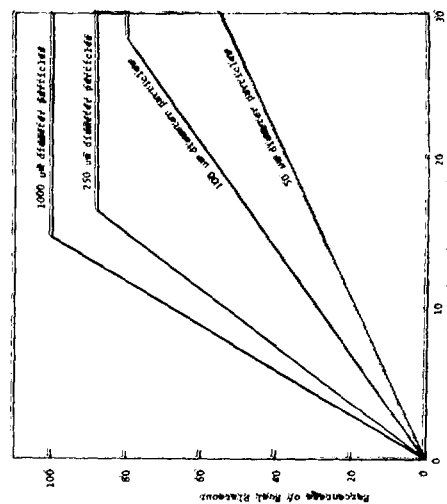


Figure 6. Angular Distance of Pin End Point of Pin Failure of Particles of Different Sizes

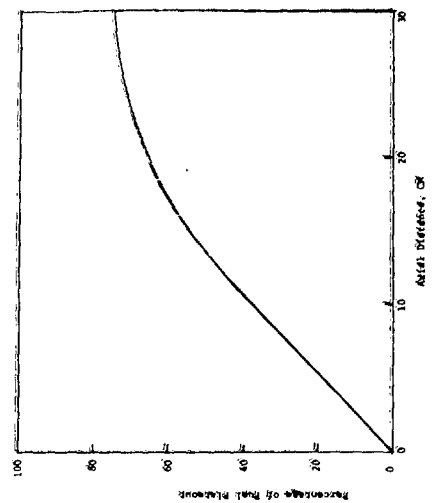


Figure 7. Angular Distance of Fuel Plateout from the Root of Failure

A HYDRODYNAMIC MODEL FOR TREATING TRANSIENT FREEZING IN PIN BUNDLES

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ABSTRACT

A one-dimensional treatment of incompressible two-component flow in pin bundles is presented. The model monitors the temporal evolution of the pin geometry due to ablation, crust formation, and growth at every axial position along the flow channel. Heat conduction and generation in the pin structure are included. The model is used to extract the dependence of the penetration distance of the fluid up to the freezing temperature on inlet pressure, inlet temperature, initial pin temperature, pin diameter, and the hydraulic diameter of the flow. Comparison with available experimental results is made. The model was used to calculate the penetration distance of molten core material in the axial blankets of a liquid metal fast breeder reactor during a hypothetical core disruptive accident.

1. INTRODUCTION

Freezing of core material as it enters cooler regions surrounding the core can have an important effect on the progression and outcome of a core disruptive accident in an LMFBR. During the initiating phase of an unprotected accident, cladding relocation and freezing can shut off coolant streaming paths [1]. This in turn can affect future motions of both steel and fuel within the subassembly. Further into the initiating phase, fuel pins break up and higher pressures in the core center result in fuel and/or steel motion out of the core [1]. The location and extent of the blockages that may form as this material freezes can have a profound effect on such things as power level and future ability to remove additional material as core disruption proceeds into the transition phases.

Available models [2,3,4] have provided insight into the possible physical phenomena that come into play during the penetration of hot fluids in and around rod bundles. Phenomena such as turbulent mixing, ablation and bulk freezing were credited for major influence on the fluid penetration distance (up to freezing). In fact, theoretical estimates of penetration distances obtained [2,4] indicate qualitative agreement with experiment [2]. More information is required, however, such as the blockage location, thickness, and the amount of ablation that has occurred at the time of freezing. This is especially important during the transition phase because these details affect the timing of the remelting process and possibly the whole accident scenario.

In this paper we describe a hydrodynamic code that provides this information and the dependence on such things as inlet pressure, fluid inlet temperature and size of flow channel. As it stands, the code is of a general nature as a research tool that can be applied to one-dimensional flow problems in different geometries and under a variety of conditions. An application of this work will provide a benchmark calculation for a simplified model to be implemented in TRANSIT [5], a code for the transition phase analysis of an LMFBR-HCDA.

In Section II we outline our basic assumptions. In Section III we describe the heat transfer models between solid regions and between solid liquid regions. The hydrodynamic equations are shown in Section IV. Our conclusions are presented in Section V.

II. MODELING ASSUMPTIONS FOR FLOW IN ROD BUNDLES

The geometry is very complex and at the onset we assume that the spacer wire wrapping each pin is smeared around the cladding, thus increasing the actual cladding thickness. The initial flow area was taken as the available flow area per pin in a subassembly.

The fluid was treated as an incompressible, one-dimensional annular flow driven by an inlet pressure. Moreover, it was assumed to be fully turbulent, and with the exception of a thermal boundary layer, the temperature profile there was assumed to be flat. In the hydrodynamic calculations, spatial differencing was based on the MAC method [6]. Time advance was done using an explicit scheme.

An axisymmetric (r, z) Eulerian grid was set up in the fuel pellet, clad, and annulus. The radial nodes (except the outermost) in the fuel pellet were chosen such that they encompass equal volumes of fuel. A similar grid was chosen for the cladding. The node in contact with the fluid was chosen such that the melting process is independent of the node size.

The fluid material that is leaving the core is assumed to be a uniform mixture of fuel and steel at thermal equilibrium with each other. The density of the mixture was taken to be the smear density of the available mass of fuel and steel in an LMFBR subassembly. The thermal conductivity of the fuel/steel mixture was taken to be [2]

$$k = k_f \frac{\alpha(k_s - k_f)}{(1 - \alpha)^{1/3} k_s + (\alpha)^{1/3} k_f} \quad (1)$$

where α is the steel volume fraction.

III. MULTI-REGION HEAT TRANSFER

A. Heat Transfer Between Liquid and Solid Regimes

The heat transfer mechanism between the fluid and the solid structure is by convection. The local convection heat transfer is given by

$$h = \frac{k \text{Nu}}{D_h}, \quad (2)$$

where the hydraulic diameter D_h is given by

$$D_h = \frac{2(r_{oa}^2 - r^2)}{r}, \quad (3)$$

The interface radius decreases when ablation occurs and increases when freezing occurs. In the event of complete cladding ablation, the molten fuel will move against the fuel pellet's surface. The Nusselt number Nu in Eq. (2) depends on whether melting is occurring or not. If the clad interface node is melting then [7]

$$Nu = Nu_o \frac{\ln(1 + B_{ms})}{B_{ms}} \quad (4)$$

where B_m is the transport number for melting given by

$$B_{ms} = \frac{C_{ps}(T - T_{ms})}{L_{hs} + C_{ps}(T_{ms} - T_{os})} \quad (5)$$

and Nu_o is the Nusselt number for the no-melting surface. Based on boundary layer theory and Reynolds' analogy, von Kármán obtained

$$Nu_o = \frac{1/2(Re \text{Pr} f)}{1 + 5 \sqrt{0.5f} : (\text{Pr} - 1) + \ln [1 + 5/6 (\text{Pr} - 1)]} \quad (6)$$

where f is the friction factor defined below. Experimental results support Eq. (6) for $\text{Pr} \geq 1$. If the clad were completely ablated away, relations similar to (4) and (5) then apply.

B. Equations of Heat Transfer in the Solid Regions

Heat conduction and generation in the solid structure was monitored. The latent heat of melting was also incorporated. For any particular node, the equations described below will hold as long as the material involved is in the solid form. After melting, energy, momentum and mass will be governed by the hydrodynamic equations as described in Section IV below.

At the center of the fuel pin, the heat conduction and generation equations in the pellet (for example) at axial position i are

$$\rho_{fs} C_{pfs} \frac{dT_f(i,1)}{dt} (r_2^2 - r_1^2) = Q_B (r_2^2 - r_1^2) + 2k_{fs} r_2 \left. \frac{dT_f}{dr} \right|_{r_2} \quad (7)$$

At the inner fuel zones ($j = 2, N - 1$) the temperature satisfies

$$\rho_{fs} C_{pfs} \frac{dT_f(i,j)}{dt} (r_{j+1}^2 - r_j^2) = Q_B (r_{j+1}^2 - r_j^2) - 2k_{fs} r_j \left. \frac{dT_f}{dr} \right|_{r_j} + 2k_{fs} r_{j+1} \left. \frac{dT_f}{dr} \right|_{r_{j+1}} \quad (8)$$

At the fuel pellet's outermost zone we have

$$\rho_{fs} C_{pfs} \frac{dT_f(i,N)}{dt} (r_{N+1}^2 - r_N^2) = Q_B (r_{N+1}^2 - r_N^2) - 2k_{fs} r_N \left. \frac{dT_f}{dr} \right|_{r_N} - H_B (T_f(i,N) - T_s(i,1)) \quad (9)$$

If the cladding is completely melted away, then Eq. (9) becomes

$$\rho_{fs} C_{pfs} \frac{dT_f(i,N)}{dt} (r_{N+1}^2 - r_N^2) = Q_B (r_{N+1}^2 - r_N^2) - 2k_{fs} r_N \left. \frac{dT_f}{dr} \right|_{r_N} + 2r_{N+1} q_{ss} \quad (10)$$

The power generated in the pin per unit volume was assumed to be constant in time. Axial variation of the blanket pin power was not incorporated because its effect is negligible. The power generated in the molten material was chosen to be the power level at the blanket core interface.

C. Equation For Crust Formation

The equation for the development of the crust can be derived from conservation of energy equations to be

$$\frac{dr_c}{dt} = \frac{1}{2(C_p \bar{T} - L_h)r_c} \left[Q_n(t) - \frac{Q}{\rho} - C_p \frac{d\bar{T}}{dt} (r_c^2 - r_{sc}^2) \right] \quad (11)$$

where

$$Q_n = \frac{r^2}{r_0} (q_{ms} r_{0r} - q_{ss} r_{sr}), \quad (12)$$

IV. HYDRODYNAMIC EQUATIONS FOR THE MOLTEN REGION

The equations of mass, momentum, and energy conservation governing the molten material and the ablation of solid material are:

$$\rho \frac{\partial v}{\partial z} = S_m \quad (13)$$

$$\rho \frac{\partial v}{\partial t} + \rho v \frac{\partial v}{\partial z} + \frac{\partial p}{\partial z} = S_M - v \frac{\partial v}{\partial z} \quad (14)$$

$$\rho \frac{\partial e}{\partial t} + \rho v \frac{\partial e}{\partial z} = S_E - \rho v \frac{\partial v}{\partial z} = S_E - v S_m \quad (15)$$

and the pressure satisfies a Poisson equation

$$\frac{\partial^2 p}{\partial z^2} = \frac{\partial S_M}{\partial z} - \frac{\partial S_m}{\partial t} - 2\rho \left(\frac{\partial v}{\partial z} \right)^2 - 2\rho v \left(\frac{\partial^2 v}{\partial z^2} \right). \quad (16)$$

Here, S_m , S_M , and S_E are mass, momentum, and energy "source" terms, the value of which depends on the processes underway (i.e., melting, freezing,...). In Eqs. (13-15), we have neglected friction and pressure terms in the energy equations since their contribution to $\dot{e}$ is small ($\dot{e} \gg C_p T$). In the event that steel ablation is occurring (whether a crust forms or not), then

$$S_m = \frac{1}{A} \frac{\partial A}{\partial t} (\rho_s - \rho) - \frac{\rho v}{A} \frac{\partial A}{\partial z} \quad (17)$$

$$S_M = \frac{\rho v}{A} \frac{\partial A}{\partial t} - \frac{\rho v^2}{A} \frac{\partial A}{\partial z} - (F_k + F_g) \quad (18)$$

$$S_E = \frac{1}{A} \frac{\partial A}{\partial t} (\rho_s C_{ps} T_{ms} + \rho C_p T) - \rho \frac{\partial p}{\partial z} T \frac{\partial A}{\partial z} + Q - \frac{2-r}{A} q_{ms}. \quad (19)$$

In case fuel freezing is occurring while the steel next (or pellet) to it has not melted, the source terms are almost identical to the case above, viz.

$$S_m = \frac{1}{A} \frac{\partial A}{\partial t} (\rho_{fs} - \rho) - \frac{\rho v}{A} \frac{\partial A}{\partial z} \quad (20)$$

$$S_M = \frac{v}{A} \frac{\partial A}{\partial t} (\rho_{fs} - \rho) - \frac{(\rho v)}{A} \frac{\partial A}{\partial z} - (\bar{F}_k + \bar{F}_g) \quad (21)$$

$$S_E = \frac{1}{A} \frac{\partial A}{\partial t} (\rho_{fs} C_{pfs} T_{mf} - \rho C_p T) - \rho \frac{v C_p T}{A} \frac{\partial A}{\partial z} + Q - \frac{2\pi r}{A} q_{ms} \quad (22)$$

In case no freezing or melting is occurring then the source terms are

$$S_m = - \frac{\rho v}{A} \frac{\partial A}{\partial z} \quad (23)$$

$$S_M = - \frac{\rho v^2}{A} \frac{\partial A}{\partial z} - (\bar{F}_k + \bar{F}_g) \quad (24)$$

$$S_E = - \frac{\rho v C_p T}{A} \frac{\partial A}{\partial z} + Q - 2\pi r q_{ms} \quad (25)$$

In the above equations $\bar{F}_k$ represents the friction force per unit length. We assume the solid-liquid interface to be smooth [3] and thus the turbulent flow, the Blasius formula [8] for the friction force per unit (channel) length gives

$$\bar{F}_k = \pi r \rho v^2 f \quad (26)$$

The friction factor f was taken to be

$$f = 0.0316 / \text{Re}^{0.2} \quad (27)$$

For Reynolds' numbers of interest ($\sim 10^5$), this expression is of the same order of magnitude as other expressions cited in the literature [8]. The sensitivity of our work and its dependence on the friction factor was conducted. On the other hand, $\bar{F}_g$ is the gravitational force per unit length.

V. RESULTS AND CONCLUSIONS

By means of the model described above, the dependence of the penetration distance of molten material on various flow and underlying pin conditions was investigated. The first of these conditions was the inlet pressure. The results, shown in Fig. 1, indicate a rather weak dependence on inlet pressure and hence material velocity over a wide range of pressures. This can be expected if momentum effects are not important, since heat transfer is only weakly dependent on velocity (see Eqs. (6) and (27)). As pressure is increased momentum effects become important and the penetration distance increases sharply. A calculation with an inlet pressure of 5 MPa resulted in an extremely large penetration distance. The effect of the inlet temperature for an inlet

pressure of 0.7 MPa and an initial cladding temperature of 1173 K is shown in Fig. 2. The linear dependence exhibited is consistent with both the bulk freezing [2] and ablation [4] models. The dependence of the penetration distance on the hydraulic diameter is shown in Fig. 3 for an inlet temperature of 3273 K, inlet pressure of 0.7 MPa, and an initial cladding temperature of 1173 K. The penetration distance for various initial cladding temperatures is shown in Fig. 4. Two processes compete here. The cooler the cladding the larger is the heat transfer from the fluid, which would indicate earlier freezing. On the other hand, the closer the cladding temperature is to the steel melting point, the more cladding will melt and mix with the fluid, cooling it down. Our results indicate that the ablation process, when it occurs, dominates. The dependence of the penetration distance on the clad thickness is shown in Fig. 5. The results seem to support the importance of ablation. One would expect penetration to decrease with increasing thickness because of the accompanying increase in heat capacity. However, when melting occurs the higher heat capacity inhibits the inward progression of the melt front and hence lowers the ablation rate.

Available experimental results [2] were compared with calculations using this model. In the experiments, a mixture of molten uranium oxide, molybdenum, and noncondensable gases produced by a thermite reaction were injected into a bundle of seven hollow pins. The penetration distances were obtained for various initial cladding temperatures. In Table I we compare our results for flow of liquid uranium oxide with the experimental results.

Next, we looked at flow of molten core material between LMFBR axial blanket pins under conditions expected in a core disruptive accident caused by an unprotected loss of coolant flow. No experimental results are available under these conditions. The fluid was taken to be a uniform mixture of uranium oxide and stainless steel in the proportion at which they are found in an LMFBR pin. The computation was stopped when the temperature of the molten material reached the freezing temperature of fuel. The results of the calculations are shown in Table II.

The results reported in this paper are consistent with steel ablation and entrainment being the dominant mechanism for freezing of flowing material. The freezing process does, however, exhibit a complicated dependence upon crust formation. It has been pointed out [3] that the mechanical properties of the crust and the degree of turbulence can have an effect on crust stability. In our model the stability of the crust was dependent on whether the underlying steel melted or not. Since steel always melted in these calculations the crust was always assumed to be swept into the flowing material. Additional experimental evidence is required to determine the importance of this effect.

In conclusion, the results presented here indicate that for conditions expected at the beginning of the transition phase [1], freezing always occurs and the penetration distances are probably less than LMFBR axial blanket dimensions. Further into the transition phase the molten material has a larger fraction of steel, due to subassembly wall melting. With this change in composition, a point might be reached where the freezing temperature of the mixture is not properly described by the freezing temperature of uranium oxide. This could drastically increase the penetration distance.

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It is a pleasure to thank W. Bohl and M. Epstein for their continued interest throughout this work. We also like to extend our acknowledgment to H. Wider for valuable comments and discussion and J. M. Kyser for help

in the programming aspect of this work. This work was performed under the auspices of the U. S. Energy Research and Development Administration.

NOMENCLATURE

A	Flow area
B_{ms}	Melting transport number
C_p	Specific heat of fluid
C_{ps}	Specific heat of molten steel
C_{pss}	Specific heat of solid steel
C_{pf}	Specific heat of molten uranium oxide
C_{pfs}	Specific heat of solid uranium oxide
D_h	Hydraulic diameter
F_k	Friction force per unit length
F_g	Gravitation force per unit length
f	Friction factor (skin friction coefficient)
h	Fluid to structure heat transfer coefficient
H_B	Gap conductance (between fuel pellet and cladding)
k_s	Thermal conductivity of molten steel
k_f	Thermal conductivity of molten uranium oxide
k_{fs}	Thermal conductivity of solid uranium oxide
k	Thermal conductivity of fluid
L_h	Latent heat of uranium oxide
N	Initial number of nodes in the pellet
Nu_o	Nusselt number for solid steel
Nu	Nusselt number for molten steel
Pr	Prandtl number
q_{ms}	Power transferred per unit area from fluid to structure
q_{ss}	Power transferred per unit area from crust to structure
Q	Power generated per unit volume in fluid
Q_B	Power generated in blanket fuel per unit volume
Re	Reynolds number
r_c	Outer radius of crust
r_{sc}	Inner radius of crust
r_{oa}	Outer radius of annulus
r	Radius at the solid-liquid interface
r_i	Radii of different nodal positions in pellet
S_E	Energy source term
S_m	Mass source term
S_M	Momentum source term
$\bar{T}$	Average temperature of crust

in the programming aspect of this work. This work was performed under the auspices of the U. S. Energy Research and Development Administration.

NOMENCLATURE

A	Flow area
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C_{pfs}	Specific heat of solid uranium oxide
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F_k	Friction force per unit length
F_g	Gravitation force per unit length
f	Friction factor (skin friction coefficient)
h	Fluid to structure heat transfer coefficient
H_B	Gap conductance (between fuel pellet and cladding)
k_s	Thermal conductivity of molten steel
k_f	Thermal conductivity of molten uranium oxide
k_{fs}	Thermal conductivity of solid uranium oxide
k	Thermal conductivity of fluid
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r_{sc}	Inner radius of crust
r_{oa}	Outer radius of annulus
r	Radius at the solid-liquid interface
r_i	Radii of different nodal positions in pellet
S_E	Energy source term
S_m	Mass source term
S_M	Momentum source term
$\bar{T}$	Average temperature of crust

T	Temperature of fluid
T_{ms}	Melting Temperature of steel
T_{os}	Bulk temperature of cladding
$T_f(i,j)$	Temperature of fuel pellet at i-th axial position and j-th radial node
$T_s(i,j)$	Temperature of cladding at i-th axial position and j-th radial node
v	Velocity of fluid
X_p	Penetration distance

Greek Symbols

α	Steel volume fraction
ϵ	Energy of molten material per unit mass
ρ_f	Density of molten uranium oxide
ρ_{fs}	Density of solid uranium oxide
ρ_s	Density of molten steel
ρ_{ss}	Density of solid steel
ρ	Density of fluid

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TABLE I

The Penetration Distances of Molten Uranium Oxide for Different Initial Cladding Temperatures. Inlet Temperature was 3473°K and Inlet Velocity was 1500 cm/sec.

Initial Clad Temperature K	Penetration Distance (m)	
	Experiment (2)	This Work
293	0.09	0.13
693	0.20	0.23
1073	0.20-25	0.25
1173	0.23	0.25

TABLE II

The Penetration Distance of Molten Core Material into an LMFBR Axial Blanket for Various Inlet Temperatures and Pressures

Inlet Temperature (K)	Penetration Distance (m)	
	Inlet Pressure	
	1 MPa	2 MPa
3200	0.027	0.057
3300	0.038	0.074
3400	0.053	0.111
3500	0.090	0.138
3600	0.111	0.162

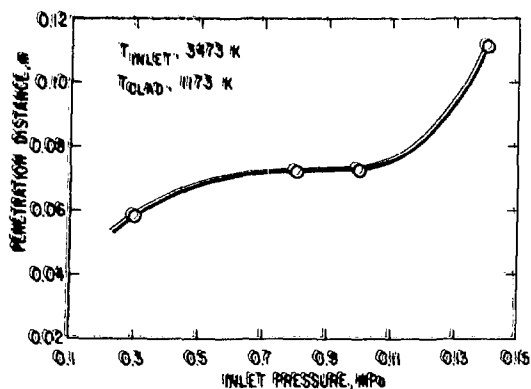


Fig. 1. The dependence of the penetration distance on inlet pressure.

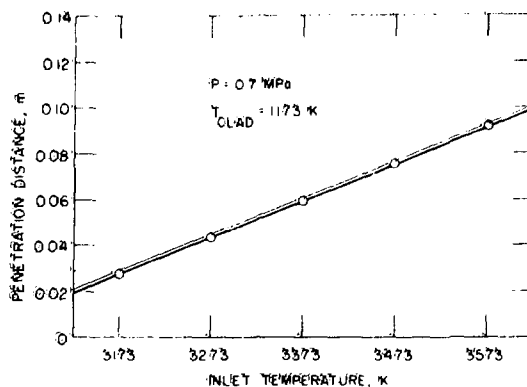


Fig. 2. The dependence of the penetration distance on inlet temperature.

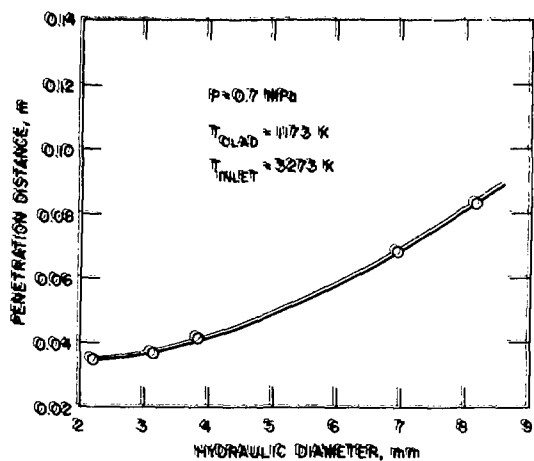


Fig. 3. The dependence of the penetration distance on the hydraulic diameter.

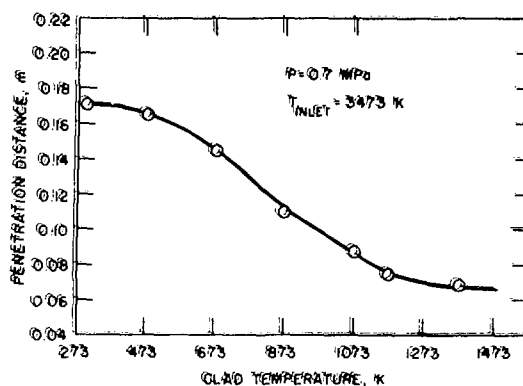


Fig. 4. The dependence of the penetration distance on the initial cladding temperature.

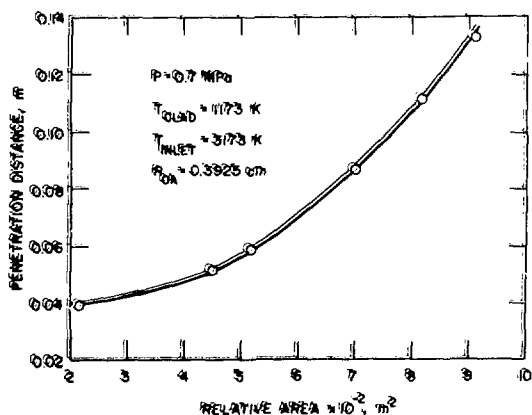


Fig. 5. The dependence of the penetration on the cladding cross sectional area. Relative area in the figure is the actual area/ π .

A COMPUTER PROGRAM FOR TRANSIENT FREEZING FLOW
INTO A CHANNEL AND ITS APPLICATION TO
LMFBR SAFETY ANALYSIS*

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ABSTRACT

The transient freezing of a flowing liquid into an originally empty cylindrical channel is discussed in terms of a computer program. In this work suitable transformations of the space variables are made to fix the position of the freezing moving boundary. The resultant nonlinear partial differential equations containing terms involving the position of the moving boundary and its time derivative are solved by numerical methods involving iteration schemes to determine the position of the moving boundary. The physical picture of the present model is based on the notion that transient freezing is governed mainly by heat conduction through a growing frozen layer at the wall, which could eventually plug the channel. In this model, radial temperature distributions in the liquid, frozen and wall regions are computed by solving simultaneously the coupled two-dimensional convection and conduction equations with appropriate boundary conditions. Various options for the outside boundary conditions and for the mesh sizes in the liquid, frozen or the wall regions have been built into the program to enable one to address different physical phenomena. Application of this model to molten fuel flow in core-disruptive accident conditions in LMFBRs is discussed.

INTRODUCTION

The relocation dynamics of molten fuel (and/or steel) material from the core region following a core-disruptive accident play an important role in the accident analysis of a Liquid Metal Fast Breeder Reactor (LMFBR).¹ A significant fuel flow into the upper plenum region at an early stage of the loss-of-flow accident, for example, could considerably influence the course of the accident. If the fuel can pass up through and out of the top of the subassemblies, it may be discounted from further criticality considerations.² In the context of post-accident heat removal, a similar transient freezing problem needs to be investigated in connection with the downward flow of molten fuel-steel mixture through the shielding region below the core. If the core debris freezes within the channels below the core, the downward relocation will be delayed until a remelting

*This work was done under the auspices of the U.S. Nuclear Regulatory Commission.

of the plug occurs. This delay would be added to cause for the post-accident heat removal problem.

In this paper, an analysis of fluid flow in a cylindrical channel is made by considering a boundary value problem with convective and conductive heat transfer for the liquid region and a pure conduction heat transfer for the frozen region and the wall. Radial temperature distributions in the liquid region, frozen region and the wall are computed by solving the coupled two-dimensional convection and conduction equations using the finite difference approach. Time and position-dependent volumetric heat sources within the liquid and frozen regions are included in the program. The change in the liquid velocity due to freezing is also taken into account. Now, to solve the freezing problem, one should first determine the distance at which the liquid starts to freeze by solving a two-region boundary value problem. This was discussed in our previous paper.⁵ As soon as freezing takes place, the problem reduces to a three-region heat transfer problem, which is the major concern of this paper.

The major difficulty encountered in fluid flow and heat transfer involving freezing arises from the fact that the frozen boundary changes with both time and the axial position. If one uses a fixed radial mesh size, the finite difference equations for the frozen layer become difficult to handle, and may cause numerical instabilities, particularly when the frozen layer is very thin. In this study this problem is alleviated by transforming the energy equations in such a way that the moving boundary is stationary in terms of a dimensionless radial space variable. Although this transformation converts the original linear partial differential equations to a set of nonlinear partial differential equations containing terms involving the position of the moving boundary and its time derivative, the numerical solution of these nonlinear equations does not present any serious difficulty.⁴ The details of the methods used and the physical assumptions of the present freezing model are explained in the following sections. The model has been applied to analyze a freezing problem with Wood's metal and compared with the experimental data. Some applications of the model for LMFBR accident analyses are also discussed.

THE MATHEMATICAL FORMULATION

The physical system under consideration is sketched in Fig. 1, where R_0 and c denote, respectively, the inner radius of the channel and the wall thickness. Liquid at a specified initial temperature and velocity enters the channel, and as it moves up along the z -axis, it loses heat to the wall and the surroundings, forming a frozen layer. Attention is focused upon the phenomena from the time when the frozen layer is just formed at the inner wall of the channel. The governing energy equation for the flowing liquid including the effect of variable fluid velocity and internal heat generation can be written as

$$\rho_f c_f \left(\frac{\partial T_f}{\partial t} + u(t) \frac{\partial T_f}{\partial z} \right) = k_f \left(\frac{\partial^2 T_f}{\partial r^2} + \frac{1}{r} \frac{\partial T_f}{\partial r} \right) + Q_f(r, z, t) \quad (1)$$

(Liquid region)

The conduction equations for the frozen layer with an internal heat source, and for the channel wall, are given by

$$\rho_f c_f \frac{\partial T_f}{\partial t} = k_f \left(\frac{\partial^2 T_f}{\partial r^2} + \frac{1}{r} \frac{\partial T_f}{\partial r} \right) + Q_f(r, z, t) \quad (2)$$

(Frozen layer)

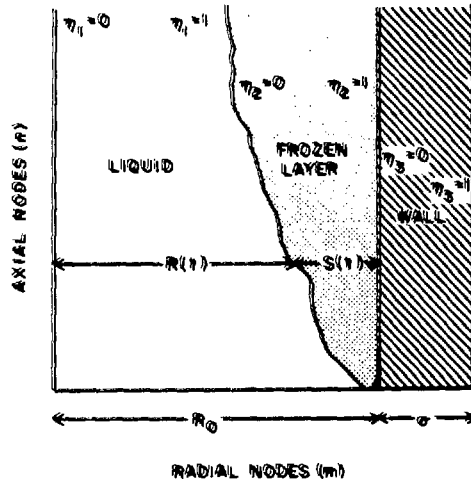


Figure 1. The Coordinate system used in the three-region freezing analysis.

and

$$\rho_s c_s \frac{\partial T_s}{\partial t} = k_s \left(\frac{\partial^2 T_s}{\partial r^2} + \frac{1}{r} \frac{\partial T_s}{\partial r} \right) \quad (3)$$

(Wall region)

The subscripts l , f , s refer, respectively to the liquid, the frozen and the wall region; ρ , c , k , T , u and Q denote the density, specific heat, thermal conductivity, temperature, liquid velocity and the internal heat generation, respectively. By letting $s(z, t)$ be the thickness of the frozen layer with $R(z, t) = R_0 - s(z, t)$, the appropriate boundary conditions to the above set of energy equations can be written as follows:

$$\frac{\partial T_l}{\partial r}(0, z, t) = 0, \quad T_l(r, 0, 0) = T_{in} \quad (4)$$

where T_{in} refers to the inlet liquid temperature.

$$k_l \left(\frac{\partial T_l}{\partial r} \right)_{R(z, t)} - k_f \left(\frac{\partial T_f}{\partial r} \right)_{R(z, t)} = \rho_l \lambda \frac{ds}{dt} \text{ and } T_l|_{R(z, t)} = T_f|_{R(z, t)} = T_{fr} \quad (5)$$

$$T_f(r, 0, 0) = T_{fr} \quad (6)$$

$$T_f|_{R_0} = T_s|_{R_0} \text{ and } k_f \left(\frac{\partial T_f}{\partial r} \right)_{R_0} = k_s \left(\frac{\partial T_s}{\partial r} \right)_{R_0} \quad (7)$$

$$T_s(r, 0, 0) = T_0 \quad (8)$$

$$k_s \left(\frac{\partial T_s}{\partial r} \right)_{R_0 + \delta} = h(T_s - T_{\text{surr}}) \quad (9)$$

In Eq. (5), λ represents the latent heat of fusion, and in Eq. (9), h and T_{surr} denote, respectively, the heat transfer coefficient and the surrounding temperature. To transform the above set of energy equations and the boundary conditions to those based on a coordinate system yielding a fixed dimensionless space variable for the moving boundary, the following changes of variables are made:

$\theta_i = (T_i - T_0)/(T_{\text{in}} - T_0)$, where i stands for l , f and s , respectively, and T_0 denotes the initial wall temperature; $\xi = r/R_0$, $\zeta = z/L$, where L is the channel length; and $\eta_1 = \xi/[1-s'(t)] = \xi/R'(t)$, $\eta_2 = 1 + (\xi-1)/s'(t)$, $\eta_3 = (\xi-1)/c'$, where $s'(t) = s(t)/R_0$, $R'(t) = R(t)/R_0$ and $c' = c/R_0$. It is to be noted that the dimensionless radial distance variables, η_1 , η_2 and η_3 each takes values between 0 and 1 in the respective region, and that the moving boundary corresponds to $\eta_1 = 1$ (or $\eta_2 = 0$) always. The transformed equations and the boundary conditions are summarized in the following.

$$\begin{aligned} \{1 - s'(t)\}^2 \left(\frac{\partial \theta_l}{\partial t} + \frac{u(t)}{L} \frac{\partial \theta_l}{\partial \zeta} \right) &= \frac{k_l}{\rho_l c_l R_0} \left\{ \frac{\partial^2 \theta_l}{\partial \eta_1^2} + \frac{1}{\eta_1} \frac{\partial \theta_l}{\partial \eta_1} \right\} \\ &- \eta_1 \{1 - s'(t)\} \frac{ds'}{dt} \frac{\partial \theta_l}{\partial \eta_1} + \frac{\{1 - s'(t)\}^2 Q_l(\eta_1, \zeta, t)}{\rho_l c_l (T_{\text{in}} - T_0)} \end{aligned} \quad (10)$$

(Liquid region)

$$\begin{aligned} s'(t)^2 \frac{\partial \theta_f}{\partial t} &= \frac{k_f}{\rho_f c_f R_0} \left\{ \frac{\partial^2 \theta_f}{\partial \eta_2^2} + \frac{1}{\eta_2 - 1 + (1/s'(t))} \frac{\partial \theta_f}{\partial \eta_2} \right\} \\ &+ (\eta_2 - 1) s'(t) \frac{ds'}{dt} \frac{\partial \theta_f}{\partial \eta_2} + \frac{s'(t)^2 Q_f(\eta_2, \zeta, t)}{\rho_f c_f (T_{\text{in}} - T_0)} \end{aligned} \quad (11)$$

(Frozen layer)

$$\frac{\partial \theta_s}{\partial t} = \frac{k_s}{\rho_s c_s R_0} \left\{ \frac{\partial^2 \theta_s}{\partial \eta_3^2} + \frac{1}{\eta_3 + (1/c')} \frac{\partial \theta_s}{\partial \eta_3} \right\} \quad (12)$$

(Wall region)

$$\frac{\partial \theta_l}{\partial \eta_1} (0, \zeta, t) = 0, \quad \theta_l(\eta_1, 0, 0) = \theta_{\text{in}} \quad (13)$$

At $\eta_1 = 1$, the moving boundary,

$$\frac{\partial \theta_l}{\partial \eta_1} - \left(\frac{1}{s'(t)} - 1 \right) \left(\frac{k_f}{k_l} \right) \left(\frac{\partial \theta_f}{\partial \eta_2} \right)_{\eta_2=0} = \frac{\rho_l R_0^2 (1 - s'(t))}{(T_{\text{in}} - T_0) k_l} \frac{ds'}{dt}$$

$$\text{and} \quad \theta_l = \theta_f = \theta_{\text{fr}} \quad (14)$$

$$\theta_f(\eta_2, 0, 0) = \theta_{\text{fr}} \quad (15)$$

At $\eta_2 = 1$: $\theta_s = \theta_f$, and

$$\left(\frac{\partial \theta_f}{\partial \eta_2}\right)_{\eta_2=1} - \left(\frac{k_s}{k_f}\right) \left(\frac{s'(t)}{\sigma'}\right) \left(\frac{\partial \theta}{\partial \eta_2}\right)_{\eta_2=0} \quad (16)$$

$$\theta_s(\eta_2, 0, 0) = 0 \quad (17)$$

At $\eta_3 = 1$:

$$\left(\frac{\partial \theta_s}{\partial \eta_3}\right)_{\eta_3=1} = \frac{h\sigma}{k_s} \left(\theta_s|_{\eta_3=1} - \theta_{surr}\right) \quad (18)$$

THE NUMERICAL METHODS

To solve Eqs. (10) through (18) numerically for the determination of the temperature fields in the three regions and the position of the frozen boundary, $s(t)$, the derivatives appearing in these equations are rewritten in terms of finite difference expressions such as

$$\frac{\partial \theta}{\partial \eta_1} = \frac{1}{4\Delta\eta_1} [\theta_{m+1,n}^{p+1} - \theta_{m-1,n}^{p+1} + \theta_{m+1,n}^p - \theta_{m-1,n}^p]$$

$$\frac{\partial^2 \theta}{\partial \eta_1^2} = \frac{1}{2(\Delta\eta_1)^2} [\theta_{m+1,n}^{p+1} - 2\theta_{m,n}^{p+1} + \theta_{m-1,n}^{p+1} + \theta_{m+1,n}^p - 2\theta_{m,n}^p + \theta_{m-1,n}^p]$$

$$\frac{\partial \theta}{\partial t} = \frac{\theta_{m,n}^{p+1} - \theta_{m,n}^p}{\Delta t}, \quad \frac{ds'}{dt} = \frac{s'^{p+1} - s'^p}{\Delta t}$$

$$s' = \frac{1}{2} (s'^p + s'^{p+1}), \text{ etc.}$$

where m and n designate the radial and axial node number respectively, and p refers to time. The finite difference expression for Eq. (10), for example, was obtained as follows:

$$\begin{aligned} -K_1 \theta_{m+1,n}^{p+1} + K_2 \theta_{m,n}^{p+1} - K_4 \theta_{m-1,n}^{p+1} &= K_1 \theta_{m+1,n}^p + K_3 \theta_{m,n}^p \\ &+ K_4 \theta_{m-1,n}^p + K_5 \theta_{m,n-1}^p + K_6 Q_d \end{aligned} \quad (19)$$

where

$$K_1 = \frac{k_d \Delta t}{2\rho_d c_d R_o^2 (\Delta\eta_1)^2} \left(1 + \frac{1}{2(m-1)}\right) + \frac{(m-1)}{8} (R'^{2,p+1} - R'^{2,p})$$

$$K_2 = \frac{1}{4} (R'^{2,p} + R'^{2,p+1})^2 + \frac{k_d \Delta t}{\rho_d c_d R_o^2 (\Delta\eta_1)^2}$$

$$K_3 = \frac{(R'^{2,p} + R'^{2,p+1})^2}{4} \left[1 - \frac{\Delta t (u_n^{p+1} + u_n^p)}{2L\Delta t}\right] - \frac{k_d \Delta t}{\rho_d c_d R_o^2 (\Delta\eta_1)^2}$$

$$K_4 = \frac{k_L \Delta t}{2r_L c_L R_o^2 (\Delta T_o)} \left(1 - \frac{1}{2(m-1)} \right) - \frac{(m-1)}{8} (R^{p+1} - R^p)$$

$$K_5 = \frac{(u_n^{p+1} + u_n^p)(R^p + R^{p+1})^2 \Delta t}{8L\Delta\zeta}$$

$$K_6 Q_L = \frac{(R^p + R^{p+1})^2 (Q_{m,n}^{p+1} + Q_{m,n}^p)}{8r_L c_L (T_{in} - T_o)}$$

Similar expressions can be obtained for the energy equations, Eqs. (11) and (12). It is to be noted that the left side of Eq. (19) contains only terms involving the nodal temperatures for the new time step, such as $t_{m,n}^{p+1}$ or $t_{m+1,n}^{p+1}$, etc. All the terms appearing on the right side are known from the old time step, except R^{p+1} ($= 1 - s^{p+1}$). These equations and the boundary conditions eventually yield a set of simultaneous equations which can be written in a matrix form:

$$\vec{AG} = B \quad (20)$$

where A denotes a square matrix containing the coefficients for the new temperature such as $-K_1, K_2$, etc., appearing on the left side of Eq. (19), $\vec{G}$ is a column matrix containing the unknown nodal temperatures and B is a column matrix, the element of which is given for example, by the right-hand side of Eq. (19). This equation can be solved provided R^{p+1} is known. The numerical solution of Eq. (20), therefore, involves an iteration scheme starting with a guess value for R^{p+1} . The temperature field G is first solved for the trial value of R^{p+1} . The temperature at the freezing boundary is then compared with the freezing temperature, and a new guess for R^{p+1} is made. This process is repeated until the freezing boundary is at the freezing temperature.

Before proceeding to solve a particular problem, the liquid velocity and the heat source term appearing in Eqs. (10) and (11) must be specified. It is clear that with the assumption of incompressible flow, the velocity field in the present freezing problem should be obtained by solving the momentum and continuity equations. However, for simplicity an analytical expression for the velocity as a function of the frozen area has been derived for use in this work. Several simplifying assumptions have been made in this approach. The instantaneous velocity of the leading edge is obtained by equating the sum of the pressure drops in the penetration distance, X, and the frozen region, Y, to the total pressure drop expected if there was no freezing. Assuming a laminar flow condition and the same density for the liquid and the frozen regions, we then have

$$v_i(X+Y)/d_L^3 = v_f X/d_L^3 + v_f Y/d_f^3 \quad (21)$$

where v_i is the initial velocity of the liquid and v_f the velocity when there is freezing; d_L and d_f indicate the average diameters of the liquid and the frozen regions, respectively. This finally produces

$$v_f = v_i / [L + (1-L)/a_f] \quad (22)$$

where L is the fraction of total length frozen $Y/(X+Y)$ and a_f is the fractional area available for liquid flow. This formula indicates that the velocity depends

on both the area blocked in the channel and the total length frozen. The velocity is zero when plugging of the channel is complete. This is illustrated in Fig. 2.

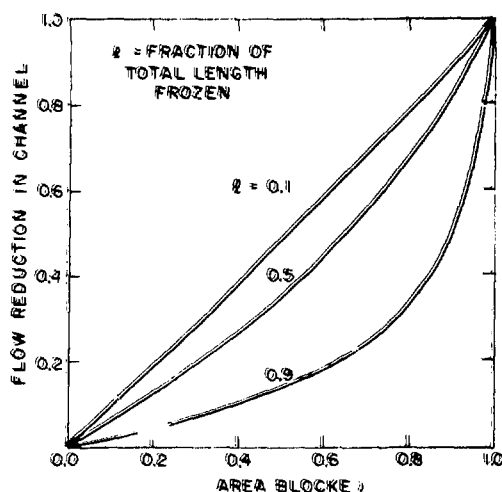


Figure 2. The change of liquid velocity shown as a function of the frozen area.

APPLICATIONS AND DISCUSSION OF RESULTS

To examine the computer program for the transient freezing flow into a channel (TRAFFIC), we have considered the Wood's metal experiment performed by Cheung and Baker.⁵ In this test Wood's metal at 100°C was allowed to flow by a constant liquid head into a copper coil of 0.476 cm ID immersed at a coolant bath at -196°C. The average flow velocity of the liquid was 16 cm/sec, and the penetration distance, measured by taking the difference in weights of the copper tubing before and after the penetration, was 9.9 cm. Before discussing the results, let us point out two sources of uncertainties in the computer simulation of the experiment: First, since the liquid coolant was stagnant, it is hard to know if the outside wall was at the coolant temperature; secondly, the tube being a helical coil, introduces further complications in the flow pattern and heat transfer as compared to a straight tube flow.

In the computer simulation of the freezing phenomena, it was found that the front temperature in the frozen region plays an important role. It is obvious that temperature gradient exists in the frozen region with the temperature at $T_2 = 0$ corresponding to the freezing temperature, δ_{fr} . If the temperatures with this gradient are used for the calculation of temperatures in the next time step, this will produce a relatively fast freezing. On the other hand, if we assume that the entire frozen region is at the freezing temperature, it will produce a slow freezing. In reality, the temperature profile in the frozen region will change slightly by heat conduction during the time the front moves to the next axial node. To account for this effect, the front temperature in the frozen region has taken as $[\alpha \delta_{fr} + (1-\alpha)\delta(T_2)]$, with $0 \leq \alpha \leq 1$. The computed results for the Wood's metal case with $\alpha=0$ yield a freezing length of about 4 cm, whereas with $\alpha=0.2$, the freezing length was found to be about 10 cm. Figure 3 shows the calculated growth of the frozen layer at the front for the case $\alpha=0$. The growth

of the frozen layers and the temperatures are also computed in this work at other axial positions. Figure 4 shows typical results of the front temperatures in the liquid region at three η_1 positions. Note that $\eta_1=0$ denotes the center line position. In this particular case, the liquid front attains the freezing temperature in about 0.3 sec.

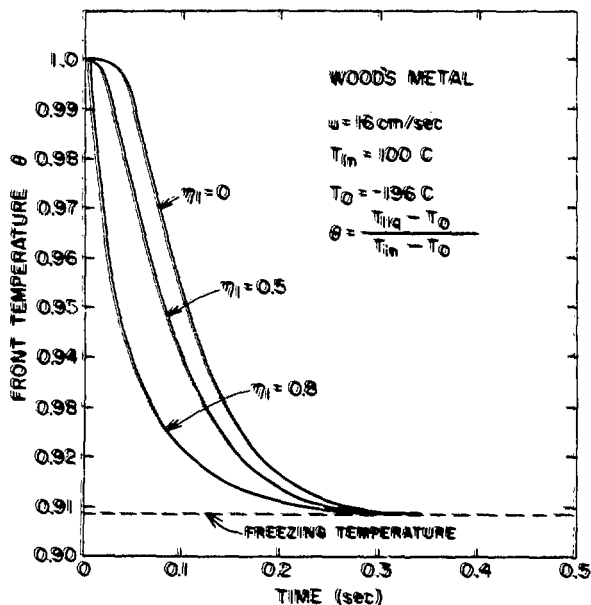
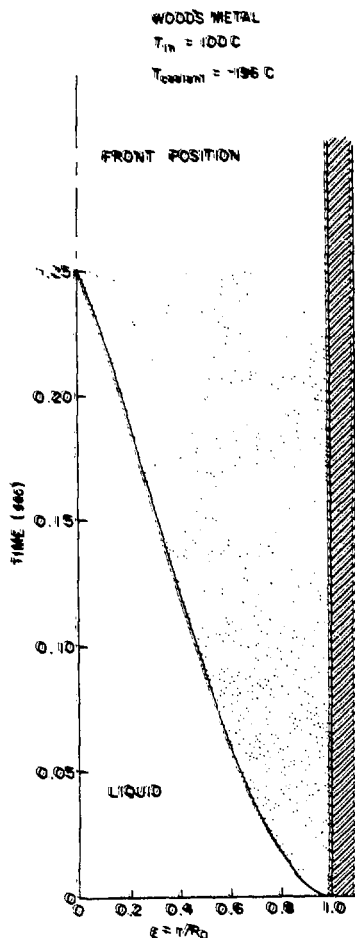
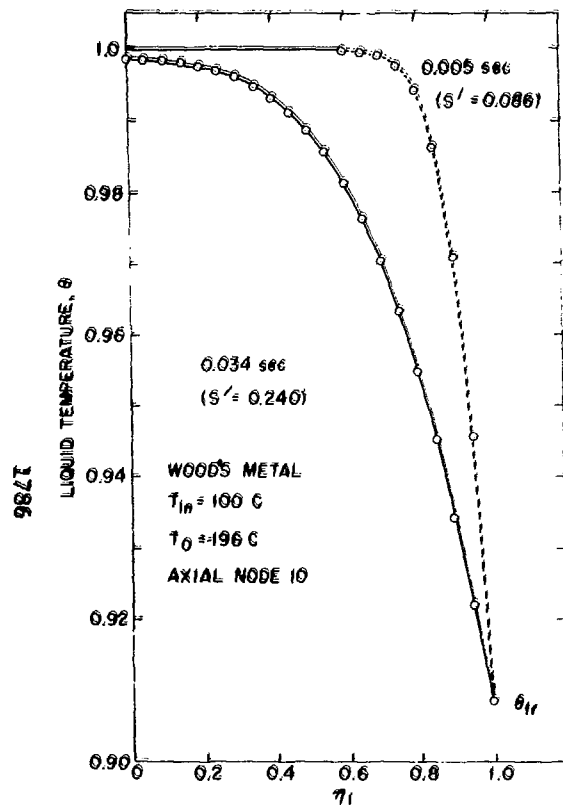


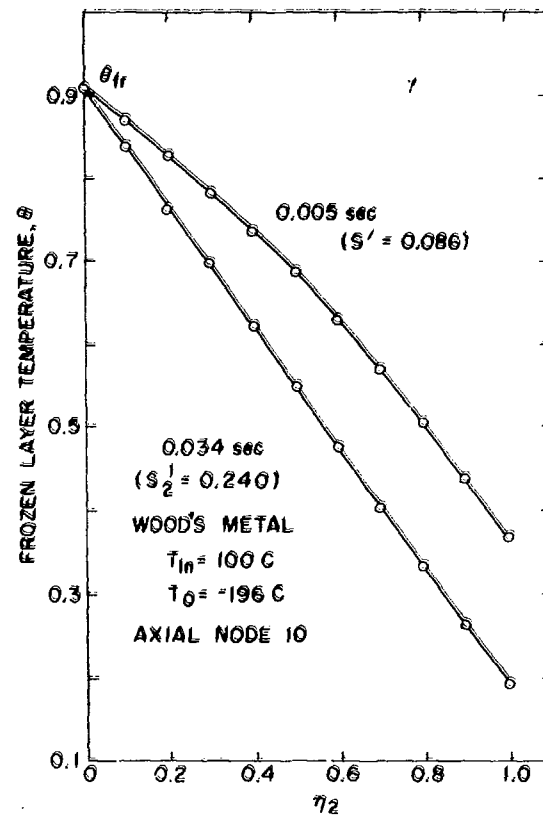
Figure 4. Temperature history of the liquid region at the front shown for three η_1 positions.

Figure 3. Growth of the frozen layer at the moving front of the liquid column shown as a function of time ($\alpha=0$ case).

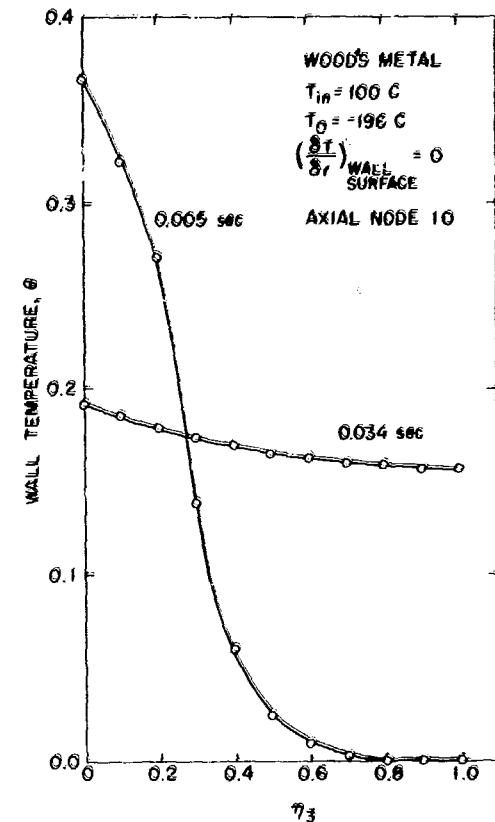
To illustrate the results of this work, temperature profiles in the liquid, the frozen layer and the wall are plotted for two different times in Fig. 5 for one computer simulation of the Wood's metal experiment. In this case heat flux at the wall was assumed to be zero. Note that as time changes, the liquid temperature, which was originally flat, develops a temperature gradient to match the boundary condition at the freezing interface. Near the completion of plugging of the channel, the liquid temperature will again be flat, but this time at the freezing temperature. It should be mentioned that in these figures, the actual thicknesses of the liquid and the frozen regions change with time. As expected, the wall temperature and its gradient change rather fast at the beginning and conform to the temperature profiles expected. The width of η_3 corresponds to the fixed thickness of the tube wall.



(a)



(b)



(c)

Figure 5. Temperature distributions in the liquid (a), frozen layer (b), and wall (c) at the axial node position 10 shown for two different times.

The model was finally applied to gather preliminary information on fuel flow under hypothetical core-disruptive accident conditions in a liquid metal fast breeder reactor such as the Clinch River Breeder Reactor. It is found that in a molten fuel flow through a steel channel, there is a sharp temperature gradient at the beginning in the molten fuel region near the steel wall because of the large difference in thermal conductivities between steel and UO_2 fuel. Freezing of fuel is initiated almost immediately. However, the frozen layers are developed very slowly. For the downward fuel relocation through the lower shield structure, both the constant wall temperature and the convective heat flux boundary conditions at the wall were used. For the molten fuel flow through the upper plenum region, a zero heat flux condition was imposed at the mid position of the cladding.

This analysis, based on the conduction model concepts, suggests that molten fuel at a high initial velocity would probably go through the entire fission gas plenum and that a considerable amount of the cladding steel will melt before a plugging of the channel could take place.⁶ Large penetration distances were also obtained for fuel flow through the lower shield structure. These conclusions, however, may be significantly affected by steel ablation and possible presence of liquid sodium film in the channel.

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ANALYTICAL AND EXPERIMENTAL STUDIES OF TRANSIENT FUEL FREEZING

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ABSTRACT

The freezing of a molten ceramic fuel-metal mixture in a single steel tube and in a seven-pin bundle has been studied experimentally. The freezing data in the single-tube experiment compare well with a simple transient heat-conduction model. It was found that the extent of penetration of a molten fuel-metal mixture into a seven-pin rod bundle is considerably greater than that predicted by existing models based on bulk freezing concepts. A tentative explanation for the observed penetration behavior is that, while melting of the steel channel structure rapidly leads to fuel freezing in a bulk manner, complete occlusion of the pin bundle channels is ultimately controlled by conduction within the melted steel.

INTRODUCTION

The sequence of events for both the postulated unprotected transient overpower and transient undercooling accidents for the Liquid-Metal-Cooled Fast Breeder reactor includes the upward dispersal of boiling core material (molten steel and molten fuel) out of the core regions. The extent of penetration of the molten material into the largely intact pin structure above the core is a major concern, as fuel freezing can lead to a temporarily "bottled-up" core condition.

Most predictions of the freezing of liquids in channels are based on the notion that the freezing process is controlled by heat conduction through a growing frozen layer at the channel wall (conduction model)[1-4]. Ostensen and Jackson [5], however, expressed the opinion that the rate of freezing of fuel in steel channels is controlled by turbulent heat transport to the steel wall. It is postulated by these authors that since the steel surface may be molten a solid fuel layer would not have an underlying surface to cling to. Moreover, the region just behind the leading edge of the flow, where freezing is expected to occur first, appears as a "slush" and freezing is complete when the latent heat of fusion is "removed" from the slush by further (turbulent) heat loss to the steel channel wall ("bulk-freezing" model).^{*} The results of previous experiments that indicate that fuel-steel mixtures will not penetrate far into subassembly structure support the bulk-freezing model [7]. An attempt to explain the mechanism of bulk freezing was made in Ref. 6.² According to the theory, if conditions in the fuel flow are such to prevent crust growth then the steel wall melting can become severe. The relatively cold melting steel rapidly mixes with the fuel and causes the fuel to freeze in a bulk manner, i.e., bulk

^{*} A detailed review of both the conduction and bulk freezing models appears in Ref. 6.

freezing takes place when the fuel crust growth process at the channel wall is transformed to a steel ablation process (ablation-induced freezing model).

The present paper describes two series of fuel freezing experiments in which the molten fuel was generated by a thermite-type reaction. In the first series of experiments 0.5 kg of thermite reaction products at 3200°C was injected into cylindrical steel tubes with different initial temperatures to gain an understanding of fuel crust growth behavior in turbulent flow. The second series of experiments was designed to determine the penetration characteristics of fuel-steel mixtures in seven-pin bundle configurations. Use of a 0.5-kg and a 2.0-kg injector allowed a fuel penetration comparison for two different types of fuel dispersals. The injection of 0.5 kg, which represents about 50% of the fuel that would be associated with 7 pins, simulates the simultaneous penetration of a fuel-steel mixture into the subassembly structure above and below the core. The 2.0-kg injection provides a simulation of extensive molten core material escaping through a limited number of subassembly channels. Explanations are postulated for the phenomena observed on the basis of simple conduction theory and the ablation-induced freezing concept proposed in Ref. 6.

EXPERIMENTAL APPARATUS AND PROCEDURES

Cylindrical Tube Experiments

The apparatus for the Cylindrical Tube Experiments is shown in Figure 1 in which the 0.5-kg thermite injector was flange mounted onto the cylindrical test section. This test section was outfitted with trace heaters to achieve a specified initial temperature and insulation was then wrapped over the heaters. Thermocouples were also mounted on the test section to ascertain the uniformity of the initial temperature profile. Thermite mixture, which is composed of metallic uranium and polyboron along with a small base quantity of bismuth trioxide is characterized in Table I and separate effects tests show that thermite reaction progressed to thermoequilibrium which produced a mixture temperature of approximately 3200°C and an initial void fraction of approximately 75%. This system of void fraction comes from the known internal volume of the thermite furnace and the volume occupied by the powder. The only measurement made during the transient was the injector pressure which was monitored by a strain gauge transducer as shown in Figure 1.

TABLE I
INJECTOR CHARACTERISTICS

Component	Mass, gm	Gram Moles
U	18	0.19
B	142	1.82
MgO ₃	390	1.39
Pust	0	0.02
		0.02

$$= 1(0.19) + 1.82(1) + 1.39(\text{MgO}_3) + 0.02(\text{CrO}_3) + \dots$$

$$= 0.19(1) + 1.39(\text{Mg}) + 0.02(\text{B}) + 0.02(\text{Cr}) + 0.19(1)$$

$$\text{Free Volume} = 1.0 \text{ cc}$$

$$\text{Mixture Volume} = 68 \text{ cc}$$

$$\therefore \text{Initial Void Fr} = 0.73$$

The test was begun by pulsing the nichrome ignitor wire with approximately 18 amps of current which then triggered the exothermic thermite reaction. Gases which were evolved during the reaction resulted in pressurization of the thermite furnace and rupture of the stainless steel diaphragm at the base of the injector, which allowed the high pressure, high temperature reaction products to flow down through the stainless steel test section. The principal variable in these tests was the initial test section temperature which was varied from room temperature to a value of 1170°K. Other variables investigated were the inside diameter and length of the test sections.

7-pin Fuel Freezing Test

Figure 2 is a schematic illustration of the apparatus used for the 7-pin test. The 7-pin configuration was flanged to a thermal standoff which in turn was mounted directly to the base of the thermite injector. The thermal standoff spool was necessary to maintain a low thermite powder temperature as the test section was being heated to either 970° K or 1170° K, which were the two initial steel temperatures studied in this experiment. Two different injectors were also used in this experiment, the first was capable of delivering 0.5 kg of molten reaction products at approximately 3200°C and the second was capable of delivering approximately 2.0 kg at this same temperature. In both of these injectors the initial void fraction upon release of the fuel was approximately 75%. Three pressures were measured in the experiment with standard unbonded strain gauge units, the injector pressure and test section pressures thirty (30) and sixty (60) centimeters from the test section inlet. Instrumentation also included thermocouples along the entire length of the 7-pin test section to assess the initial temperature distribution. These thermocouples show the initial distribution to be uniform except in the immediate vicinity of the cooling spool where axial heat loss resulted in a somewhat lower temperature at the immediate inlet. Following tests three and four with 2.0 kg injections, the spacing of trace heaters at this inlet section was altered to offset most of this axial heat loss. The desired test section temperature was established within 3 cm of the inlet. As with the cylindrical tube tests, when the test section has reached its desired temperature, the ignitor wire was pulsed with approximately 18 amps of current. This current pulse initiated the thermite reaction which produced a nominal pressure of 40 to 50 atmospheres within the injector, which then resulted in rupture of the stainless steel diaphragm at the bottom of the injector assembly. Following this rupture, 3200°C uranium dioxide and metallic molybdenum flowed down through the bundle configuration. The pressure measurements themselves give an indication of when the mixture plug passes that specific location, and thus time of flight measurement for the travel of the fuel-metal mixture from which an average velocity can then be calculated. The first 15 cm of each pin in the test section is solid steel, the next 120 cm is hollow cladding (5.8-mm OD x 5.1-mm ID), and the last 2.5 cm is again solid steel. In order to protect operating personnel, the 7-pin test section was shrouded with six 1/8 x 1/2 x 48 inch steel backup plates, which were then welded together along the length of the test section. In addition, the entire test section was contained within an outer shroud as shown in Figure 2 which would divert any test section failure down into the sand pit below the bottom of the apparatus.

DISCUSSION OF THE RESULTS

Cylindrical Tube Experiments

Table II lists the results of the various cylindrical tube experiments. A frozen layer of thermite material is formed along the tube wall and the thickness of the layer is essentially independent of the tube inside diameter (ID) for IDs > 4.0 mm. As the initial (steel) tube wall temperature, $T_{s,i}$, is increased, the thickness of the frozen layer decreases. Initial steel temperatures 700°C

resulted in partial or complete ablation of the tube wall. This behavior is explained below in terms of simple heat-conduction theory.

TABLE 11. Small Scale Fuel Freezing Tests

ID	OD	Test Section		Mass Increase	Mass of Fuel Injected	Initial Injector Pressure	Average Film Thickness
		Length	Initial Temp				
(mm)	(mm)	(mm)	(°K)	(kg)	(kg)	(M Pa)	(mm)
9.3	13.7	457	293	0.080	0.526	6.9	0.76
9.3	13.7	457	570	0.057	0.427	5.0	0.53
9.3	13.7	457	770	0.045	0.360	4.6	0.42
9.3	13.7	457	870	0.044	0.365	3.6	0.41
9.3	13.7	457	1170	-0.104	0.476	5.2	--
9.3	13.7	457	970	-0.063	0.403	4.3	--
9.3	13.7	457	670	0.061	0.406	3.7	0.54
9.3	13.7	457	670	0.042	0.473	4.6	0.37
6.8	10.3	338	293	0.036	0.505	5.9	0.65
6.8	10.3	457	293	0.045	0.532	4.5	0.60
6.8	10.3	338	1170	Pipe Destroyed	0.457	5.5	--
4.8	12.7	235	293	0.019	0.538	9.7	0.76

The solidification process is initiated as the molten thermite mixture (fuel) at temperature $T_{f,o}$ enters the tube. As the frozen fuel layer on the wall increases in thickness, heat is transferred by convection from the flowing fuel melt to the solid layer-liquid interface at a rate which is proportional to the molten fuel superheat, namely $h(T_{f,o} - T_{f,mp})$. Fuel crust growth proceeds as long as the conduction heat flux through the steel wall exceeds that to the crust surface from the flowing fuel. Once the thermal boundary layer in the wall is sufficiently large (or, perhaps, reaches the opposite side of the wall), the conduction heat flux into the wall exactly balances convection from the flowing fuel and there is no latent heat generated. The fuel crust reaches a maximum thickness. After this, convective heat transfer becomes more than what can be conducted away, resulting actually in reduction in crust thickness through melting. The crust disappears when melting is complete. This heat-conduction problem has been treated in detail in Ref. 8. From this work, the fuel crust thickness-time history, $\delta_f(t)$, on a thick steel wall is found to be well-represented by the simple form

$$\delta_f = 2\lambda_f(\alpha_f t)^{1/2} = (\alpha_f t)/\delta_{\max} \quad (1)$$

$\delta_{\max}$ is the maximum possible crust thickness corresponding to crust growth behavior on an isothermal wall:

$$\delta_{\max} = \frac{k_f(T_{f,mp} - T_{s,o})}{h(T_{f,o} - T_{f,mp})} \quad (2)$$

α_f , the "growth constant", is a function of the physical properties and initial temperatures of both the flowing fuel melt and the steel wall [9]; however, for most fuel-steel initial temperatures of interest $T_{f,o} < 0.8$. For the sake of simplicity, let us assume that turbulent heat transport within the tube is represented by the Reynolds' analogy which provides a linear relationship between the heat-transfer coefficient, h , and the liquid fuel flow velocity U , viz

$$h = \frac{f}{2} \cdot \rho_f c_f U \quad (3)$$

where f is the dimensionless coefficient of friction ($f = 0.005$). The value of time, $t_{\max}$, at which the fuel crust thickness peaks is obtained by setting $d\delta_f/dt=0$ in Eq. (1) and using Eqs. (2) and (3) in place of $\delta_{\max}$, i.e.,

$$t_{\max} = \frac{4}{(1U)^2} \left(\frac{T_{f,mp} - T_{s,o}}{T_{f,o} - T_{f,mp}} \right) \quad (4)$$

It is presently felt that the minimum possible experimental molten fuel velocity in the vicinity of the tube wall is $U \sim 500 \text{ cm sec}^{-1}$; therefore, for the experimental conditions $T_{f,o} = 3200^\circ\text{C}$ and $T_{s,o} = 20^\circ\text{C}$, we calculate $t_{\max} = 0.18 \text{ sec}$. Injection pressure-time measurements indicate that the thermite mixture release time in all cases exceeds 0.5 sec . It is then established that the observed crust left behind in the test section passed through the growth period $t < t_{\max}$ and was undergoing melting at the termination of the injection period.

Equations (1) and (2) suggest that, for a given fuel velocity and fuel (thermite) release time, the experimental fuel crust thickness is a linear function of $(T_{f,mp} - T_{s,o})^{-1}$. One notes in Fig. 3 that, considering what has been left out of the analysis (see below), the correlation is satisfactory. The experimental linear relationship δ_f vs $(T_{f,mp} - T_{s,o})^{-1}$ can be used in combination with Eqs. (1), (2), and (3) to infer the fuel release time and average fuel velocity. The predicted release time of 1.5 sec is about a factor of 2 higher than the observed range $0.5\text{--}0.8 \text{ sec}$, while the predicted average fuel velocity of 420 cm sec^{-1} appears to be too small by roughly a factor of 2. Inference of accurate absolute values may require refinements of the present analysis. In particular, the effects of cylindrical geometry, finite pipe wall, and the multi-phase nature of the thermite release products should be incorporated.

The fuel crust lifetime, t_{life} , is found by setting $\delta_f=0$ in Eq. (1) and is given by $t_{\text{life}} = 4.0 t_{\max}$. For the fuel flow conditions $U \sim 500 \text{ cm sec}^{-1}$, $T_{f,o} = 3200^\circ\text{C}$, and the test section temperature $T_{s,o} = 700^\circ\text{C}$, above which ablation of the steel was observed, we get $t_{\text{life}} = 0.42 \text{ sec}$. This is less than the thermite release time. Indeed the observed crust thicknesses are consistent with heat-transfer controlled growth and melting. At the higher tube wall temperatures, the melting front in the fuel crust penetrates the steel wall when $t > t_{\text{life}}$ causing the observed steel ablation.* It can be shown that the temperature profile within the solid steel wall will asymptotically approach a simple, steady exponential form [10] involving the melting velocity, v_m , (or wall ablation rate) which, in turn, will become a linear function of the convective heat flux according to

$$v_m = \frac{h(T_{f,o} - T_{s,mp})}{\rho_s [L_s + c_s (T_{s,mp} - T_{s,o})]} \quad (5)$$

For the experimental conditions of interest, namely $U \sim 500 \text{ cm sec}^{-1}$, $T_{f,o} = 3200^\circ\text{C}$ and $T_{s,o} = 800^\circ\text{C}$, we get a steel wall melting velocity $v_m \sim 2 \text{ cm sec}^{-1}$. The time, $t_{a,s,o}$ to completely ablate a tube wall of thickness δ_w is $t_a = \delta_w/v_m$. A typical value is $t_a \sim 0.1 \text{ sec}$.

7-Pin Fuel Freezing Test

The observations of the frozen plug locations in the seven-pin fuel-freezing experiments are given in Table III. The penetration length is relatively insensitive to the initial pin bundle temperature. If the mass of fuel injected is increased, however, the thermite penetration length increases. Most impor-

*When the initial steel temperature exceeds $\sim 800^\circ\text{C}$ and the fuel velocity $\sim 1000 \text{ cm sec}^{-1}$, the steel may begin to ablate immediately following contact with molten fuel, as the "protective" fuel crust under these conditions is predicted to be mechanically unstable [6].

tant of all, the absolute observed penetration distances exceed the 10 to 20-cm penetration length predicted by either the bulk freezing model [5] or the ablation-induced freezing model [6] when applied to pure fuel. The measured pressure-time histories in the injector, 30 cm from the test section inlet (P_1), and 60 cm from the test section inlet (P_2) are shown in Figs. 4-8. In the 0.5-kg injections, there was no response from P_2 indicating that the original thermite penetration behavior was to traverse the first 30 cm of channel length in ~ 40 msec and then stop before reaching the location of pressure tap P_2 . A relatively slow pressurization $P_1(t)$ is observed in Run 2 (see Fig. 5). This is consistent with posttest observations which show that in Run 2 most of the thermite material did not penetrate much beyond 30 cm (see Table III). The pressure measurements shown in Figs. 6-8 indicate that early thermite motion was essentially the same during Runs 3-5. The thermite discharge reached location 1 in 40 msec and passed location 2 some 35 to 60 msec later.

TABLE III
7-Pin Fuel Freezing Tests
(FFTF Reflector Plus Fission Gas Plenum)

Run	Initial Steel Temperature	Mass of Fuel	Experimental Observation of Plug Location	Blockage
	$^{\circ}\text{K}$	Kg		
1	1170	0.5	Back--10 cm Front--43 cm	Porous
2	970	0.5	Back--10 cm Front--30 cm	Porous
3	1170	2.0	Back--60 cm Front--Entire Length	Complete
4	970	1.7	Back--60cm Front--Entire Length	Complete
5	1170	1.9	Back--70 cm Front--Entire Length	Complete

It is worthwhile to briefly review the ablation-induced freezing concept, since it is believed that this mechanism offers a possible explanation of the observed penetration behavior. It was proposed in Ref. 6 that, beginning at the pin bundle inlet, shortly after the molten fuel enters the pin bundle channels, an ablation wave will pass down the channel walls at a velocity approximately equal to that of the fuel front. A molten fuel-steel mixing zone will quickly be established in the region just behind the ablation wave. The steel melt at its melting temperature (1419°C) is rapidly entrained by the flowing molten fuel at a much higher temperature, say, 3200°C . The entrained steel will decrease the enthalpy of the molten fuel while its temperature is raised by extracting the corresponding enthalpy. When a sufficient fraction of the fuel-steel mixture is in the frozen state, the flow will come to a stop. Figure 9 illustrates the proposed sequence of events following molten fuel penetration into a reactor subassembly tube bundle. For a detailed discussion of fuel-steel mixing zone characteristics, the reader is referred to Ref. 6.

In the above description, the flow was assumed to consist of pure molten ceramic fuel; however, this is not the case in most applications of interest. For example, in the freezing experiments reported here, the flow contains metallic molten molybdenum, which is a by-product of the thermite reaction. In a reactor environment under hypothetical accident conditions, molten stainless steel would be present in the flow. It is prudent, therefore, to consider the

following important question regarding ablation-induced freezing: Does the presence of molten metal modify the usual criterion that a flow blockage occurs upon freezing of the fuel alone? In other words, does the molten fuel act as the continuous or discontinuous component in the fuel-steel mixing zone? To address this question, we consider the mixing of a known mass of molten thermite, which contains a mass m_{mo} of molten molybdenum plus a mass m_f of molten fuel, with the mass of entrained steel, m_s , required to cool the fuel down to freezing. From an enthalpy balance we obtain

$$\frac{m_s}{m_f} = \frac{c_{f,liq}(T_{f,o} - T_{f,mp}) + L_f}{c_{s,sol}(T_{s,mp} - T_{s,o}) + L_s + c_{s,liq}(T_{f,mp} - T_{s,mp})} \frac{(m_{mo}/m_f)c_{mo,liq}(T_{f,o} - T_{f,mp})}{(6)}$$

The term $c_{s,sol}(T_{s,mp} - T_{s,o}) + L_s$ appears in Eq. (6) because a fraction of the heat given up by the flowing mixture goes into raising the solid steel temperature to the melting temperature and melting the solid surface. The liquid metal (molybdenum plus steel) volume fraction consistent with fuel freezing is

$$\alpha_{liq} = \frac{m_s/\rho_{s,liq} + m_{mo}/\rho_{mo,liq}}{m_s/\rho_{s,liq} + m_{mo}/\rho_{mo,liq} + m_f/\rho_{f,sol}} \quad (7)$$

Table IV contains some typical values of α_{liq} calculated from Eqs. (6) and (7) for the injection temperature 3200°C and the molten molybdenum-fuel mass ratios $m_{mo}/m_f = 0, 0.24$, which correspond to pure fuel and the actual thermite mixture proportion, respectively.

TABLE IV
Values of α_{liq} for Freezing Fuel

$T_{s,o}$, °C		600	800	1000
α_{liq}	Pure fuel	0.231	0.253	0.266
	Thermite	0.398	0.408	0.416

It is clear from Table IV that for initially pure fuel, frozen fuel is the continuous phase in the mixing zone; in fact, the frozen fuel component volume fraction (i.e., $1 - \alpha_{liq}$) is approximately equal to the maximum packing volume fraction. A flow blockage is likely, therefore, when the initially pure fuel component freezes. On the other hand, the relatively large volume fractions of molten metal present in the thermite mixing zone ($\alpha = 0.4$; see Table IV) indicate that the molten metal may become the continuous component before fuel freezing is complete. In general, it is difficult to predict which component is favored as the continuous phase for liquid-liquid combinations in the volume fraction range 0.3 to 0.7. An often quoted rule is that the component present in a greater amount tends to be the continuous phase-i.e., the frozen fuel component. Even if this were true, however, the molten metal volume fraction of 0.4, in combination with driving pressures in the range of 40 to 50 atm, should provide sufficient hydrodynamic lubrication for thermite penetration to continue beyond the location of complete freezing of the fuel component.* Thus it is the presence of a molten metal component (molybdenum) in the initial thermite mixture that

* Freezing of the molten metallic molybdenum component will occur when the mixture temperature falls below 2610°C. An enthalpy balance at this temperature level indicates that the volume fraction of molten metal (steel) remains in the 0.35-0.4 range, so that thermite motion may continue. Note that the melting temperature of the metal components may change since molybdenum is an alloying element for steel.

offers a possible explanation for the relatively long penetration distances (compared with bulk freezing predictions) measured in the freezing experiments.

It remains to identify the mechanism responsible for bringing the penetrating molten flow to rest. Of course, we must recognize that there is always the possibility that the volume fraction of the fuel frozen in a bulk manner may be large enough to block the flow by a mechanical jamming mechanism. Alternatively, if the slurry of solid fuel, solid molybdenum and molten steel does not plug the pin bundle channels, and since the molten steel component cannot freeze in a bulk manner by the addition of melting steel from the channel walls, complete freezing and plugging must ultimately involve the conduction-limited growth of a frozen steel layer at the channel wall. The conduction mechanism can explain the observed dependence of thermite freezing behavior on the initial inventory of molten thermite. As the flow penetrates further into the pin bundle, the ablation process continues and the melted steel becomes the continuous phase in the region behind the flow front. The instantaneous contact temperature between the flowing molten steel and the channel wall is $T_i = (T_{f,o} + T_{s,o})/2$. Here we shall assume that when T_i falls below the steel melting temperature, $T_{s,mp}$, steel ablation ends and conduction-limited steel crust growth begins. The mass of entrained steel required to cool the flow down to the temperature T_i such that $T_i = T_{s,mp}$ is (assuming the heat capacities and heats of fusion of all species are identical and equal to c and L , respectively)

$$\frac{m_s}{m_f} = \frac{(T_{f,o} - T_{s,mp}) - (T_{s,mp} - T_{s,o}) + L/c}{2(T_{s,mp} - T_{s,o}) + L/c} \quad (8)$$

The minimum conduction-limited penetration length, X , for molten steel in a steel channel of diameter D is [6]

$$X = \frac{D \cdot L}{16 \lambda_s^2 \tau_s} \quad (9)$$

where τ_s , "the growth constant" for steel, is of order unity. From the two pressure measurements spaced 30 cm apart (see Figs. 6-8), the thermite flow velocity is estimated to be 700 cm sec^{-1} . Taking $D = 0.2 \text{ cm}$, we get $X = 50 \text{ cm}$. The mass (or volume) of entrained molten steel, m_s , when spread out, say, between the pins covers a distance $m_s/(A_s)$, where A_s the cross-sectional area for flow, 2.0 cm^2 . Clearly, if this distance is less than X , complete freezing and plugging of the pin bundle is impossible. In terms of mass of thermite injected, m_f [see Eq. (8)], complete freezing must be ruled out for thermite injections less than 0.7 kg. This is consistent with the rough, porous, frozen debris observed at the conclusion of the 0.5 kg injections. For such injections the frozen debris is found to be spread over the intact pin bundle structure. This must be compared with the solid steel plugs observed at the conclusion of the 2.0 kg injections, where complete occlusion of the channels must have first occurred in the vicinity of the pin bundle exit. In the last 2.5 cm of channel length the pins are cylinders of solid steel (pin end cap) and, therefore, "contain" enough heat capacity to freeze the flowing steel during the injection period.

CONCLUDING REMARKS

The most significant experimental finding of this paper is that boiling fuel steel mixtures under sustained pressure penetrate far into subassembly structure and even travel distances which exceed the dimension of the LMFBR subassembly. It is postulated here that, while steel ablation rapidly leads to fuel freezing in a bulk manner via turbulent mixing between fuel and melted steel, bulk freezing does not provide significant containment capability. Instead, frozen steel is required to stop the flow and fuel penetration is

conduction controlled in the steel component. The previously reported short penetration distances for thermite mixtures [7] are now thought to be due to the limited quantity of thermite material employed. Of course, the very idea of there being a single dominant mechanism for flow blockage in this complicated freezing process may be an over-simplification and the above conclusions must await additional experimental evidence. Nevertheless, the trend of the data is clear and it is concluded that boiling fuel-steel mixtures moving out of the core region during a postulated accident will rapidly penetrate the axial blanket or reflector region following initial core disruption.

ACKNOWLEDGMENT

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NOMENCLATURE

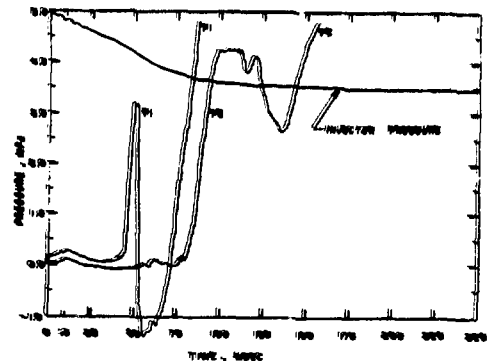
c	heat capacity	α	thermal diffusivity
D	channel or tube diameter	α_{liq}	liquid metal volume fraction
f	coefficient of friction	δ_f	fuel crust thickness
h	heat-transfer coefficient	δ_{max}	maximum possible fuel crust thickness
k	thermal conductivity	λ_f	growth constant for fuel crust
L	Latent heat of fusion	λ_s	growth constant for steel crust
m	mass	ρ	density
P	pressure		
t	time		
t_{max}	time at maximum crust thickness		
t_{life}	fuel crust lifetime		
T	Temperature		
U	flow velocity		
v_m	steady-state melting velocity		
X	conduction-limited penetration length		

Subscripts

f	fuel
liq	liquid state
mo	molybdenum
o	fuel temperature at channel entrance or initial steel wall temperature
s	steel
sol	solid state
1	location 30 cm from test section inlet
2	location 60 cm from test section inlet

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7-Pin Fuel-Freezing Test No. 3

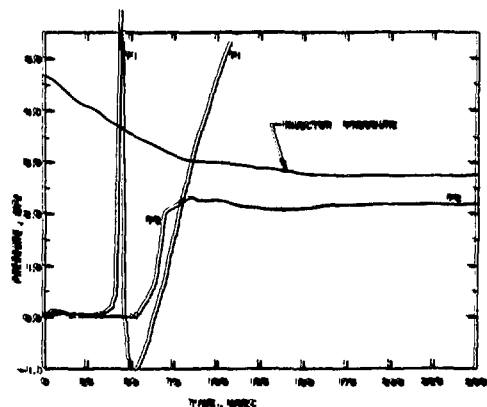
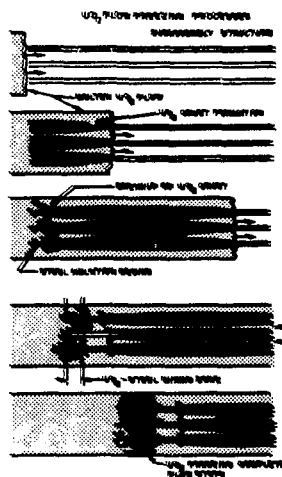


**Fig. 8 Measured Pressure-time History
7-Pin Fuel-Freezing Test No. 5**



1798

SIMULATION OF TRANSIENT FREEZING OF CLADDING
IN GCFR BLANKET CHANNELS*

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ABSTRACT

A multi-rod bundle experiment was performed to simulate the transient freezing of cladding material in the blanket region during GCFR LOF situation. A set of single Pb/Sn tube tests was carried out to obtain data on the time required for the molten alloy to solidify and block the channel. A mathematical model was established and solved by a numerical method. The temperature distributions in the liquid and the solid, and the profile of the fusion interface were determined in detail. The calculated and measured time to plug were compared. The theoretical model compared well within the limits of the experimental data. This model was then extended to predict the freezing of relocated stainless steel cladding.

Transient freezing and relocation of molten cladding in coolant channels in the lower blanket region is a probable and important heat transfer process in GCFR loss-of-flow accidents. Starting from slightly below the core mid-plane, the cladding melts from the inside out. The internal pressure produces bulges in the cladding, which burst and drain when the viscous forces is overcome by gravity. Once the midplane section is bare, the cladding above falls off in tubular segments as its inside layer becomes molten. The liquid state is at low superheat and could freeze upon contact with the lower powered blanket cladding and plug the coolant channels. Plugging of the lower channels could result in increased reactivity (and power) and reduced cooling capability. This would lead potentially to further core damage. On the other hand, if the cladding does not freeze in the lower blanket, the molten cladding would sweep into the lower plenum. Depending on the cladding disposition, the times involved, and the actions of the backup systems will determine the seriousness of the events.

The purpose of this research is to develop a model to describe the sequence of events and times involved after a loss of flow situation.

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BACKGROUND

Problems of heat transfer involving melting or solidification are complicated because of the non-linear character at the interface between the solid and the liquid. Solutions are found by one of the following techniques: analytical, numerical and experimental methods. Among the analytical solutions, Stefan and Neumann² are the pioneers for the elementary problems of a stationary liquid.

Bankoff³ developed the one-dimensional melt thickness under maximum and minimum bounds and concluded that at constant heat flux, the melt thickness initially grew linearly with time. Boley et al⁴ were primarily concerned with aerodynamic ablation and had recently extended the "embedding technique" to solve fuel rod melting problems in nuclear reactor accident conditions. For steady state problems, the effect of solidification in a circular tube were examined by Zerkle and Sunderland⁵. Stephan⁶ studied the problem of fluids by assuming a parabolic and transient temperature distribution in the solid phase. The stability of the freezing process in pipe flow was also analyzed. Ozisik and Mulligan⁷ indicated that their transient results were applicable only when the rate of change of the thickness of the solid layer was small, and were not accurate during the initial and final times. Epstein et al⁸ provided clarification of the differences between conduction limited⁹ and steel ablation controlled bulk solidification¹⁰ processes in the context of their application to oxide fuel freezing in LMFBR safety analysis.

Standard numerical technique can be seen in Beaubouef and Chapman's paper¹¹ Murray and Landis¹² suggested two generalized methods: the variable space network and the fixed space network. Propagation of melting, freezing and relocation of fuel debris responded in reactors during failure conditions was partly described in a few computer programs such as SAS¹³, CODAPP¹⁴, and CMOT¹⁵.

Until recently only limited experiments have been carried out on melting and freezing. Seban and London¹⁶ measured the times of complete freezing of vegetables of high water content. Zerkle and Sunderland⁵ carried out observations on phase change and measured the pressure drop and the rate of heat transfer within a circular tube as a function of flow. Recent experiments on fuel freezing in the fast reactor subassembly structure were performed by Ostensen et al¹⁰ Cheung and Baker⁹ investigated the transient freezing of water, Wood's metal, naphthalene, paraffin wax, and olive oil in copper coil. They further interpreted the empirical correlation to transient flow and freezing of molten core debris in coolant channels in LMFBR.

The analytical approach is handicapped in application because of various assumptions inherent in finding a closed-form solution. The complex geometry associated with reactor accident models can only be approached satisfactorily by the numerical solution of the basic momentum and energy equations. It is also desirable to provide experimental verification to the theory. Thus, the objectives of the present paper are to: (1) experimentally simulate the motion of molten stainless steel by a Pb/Sn alloy and observe its solidification process in a channel, (2) theoretically model the transient heat transfer to describe the freezing of the moving liquid and plugging of the channel.

EXPERIMENTAL SIMULATION

Two sets of experiments were carried out concerning the behavior of molten cladding descending into the blanket region of the fuel lattice, assuming a complete loss of flow situation. A qualitative test using multi-rod subassemblies simulated the annular flow pattern formed by the flowing cladding from the core. A quantitative test with single tubes was used for a parametric

study and verification of the theoretical model. The cladding and the spacers surrounding the failing rod most probably have been melted in the core region. The molten debris has little superheat when it enters the cooler blanket region where the fuel and the intact cladding are calculated at 1650 C (3000 F) and 577 C (1070 F), respectively, based on the lump-parameter technique. Mullite (Al_2O_3 and SiO_2) is substituted for U-PuO₂ fuel and 50Pb/50Sn for stainless steel cladding. Table I shows their thermal properties used in the simulation.

TABLE I
Properties of UO₂, M330, S.S. 316, and 50Pb/50Sn

Material	UO ₂	M330	S.S. 316	50Pb/50Sn
Properties				
c (cal/gm-C) p specific heat	0.064(25 C)	0.23	0.12(0-100 C)	0.051(16-183 C)
h_{sf} (cal/gm) latent heat of fusion	120	352 ^a	76.7	12.8(183-216 C)
k (cal/cm-sec-C) thermal conductivity	0.0191	0.0062	0.0372(100 C) 0.05(500 C)	0.111(34 C)
ρ (gm/cm ³) density	10.5	2.8	8.0	8.89
T_{sf} (C) solidus temperature			1375	183
T_{lf} (C) liquidus temperature			1497	216
T_f (C) melting temperature	2760	1750 ^b		
α (cm ² /sec) thermal diffusivity	0.0361(1000 C) 0.0191(400 C) 0.0101(1000 C)	0.0096	0.04(93 C) 0.0516(727 C)	0.23

a: for pure Al_2O_3

b: maximum working temperature

In these experiments the following observations will be made:

1. The extent to which molten cladding passes through the blanket assembly and grid spacers to exit the bottom of the core.
2. The extent to which coolant channels are blocked by solidified cladding, and the location of the blockage.

Experiment I with multi-rod subassemblies:¹⁷

It was assumed that the flowing cladding would enter the blanket region in annular flow patterns around each fuel rod and that its entry velocity would be near zero. The assumption was also made that the grid spacer at the lower part of the core had been melted away at the time flow began, and that the cladding on the fuel rod above this interface had been bared as the result of clad melting from within. The amount of molten material to be poured was taken to be the total volume of cladding and grid spacers initially present in the active core region of the fuel bundle. Each "fuel" rod was 60.96 cm (24") long with an outside diameter of 6.35 mm (0.25") clad over the lower 45.72 cm (18") by the Pb/Sn at a thickness of 0.889 mm (0.035"). The equivalent diameter of the channels is 7.01 mm (0.276") in the rod bundle and 2.05 mm (0.0807") at the grid spacer. The upper 15.24 cm (6") of the rod was left bare to represent fuel in

the core region. The 37 rods were assembled in a hexagonal array bound together by two grid spacers located 17.78 cm (7") and 43.18 cm (17") from the top of the rods. The experiment was operated at temperatures of 265 C (509 F) and 370 C (698 F) of the molten Pb/Sn using pouring mass of 3 kg (6.6 lb) and 1 kg (2.2 lb), respectively.

The descending molten cladding stripped away most of the blanket cladding above the top grid spacer, but the cladding below this spacer appeared totally undamaged. The molten cladding backed up above the grid spacer forming the original grid structure was melted by the intruding molten material. Close inspection of the top spacer, in cross section, indicated that the flowing cladding had clogged the spacer in most locations without melting the grid itself. The molten solder plugged 15 cm (5.9") of the bundle and to an average depth of 0.8 cm (0.315") into the grid spacer.

Since there are many variables interfering with one another in a complex system, only a qualitative result can be obtained from this experiment. It was necessary to revise and confine the experiment to single tubes in order to obtain a measurable result.

Experiment II with single tubes¹

Superheated molten alloy was poured into the preheated alloy tubing (Fig.1) and the penetration length and the descending velocity were measured by the following techniques. An electronic circuit was implanted in the tube. Three pairs of wire were connected with batteries and inserted into the tube wall at different levels. As the hot liquid alloy was poured down into the test tube, each pair would shortcircuit in succession so that the voltage input to a Wave-tek Generator collected from batteries changed. This provided a change in the output frequency which was recorded on a multichannel scaler (MCS). Data were viewed from the oscilloscope and typed onto paper tape. The time at which each shortcircuit occurred was taken and an average velocity was calculated on dividing the penetration length by the time. Finally, the tube was cut longitudinally and the uncertainty in measurement of penetration length was marked. The cross section showed that if the fluid had low superheat, solidified material started to form from the inlet downward. However, with higher superheated flow, the entrance region of the tube wall was melted. Solidification started later only when the liquid temperature was lowered. Data on velocity were difficult to obtain. The average velocity was measured ranging from 48 cm/sec to 141 cm/sec. An average velocity 100 cm/sec predicted from a free fall in a distance 50 cm with zero initial velocity fits within the experiment category. The time to plug was obtained on dividing the penetration length by the average velocity.

THEORETICAL MODEL

The analysis is based on the energy balance in the liquid, solid and at their interface during transient phase change. A full flow of molten alloy enters a circular channel made of the same material. The axial velocity component of the molten fluid is described by⁷

$$\left(\frac{\partial}{\partial t} + u_z \frac{\partial}{\partial z} \right) T_l(r, z, t) = \alpha_l \left(\frac{\partial^2}{\partial r^2} + \frac{1}{r} \frac{\partial}{\partial r} \right) T_l(r, z, t) \quad 0 \leq r < \delta \quad (1)$$

where u_z is the falling velocity of the melt, δ is the radius of the fusion interface measured from the center of the channel, and the subscript l represents liquids. The transient heat conduction equation for the solid phase is

$$\frac{\partial}{\partial t} T_s(r, z, t) = \alpha_s \left(\frac{\partial^2}{\partial r^2} + \frac{1}{r} \frac{\partial}{\partial r} \right) T_s(r, z, t) \quad \delta < r \leq R + w \quad (2)$$

where w is the thickness of the channel wall, and R is the channel inner radius, the subscript s represents the solid. The coupled energy equation at the liquid-solid interface is

$$\delta_s h_{sf} \frac{\partial \delta}{\partial t} = k_s \frac{\partial T_s}{\partial r} - k_l \frac{\partial T_l}{\partial r} \quad r = \delta \quad (3)$$

For the initial and boundary conditions, the following assumptions are made:

(1) Before the melt enters the channel, the channel is assumed to be everywhere at the freezing temperature. This condition is necessary to start the numerical solution without affecting the accuracy of the result.

$$T_s(r, z, t) = T_l(r, z, t) = T_{fs} \quad t \leq 0 \quad (4)$$

(2) The wall temperature drops to the ambient condition instantaneously as soon as the liquid enters the channel.

$$T_s(r, z, 0) = T_w \quad (5)$$

(3) The channel outer wall which simulated the fuel-cladding interface is insulated.

$$\frac{\partial T_s}{\partial r}(R + w, z, t) = 0 \quad t > 0 \quad (6)$$

(4) The fusion interface always stays at the freezing temperature.

$$T_s(\delta, z, t) = T_l(\delta, z, t) = T_{fs} \quad t > 0 \quad (7)$$

(5) Liquid enters at the top of the channel at a constant superheated temperature

$$T_l(r, 0, t) = T_{sup} \quad t > 0 \quad (8)$$

where T_{sup} is the initial temperature of the superheated liquid, and T_w is the initial wall temperature.

The Crank-Nicolson finite differencing scheme¹³ is used to solve equations (1) through (8). A matrix form is illustrated in Eq. (9) for a case where there are 3 nodes in the solid cladding wall and 3 nodes in the flowing zone of molten cladding:

$$\begin{pmatrix}
 1+2a_s-d_1^s & -2a_s+d_1^s & 0 & 0 & 0 & 0 & 0 \\
 -a_s+d_2^s & 1+2a_s-d_2^s & -a_s & 0 & 0 & 0 & 0 \\
 0 & -a_s+d_3^s & 1+2a_s-d_3^s & 0 & 0 & 0 & 0 \\
 0 & c_s & -4c_s & 1 & -4c_l & c_l & 0 \\
 0 & 0 & 0 & 0 & 1+2a_l+b-d_5^l & -a_l & 0 \\
 0 & 0 & 0 & 0 & -a_l+d_6^l & 1+2a_l+b-d_6^l & -a_l \\
 0 & 0 & 0 & 0 & 0 & -2a_l+d_7^l & 1+2a_l+b-d_7^l
 \end{pmatrix}
 \begin{pmatrix}
 T_1^{k+1} \\
 T_2^{k+1} \\
 T_3^{k+1} \\
 \delta^{k+1} \\
 T_5^{k+1} \\
 T_6^{k+1} \\
 T_7^{k+1}
 \end{pmatrix}
 =
 \begin{pmatrix}
 (1-2a_s-d_1^s)T_1^k + (2a_s-d_1^s)T_2^k \\
 (a_s-d_2^s)T_1^k + (1-2a_s+d_2^s)T_2^k + a_sT_3^k \\
 (a_s-d_3^s)T_2^k + (1-2a_s+d_3^s)T_3^k + 2a_sT_f \\
 \delta^k - c_sT_2^k + 4c_sT_3^k + 4c_lT_5^k - c_lT_6^k - 6(c_s+c_l)T_f \\
 2(a_l-d_5^l)T_f + (1-2a_l-b+d_5^l)T_5^k + a_lT_6^k + b(T_{5,j-1}^k + T_{5,j-1}^{k+1}) \\
 (a_l-d_6^l)T_5^k + (1-2a_l-b+d_6^l)T_6^k + a_lT_7^k + b(T_{6,j-1}^k + T_{6,j-1}^{k+1}) \\
 (2a_l-d_7^l)T_6^k + (1-2a_l-b+d_7^l)T_7^k + b(T_{7,j-1}^k + T_{7,j-1}^{k+1})
 \end{pmatrix} \quad (9)$$

where

$$a_s = \frac{\alpha_s \Delta t}{2(\Delta r_s)^2} \quad a_l = -\frac{\alpha_l \Delta t}{2(\Delta r_l)^2} \quad b = \frac{u_z \Delta t}{2\Delta z}$$

$$c_s = \frac{k_s \Delta t}{2\rho_s h_{sf} \Delta r_s} \quad c_l = \frac{k_l \Delta t}{2\rho_l h_{sf} \Delta r_l} \quad d_1^s = \frac{\alpha_s \Delta t}{2r_1 \Delta r_s} \quad d_1^l = \frac{\alpha_l \Delta t}{2r_1 \Delta r_l}$$

j indicates the node distributed axially. It is omitted whenever the current node is referred. And r_1 is the radial position of the i th node.

The axial velocity is assumed to be constant 100 cm/sec. The specific heat for alloys with a freezing temperature transition is taken as¹⁵

$$C(T) = \begin{cases} c_p & T < T_{fs} \\ c_p \frac{T_{fs}}{T} + (1 - \frac{T_{fs}}{T}) \frac{h_{sf}}{T_{fl} - T_{fs}} & T_{fs} < T < T_{fl} \\ c_p \frac{T_{fs}}{T} + \frac{h_{sf}}{T} + c_p (1 - \frac{T_{fl}}{T}) & T_{fl} < T \end{cases}$$

RESULT AND CONCLUSION

The result of the computer program shows temperature distributions in the solidified and the fluid region as well as the fusion interface location at any instantaneous time for any specific radial and axial position. Adjacent to the inlet, the heat flux to the wall is larger than downstream due to the supply of superheated liquid. Therefore, the solid temperature may be raised to the melting temperature. While in the downstream of the channel, both the wall and liquid are relatively colder creating conditions more suitable for freezing. The profile of the liquid-solid interface is illustrated in Fig. 3. Two characteristics are revealed from Fig. 3: (1) The thickness of the solidifying alloy grows faster at succeeding axial locations as the liquid front moves downward. This occurs because at each previous location some heat is absorbed by the wall, and the average temperature in the liquid is reduced. (2) There is a continuing melting of the solid in a radial direction behind the moving front as more liquid enters the cooler region. The rate of melting becomes slower since the continuing flow increases the average "wall" temperature toward its solidus

value. It finally reaches an equilibrium as the fusion interface approaches the center of the channel.

If the liquid superheat is low, freezing starts to plug the entrance of the channel prior to any significant melting of the wall. On the other hand, with a superheated temperature above 350 C in the single tube experiment, melting preceded solidification at the channel entrance. Evidently the blocking location will be shifted toward the bottom of the channel. This agrees with the experimental observation.

Depending on the channel dimensions and the degree of superheat in the liquid, there is a competition between the melting of the wall and the freezing of the liquid. If melting predominates, the channel may plug, at the moving liquid front or at an intermediate location. Otherwise, the plugging occurs near the entrance. The calculated and measured penetration lengths versus the superheat in the molten alloy are compared in Fig. 2. The penetration length is not a sensitive function as related to the initial superheat temperature. This may be expected if one considers that after the superheat is removed by melting the walls near the entrance, the rest of the history of freezing will be similar. Melting will occur near the entrance due to the high heat transfer rates in liquid metals and to the large latent heat of fusion on phase change compared to the sensible heat contained in the superheated liquid.

The theoretical model is extended to predict the freezing of stainless steel cladding. This result is shown as the dotted line in Fig. 2. In reality, the molten stainless steel cladding possesses very little superheat as it first enters the lower blanket region at the early stage of the accident phase. By using 100 cm/sec as average falling velocity, stainless steel at 1650 C would plug off the coolant channel at a distance 43.25 cm downward from the core-blanket interface.

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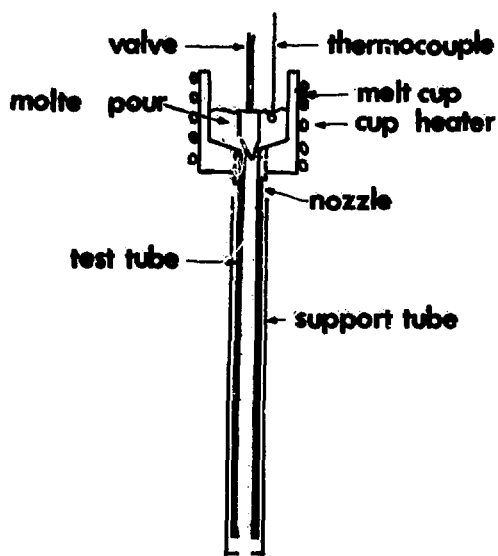


Fig. 1. Test assembly: single tube pour test

Fig. 2. Time to plug in the coolant channel vs. initial superheated liquid temperature. Liquid pouring mass = 50 gm, i.d. of the channel = 0.645 cm.

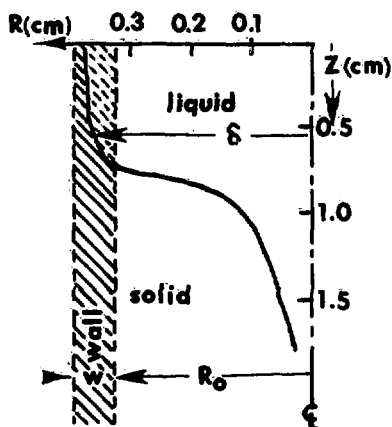
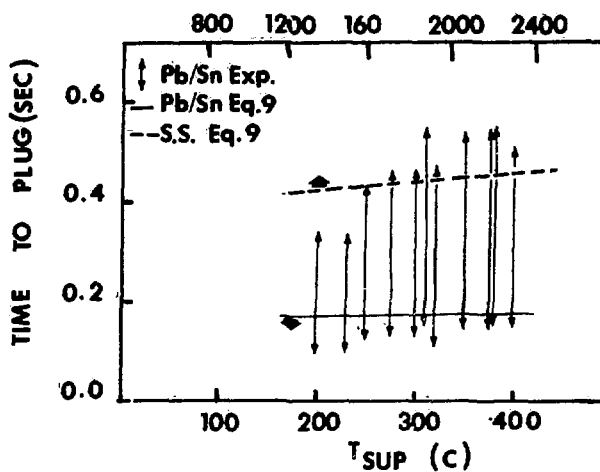


Fig. 3. Profile of fusion interface at equilibrium $T_{sup} = 216$ °C

DYNAMICS OF SOLIDIFICATION OF FLOWING FLUIDS
WITH APPLICATIONS TO LMFBR POST-ACCIDENT FUEL RELOCATION

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ABSTRACT

The process of solidification of a flowing fluid has been studied both analytically and experimentally. A series of tests was performed, using paraffin wax and Wood's metal as flowing hot fluids, to verify the model as well as to investigate experimentally the effects of various parameters on the process of transient flowing and freezing through a vertical channel. Preliminary experimental results for plugging times and the total mass of molten fluid displaced through the channel before plugging are presented. Effects of important parameters on the transient freezing process have been examined experimentally. A preliminary evaluation of the model is made based on comparison with the experimental results.

INTRODUCTION

Assessment of the dynamics of molten fuel relocation following hypothetical core-disruptive accidents in LMFBRs is of importance in studies of accident energetics and of post-accident heat removal. The energetics of a molten core recriticality event is dependent upon the quantity of fuel remaining in the core and, hence, on the extent of material relocation by the streaming and freezing processes. The potential for safe containment of the core materials of an LMFBR following a hypothetical accident depends to a great extent on the post-accident distribution of fuel and other core materials, and on the time scales for material relocation. Upward fuel relocation may be driven by sodium vapor pressure in a transient overpower accident (TOP), and by fuel or steel vapor pressure in a loss-of-flow (LOF) accident. Molten fuel relocation in the downward direction would be driven by static head pressure in addition to vapor pressure. In either case, if coolant channels are not plugged, ejection of the molten core debris can occur within a few seconds. However, deposition of the molten debris by freezing on cold structures leads to plug development and the formation of a "bottled" core region. In such a case, fuel dispersal could only occur after the rather slow melt-out of the plug or the lower assembly structure [1]. The real core material relocation will depend upon the composition and on the phase distribution of the flowing material. These, in turn, will depend on

the flow regimes existing within the core region. This report is directed towards the processes involved in the flow of single-component, single-phase molten fuel through the cold lower structure of an LMFBR under relatively low driving pressures.

The freezing of a flowing fluid falls into a general category of "Stefan" [2] type problems. These problems involve heat transfer between multiple regions with phase changes occurring at the moving region boundaries. Those works reviewed by the authors could be grouped into a few broad categories: (a) the exact closed form solutions of Stefan [2], Carslaw and Jaeger [3], and Rosenthal [4], which exist for some special cases where conduction is the sole mode of heat transfer, (b) approximate analytical solutions to problems of freezing and melting, which take into consideration the effects of convective heating, such as the work done by Libby and Chen [5], and Lapadula and Mueller [6]. (c) those works offering finite-difference solutions, among which those by Lendau [7], Bilenas and Jiji [8], and Beaubouef and Chapman [9] are outstanding (d) a parametric study which results in an empirical correlation of the transient freezing data as the work done by Cheung and Baker [10], (e) several ad hoc models 11-13 that have been proposed for treatment of the fuel relocation problems. On the dynamics of the solidification process, in particular, two experimental results, those of Hirschberg [14] and the work done at Argonne National Laboratory [10, 13] have been reported.

The objective of the present work was to study the process of freezing of a liquid in transient flow inside a cooled cylindrical tube. A simulation experiment was designed to evaluate a mathematical model developed at BNL 15 which describes the solidification of single-phase molten fuel flowing through cold channels with non-melting boundaries. The mathematical formulation is briefly outlined. The experimental apparatus and techniques are described and experimental results are described. A preliminary evaluation of the model is presented.

ANALYTICAL MODEL

A two-dimensional transient freezing model in cylindrical geometry has been formulated [15] to study the phenomenon of solidification of flowing fluid inside a circular channel, the walls of which are cooled below the freezing temperature of the flowing fluid. The model is discussed in Ref. 15, and is not presented here in detail. Rather, the model is outlined as applied to the experimental test section. The test section shown schematically in Fig. 1, was in an annular co-axial counterflow heat exchanger configuration. The molten simulant fluid flowed downward through the inside tube. Cooling water flowed upward in the annular region. The model determines the thickness of the solidified shell on the inside wall of a vertical channel in terms of time and axial location. Since exact solutions are not feasible for the coupled nonlinear partial differential equations that describe the heat transfer and fluid dynamics of the solidification process, a number of simplifying assumptions and approximations were introduced which include:

- (a) A quasi-steady state radial temperature field exists in the frozen layer.
- (b) A quasi-steady flow exists in the liquid region.
- (c) Heat transfer from the flowing fluid to the solid-liquid interface is assumed to be a quasi-steady state process which is

independent of the changing radius and solidification occurring at the interface.

- (d) Axial heat conduction in the solidified shell and in any surrounding material is neglected.
- (e) No melting of wall material occurs.
- (f) No heat generation in the simulant fluid.
- (g) The temperature of the coolant flow in the test section annulus (T_{∞}) is constant.

With the above assumption, the model is formulated by incorporation of steady conduction equation for the solidified shell and for the inner tube wall. The steady Bernoulli equation, which includes friction losses and the acceleration term due to channel area change, is applied to the molten fluid flow. The pressure drop across the test section length is assumed held constant. The molten fluid energy equation is written as

$$\rho C_p v \frac{dT_m}{dx} + \frac{2h_i}{R} (T_m - T_f) = 0 \quad (1)$$

with inlet condition $T_m(x = 0) = T_i$. The boundary condition at the moving interface is

$$-k \frac{\partial T}{\partial r} \bigg|_R = h_i(T_m - T_f) + \rho \lambda \frac{dR}{dt} \quad (2)$$

Heat transfer from the inner tube to the coolant (at T_{∞}) was assumed to occur across a film with conductance h_o . The model equations were nodalized by dividing the axial length into a number of equidistant nodes. The method of solution is discussed in detail in Ref. 15. The interior heat transfer coefficient (h_i) for the laminar regime is obtained from the Sieder-Tate equation [16], while those for the turbulent regime and the exterior heat transfer coefficient (h_o) are obtained from the Dittus-Boelter type equation. The interior heat transfer coefficient (h_i) used in the analysis of the Wood's metal case, however, is evaluated from the correlation developed for liquid metal by Seban and Shimazaki [17].

EXPERIMENTAL METHOD

Apparatus Description

Two series of experiments were designed; one using paraffin wax, the other Wood's metal as fuel simulants. The major components and the experimental system, shown in Fig. 2, were: a reservoir tank, the test section and a catch tank. The reservoir tank was a cylindrical vessel, where the molten fluid was stored at a controlled initial temperature and pressure. The two identical test sections were single-pass countercurrent heat-exchangers. Both inner and outer tubes were made of brass. Dimensions are shown in Table I below. Molten fluid flowed through the inner tube, which was cooled by water flowing vertically upward through the cooling jacket formed by the annulus between the inner and

TABLE I

Test Section Geometry

	Small Test Section		Large Test Section	
	Inner Tube	Outer Tube	Inner Tube	Outer Tube
O.D. (cm)	0.635	1.270	1.270	1.905
I.D. (cm)	0.472	1.107	1.107	1.575
Length (cm)	68.2	60.5	68.8	59.0

outer tubes. The catch tank received the molten fluid leaving the vertical test section. The mass of fluid collected was monitored using a load-cell and associated recording equipment.

Test Parameters

The experiments were designed to measure, for each set of initial and boundary conditions, the time required for complete channel plugging, the mass of molten fluid displaced as a function of time, and the transient molten fluid exit temperature. These quantities were chosen in order to provide a direct comparison with the mathematical model. Plugging times were measured using an electrical timer. Fluid displacement and fluid exit temperature were measured by a load-cell and with an exposed thermocouple, respectively. Also measured were the molten fluid inlet temperature, reservoir pressure, water coolant flow rate and inlet and outlet temperatures.

The controllable test parameters were: (a) fluid type, (b) initial molten fluid temperature, (c) internal diameter of inner tube, (d) coolant water flow rate through test section annular space, and (e) the driving pressure behind the hot simulant fluid.

As stated above, two fluids (paraffin wax and Wood's metal) were chosen based primarily on their respective Prandtl numbers. The Prandtl number of UO_2 at 3000°C is about 0.86. For paraffin wax and Wood's metal, the Prandtl numbers are 43 and 0.018, respectively, within the range of experimental conditions. Thermo-physical properties of the two simulant fluids are shown in Table II.

TABLE II
Properties of Simulant Fluids

	Paraffin Wax		Wood's Metal
	Solid (16 - 52°C)	Liquid (52 - 100°C)	
Melting Point ($^\circ\text{C}$)	52		73
Heat of Fusion (cal/g)	42		11.1
Density (g/cm^3)	0.91 - 0.81	0.797 - 0.788	9.2
Thermal Conductivity ($\text{cal}/\text{cm}\cdot\text{sec}\cdot^\circ\text{C}$)	5.3×10^{-4}	4.8×10^{-4}	3.2×10^{-2}
Specific Heat ($\text{cal}/\text{g}\cdot^\circ\text{C}$)	0.5 - 0.55	0.58 - 0.6	3.6×10^{-2}
Viscosity (poise)	0.06	0.036 - 0.031	1.6×10^{-2}

Test Procedure

The reservoir tank was charged with the molten fluid to be tested. Power was then applied to the heater coil surrounding the reservoir where a mechanical stirrer maintained a uniform fluid temperature distribution. At the same time,

the coolant water flow rate and the pressure in the reservoir were set at the desired levels. When the temperature of the molten fluid reached the desired value, the heater power was turned off and a 4-way air-valve was opened by means of a push-button. This triggered on the recording systems and pulled the plug (Fig. 2) instantly upward; the molten fluid at this point began to flow into the test section. The displaced molten fluid was received inside the catch tank located immediately below the exit of the test channel. At the completion of the test, the monitoring and recording systems were terminated by means of a push-button as soon as the molten fluid stopped flowing as a result of either complete plugging or exhaustion of the entire initial mass of fluid in the reservoir. After all the appropriate quantities were measured, the inside surface of the test section was cleaned with the flow of steam.

EXPERIMENTAL RESULTS AND COMPARISON WITH MODEL

Results have been obtained for both paraffin wax and for Wood's metal under a broad range of experimental parameters. Included among the experiments were a number of runs directed towards evaluation of the reproducibility of the experimental system. Representative experimental results are presented here. For paraffin wax, these include data for the plugging time and transient molten fluid displacement. For Wood's metal, experimental results for plugging times are presented and discussed.

Paraffin Wax Results

Representative results for transient molten fluid displacement are shown in Figs. 3-7. In the data of Figs. 3-5, all parameters were held fixed except for the inlet molten fluid temperature. The experimental results presented in Fig. 3 are reasonably reproducible. The experimental results fall about 20-25% below the model predictions for times up to about 150 sec. Computational difficulties were encountered at about 80 sec, and the dashed line is an extrapolation from earlier times. The apparent discrepancy between theory and experiment subsequently increases. For the results of Fig. 4, the mathematical model prediction lies within the apparent data scatter. The agreement is to within roughly +40% for times greater than 10 sec. In Fig. 5, the prediction lies well below the experimental data. The data shown in Figs. 4 and 5 reveal a degree of uncertainty concerning the reproducibility of the experimental data.

Figs. 6 and 7 present data for experiments in which the driving pressure and flow channel diameter were varied. A comparison of these data with those of Figs. 3-5 indicate that the molten fluid displacement increases rather significantly with increasing driving pressure and increasing channel diameter. In both Figs. 6 and 7, predictions lie well below the experimental data. In Fig. 7, theoretical results for $T_i = 75^\circ\text{C}$ and $T_i = 65^\circ\text{C}$ were almost indistinguishable.

Experimental data for plugging time are presented in Fig. 8. Good agreement between theory and experiment are apparent for small $C_p(T_i - T_f)/\lambda$. The results for larger $C_p(T_i - T_f)/\lambda$ are inconclusive at this time (computations have not been performed for larger values of $C_p(T_i - T_f)/\lambda$).

Wood's Metal Results

Experimental results for plugging time are shown in Fig. 9. The data scatter is relatively large. Agreement between the mathematical model and the data is, generally, within $\pm 60\%$. While the model predicts a two-fold

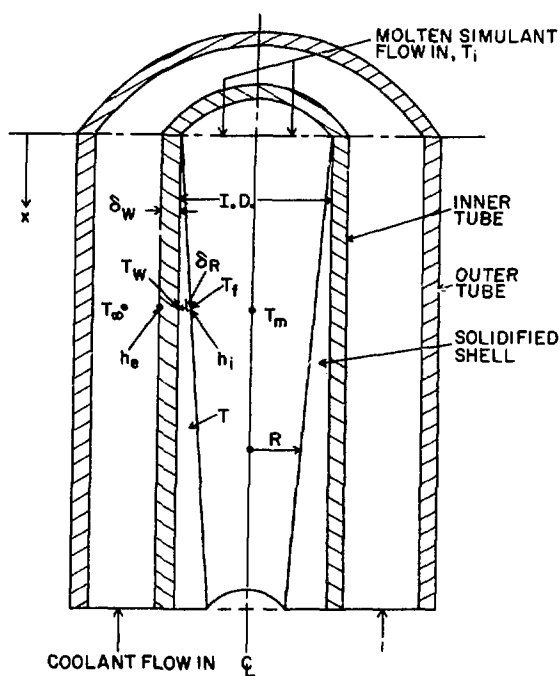


Fig. 1. Schematic of countercurrent flow test section

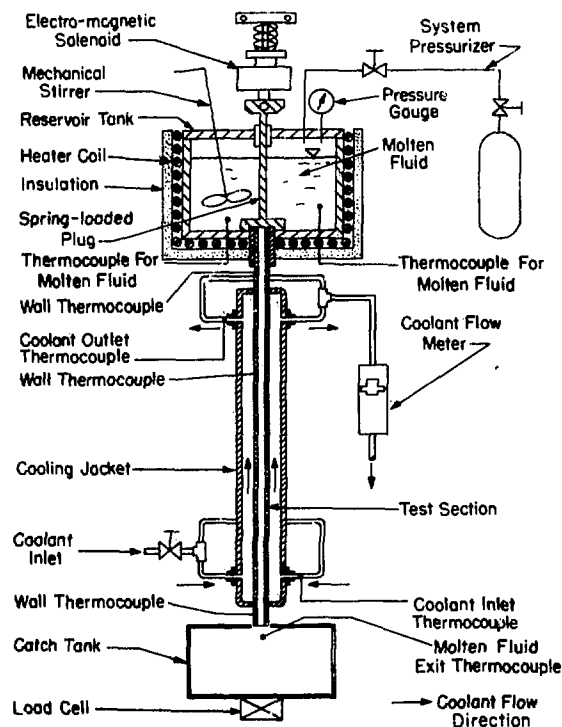


Fig. 2. Schematic diagram of experimental apparatus

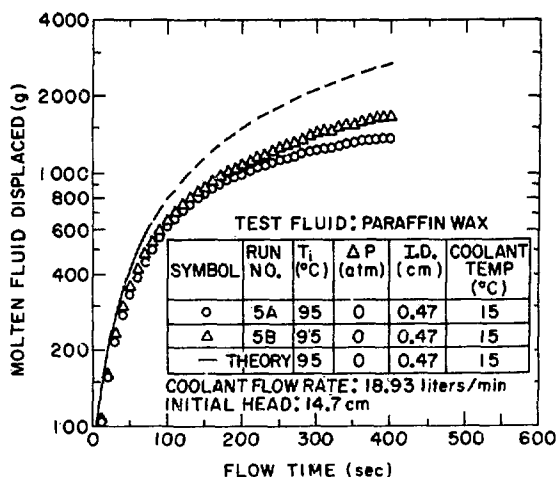


Fig. 3. Reproducibility and comparison with theory for paraffin wax inlet temperature of 95°C

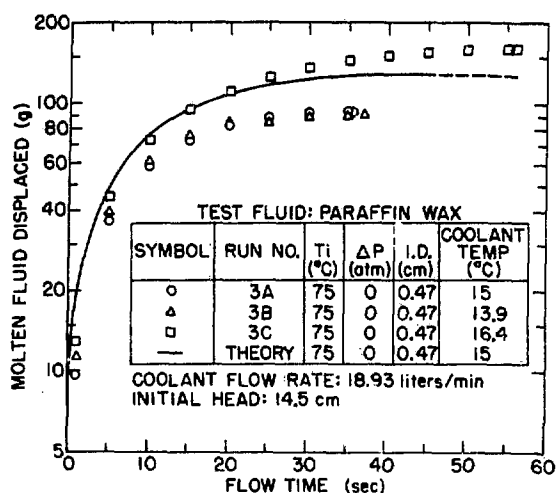


Fig. 4. Reproducibility and comparison with theory for paraffin wax inlet temperature of 75°C

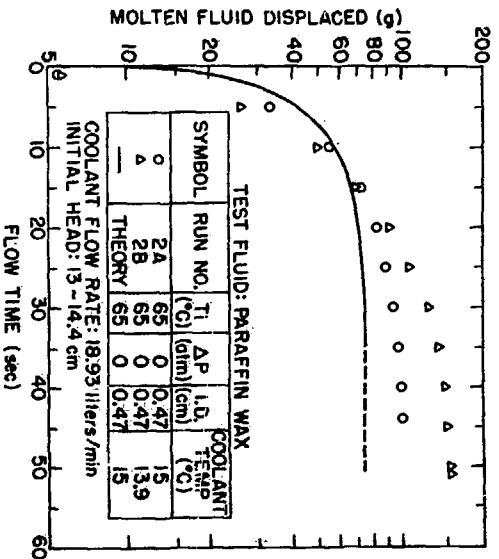


Fig. 5. Reproducibility and comparison with theory for paraffin wax inlet temperature of 65°C.

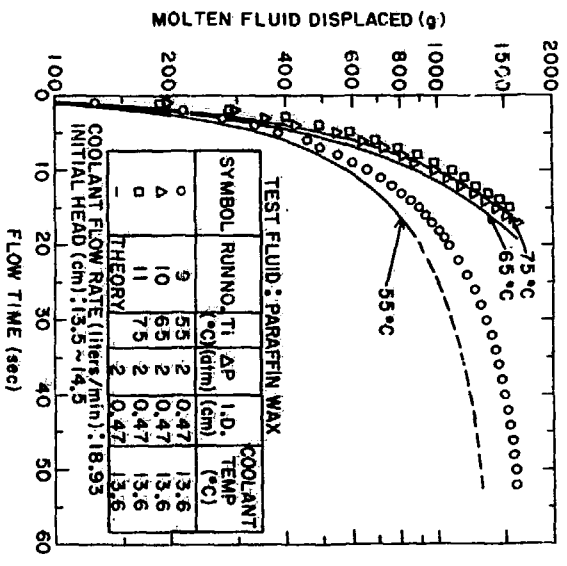


Fig. 6. Mass displaced vs. time ($\Delta p = 2$ atm.).

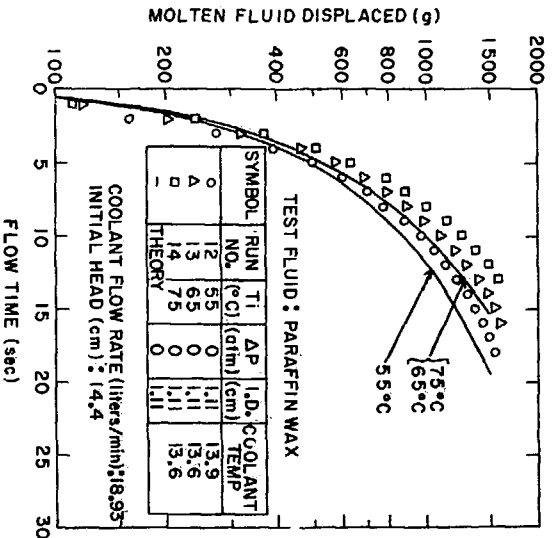


Fig. 7. Mass displaced vs. time ($D_1 = 1.11$ cm).

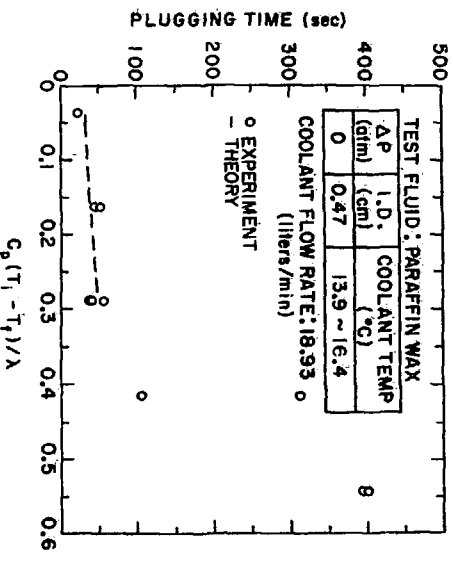
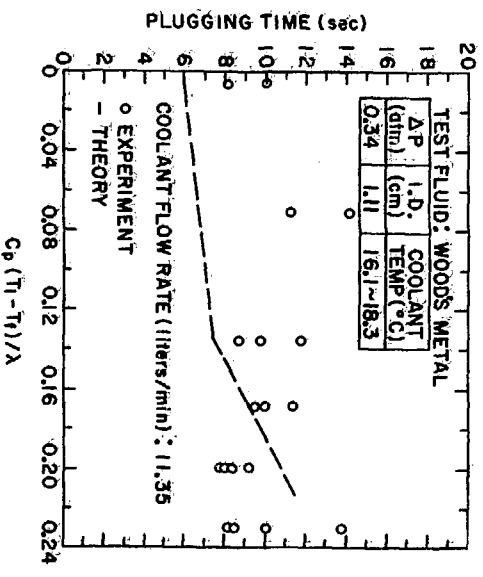


Fig. 8. Plugging time vs. $C_p(T_1 - T_f)/\Delta$ (Paraffin wax).

Fig. 9
Plugging time vs.
 $C_p(T_1 - T_f)/\Delta$
(Wood's metal).



variation of plugging time over the range of $C_p(T_i - T_f)/\lambda$ considered, this trend is not apparent from observation of the experimental data.

Summary of Parametric Effects

The effects of various parameters observed on the paraffin wax and the Wood's metal freezing process may be briefly summarized as follows:

(1) Effect of the Molten Fluid Inlet Temperature:

Results of the tests with paraffin wax showed that the plugging time as well as the total amount of fluid displaced through the channel prior to the plugging increased as the molten fluid temperature level increased when other conditions were held constant (the maximum superheat used in the paraffin wax test was 43°C). On the other hand, Wood's metal test results did not reveal that both the plugging time and the total mass displaced were appreciably affected by the difference in superheat alone for the test conditions used in the present work (the maximum Wood's metal inlet temperature and the superheat were 145°C and 72°C , respectively).

(2) Effect of Cooling Rate:

The effect of the change in cooling rate (by changing the coolant flow rate from 18.93 liters/min to 11.35 liters/min, thereby reducing the exterior heat transfer coefficient, h , by 30%) on the paraffin wax transient freezing process was not appreciable.^e This was partly due to extremely low thermal conductivity of the paraffin wax which could, in effect, act as a thermal insulator when its frozen shell thickness was appreciably large; the ratios of thermal resistances to the solidified shell (when shell thickness is 0.1 cm), to the tube wall, and to convective heat transfer (for 11.35 liters/min coolant flow rate) were 245:2:0.4, respectively. With respect to the Wood's metal, the present experimental data did not offer a conclusive evidence.

(3) Effects of Driving Pressure and the Channel Diameter:

The amount of fluid displaced is extremely sensitive to the driving pressure for the case of the molten paraffin wax whose density is very small. The effect of the driving pressure on the Wood's metal, however, was far smaller than that for the paraffin wax.

The effect of the channel diameter has been examined with paraffin wax only (by changing the test section inside diameter). The total displaced mass for smaller test section ($D_i = 0.47$ cm) was between 35g and 165g (at $T_i = 55 \sim 75^\circ\text{C}$) for flowing time of 23 ~ 57 seconds, while for larger test sections it took between 24 ~ 31 seconds to displace all the initial mass charge (~1610g). Thus, there was an order of magnitude difference in the total amount of displaced mass (for approximately the same flowing time) when the ratio of the cross-sectional area of the two sections was only 5.6. This was mainly due to increase in average flow velocity for the larger test section.

CONCLUSIONS

The process of solidification of a single-phase flowing hot fluid in a cylindrical tube has been studied both experimentally and analytically. Effects of several parameters such as initial fluid temperature, coolant flow rate, driving pressure of the hot fluid, and the internal diameter of the tube have

been examined experimentally and analytically for two fluids characterized by widely varying Prandtl number. Preliminary experimental results are presented.

A comparison of the preliminary experimental data with the mathematical model of the freezing process has been presented. This comparison has indicated that the molten fluid displacement transients can, generally, be predicted to within $\pm 40-50\%$.

In the case of paraffin wax, the plugging time results indicate good agreement between experiment and prediction for small $C_p(T_i - T_f)/\lambda$. In the case of Wood's metal, the plugging time can, generally, be predicted within $\pm 60\%$. The trend of increasing plugging time with increasing $C_p(T_i - T_f)/\lambda$ is apparent from the experimental data of paraffin wax, whereas the same trend is not apparent for the case of Wood's metal.

The reasons for the discrepancies between the model prediction and the experimental data are not clear at this time. Sources of random and systematic error are currently being reevaluated.

NOMENCLATURE

C_p = specific heat

D_i = tube inside diameter

h_e = exterior heat transfer coefficient between channel wall and coolant water

h_i = interior heat transfer coefficient between flowing fluid and solidified shell

k = thermal conductivity

p = pressure

r = radial coordinate

R = radius of liquid-solid interface

t = time

T = temperature field within the frozen layer

T_f = fusion temperature

T_i = inlet fluid temperature

T_m = fluid bulk temperature

T_w = wall temperature

T_∞ = sink temperature

v = fluid velocity

x = axial coordinate

δ_R = solidified shell thickness

δ_w = tube wall thickness

λ = heat of fusion

ρ = density

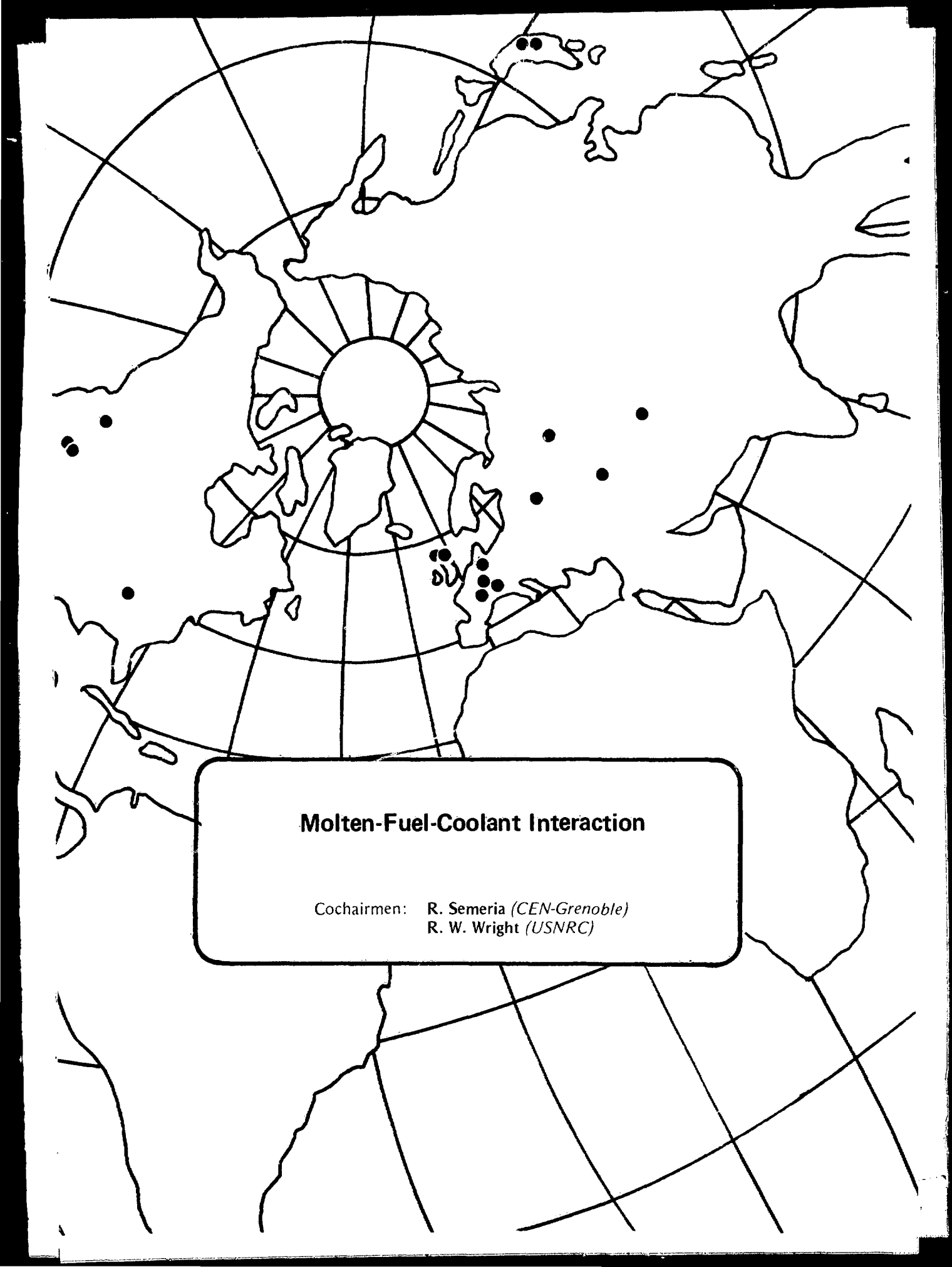
ACKNOWLEDGMENTS

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Molten-Fuel-Coolant Interaction

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A CRITIQUE OF THE BOARD-HALL MODEL FOR THERMAL DETONATIONS IN UO_2 -Na SYSTEMS

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ABSTRACT

The Board-Hall model for detonating thermal explosions is reviewed and some criticisms are offered in terms of its application to UO_2 -Na systems. The basic concept of a detonation-like thermal explosion is probably valid provided certain fundamental conditions can be met; however, Board and Hall's arguments as to just how these conditions can be met in UO_2 -Na mixtures appear to contain serious flaws. Even as given, the model itself predicts that a very large triggering event is needed to initiate the process. More importantly, the model for shock-induced fragmentation greatly overestimates the tendency for such fragmentation to occur. The shock-dispersive effects of mixtures are ignored. Altogether, the model's deficiencies imply that, as given, it is not applicable to LMFBR accident analysis; nonetheless, one cannot completely rule out the possibility of meeting the fundamental conditions for detonation by other mechanisms.

INTRODUCTION

In 1974 the British workers S. J. Board and R. W. Hall [1] proposed a model for the propagation of vapor explosions, which was based upon a close analogy with chemical detonations. Since the model predicts that, under certain conditions, very powerful explosions can occur in molten UO_2 -Na mixtures, it is of obvious interest to the LMFBR safety community. In this paper we review the model and its theoretical foundation, and then offer some criticisms which suggest that Board and Hall may have overestimated the possibility that such explosions could occur in LMFBR accident situations. We conclude with some additional discussion, including some cautionary remarks against overinterpreting the results offered here.

SUMMARY OF THE MODEL

Since the Board-Hall model is based upon shock and detonation physics, we briefly review this topic, omitting many refinements and qualifications that are required when dealing with solid materials [2].

Consider a severe stress wave propagating through a medium for which the sound speed tends to increase as the material is compressed; most materials satisfy this condition. The high-pressure portion of the wave then tends to overtake the leading edge, so that the compression phase of the wave steepens into a near-discontinuous jump in the pressure called a shock. By applying the equations of mass, momentum, and energy conservation, it may be shown that the material properties before and after shock passage must be related by the well-known Rankine-Hugoniot jump relations:

$$U = V_1 [(P_2 - P_1)/(V_1 - V_2)]^{1/2} , \quad (1a)$$

$$u = [(P_2 - P_1)(V_1 - V_2)]^{1/2} , \quad (1b)$$

$$E_2 = E_1 + \frac{1}{2}(P_2 + P_1)(V_1 - V_2) . \quad (1c)$$

Here U , u , E , V , and P are the shock propagation velocity, shock-induced material velocity, specific energy, specific volume, and pressure, respectively; subscripts 1 and 2 refer to conditions before and after shock passage, respectively.

In addition, the material obeys an equation of state of the form $P = P(E, V)$. For a given set of initial conditions, we have four equations in five unknowns (U , u , P_2 , V_2 , E_2). Specifying any one of the latter in effect specifies the other four. In particular, for any two of these quantities (Q_i and Q_j , say) a plot of the states which may be obtained (from given initial conditions) by shocks of various strengths lies along a curve in the $Q_i - Q_j$ plane called the Hugoniot. For many fully-dense (non-porous) materials, it has been found experimentally that the $U-u$ Hugoniot approximately follows an especially simple form,

$$U = c_0 + su , \quad (2a)$$

where s is an empirical, non-dimensional constant which usually lies between 1 and 2; c_0 is also basically an empirical constant but it is generally quite close to the normal sound speed and we shall take it to be equal to the sound speed here. By combining Eq. (2a) with Eq. (1a) and (1b), it may be shown that the $P - V$ Hugoniot will then be of the form:

$$P = c_0^2 \epsilon / [V_0 (1 - s \epsilon)^2] = K_0 \epsilon / (1 - s \epsilon)^2 , \quad (2b)$$

where the subscript o refers to the uncompressed reference state, ϵ is the volumetric strain ($1 - V/V_o$), and K_o is the isentropic bulk modulus. It is also worth noting that the difference between the Hugoniots and isentropes is slight for small ϵ ; in some of what follows, this difference is ignored.

The detonation of chemical high explosives is more complicated. It involves the following sequence of events:

1. The detonation subjects the unreacted explosive to a severe shock.
2. As a result, the explosive undergoes energetic decomposition on a time scale, t , so short that the reaction zone thickness Ut is usually < 1 mm, often $\ll 1$ mm.
3. The hot reaction gases expand with a substantial conversion of heat energy to work, which supplies further energy to continue driving the detonation wave.

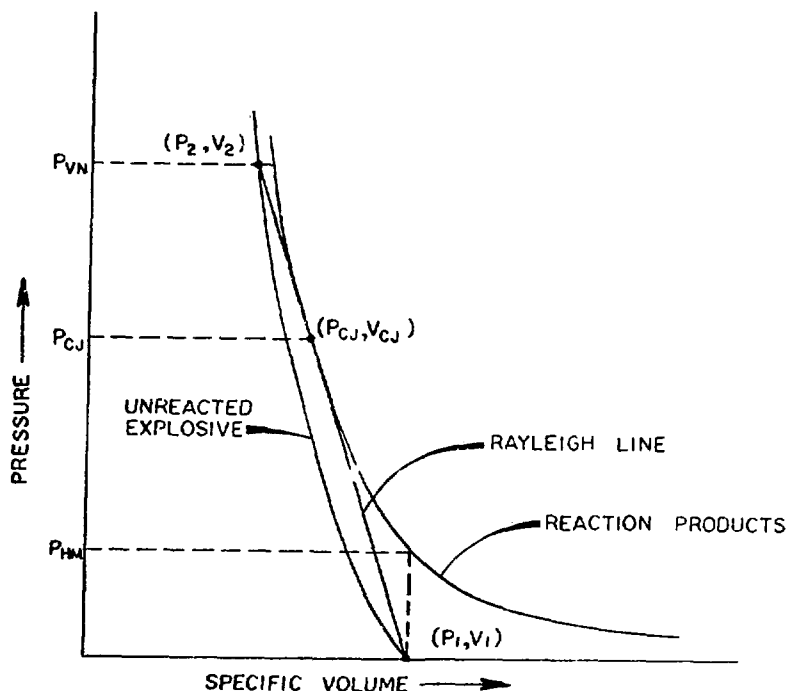


Figure 1. Schematic P-V Diagram for a Detonating Explosive

The system is diagrammed in terms of the P-V Hugoniots in Fig. 1. The lower curve represents the Hugoniot for the unreacted explosive initially in the state (P_1, V_1) . The upper curve represents the Hugoniot for the high-temperature, gaseous reaction

products. It can be shown that the detonation wave will propagate steadily, without attenuation or growth in amplitude, if and only if the line connecting the initial state with the state existing at the completion of the reaction is tangent to the reaction product Hugoniot. The point of tangency, (P_{CJ}, V_{CJ}) , gives the conditions at the end of the reaction and is called the Chapman-Jouget point, or CJ point, and is a characteristic constant for a given explosive at a given (P_1, V_1) .

From Eq. (1a), the slope of the line connecting (P_1, V_1) and the CJ point, called the Rayleigh line, is equal to $-U^2/V_1^2$. Since the shock initially propagates through unreacted explosive, the initial pressure must be given by the intersection of this line with the corresponding Hugoniot at P_2V_2 . This still higher pressure, P_{VN} , is called the Von Neuman spike; it is so narrow, however, that it attenuates very rapidly in a non-explosive material and the CJ pressure is normally the quantity of interest.

The basis of the Board-Hall model was its authors' observation that thermal explosions can proceed with an essentially identical structure if the following three fundamental assumptions are valid:

1. The liquid-liquid system is initially in the form of a mixture that is too coarse to permit significant heat transfer on a time scale comparable to the time required for the detonation wave to traverse the system.
2. A strong triggering shock is supplied.
3. A shock having the CJ amplitude will fragment the coarse initial mixture into a much finer mixture permitting extremely rapid heat transfer, with the total time required for fragmentation and thermal equilibration being much less than that required for the detonation wave to traverse the system.

Given these three assumptions, Board and Hall show that the explosion can propagate with a structure identical to that of the chemical explosion, with the zone of rapid fragmentation and heat transfer corresponding to the reaction zone for the chemical case. They also show that such a detonation can actually generate pressures considerably higher and work potentials somewhat higher than those calculated by Hicks and Menzies [3]. The reason is that, here, the mixture is first compressed by the shock and heat is then transferred to the volatile component at above normal densities; Hicks and Menzies assumed heat would be transferred at a constant volume corresponding to the normal density thereby leading to a pressure approximately equal to P_{HM} in Fig. 1.

The author knows of no flaw in the basic argument, but it is much more debatable as to whether the model's three fundamental assumptions are valid in practice. The third assumption is the crucial one in terms of the model's internal structure; the first two are actually assumptions about the presumed initial conditions.

Board and Hall argue that this third assumption will be met by noting that, due to the large UO_2 -Na density difference, the shock will accelerate the two phases to quite different velocities and the resulting velocity differential, v , will tend to induce UO_2 fragmentation due to Taylor instabilities. Drawing an analogy with the data which Simpkins and Bales [4] obtained for shock-induced breakup of liquid droplets in gases, they concluded that the time, t , required for fragmentation of a UO_2 drop of radius, r_1 , is given by:

$$(\rho/\rho')^{1/2} vt/r_1 = 44 B_0^{-1/4} , \quad (3a)$$

where ρ and ρ' are the Na and UO_2 densities and the Bond number, B_0 , is defined by:

$$B_0 = \rho' gr_1^2/\sigma = 3\rho v^2 r_1 C_D/(8\sigma) , \quad (3b)$$

where g is the acceleration imparted by drag forces, σ the surface tension, and C_D the drag coefficient. It is then assumed that the drop fragments down to a final size r_2 governed by a Weber-type criterion, $\rho v^2 r_2/\sigma \approx 8$; r_2 turns out to be so small (microns or less) that heat transfer is essentially instantaneous compared with the time required for fragmentation.

On the other hand, Board and Hall indicate that the relative velocities of the two fluids should tend to equilibrate during a characteristic time t' given by:

$$t' \approx 8\rho' r_1/(3\rho v C_D) . \quad (4)$$

This estimate was evidently obtained simply by taking $t' = v/g$, where the acceleration, g , is given by the drag force, $F_D = \frac{1}{2}\rho v^2 C_D A$ divided by the droplet mass, $4\pi\rho' r_1^3/3$, and A is the cross sectional area πr_1^2 . Based upon the results for liquid droplets, the drag coefficient, C_D , was taken to be about 2. Board and Hall then argue that the third fundamental assumption will be satisfied if t' is greater than the fragmentation time, t .

Board and Hall consider the case of UO_2 at 3550 K, Na at 700 K, a UO_2 /Na ratio of 10 by weight, and a 50 percent void (vapor) fraction by volume. They find that the fragmentation criterion is indeed met and calculate $U = 1.9 \times 10^5$ cm/sec, $P = 15$ kbar, and $u = 2.3 \times 10^4$ cm/sec at the CJ point; if vapor is absent, they state that P increases considerably.

The authors also note that the reaction zone thickness, $X_r = Ut$, is very much greater than for chemical explosives, of the order of 10 cm or more if $r_1 = 0.5$ cm, as they assumed. The model is one-dimensional and cannot apply without substantial modification unless the dimensions of the system, L_s , are much greater than X_r . Thus, the phenomenon is predicted to be possible in full-scale LMFB accidents but not in much smaller scale experiments. In particular, no such explosions would be expected in any UO_2 -Na experiments performed to date, both because of their relatively

small scale and also because of the absence of a strong trigger. Thus, the fact that almost all of these experiments yielded benign responses is not very relevant to the question of whether the model is valid.

Indeed, these facts point to a singularly vexatious aspect of the Board-Hall model from the point of view of LMFBR safety analysis: the model predicts that extremely powerful explosions are possible in full-scale LMFBR accidents, yet the model also predicts that it will be virtually impossible to give the model any rigorous experimental test in an actual $\text{UO}_2\text{-Na}$ experiment of reasonable size. Before attempting an experimental test, it is therefore worthwhile to review some features of the model a little more carefully.

SOME CRITICISMS OF THE MODEL

As noted earlier, a crucial feature of the Board-Hall model is the question of shock-induced fragmentation of the initially-coarse mixture of the two liquids; it is this question that we shall emphasize in the present section. Board and Hall noted that the calculated value of u at the CJ point is adequate to cause breakup according to Eqs. (3) and (4); they also assert that, in the fragmentation zone, the differential velocity, v , will be greater than this value. The latter is true if we can assume that v is of the same order of magnitude as u , as given by Eq. (1b) in the fragmentation zone; we shall accept this assumption in what follows.

To evaluate u (or v) as a function of the shock amplitude P requires an approximation for the Hugoniot of the unfragmented mixture through which the shock initially propagates. This is actually a very complex problem which we can treat here only with the following simple approximations. We assume that, for Na and UO_2 , the Hugoniots can be expressed by Eq. (2) with $s = 1.27$ and 1.5 , respectively (results that follow are insensitive to s). For the composite, we assume the P - ϵ curve can be constructed by evaluating ϵ individually for the two constituents at a given P and taking a volume-weighted average. This assumption is clearly not precisely correct, but it is unlikely to have any order-of-magnitude effects upon the results to be given.

If void space (i.e., vapor or gas) is present, we let V_0 represent the mean specific volume of the mixture without void space and represent the specific volume including voids as $V_1 = V_0/(1 - \alpha)$, where α is the void fraction. We assume any reasonable final pressure completely collapses the voids and that the final volume, V_2 , is independent of α . Again, there are approximations involved here, but refining them would decrease calculated values of v and actually strengthen the conclusions that follow.

We next estimate the magnitude of the triggering event required to initiate the Board-Hall process, which turns out to be quite large; we then go on to apply a correction to their analysis, which raises serious questions as to whether fragmentation can be induced even by shocks considerably greater than the CJ amplitude. We conclude this section with a brief discussion of the possible influence of the shock-dispersive effects of mixtures.

Initiating Events

Initiation requires a shock above some minimum value P_m of duration at least t . The magnitude of P_m is set by the need to meet the fragmentation criterion. By inserting numerical values for UO_2 and Na material properties [5] into Eqs. (3) and (4), we obtain:

$$t \approx 710 r_1^{3/4} / v^{3/2}; \quad t' \approx 13.1 r_1 / v \quad (5)$$

For shocks below some limiting amplitude, v will be low enough so that the fragmentation criterion, $t < t'$, will not be met; if $r_1 = 0.5$ cm, for example, Eq. (5) implies $v > 4000$ cm/sec is required.

A computer code, based upon the assumptions discussed above, was written to estimate the value of P_m sufficient to give a velocity u , as calculated from Eq. (1b), to meet the fragmentation condition. Equations (1a) and (1c) were then used to estimate the corresponding values of U and E . Since the initiating pressure must be applied for at least a time t , the initiation zone must be of a thickness $X_r = Ut$. If the initiation region must be an order of magnitude greater in lateral extent than the thickness in order to preserve one-dimensionality, the volume of the initiation region is of the order $100 X_r^3$ and the total energy, E_{tot} , imparted to it is $\sim 100 X_r^3 E(1 - \alpha)/V_0$. The latter may be an overestimate if the lateral extent of the initiation region need not be as great as assumed; on the other hand, we have only attempted to estimate the energy imparted to the initiation region by virtue of its being subjected to the triggering shock. The total energy available to the triggering event itself must be considerably larger.

Results are summarized in Table 1, where P_m , X_r , and E_{tot} are given for various values of α and r_1 . For $r_1 = 0.5$ and $\alpha = 0$, the "trigger" needed is extremely massive, hardly less destructive than the fully-developed reactor-wide explosion would be. The more realistic cases with $\alpha > 0$ have lesser triggering requirements but they are still very large. If $r_1 = 0.05$, total trigger energy needed is considerably less, but still substantial and P_m is actually increased considerably.

TABLE 1
Magnitude of the Initiating Event Required
for Various Parameter Values

α	$r_1 = 0.5$ cm			$r_1 = 0.05$ cm		
	X_r (cm)	P_m (bars)	E_{tot} (J)	X_r (cm)	P_m (bars)	E_{tot} (J)
0	180	3×10^3	3×10^9	7.0	10^4	1.5×10^6
0.1	60	700	8×10^7	4.0	5×10^3	3.0×10^5
0.33	18	200	2×10^6	1.7	2×10^3	1.9×10^4
0.5	13	80	4×10^5	1.3	800	4.0×10^3

Thus, even when taken at face value, the model itself predicts that the initial conditions required involve a combination of a rather idealized mixture and a strong initiating event that seems unlikely to be realized in practice.

UO₂/Na Mass Ratio*

Even more serious to the model, Eqs. (3) and (4) would, at best, be valid for an isolated drop of UO₂ in an infinite sea of sodium, yet they were applied to a situation with a UO₂/Na mass ratio of about 10. The interfluid drag force applies equally to each component and the acceleration of the sodium is therefore about 10 times that of the UO₂. Both velocity changes are, of course, in the direction to decrease v , so that the rate of velocity equilibration was seriously underestimated, and t' is correspondingly overestimated, by Eq. (4).

We estimate the importance of this effect by assuming the interfluid drag force per unit volume, F_D , for a mixture of two fluids, a and b , to be:

$$F_D = \bar{\rho} v^2 C_D A/2 \quad , \quad (6)$$

where A is the perpendicular fluid-fluid interfacial area per unit volume and $\bar{\rho}$ is the average density $f_a \rho_a + f_b \rho_b$, where f 's and ρ 's are the volume fractions and densities of the two fluids, respectively; for the moment, we let Eq. (6) be the effective definition of C_D . Since the drag force acts equally on both constituents, the differential velocity decays at a rate given by:

$$\frac{dv}{dt} = - \left(\frac{1}{f_a \rho_a} + \frac{1}{f_b \rho_b} \right) F_D = - \frac{1}{2} \frac{\bar{\rho}^2}{f_a \rho_a f_b \rho_b} v^2 C_D A \quad . \quad (7)$$

If we let f_m be the volume fraction of the less abundant fluid (i.e., $f_m = \text{Min}(f_a, f_b)$) and assume this is in the form of spherical drops of radius r_1 dispersed in the more-abundant fluid, $A = 3f_m/4r_1$, and

$$\frac{dv}{dt} = - \frac{3}{8} \frac{\bar{\rho}^2}{\rho_a f_a \rho_b f_b} v^2 C_D f_m / r_1 \quad . \quad (8)$$

The corrected value of the characteristic equilibration time, t'_c , becomes:

$$t'_c = v / (dv/dt) = 8 \rho_a f_a \rho_b f_b r_1 / (3 \bar{\rho}^2 v C_D f_m) \quad . \quad (9)$$

* S. George Bankoff, Northwestern University, has independently made points similar to those to be discussed here [6].

If one fluid overwhelmingly predominates, Eq. (9) is easily shown to reduce to the same as Eq. (4); hence, the value of C_D defined by Eq. (6) reduces to the value appropriate for an isolated spherical drop of either fluid moving in a sea of the other fluid, as it should. Of course, C_D may also depend upon the UO_2/Na ratio for intermediate cases, as well as upon the other usual parameters.

Inserting numerical values for the case of interest with $f_a = f_b = 0.5$ gives:

$$t'_c \approx v/(dv/dt) = 0.45 r_1/(vC_D) \quad , \quad (10)$$

which implies $t'_c \ll t'$ as given by Eq. (5), unless $C_D \ll 1$.

As a cross check, we may estimate t'_c by approximating the flow of sodium relative to UO_2 as a flow through a packed bed. Starting with a correlation due to Ergun [7] for the resistance to such a flow, and omitting terms that are small in the present case, we obtain:

$$t'_c \approx v/(dv/dt) = 0.52 r_1/v \quad , \quad (11)$$

which is very close to (1) with $C_D \approx 1$. Though we are applying Ergun's relation to values of v considerably higher than those for which it was established, this result suggests our estimate for t'_c is of the right order of magnitude.

Evaluating t'_c from Eq. (10) with $C_D = 1$ and still evaluating t from Eq. (3) shows that, with a shock amplitude of 30 kbar, t'_c/t ranges from about 0.05 to 0.16 for all cases considered in Table 1. That is, even for an initiating shock with an amplitude equal to that of the fully-developed Board-Hall detonation, the fragmentation criterion fails to be met by about one order of magnitude. Hence, there appears to be no way that a detonation-like explosion can propagate in just the way proposed by Board and Hall unless t is also an order of magnitude or more shorter than those workers proposed. This is conceivable, but it is worth noting that either Eq. (10) or (11) implies that the relative velocity will decay to less than 10 percent of its initial value before the total relative motion reaches the order of the mixture scale, which we may take to be the drop diameter, $\sim 2r_1$. Since fragmentation implies liquid-liquid interpenetration, which presumably requires liquid relative motion at least of the order of the mixture scale, it is legitimate to raise the question as to whether the shock-induced velocity differential can cause complete fragmentation by any mechanism.

As the Na/UO_2 ratio increases toward infinity, t'_c , as given by Eq. (9), increases toward t' as given by Eq. (3), but the amplitude of the detonation wave decreases. It seems very questionable as to whether one could find any mixture ratio such that, for a shock of the CJ amplitude, the necessary condition $t'_c > t$ would be satisfied; however, this question has not been investigated in any detail.

Throughout this discussion, we have assumed that the relevant shock amplitudes for evaluating fragmentation should be P_{CJ} , not the higher value P_{VN} . Actually, P_{VN} is not nearly high enough to reverse our conclusions. More fundamentally, the Von Neumann spike would be narrow compared with the fragmentation zone, and the pressure wave causing fragmentation will have to be of at least the width of the latter.

In principle, fragmentation might be induced by applying an initiating shock very much greater than the CJ amplitude. Since t'_C/t varies only as $v^{-1/2}$ and v generally varies only as something between P and $P^{1/2}$, a shock in the megabar region is likely necessary (though our simple approximations no doubt would fail in innumerable ways for such extreme conditions). In any case, detonation theory itself tells us that such an ultrastrong initiator would die down to the CJ amplitude, whereupon the detonation would snuff out due to the lack of continued fragmentation.

Shock-Dispersive Characteristics

Board and Hall did not take into account the shock-dispersive characteristics of composites. Speaking roughly, a sharply-defined pressure wave undergoes multiple, partial reflections at the interfaces between the two constituents. The wave profile therefore spreads and becomes rounded; it also attenuates unless backed by a sustained driving pressure. These effects become very important when there is a large acoustic impedance mismatch between the two constituents, as is the case of UO_2 -Na mixtures.

L. M. Barker [8] has analyzed composite response to stress waves. He showed that composites, to a good approximation, could be modeled as a stress-relaxing material. Details cannot be given here, but the key point is that such materials cannot support a steadily-propagating, sharply defined shock at all unless the amplitude exceeds a certain minimum value; lesser-amplitude steady waves must have a rounded profile. Above the minimum value, part (but not all) of the pressure rise may appear as a near-discontinuous jump or shock.

The author applied Barker's model to UO_2 -Na mixtures, using simple stress-strain relationships based upon Eq. (2), and it was found that the minimum value of the pressure permitting partial shock formation probably lies between 25 and 50 kbars. This is at least as high as the CJ pressure suggested by Board and Hall, and it is therefore questionable as to whether even the fully-developed detonation could propagate as a sharply defined shock. Failure to achieve a sharp shock would reduce still further the driving force for fragmentation, which has already been shown to be inadequate. It would also require careful re-examination of the entire analogy with chemical detonations.

The analysis just summarized would apply directly only when there is little or no void space. With a substantial void fraction, the situation is more complicated and a relatively sharp pressure front cannot be ruled out if the voids consist primarily of condensible vapor rather than non-condensable gas; even then, it is not clear how the pressure front can be much more sharply defined than the mixture scale. In any case, shock-dispersive effects are still expected and they must be considered. If nothing else, they probably rule out formation of a clearly-defined Von Neumann spike,

supporting still further the use of the CJ pressure in the fragmentation analysis, as was done here.

SUMMARY AND CONCLUSIONS

If the criticisms offered here are valid, the Board-Hall model, in its present form, cannot be treated as a significant factor in LMFBF safety analysis. When the Board-Hall approach is refined along the lines indicated, the third of the three fundamental conditions for detonation-like behavior (shock-induced fragmentation) fails to be satisfied by rather wide margins and the effects of shock-dispersiveness and the need for very large triggering events cast further doubt upon the model's practical utility in safety analysis, even if the idealized mixture considered could be achieved in practice, which is itself questionable [9].

On the other hand, this rather negative conclusion should not be over-interpreted. The present work was basically limited to refining certain aspects of the original study and showing that, with these refinements, the conditions essential for internal self-consistency are no longer satisfied. It is, nonetheless, conceivable that quite different mechanisms could cause the rapid fragmentation required to generate detonation-like behavior.

Since fragmentation is a purely mechanical effect in the Board-Hall approach, there is no need to study it with hot-cold liquid pairs. Thus, it could prove useful to perform experiments subjecting mixtures of highly dissimilar liquids (e.g., mercury and water) to strong shocks, either with or without vapor present. Care must be taken to ensure that such experiments are consistent with the model's requirements. For example, the input pressure pulse must be relatively long in duration, not only because the Board-Hall mechanism requires such pulses, but also because short pulses could induce fragmentation by mechanisms that would not be present in the long-duration pulses of interest here. If fragmentation is observed, it would then be necessary to establish that it was a prompt effect rather than a delayed effect.

Finally, even if the Board-Hall approach could be shown to be totally invalid, this would not necessarily mean that the possibility of large-scale, coherent interactions between molten UO_2 and Na can be laid to rest. There is considerable experimental evidence [10] that both triggering and scaling effects are indeed important in vapor explosions, whatever the underlying reason. Unless major advances in the theoretical understanding of vapor explosions are made in the near future, it may eventually be desirable to conduct large-scale UO_2 -Na experiments with strong triggering pulses provided.

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MECHANISMS OF VAPOR EXPLOSIONS

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ABSTRACT

This paper consists of two parts: 1. A recalculation of the Board-Hall theory of thermal detonation waves, taking into account the fuel-coolant mass ratio in determining the velocity equilibration time constant. The required peak pressure is now $\sim 10^5$ bar, so that fast droplet breakup by Taylor instability cannot be visualized. 2. Crust breakup times of $\sim 10^{-3}$ - 10^{-4} sec. are calculated for a fuel drop entering the sodium coolant due to bubble growth and collapse, followed by jet penetration of the crust. Coarse premixing of the fuel and coolant is therefore difficult to attain. Taken together, these calculations imply that the probability of an efficient fuel-sodium explosion is very small.

INTRODUCTION

We consider here the steady propagation of a plane wave in a coarsely premixed fuel-coolant mixture¹, in which the fuel droplets are shattered behind the detonation front by a Taylor instability mechanism^{2,3}, with breakup times of the order of 10^{-3} to 10^{-4} s. Such rapid breakup leads to detonation velocities of the order of 10^4 cm/s, with peak pressures of the order of 10^4 atm for an equal-volume mixture of Na and UO_2 . These pressures are somewhat higher than the Hicks-Menzies⁴ pressures, in view of the compression of the mixture as it enters the shock front before breakup takes place. If realizable, they would require a new containment concept.

In the present work it is shown that they are not realizable, at least by the proposed mechanisms, for two reasons: 1. Velocity equilibration takes place much more rapidly in a dense liquid-liquid dispersion than for a single drop. 2. The estimate of the initial relative velocity as the equilibrium mixture velocity relative to the shock is not conservative for a dense dispersion.

THEORY

Harper, Grube and Chang² analyzed the break-up of a single liquid drop due to passage of a shock wave in the surrounding gas, and predicted rapid breakup due to Taylor instability on the upwind face of the drop for Bond numbers greater than 10^5 . Simpkins and Bales³ verified this prediction experimentally for water drops in air, resulting in the following equation for the breakup time, t_b :

$$\left(\frac{\rho_f}{\rho_d}\right)^{\frac{1}{2}} \frac{v_r t_b}{r_d} \approx 44 \text{ Bo}^{-\frac{1}{2}} \quad (1)$$

where v_r is the velocity of the drop relative to the surrounding medium, ρ_d and

r_d are the drop density and radius, and the Bond number is given by:

$$Bo = \frac{\rho_d g r_d^2}{\sigma} = \frac{3}{8} C_D We = \frac{3}{8} C_D \frac{\rho_f r_d v^2}{\sigma} \quad (2)$$

Here C_D is the drag coefficient, taken to be approximately two; σ is the interfacial tension; g is the initial acceleration of the drop due to drag forces only; and We is the Weber number. No similar data are available for droplet breakup in liquid-liquid systems. In these experiments the acceleration of the drop due to compression in the shock is negligible compared to that due to drag by the gas flow. For $Bo < 10^5$, the cutoff wavelength for Taylor instability becomes comparable to the drop diameter, so that breakup occurs by aerodynamic stripping, which is a relatively slow process.

Board and Hall¹ have considered the analogous case of the breakup of a single, hot heavy drop suspended in a cold, volatile, less dense fluid, which may consist of a liquid or a liquid-vapor mixture. To estimate the initial relative velocity after the shock front passes the drop, the mixture velocity at the Chapman-Jouguet point is used.

We begin by recalculating the velocity equilibration time constant, taking into account a dense liquid-liquid dispersion.

A momentum balance between a single drop and the associated surrounding fluid yields:

$$m_d \frac{dv_d}{dt} = -m_f \frac{dv_f}{dt} = \frac{C_D A \rho_f v^2}{2} \quad (3)$$

where ρ_f and m_f are the fluid density and mass, respectively; m_d and v_d are the drop mass and velocity; and A is the projected area of the drop. Eqs. (3) can be combined to give:

$$\frac{1}{v_r} \frac{dv_r}{dt} = -\frac{1}{2} C_D A \rho_f \left(\frac{1}{m_d} + \frac{1}{m_f} \right) \quad (4)$$

subject to the initial condition: $v_r = v_{ro}$ at $t = 0$, corresponding to the time when the shock front passes the drop. Assuming a spherical drop of radius r_d , an estimate of the time for the relative velocity to fall to a small fraction of its initial value is then given by:

$$t_e \sim \frac{4 \rho_d r_d}{3 \rho_f v_{ro} (1+\gamma)}; \quad \gamma = \frac{m_d}{m_f} \quad (5)$$

For $\gamma = 0$ this reduces to the single-drop case treated by Board and Hall. Defining a breakup number $N = t_b/t_e$, the requirement for fast breakup is $N \ll 1$. From Eqs. (1) and (5):

$$N = 33 \left(\frac{\rho_d}{\rho_f} \right)^{\frac{1}{2}} Bo^{-\frac{1}{2}} (1+\gamma) \quad (6)$$

The breakup number is thus a weak function of the initial relative velocity ($N \propto v_{ro}^{-\frac{1}{2}}$). Assuming equal pressure jumps across the shock front, one obtains:

¹An examination of the boundary conditions in the Harper, et al. analysis indicates that for a liquid-liquid mixture, a modified Bond number, Bo' , should be used in Eq. (1), where $Bo'/Bo = (\rho_d - \rho_f) \sigma_d / \rho_d (\sigma_d - \sigma_f)$, and the liquid-liquid interfacial tension, σ_{df} , has been approximated by $(\sigma_d - \sigma_f)$. This correction is seen to be quite small.

$$N_m = N_s (1+\gamma) b^{-\frac{1}{2}} \quad (7)$$

where N_m and N_s are the multiple- and single-drop numbers, respectively, and $b = v_{ro,m}/v_{rc,s}$ is the ratio of the initial relative velocities.

Consider now the examples treated by Board, et al. For an equal-volume mixture of tin at 1000°C and water at 100°C, with 1 cc steam/g mixture present, the pressure jump across the fragmentation zone is estimated from the C-J condition to be $\Delta P \sim 800$ atm., corresponding to $Bo = 4 \times 10^5$. This leads to $N = 0.63$, so that sufficient fragmentation to sustain a steady-state thermal detonation wave may be possible. However, this estimate uses the dense-mixture thermodynamic properties in determining the C-J point, with single-drop breakup theory. When multiple-drop breakup is taken into account, we find that $N_m = 5$, since $\gamma = 6.8$, indicating the absence of fast fragmentation. Similarly, for 10 g of UO_2 at 3550°K and 1 g sodium at 700°K, with 0.5 cc. vapor/g mixture initially, $\Delta P \sim 1.0 \times 10^4$ atm. at the C-J point, corresponding to a Bond number of 1.9×10^6 . For this case $N_s = 0.5$, $\gamma = 10$ and $N_m = 5.5$, so that rapid breakup again appears to be unlikely. In both cases the estimates of N_s are those of Board, et al. Solidification further adds to the improbability of fast fragmentation by Taylor instability.

In order to obtain some feeling for the sensitivity of this simple calculation, one can estimate the pressure jump required, in order for $N \leq 1$, corresponding to droplet breakup before the velocities become equalized. For this one has recourse to the jump shock relations:

$$\rho_1 u_1 = \rho_2 u_2 \quad ; \quad \rho_1 u_1^2 + P_1 = \rho_2 u_2^2 + P_2 \quad (8a,b)$$

where

$$u_1 = w - v_1 = w \quad ; \quad u_2 = w - v_2 \quad (9a,b)$$

Here w is the shock velocity, the subscripts 1 and 2 refer to quantities immediately ahead of and behind the shock front, and u and v are velocities referred to the shock front and laboratory coordinates, respectively. Then

$$v_2^2 = \Delta P \left(\frac{1}{\rho_1} - \frac{1}{\rho_2} \right) \quad ; \quad \Delta P = P_2 - P_1 \quad (10)$$

Applying this relationship to the coolant phase surrounding the droplets, one obtains

$$v_2 = \Delta P \left[\frac{\epsilon_f}{\rho_{f1}} \left(1 + \frac{\beta}{\epsilon_f \Delta P} \right) \right]^{\frac{1}{2}} \quad (11)$$

where β is the initial volumetric ratio of vapor to cold liquid, ρ_{f1} is the cold liquid density ahead of the shock, and ϵ_f is the liquid compressibility. A similar relationship holds for the drops, but a conservative assumption ignores the acceleration of the drops by the shock. On this basis one can calculate the (unsteady) pressure jump required to maintain $N = 1$. These are 6×10^4 atm. for the UO_2 -Na mixture, and 2.7×10^4 atm. for the tin-water mixture. No physical mechanisms for attaining these pressures can at present be visualized.

We turn now to the modifications of the classical Chapman-Jouguet theory which must be introduced when the detonation shock medium is an initially coarse mixture of phases of differing densities and compressibilities. Strictly speaking, the Chapman-Jouguet condition can be applied only to homogeneous two-phase flows. The sonic mixture velocity is uniquely defined for equal phase velocities

as the minimum shock velocity on the detonation branch of the equilibrium Hugoniot⁵ (Fig. 1). In this case the pressure jump at the front corresponds to the intersection of the Rayleigh line with the shock adiabat (von Neumann spike). The pressure at this point is maximum, and likewise the velocity relative to laboratory coordinates; as the reaction proceeds, the state of the system moves along the Rayleigh line to the C-J point, where the velocity relative to the shock front is sonic.

With unequal phase velocities, however, a third coordinate must be added to the P-V diagram, such as the phase velocity ratio, $S = u_f/u_d$. Upstream and far downstream of the shock front, $S = 1$, but elsewhere $S < 1$. The von Neumann spike may or may not exist. Thus, for a normal shock wave in a particle-laden gas, Kriebel⁶ has demonstrated that the pressure increases, and the gas velocity relative to the front decreases behind the shock. This is opposite to the behavior of a shock proceeding around a single drop, where the drop velocity is essentially unaffected by the shock, but the gas velocity at the von Neumann spike is minimum relative to the shock, and thereafter increases. In this case the initial relative velocity is supersonic, with v_d remaining practically constant and v_f decreasing through the shock zone. For a dense dispersion, however, v_r is subsonic initially, and decreases rapidly thereafter, as both v_f and v_d increase. The assumption that the initial relative velocity is the equilibrium sound speed¹ thus over-estimates the potential for drop breakup in a droplet-laden mixture. In addition, the relative velocity, and hence the acceleration imparted to the drops, is not constant, as implied in the derivation² of Eq. (1), and realized in the Simpkins-Bales work³, but decreases rapidly behind the shock front.

Thus breakup by Taylor instability does not appear to be fast enough to support a steady (Chapman-Jouguet) detonation wave. One would therefore expect that slower mechanisms, such as aerodynamic boundary-layer stripping, would be even less effective. Experimentally it is found⁷ that Eq. (1) holds for this mode of breakup also if the right-hand side is set equal to 8, compared to about 1.4 for the examples treated above. Hence the breakup time is increased by a factor of 5-6. Furthermore, an empirical relation for the amount of mass stripped from a drop, m_s , as a function of time is given by⁸

$$m_s/m_0 = 0.5 (1 - \cos(\pi t/t_b)) \quad (12)$$

where m_0 is the original drop mass. Hence most of the mass is stripped towards the end⁹ of the breakup time. Thus, less than 5% of the droplet mass is stripped away by the time the drop and fluid velocities effectively equalize. On this basis one can state that no mechanism has been identified to support a steady-state Chapman-Jouguet detonation wave for these cases.

MIXING DUE TO BUBBLE COLLAPSE

We turn now to the jet penetration hypothesis which has been advanced in several quarters^{9,10,11} to explain the sensitivity to water temperature observed in experiments with tin^{12,13,14}, aluminum¹⁵ and other metals. It is assumed that bubbles form due to subcooled nucleate boiling at the liquid-liquid interface, grow to some maximum radius, and then collapse. The jet penetration hypothesis is based on analogy to the cavitation bubble collapse at a solid surface, which has been shown numerically¹⁶ to result in a high-velocity jet directed normally to the surface. In this case, however, the bubble was tangent to or quite near the rigid surface, so that the liquid was everywhere in contact with the stationary surface as the bubble collapsed. On the other hand, vapor blanket collapse around a hot drop, jet or foil appears to be only a partial contraction in a rhythmic oscillation; the three- (or four-) phase bubble-like region is everywhere in contact with the hot surface and the curvature of the bubble-like surface remains small, so that the asymmetry responsible for jet formation is very

weak. Furthermore, with cryogenic materials, such as LNG, vigorous vapor explosions occur when the cold liquid is in the saturated state, such that bubble collapse is not possible. For this reason the bubble collapse mechanism is difficult to justify when the hot liquid is surrounded by an unstable vapor film, and a different approach, centering on random contacts in film boiling^{17,18}, may be preferable.

On the other hand, with the UO_2 -sodium system, the contact temperature is too low to maintain film boiling, and subcooled nucleate boiling may be expected if UO_2 drops are introduced into a larger mass of sodium. In the next section we examine the stability of the growing UO_2 crust to bubble collapse and jet penetration, and show that fragmentation indeed occurs very rapidly.

BREAKUP OF UO_2 DROPLETS INJECTED INTO LIQUID SODIUM

Suppose that a molten UO_2 droplet is injected into, or moves towards a subcooled liquid sodium region, whose dimensions are large compared to the drop diameter. We assume that initially there is an intervening gas film separating the two liquids. This film, however, is quickly swept away, allowing liquid-liquid contact. The droplet might originate from the prior breakup of a gassy fuel jet penetrating the interior of a sodium pool, or from some other source. Assuming the UO_2 is at its melting point, the hot oxide fuel will freeze and form a crust at the interface. For sodium temperatures of 400°C , 600°C and 800°C , this interface temperature is found to be 1170°C , 1370°C and 1570°C , respectively. This is well below the homogeneous nucleation temperature of sodium, as well as the minimum steady state film boiling temperature.²⁰ Subcooled nucleate boiling will therefore take place on the front face of the sphere. The growing bubbles, however, collapse after reaching far enough into the cold sodium and cause the formation of a microjet of liquid sodium. By estimating this impulsive force one can determine whether the UO_2 crust, which is growing continuously, will collapse or not. It may be noted that the growing sodium bubbles are held onto the crust surface by the relative motion of the drop and the surrounding liquid. This is contrary to the usual situation in subcooled nucleate boiling where the bubbles detach themselves from the heated surface shortly after they cease to grow, on account of the inertia of the surrounding liquid.

Since the scale of this phenomenon is about two orders of magnitude smaller than a typical drop size, the problem will be treated in rectangular coordinates with infinite extents of both liquids. When UO_2 solidification occurs, the crust growth occurs²¹ according to

$$x(t) = 2 \lambda \sqrt{\alpha_2 t} \quad (13)$$

where the growth constant λ is given by the solution of the following transcendental equation for UO_2 initially at its melting point:

$$\frac{k_1 \sqrt{\alpha_2} (T_m - T_{10}) \exp(-\lambda^2)}{k_2 \sqrt{\alpha_1} + k_1 \sqrt{\alpha_2} \exp(\lambda)} = \frac{L \lambda \sqrt{\pi}}{C_2} \quad (14)$$

where T_m is the melting temperature; the subscripts 1, 2, and 3 refer to the sodium, solid UO_2 and liquid UO_2 , respectively; and T_{10} is the initial sodium temperature. The liquid temperature, $T_1(x, t)$, is given by:

$$\frac{T_1 - T_{10}}{T_m - T_{10}} = \frac{k_2 \sqrt{\alpha_1}}{k_2 \sqrt{\alpha_1} + k_1 \sqrt{\alpha_2} \cdot \operatorname{erf} \lambda} \left(1 + \operatorname{erf} \frac{x}{2 \sqrt{\alpha_1 t}} \right) \quad (15)$$

from which the interface temperature is found to be independent of time:

$$T_i = T_1 + \frac{k_2 \sqrt{\alpha_1} (T_m - T_{10})}{k_2 \sqrt{\alpha_1} + k_1 \sqrt{\alpha_2} \operatorname{erf} \lambda} \quad (16)$$

The thermal boundary layer thickness is then defined implicitly by:

$$\frac{T_1(-\delta(t), t) - T_{10}}{T_i - T_{10}} = 0.01 \quad (17)$$

Incipient boiling superheats for sodium vary widely, depending upon the amount of inert gas in the system. For steady convective saturated boiling the wall superheat²² typically ranges from 17 to 117°F over a pressure range of 8-24 psia. If one chooses $\Delta T_s = 70^\circ\text{F}$ this is probably conservative, since subcooled transient boiling requires larger superheats than steady saturated boiling. From the Laplace and Clapeyron equations, one obtains for the critical bubble diameter, d_n :

$$d_n = \frac{4\sigma v_{fg} T_v}{h_{fg}(T_i - T_s)} = \frac{4\sigma}{\Delta p_1} \quad (18)$$

where σ is the interfacial tension, v_{fg} and h_{fg} are the changes in specific volumes and enthalpy due to vaporization, T_v is the mean vapor temperature, and Δp_1 is the excess bubble pressure over the ambient.

Bubbles of this size will be formed after an initial waiting period determined by the requirement that the nucleating bubble lie entirely within the thermal boundary layer. Thus, the waiting period or nucleation time, t_n , is found from Eq.(15) as the time at which the liquid temperature attainsⁿ the desired value $T_1 = T_s + \Delta T_s$ at distance $x = -d_n$. Thus t_n is obtained by solving:

$$T_s + \Delta T_s = T_{10} + \frac{k_2 \sqrt{\alpha_1} (T_m - T_{10})}{k_2 \sqrt{\alpha_1} + k_1 \sqrt{\alpha_2} \operatorname{erf} \lambda} \left(1 - \operatorname{erf} \frac{d_n}{2 \sqrt{\alpha_1 t_n}} \right) \quad (19)$$

At time $t = t_n$, the bubble starts growing according to the Rayleigh equation,

$$R \ddot{R} + \frac{3}{2} \dot{R}^2 = \frac{\Delta p}{\rho_f} \quad (20)$$

For the initial growth period, $\Delta p = \Delta p_1$ and one obtains

$$R = \sqrt{\frac{2}{3} \frac{\Delta p_1}{\rho_f}} (t - t_n) + \frac{d_n}{2} \quad (21)$$

Δp remains constant until the bubble protrudes from the thermal boundary layer, and then begins to decrease. Hence we assume that the constant-pressure growth stage continues until $R_1/\delta = 1, 2, 3, 4,$ or 5 , and henceforth use each of these values as a parameter. The second stage is assumed to start at $t = t_1$ when $R = R_1$ and continue until $t = t_{\max}$ when $R = R_{\max}$, $\dot{R} = 0$, signifying the maximum size attained by the bubble. At time t_1 cold liquid surrounds the bubble, so that $\Delta p \approx -p_\infty$. The first integral of Eq.(20) is readily found to be:

$$R^3 \dot{R}^2 - R_1^3 \dot{R}_1^2 = -\frac{2}{3} \frac{p_\infty}{\rho_f} (R^3 - R_1^3) \quad (22)$$

and $R_{\max}$ is found by setting $\dot{R} = 0$ in Eq. (22). An exact calculation of $t_{\max}$, involves evaluation of an elliptic integral or recourse to numerical methods. However, calculations show that the second stage is very short compared to the first stage. Therefore, a reasonably good approximation can be made by assuming the second stage is completed with an average velocity of $R_1/2$, and finding $t_{\max}$ from:

$$t_{\max} = t_1 + \frac{2(R_{\max} - R_1)}{\dot{R}_1} \quad (23)$$

From time $t_{\max}$ onwards, the bubble collapses, forming an axisymmetric jet of liquid directed towards the wall. The collapse time is small, and can be approximated by the spherical bubble collapse formula:²³

$$t_c = t_{\max} + 0.915 R_{\max} \sqrt{\frac{\rho_f}{P_{\infty}}} \quad (24)$$

A numerical simulation²⁴ of this situation shows that the velocity, length and diameter of the jet when it strikes the fuel surface are:

$$v_o = 13 \left(\frac{\Delta p}{\rho_f} \right)^{1/4} \quad (25)$$

$$L_o = 0.493 R_{\max} \quad (26)$$

$$d_o = 0.237 R_{\max} \quad (27)$$

According to Buchanan²⁵ the two principal mechanisms for target breakup are:

1) High velocity jet penetration (slow breakup)

2) Low velocity mechanical failure of target (sudden fracture). The former holds when the pressure exerted by the jet is very much greater than the yield stress of the target. i.e./

$$\frac{1}{2} \rho_f v_o^2 \gg \sigma_y \quad (28)$$

For cases of interest here, σ_y is at least an order of magnitude larger, and hence breakup does not occur by this mechanism. To investigate the second mechanism we follow Buchanan in assuming that the target is in the form of a flat circular disc which is neither clamped at the edges nor simply supported. The maximum bending stresses at the center of a circular disc for the case of clamped edges, σ_c , and a simply supported body, σ_s , are given by²⁵

$$\sigma_c = \frac{3}{2} \frac{(1 + \mu) q b^2}{x^2} \left[\ln \left(\frac{a}{b} \right) + \frac{b^2}{4a^2} \right] \quad (29)$$

$$\sigma_s = \frac{3}{2} \frac{(1 + \mu) q b^2}{x^2} \left[\ln \left(\frac{a}{b} \right) + \frac{1}{1 + \mu} - \frac{(1 - \mu)}{4(1 + \mu)} \frac{b^2}{4a^2} \right] \quad (30)$$

where μ is Poisson's ratio; b is the jet radius; and it is assumed that the target radius, $a \gg x$, the target thickness. The target radius is determined by the distance from the center where the effect of the jet can be felt. The pressure q exerted by the jet is either the waterhammer pressure, q_{wh} , or the stagnation pressure, q_s , where

$$q_{wh} = \rho_1 s_1 v_o \left(\frac{\rho_2 s_2}{\rho_1 s_1 + \rho_2 s_2} \right) \quad (31)$$

$$q_s = \frac{1}{2} \rho_f v_o^2 \quad (32)$$

and s_1 and s_2 are sonic velocities in the liquid and solid. For low velocity jets, $q_{wh} > q_s$, but the duration of q_{wh} is much shorter than that of q_s . Buchanan assumes that q_{wh} causes penetration only if $s_2 b/s_1 \gg X$ (i.e., the effect can be felt simultaneously throughout the target thickness) and $q_{wh} > \sigma_y$. Otherwise, the effect of q_s is investigated. Thus, the values of a to be inserted in Eqs. (29) and (30) are $s_2 L/v_o$ and $s_2 b/s_1$, corresponding to q_s and q_{wh} respectively. The jet radius, b , is found from Eq. (27) and the crust thickness, X , is found from Eq. (13) at time, t , for bubble growth and collapse. If $\sigma_c > \sigma_y$, jet penetration and crust breakup takes place, while $\sigma_s < \sigma_y$ signifies no penetration. It can be readily shown from Eqs. (29) and (30) that $\sigma_s > \sigma_c$ if $b^2 < 2a^2$, which is satisfied in all applications considered here.

Table 1 shows calculated values of the jet velocity, bubble growth-and-collapse time, crust thickness, and maximum clamped and supported bending stresses, for sodium temperatures of 400, 600 and 800°C. It is seen that the estimated yield stress of $\sigma \sim 8 \times 10^8$ dynes/cm² is easily exceeded in all cases, giving fragments with a minimum dimension of 10^{-3} cm. One would expect these fragments to be rapidly displaced by boundary layer stripping, turbulence induced by subcooled boiling, and blowouts occasioned by entrapment of sodium in the UO₂ surface layers. Other fragmentation mechanisms, such as thermal stress cracking and Taylor instabilities, will add to the rapid dispersion.

CONCLUSIONS

1. Because velocity equilibration in a dense fuel-coolant mixture (UO₂-sodium or tin-water) takes place very rapidly, Taylor instability does not appear to be a viable mechanism for supporting a steady-state (Chapman-Jouguet) thermal detonation wave. Boundary-layer stripping also appears to be too slow.

2. Molten UO₂ fragments rapidly due to bubble collapse upon being mixed with a larger volume of subcooled sodium. Hence the coarse pre-mixing required for a detonation wave is not attained, and continuous violent boiling occurs.

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Table 1. Calculated Maximum Bending Stresses for UO_2 Crust due to Bubble Collapse

$\frac{R_1}{\delta}$	T_1	v (cm/sec)	t (sec)	$X(t_c)$ (cm.)	σ (dynes/cm ²)	σ_s (dynes/cm ²)
400°C						
1		1.5×10^4	6.0×10^{-5}	8.8×10^{-4}	7.5×10^9	1.8×10^{10}
3		1.5×10^4	4.5×10^{-4}	2.4×10^{-3}	6.4×10^{10}	1.6×10^{11}
5		1.5×10^4	1.2×10^{-3}	4.0×10^{-3}	1.8×10^{11}	4.4×10^{11}
600°C						
1		1.55×10^4	3.7×10^{-5}	6.7×10^{-4}	6.2×10^9	1.5×10^{10}
3		1.55×10^4	3.1×10^{-4}	1.9×10^{-3}	5.5×10^{10}	1.3×10^{11}
5		1.55×10^4	8.7×10^{-4}	3.2×10^{-3}	1.5×10^{11}	3.7×10^{11}
800°C						
1		1.61×10^4	2.6×10^{-5}	5.3×10^{-4}	5.3×10^9	1.3×10^{10}
3		1.61×10^4	2.4×10^{-4}	1.6×10^{-3}	4.8×10^{10}	1.2×10^{11}
5		1.61×10^4	6.6×10^{-4}	2.7×10^{-3}	1.4×10^{11}	3.3×10^{11}

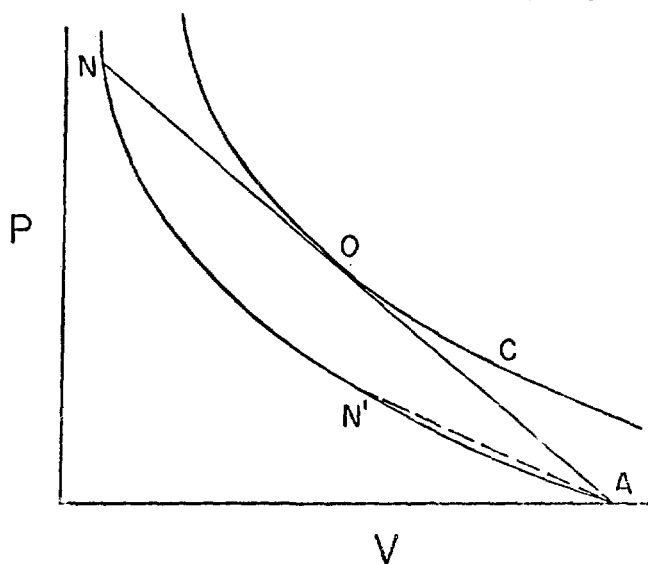


Fig. 1. Pressure-Specific Volume Plot for Thermal Detonation Wave. The curved line AN represents the shock adiabat for isentropic compression from the initial state A to the von Neumann spike N. The curved line CO represents the equilibrium Hugoniot, where temperature and velocity equilibration have been achieved. The straight line AON is the Rayleigh line, and the tangency condition at O implies that the mixture velocity is equal to the local sonic velocity relative to the shock front (Chapman-Jouguet condition).

For a dense dispersion, with unequal phase velocities, an extra coordinate must be added, such as $S = u_d/u_f$. The shock adiabat then becomes a surface in (P, V, S) space, and the system follows a curved path to the equilibrium state. The pressure is generally not a maximum immediately behind the shock front, but rises, as the fluid is slowed down by viscous drag from the drops. The von Neumann spike, N, is thus replaced by a point on the shock adiabat surface, shown symbolically as N' .

LIQUID-LIQUID CONTACT IN VAPOR EXPLOSIONS

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ABSTRACT

A self-mixing theory for initiation and early propagation of vapor explosions is presented. Random contacts are postulated to cause local regions of high pressure at the contact interface, which result in annular jets tending to mix the fluids. The criterion for successful initiation depends upon the minimum Weber number for coalescence being exceeded over an area at least equal to the original contact area. Experimental free-fall droplet data are presented from which the minimum Weber number correlation is determined. Satisfactory agreement is shown with oil-Freon mixing data of peak pressures due to Henry, et al.¹¹

INTRODUCTION

A self-mixing theory for the initiation and early propagation of a vapor explosion is proposed herein, which is consistent with the known characteristics of film boiling near the transition point. The theory is probabilistic, since a spectrum of liquid-solid contact areas and times¹ is observed, presumably due to tongues of liquid which momentarily bridge the vapor gap between the two liquids. It assumes that the time scales for new vapor formation are too long, and the process too incoherent, unless the conditions for spontaneous nucleation are met. It then focuses on the consequences of the formation of a thin, high-pressure layer at the contact interface, resulting in a coherent splash, or annular jet, of the hot liquid directed towards the cold liquid. A conservative approach to the early propagation stages states that the new contact area between the annular jet and the cold liquid must at least equal the original contact area of the cold liquid tongue in order for the process to escalate. It is known from experimental correlations, determined in the present work, that a minimum approach Weber number must be achieved in order to obtain sustained liquid-liquid contact between a hot and a cold liquid. This experimental correlation is incorporated into the theory, and determines the probability of a successful propagation step. The calculation is quite conservative, since it does not take into account irreversible penetration of the cold liquid by the annular jet. In the falling-drop experiments upon which the experimental correlation is based, buoyant forces separate the drop after penetration into the hot liquid. Such forces are not present, in general, in the early mixing stages of a vapor explosion. For this reason, one can expect the present theory to apply only to the initiation and early propagation stages, after which the propagation zone becomes three-dimensional, rather than two-dimensional.

The present theory is based on three additional assumptions relative to the peak pressure generated by the fuel-coolant interaction:

1. For liquid-liquid interfacial temperatures below the spontaneous nucleation temperature, negligible peak pressures result. This is consistent with

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the hypothesis of Fauske² that the time scales for vapor generation are too long for significant pressure shock waves if the instantaneous liquid-liquid contact temperature, T_i , is below the threshold for nucleation by statistical density fluctuations, T_i^{sp} . Supporting evidence is found³ in extensive experiments on the lower hydrocarbons in contact with water, which conclude that the water bulk temperature must lie in a narrow range above the homogeneous nucleation temperature of the hydrocarbon in order for a vapor explosion to occur. On the other hand, vigorous interactions have been observed with tin drops in water at calculated interfacial temperatures somewhat below the homogeneous nucleation temperature of water.^{4,5} However, as demonstrated in the classical experiments of Briggs⁶ on the tensile strengths of water and organic liquids, it is extremely difficult to prevent heterogeneous nucleation of water vapor bubbles, due to the strong hydrogen bonding and hence unique wetting properties of water. With poorly-wetting liquids the vapor-free heterogeneous nucleation temperature may be lower than the homogeneous nucleation temperature by 20°C or more.⁸ With temperatures in this range it is indeed difficult, and probably unimportant, to distinguish between spontaneous nucleation and nucleation from extremely small pre-existing gas traps.

2. Peak pressures become negligible in the free-contacting mode when $T_i > T_{cr}$, the critical temperature of the cold liquid. This is based on the idea that the vapor film is stable to small perturbations under these conditions. However, the present theory does not take into account the effect of the container walls and bottom in enhancing liquid-liquid entrapment.^{9, 10}

3. The maximum pressure observed in the vapor explosion is the vapor pressure of the cold liquid at the instantaneous contact temperature. This is in accord with experimental observations,^{11,12} and presumes that the propagation ceases when the pressure is too high to allow vapor formation.

Before proceeding to the development of the theory, some experimental results are reviewed which are used in formulation of the propagation criterion.

LIQUID-LIQUID SURFACE IMPACTION EXPERIMENTS

Small drops of a cold volatile liquid were dropped onto a free surface of a hot, non-volatile liquid by Waldram,¹⁴ and the results have been interpreted by Fauske² and by Waldram, et al.¹⁵ The drops either rebounded smoothly, resulting in film (or Leidenfrost) boiling, or coalesced with the hot liquid depending upon whether or not the impact Weber number was greater than some minimum value, We_{min} . If coalescence occurred, the droplet would either evaporate smoothly or spatter explosively after some delay time, depending on whether the hot liquid temperature was below or above the homogeneous nucleation of the cold liquid. If the initial interface temperature, calculated from conduction theory, was above the homogeneous nucleation temperature, a fine spray of a few spattered droplets would form immediately, but the bulk droplet would usually remain in film boiling. The critical role of the mode and condition of liquid-liquid contacting was thus verified. Some additional experimental work has been carried out with Freon - 113 and pentane as the volatile fluids, and with Dow-Corning 710 Silicone oil and glycerol as the hot fluids, extending the ranges of diameters and temperatures for We_{min} . Markedly different behavior for the two hot liquids is observed. We_{min} for silicone is higher at all temperatures than for glycerol by a factor of 2-4, increasing with temperature and with drop diameter. For glycerol We_{min} is nearly independent of both T_h and d ; moreover, spattering does not occur after wetting, but only isolated, fairly weak centers of vapor formation. The data were correlated by a least squares routine (Figs. 1 and 2) by an equation of the form:

$$We_{min} = A + B\eta^n ; \quad \eta = \frac{d}{\lambda} \left(\frac{T_i - T_s}{T - T_i} \right) \quad (1)$$

where T_s and T_c are the saturation and critical temperatures of the volatile liquid; $\lambda_s = 2\pi(\sigma/\rho_c g)^{1/2}$ is the capillary wavelength of the cold liquid; and T_i is the calculated initial interfacial temperature. Since $n > 0$, the form of the temperature dependence implies that wetting is not possible if $T_i \geq T_{cr}$. At low temperatures the second term becomes small, indicating that vaporization from the approaching droplet plays a minor role. The empirical constants are different for different liquid pairs, and depend upon physical properties and surface wetting effects. Thus, for silicone oil, $n = 0.25 - 0.75$, indicating a temperature dependence which is weak far from the critical point, but becomes dominant as the critical point is approached.

SPLASH THEORY FOR VAPOR EXPLOSIONS

We shall now formulate an unstable film-boiling model for vapor explosions in spontaneously-nucleating liquid-liquid systems. We begin with the assumption that locally a hot non-volatile liquid is separated from a cold volatile liquid by a film of vapor. Assume that a random contact is made between the two liquids, due to capillary instability of the vapor film. The contact radius is of the order of the capillary wavelength

$$\lambda_j = 2\pi \sqrt{\frac{\sigma_j}{g(\rho_{fj} - \rho_v)}} \quad (2)$$

where $j = 1$ for cold fluid, and $j = 2$ for hot fluid. In this case the appropriate mean vapor film thickness is given by the modified Taylor stability analysis of Berenson¹⁶ for film boiling:

$$b = 2.35 \left[\frac{\mu_v k_v (T_2 - T_{1s})}{h_{fg} \rho_v g(\rho_{f1} - \rho_v)} \sqrt{\frac{\sigma_1}{g(\rho_{f1} - \rho_v)}} \right]^{1/4} \quad (3)$$

As pointed out by Henry and Fauske,¹⁸ homogeneous nucleation cannot occur until sufficient heat has been absorbed by the cold fluid to stabilize the existence of a bubble of critical radius, r_{cr} , with liquid at the homogeneous nucleation temperature T_{hn} . The spontaneous nucleation time is given approximately by:

$$t_{sp} = \frac{r_{cr}^2}{\alpha_1} \left(\frac{T_i - T_1}{T_i - T_{hn}} \right)^2 \quad (4)$$

This time is typically $\sim 10^{-9}$ s. Likewise, from the Rayleigh equation for inertia-dominated bubble growth, the time, t_{bg} , for the radius to increase by three orders of magnitude is $\sim 10^{-8}$ s. On the other hand, the acoustic relief time is $t_a = 2\ell/c$ where ℓ is the mean distance to a free surface and c is the sonic velocity in the liquid. In the Henry-Fauske theory, $\ell = \xi d$ where $\xi < 1$ is a shape factor related to the degree of spreading of the drop on the hot liquid surface. In the present theory a tongue of liquid, rather than a drop, contacts the opposite surface, and the contact radius, a , is related to the Taylor instability wavelength, by $a \approx \gamma \lambda_j$; $j = 1, 2$, where γ is a random variable. From Eq. (7), since $a \sim \ell$ and $\lambda_1 \approx 0.5$ cm, $t_a \sim 10^{-5}$ s $\gg t_{sp} + t_{bg}$. For Freon 22 at

* For transient film boiling with relative velocity between the liquids⁷ the film thickness may be an order of magnitude less than the Berenson value. We have not used the Henry¹⁷ correlation here, since this depends on enhancement of heat transfer by surface contacts.

atmospheric pressure⁵, homogeneous nucleation theory predicts the spontaneous nucleation rate $J = 1.3 \times 10^6 \text{ cm}^{-3} \text{ s}^{-1}$, ($r_{hn} = 40 \text{ \AA}$) at $T_i = 54^\circ \text{C}$, and $4.2 \times 10^{22} \text{ cm}^{-3} \text{ s}^{-1}$ ($r_{hn} = 29 \text{ \AA}$) at $T_i = 60^\circ \text{C}$. At the latter temperature this would imply one critical-radius bubble for each surface element of area 10^{-11} cm^2 in the thermal boundary layer by the time, t_a , has elapsed. Clearly, therefore, an explosive growth and coalescence of the vapor bubbles occurs, resulting in a high-pressure vapor layer at the liquid-liquid contact area. This amounts to an impact pressure applied to the free surface over the region $r < a$, with a resulting velocity distribution obtained from potential flow theory. The normal velocity of the surface for $r > a$ is given by

$$v(y) = \beta(y^{-\frac{1}{2}} - \tan^{-1} y^{-\frac{1}{2}}) \quad (5)$$

where $y \equiv \frac{r^2}{a^2} - 1 > 0$ and $\beta = \frac{2U}{\pi}$. Here U is the impact velocity of a cylinder of radius a which produces the same total impulse, I , given by

$$I = \frac{4}{3} \rho_f a^3 U \quad (6)$$

The splash velocity determined by Eq. (5) produces an annular jet which crosses the vapor space and impacts upon the opposite liquid surface. If liquid-liquid contact is made over an area at least equal to the original contact area, the propagation step can lead to exponential growth of the interaction zone. The relevant area is thus an annulus, with inner radius, a and outer radius, $a\sqrt{2}$. The average velocity over this region is $\bar{v} = \int_0^1 v dy$, and from Eq. (5) one finds that $\bar{v}_j = \beta_j$, $j = 1, 2$.

The impulse due to the vapor layer is approximated by

$$I \sim \pi a^2 t_a (p_{sp} - p_\infty) \quad (7)$$

where p_{sp} is the vapor pressure of the volatile liquid at the spontaneous nucleation temperature. Acoustic pressure relief occurs through radial expansion of the free surface of the cylindrical tongue of liquid making contact with the hot liquid surface. During this acoustic relief period an impact velocity is transmitted to both liquid surfaces, so that the lateral displacement of the adjacent surfaces of the vapor film is given by $2Ut$. Since the thickness of the heated zone is $2\sqrt{\alpha t}$, the ratio $N = U\sqrt{t_a}/\alpha$ determines the time scale for radial expansion of the high-pressure vapor layer into the adjoining free vapor space. For $T_i = 60^\circ \text{C}$, $U \sim 400 \text{ cm/s}$ from Eqs. (10) and (11); $t_a \sim 10^{-5} \text{ s}$; so that $N \sim 40$. Hence the pressure falls quickly after the acoustic relief period. The pressure within the bubbly layer rises quickly during the heating and growth time ($t_{bg} \sim 10^{-8} \text{ s}$).

From Eqs. (5)-(7) $\bar{v} = 3(p_{sp} - p_\infty)/\rho_f c$, which is similar to the water hammer equation, except for the numerical coefficient. Note that the impact velocity, and mean splash velocity, is independent of a , and is hence deterministic, while the impulse is a stochastic variable. Defining now an impact Weber number by $We_i = \rho_f \bar{v}_j^2 a (\sqrt{2} - 1)/\sigma$, we see that the probability distribution for We_i is the same as for the contact radius, a . The data were taken only with $j = 1$ (cold liquid drops impacting the bulk hot liquid), but it is assumed here that the correlation holds also for $j = 2$. This states that the relative normal velocity of the two liquids determines the minimum vapor film thickness between them, and hence the probability of coalescence. In any case the predicted pressure curves are not very different¹³ for $j = 1$ and 2. Unless otherwise stated, we employ $We = We_1$. It is known that the probability distribution for the small-size range of fragmented material follows a log-normal distribution, and if we assume a similar distribution for γ , one obtains

$$P(We > We_{\min}) = \frac{1}{\sqrt{\pi}} \int_{X_{\min}}^{\infty} e^{-t^2} dt \quad (8)$$

where

$$X = \frac{1}{S} \log \frac{V}{\bar{V}} \quad (9)$$

Here S and $\bar{V}$ are the standard deviation and mean, and $X = X_{\min}$ when $We = We_{\min}$. The mean time for one successful propagation step is thus

$$t_m = \frac{t_{sp} + t_{bg} + t_a + t_s}{P(We > We_{\min})} \quad (10)$$

where the splash time, t_s , given by

$$t_s = b/\bar{V} \sim 10^{-4} \text{ s} \quad (11)$$

is the rate-limiting step at any one contact point. However, there will be a large number of random contacts on a macroscopic scale, and the maximum detected pressure p_d , can be approximated by

$$p_d = \epsilon p_n; \epsilon = m t_a / t_m \leq 1 \quad (12)$$

where m is the average number of pressurized contact areas at any instant. For $T_i \leq T_n$, $t_m = 0$ for a well-wetted system, so that no pressure wave is predicted. When T_i increases slightly above T_n , $\epsilon \sim 1$, since mutual pressurization ensures that the detected pressure can never exceed the maximum vapor pressure within the system. However, as T_i increases beyond this point, p_d begins to decrease, in view of Eq. (10), and eventually approaches zero at the critical temperature. Using the parameters for Eq. (1) from Fig. 1, Fig. 3 shows a typical comparison of the predictions of Eqs. (12)-(15) with data for 100-300 ml. Freon-22 poured into discolored (through standing in open containers) and pure mineral oil^{11,12}

Depending upon the choice of S and $\bar{V}$, one obtains predicted curves which form a fairly good envelope for all data, but follow the impure oil data more closely. In agreement with the data, there are no energetic interactions with this mode of contacting for $T_i < T_{hn}$. The maximum pressure rises rapidly towards its theoretical value when T_i exceeds T_{hn} , independent of choice of parameters. Parametric effects appear only after the peak is reached. If one assumes a broad distribution of random contacts ($S \approx 2$), which physically seems to be likely, the resulting curves are quite insensitive to the mean value of the contact area, which is proportional to $\bar{V}$. It should be noted that these curves were calculated using the empirical correlation for the critical Weber number, Eq. (1), but using the physical properties of the oil, which forms the first splash, to determine We_2 . If We_1 had been used instead, quite similar curves are obtained.¹³

CONCLUDING DISCUSSION

We have focused here on a new mechanism for preliminary mixing of the two liquids in the initiation and propagation stages. This consists of the annular splash resulting from rapid vapor generation at random liquid-liquid contact areas under film boiling conditions. The evidence for this mechanism is sparse, as it is indeed for other detailed mechanistic theories. It is consistent with

peak pressure data obtained in the Freon-oil system, which exhibits autocatalytic explosivity over a fairly narrow range of interfacial contact temperatures. At the low side of this range the spontaneous nucleation theory is invoked; at the high side the vapor film becomes quite stable. Many other systems show similar properties, such as the light hydrocarbons and water³, and tin-water.²⁰ The theory attempts to quantify the destabilization of film boiling in a liquid-liquid system when the interfacial contact temperature, T_i , reaches some minimum value for stability. Solidification is not taken into account, nor are energetic triggers, such as the acceleration of the two liquids towards each other (shock tubes⁶) or the arrival of a strong pressure pulse. Entrapment of the cold liquid within or by the hot liquid in close proximity to the container walls²⁰ or bottom,⁹ leading to strong local pressure pulses, is also not considered. Energetic events of this sort can lead to intimate contact even when the interfacial temperature exceeds the coolant critical temperature. The form of the minimum Weber number correlations is not suitable for systems where $T_i > T_{cr}$, and, indeed, no data exist in this range. The physical properties of the hot liquid are shown here to have a strong influence on vapor film stability, and are taken into account through the constants in Eq. (1), which must be determined experimentally. Data of this sort must be acquired for liquid metals with water before the theory can be applied to these systems. Finally, it has been observed prior to tin-water, aluminum-water, and to a lesser extent, lava-water explosions that a multi-phase interaction zone is formed, consisting of molten and solidified droplets in a water-steam bubble-like region. Increasing pressure oscillations are observed as this region overexpands and contracts fairly periodically, growing with each oscillation and tearing more molten material loose from the main body of the melt. A bubble collapse model has been invoked to explain this predispersal, but the region does not collapse completely, and the surface curvature remains small. On the other hand, random contacts should be induced during the contraction portions of the cycle, which can then lead to dispersal by the mechanism proposed herein.

ACKNOWLEDGMENTS

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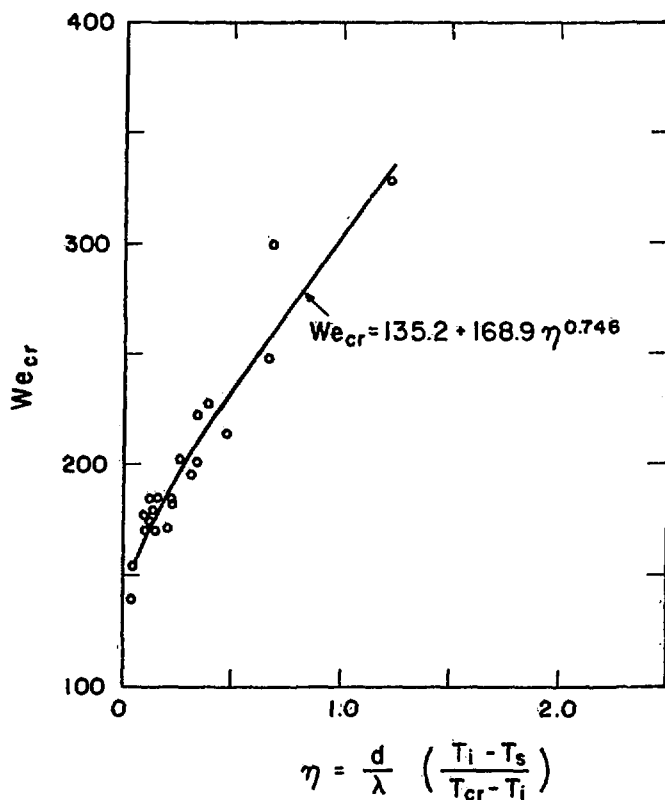
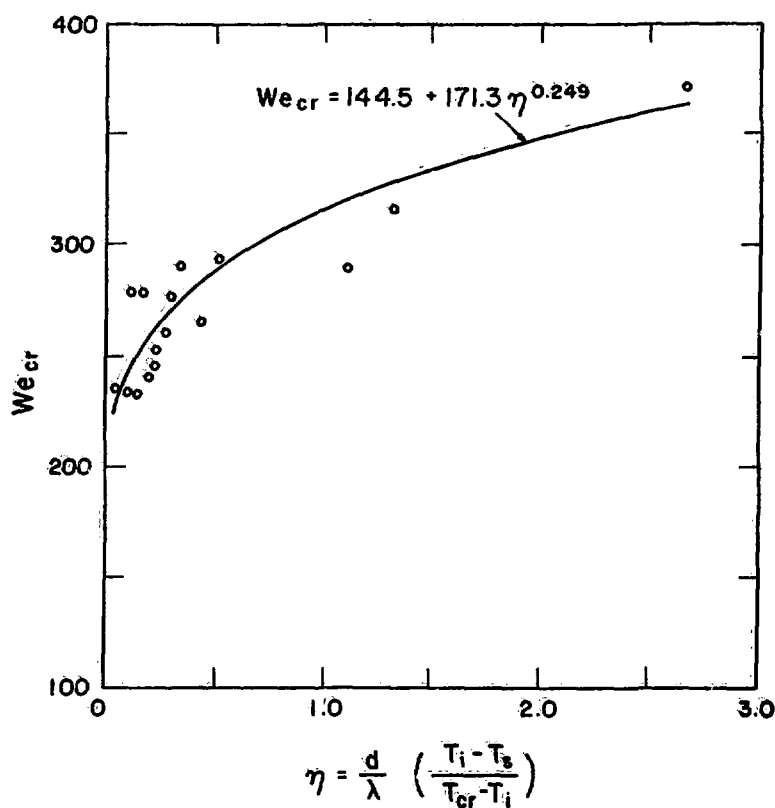


Fig. 1
Experimental Correlation
of Critical Weber Number
for Freon-113 Droplets
Impinging on a Silicone
Oil Surface

Fig. 2
Experimental Correlation of Critical
Weber Number for
n-Pentane Droplets
Impinging on a
Silicone Oil Surface



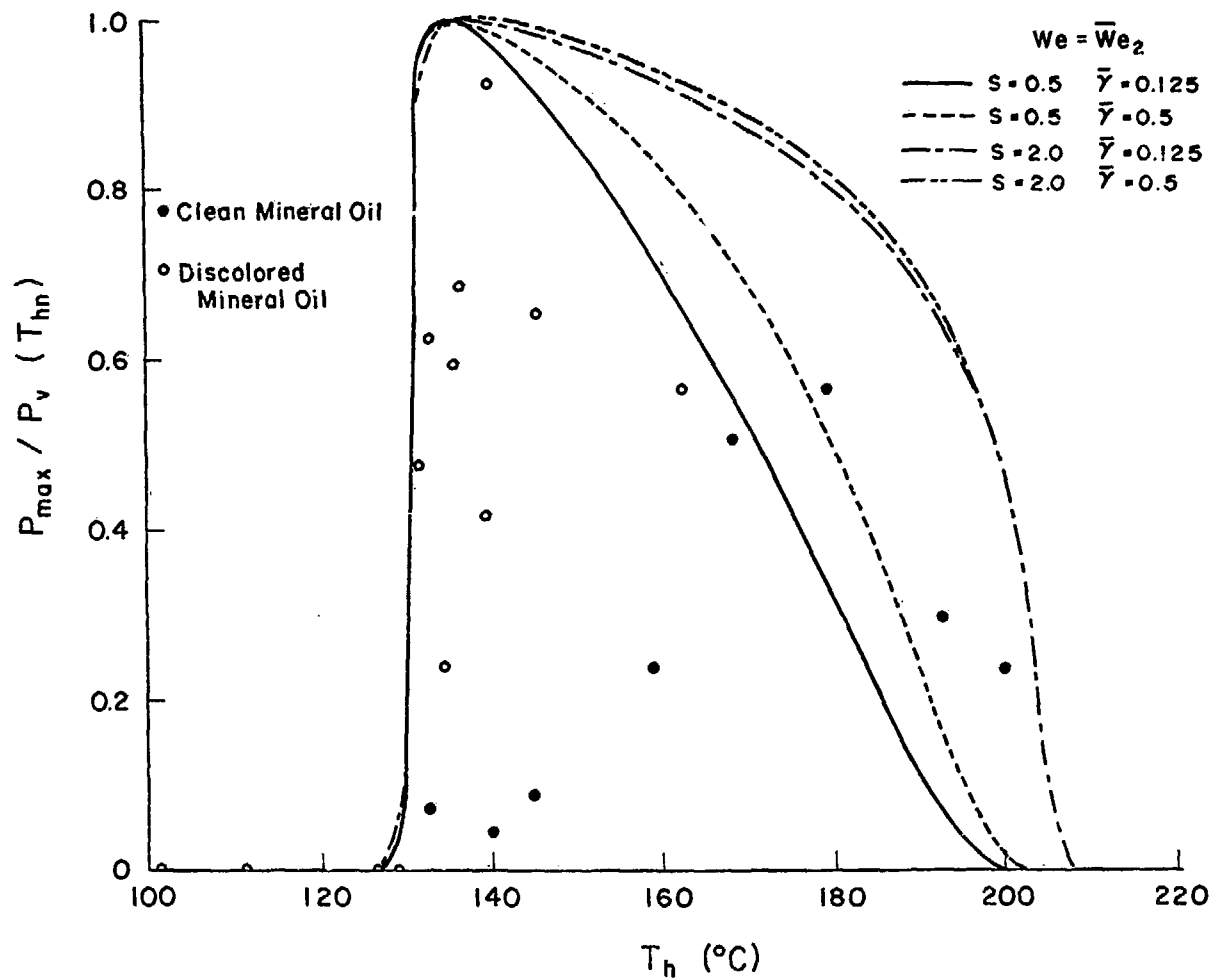


Fig. 3. Explosive Interaction Pressures for Freon-22 Poured into Mineral Oil

SOME ASPECTS OF MIXING IN LARGE-MASS, ENERGETIC FUEL-COOLANT INTERACTIONS

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ABSTRACT

Certain aspects of mixing involved in large-mass fuel-coolant interactions are examined, considering the energy required as well as the dynamics of mixing. It is observed that: (1) an energetic, coherent interaction involving large masses of molten fuel appears extremely unlikely, unless the molten fuel is coarsely premixed with liquid coolant prior to the interaction and the interaction propagates rapidly throughout the entire system, (2) the premixing that is likely to produce an energetic interaction is one in which molten fuel is initially separated from liquid coolant by a vapor layer, and (3) while details of the fine-scale intermixing leading to an energetic interaction are not fully understood; it would appear to be a one-step process. The observations along with consideration of accident situations seem to suggest that the possibility of a large-mass, energetic interaction is extremely remote in oxide-fueled LMFBR systems.

INTRODUCTION

Since Hicks and Menzies [1] first demonstrated the thermodynamic work potential of molten fuel-coolant interaction (FCI), a number of calculational models have been developed to investigate the severity of FCI in hypothetical LMFBR accidents [2, 3, 4]. The basic approach used in these models is similar. All assume intimate mixing between molten fuel and coolant and calculate the consequences of the resulting heat transfer in terms of transient pressure generation and mechanical work done. It is generally assumed that the mixing zone consists of finely fragmented fuel particles dispersed in a finite volume of liquid coolant. The mixing is parametrically characterized by the fuel particle size and the time constant for fragmentation and mixing. Because of the very nature of the mixing assumption, the models describe a single, coherent interaction. While such models have provided a framework for a scoping, parametric investigation of LMFBR accident energetics, their usefulness seems quite limited. It has not been possible to extract in a meaningful way the model parameters from quantities measurable in experiments. Moreover, for the UO_2 -sodium system, there is no experimental evidence for such a large-mass, energetic interaction as can be calculated with the models. It is thus necessary to consider what is physically required to produce an energetic interaction,*

*An energetic interaction means that the interaction efficiency (in terms of the conversion from thermal to mechanical energy) is of the same order of magnitude as the thermodynamic upper limit or at least a substantial fraction of it.

especially for the case when large masses of molten fuel and coolant* are involved. This paper examines some aspects of mixing that are involved in a large-mass, energetic interaction.

MIXING REQUIREMENTS

The interaction between molten fuel and coolant becomes energetic when the time scale for heat transfer between the two fluids is small compared with that for overall expansion of the fuel-coolant mixture. The limiting process for such a rapid heat transfer appears to be the fine-scale mixing of the two fluids which provides a large heat-transfer surface area.

Energy Requirement; An order-of-magnitude Estimate

Consider a mixing process as shown in Fig. 1. A fuel volume V is broken up into particles of radius R and intermixes with an equal volume of coolant. The breakup and intermixing is assumed to occur in one step. An order-of-magnitude estimate of the energy required for mixing may be made by considering the fuel surface energy for breakup, the kinetic energy for fuel particle movement, and the energy dissipated (friction) during the movement of the fuel particles through the coolant. The various energy contributions are as follows:

$$\text{Surface Energy} = N(4\pi R^2)\sigma = \frac{3V}{R}\sigma \quad (1)$$

$$\text{Kinetic Energy} = N\left(\frac{4}{3}\pi R^3\right)\rho_f \frac{u_m^2}{2} = V \frac{\rho_f u_m^2}{2} \quad (2)$$

$$\text{Frictional Dissipation} = N C_D \pi R^2 \left(\frac{1}{2} \rho u_m^2\right) L_m = V \left(\frac{3}{8} \rho u_m^2\right) \left(\frac{L_m}{R}\right) \quad (3)$$

where N = number of fuel particles, σ = fuel surface tension, u_m = mixing velocity, L_m = mixing distance, ρ_f = fuel density, ρ = coolant density, and C_D = drag coefficient (taken to be unity in Eq. (3)). In situations of practical interest, the mixing energy is due mainly to the frictional dissipation and other contributions may be ignored. We now take the mixing velocity, u_m , to be equal to L_m/t_m where t_m is the mixing time and approximate L_m by $V^{1/3}$. The one-step mixing energy is then given by

$$(E_m)_{\text{one-step}} = \frac{3}{8} \frac{V^2}{t_m^2 R} \quad (4)$$

The mixing energy depends on the mode of breakup and intermixing. It can be considerably less than given by Eq. (4) if the breakup and intermixing occurs progressively, involving a number of steps. We shall derive the energy required for progressive mixing, assuming that the fuel size is reduced in geometrical progression and that the mixing velocity is identical for each step (see Fig. 2). If the energy dissipation is the only contribution, the mixing energy involved in going from the i th to the $(i+1)$ th stage is given by

$$(E)_{i \rightarrow i+1} = V \left(\frac{3}{8} \rho u_{mi}^2 \right) \frac{L_{i+1}}{L_{i+1}} \quad (5)$$

* No attempt is made here to define precisely what is meant by a large-mass interaction. For practical purposes, a large-mass interaction may be considered to involve a non-negligible fraction of the total reactor fuel loading.

Now, by assumption,

$$L_{i+1} = \gamma L_i \quad (\gamma < 1) \quad \text{and} \quad u_{mi} = u_m = \text{constant} \quad (6), (7)$$

γ is the reduction factor of fuel particle size in each step and is related to the initial and final sizes by

$$\gamma = \left(\frac{L_n}{L_o} \right)^{\frac{1}{n}} = \left(\frac{R}{L_o} \right)^{\frac{1}{n}} \quad (8)$$

where n is the number of steps. The mixing energy for the entire process becomes

$$E_n = V \sum_{i=0}^{n-1} \frac{3}{8} \rho u_{mi}^2 \frac{L_i}{L_{i+1}} = V \left(\frac{3}{8} \rho u_m^2 \right) \left(\frac{n}{\gamma} \right) \quad (9)$$

The mixing time, t_m , is the sum of the individual mixing times involved, viz.

$$t_m = \sum_{i=0}^{n-1} \frac{L_i}{u_{mi}} = \frac{1}{u_m} \sum_{i=0}^{n-1} \gamma^i L_o = \frac{L_o}{u_m} \left(\frac{1-\gamma^n}{1-\gamma} \right) \quad (10)$$

Elimination of u_m from Eqs. (9) and (10) gives

$$E_n = V \frac{3}{8} \rho \frac{L_o^2}{t_m^2} \left(\frac{1-\gamma^n}{1-\gamma} \right)^2 \frac{n}{\gamma} \quad (11)$$

For $n = 1$ (one-step process), Eq. (11) is identical to Eq. (4). The minimum value of E_n occurs at $n = (1/1.74) \ln(L_o/R)$ and is given by

$$(E_m)_{\text{progressive, min}} = 1.81 \rho V \left(\frac{V}{t_m^2} \right)^{2/3} \left(1 - \frac{R^2}{V^{2/3}} \right) \ln \left(\frac{V}{R} \right) \quad (12)$$

where L_o has been taken to be equal to $V^{1/3}$ as before.

From the foregoing consideration of mixing energy requirements, we make the following observations. (1) The energy required for mixing must be considered in relation to the available thermal energy of fuel. For a given t_m and R (i.e. for about the same FCI efficiency), the smaller the mixing volume, the smaller fraction of fuel thermal energy is needed for mixing. To illustrate this, consider the one-step mixing process and suppose f is the fraction of fuel thermal energy used for mixing. Then, we have

$$f V c_f \rho_f \Delta T = \frac{3}{8} \rho \frac{V^2}{t_m^2 R} \quad \text{or} \quad f \sim V \quad (13)$$

where c_f is the fuel specific heat. Clearly, f is proportional to V . This means that as the mixing volume increases, it becomes difficult to achieve an efficient, self-driven* interaction. Thus, the energy consideration suggests that the concept of premixing and rapid propagation, such as used by Board and Hall [5], would provide a better explanation for large-mass energetic inter-

*self-driven because no external forces are available for mixing.

actions than the concept of escalation (snowballing effect) proposed by Colgate [6] and also by Buchanan [7]. (2) For a given mixing energy, Eq. (4) or (12) defines the maximum mixing volume of fuel (or coolant) as a function of t_m and R . As an illustration, suppose 1% of the fuel thermal energy corresponding to a temperature difference of 3000°C is used for mixing (say, the thermal-to-mechanical conversion efficiency is 10% and 10% of the mechanical energy produced is used for mixing). For $t_m = 1$ msec and $R = 100 \mu m$, the maximum mixing volume is found to be 32 ml from Eq. (4) and 950 ml from Eq. (12). (3) In the progressive mixing, the surface area for heat transfer increases with time exponentially. In the one-step mixing process, the surface area increases linearly with time (this linear increase is implied in the concept of one-step mixing). For the progressive mixing with minimum energy, it can be shown that the surface area produced following the i th stage, A_i , is

$$A_i = A_n \left(\frac{R}{L_o} \right)^{\frac{n-i}{n}} \approx A_n \left(\frac{R}{L_o} \right) e^{1.74i} \quad (14)$$

where $A_n = A_f$ = final fuel surface area. The corresponding time, t_i (measured from the beginning of fuel breakup and intermixing), is

$$t_i = t_m \frac{1 - (R/L_o)^{i/n}}{1 - (R/L_o)} \approx t_m \frac{1 - e^{-1.74i}}{1 - (R/L_o)} \quad (15)$$

A plot of A_i/A_f vs t_i/t_m is shown in Fig. 3. The progressive mixing is seen to consist of several steps of coarse mixing (area generation being insignificant) followed by 1-2 steps of fine-scale mixing. This behavior is no surprise. Actually it is exactly the way the progressive mixing was defined. The progressive mixing concept implies some sort of coarse pre-mixing preceding the final fine-scale mixing. Thus, the fact that the progressive mixing requires much less energy than the one-step mixing may be viewed as another indication that a coarse premixing between molten fuel and coolant would be needed prior to a large-mass, energetic interaction. Fig. 3 also suggests that the rapid heat transfer leading to an energetic interaction would occur in the final one-step mixing process. (4) Using the one-step mixing, we shall examine the idea of escalation, such as proposed by Colgate [6]. Consider an arbitrary initiator of volume V and suppose f is the fraction of the fuel thermal energy used for mixing (f is assumed to be constant throughout the entire escalation process). The mixing energy provided by the initiator is then $E_o = f V_o e_{th}$ where $e_{th} = c_f \rho_f \Delta T$ (thermal energy unit). The volume of mixing brought about by this energy is, from Eq. (4),

$$V_1^2 = \frac{8}{3} \frac{t_{m1}^2 R}{\rho} f V_o e_{th} \quad (16)$$

where t_{m1} is the mixing time involved in the first escalation. For the i th escalation, we have

$$V_i^2 = \frac{8}{3} \frac{t_{mi}^2 R}{\rho} f V_{i-1} e_{th} \quad (17)$$

If we assume $t_{mi} = t_m$ = constant for each step of escalation, we have

$$V_i^2 = V^* V_{i-1} \quad \text{where } V^* = \frac{8}{3} \frac{t_m^2 R f e_{th}}{\rho} \quad (18)$$

(V^* corresponds to the maximum volume that can be mixed in a one-step, self-driven mixing process). Using the above recurrence formula, it can be shown that the mixing volume for the n th stage of escalation, V_n , is given by

$$V_n = \left(\frac{V_o}{V^*} \right) \left(\frac{1}{2} \right)^n V^* \quad (n \geq 1) \quad (19)$$

Clearly, $V \rightarrow V^*$ as $n \rightarrow \infty$. There is no snowballing effect such as indicated by Colgate.ⁿ The way in which V approaches V^* depends on the value of (V_o/V^*) , as shown in Fig. 4. A similar conclusion is obtained when the mixing velocity (rather than the mixing time) is assumed to be constant for each escalation step. The above observation indicates that an initiating event cannot escalate indefinitely in an initially unmixed system, i.e. when masses of molten fuel and coolant are initially separated. From a dynamic point of view, an indefinite escalation would seem even more untenable, since in an initially unpremixed system, the optimum mixing leading to an efficient interaction is not likely.

Simple Mixing Dynamics

Consider a one-dimensional system in which a number of flat layers of molten fuel are interleaved with coolant layers, as shown in Fig. 5(a). We assume a sudden pressure generation Δp to occur at one end of the premixed system and examine the resulting flow of coolant and the breakup of molten fuel caused by the flow. Let L represent the length over which Δp occurs. The sudden application of Δp over L causes the fuel and coolant masses to accelerate, the acceleration, g , being given by $g = 2\Delta p/[L(\rho_f + \rho_c)]$. At the interface where the light coolant accelerates into the heavy molten fuel, one expects intermixing due to Taylor instability. The scale of intermixing, λ , may be considered to be half the Taylor wave length of the fastest growing instability, viz.

$$\lambda_m = \frac{1}{2} \left[2\pi \sqrt{\frac{3\sigma}{g(\rho_f - \rho)}} \right] = \pi \sqrt{\frac{3}{2}} \sqrt{\frac{\sigma L}{\Delta p}} \sqrt{\frac{\rho_f + \rho}{\rho_f - \rho}} \quad (20)*$$

Eq. (20) has been derived assuming that there is no gas/vapor volume in the premixed region. In this case, L is at least equal to the overall length of the premixed system. If, however, there is a preexisting vapor layer between molten fuel and coolant, L could be as small as the scale of the initial premixing. Suppose there is a collapsable vapor layer between the molten fuel and coolant layers, as shown in Fig. 5(b). In such a case, the mass to be accelerated by Δp would initially consist of one layer of molten fuel and one layer of coolant rather than the entire length of the premixed system. That is, the inertial length would correspond to the scale of the initial premixing, L . The non-linear interpenetration following Taylor instability grows as $\sim (1/2)gt^2$ (t = time). Thus, if the thickness of the vapor layer is of the same order of magnitude as the thickness of the fuel (or coolant) layer (i.e. the scale of the initial premixing), the intermixing between the two layers of molten fuel and coolant would be complete before they collide and merge with the next layers of fuel and coolant due to the collapse of the vapor layer. If the vapor layer is too thin, it would collapse before the intermixing is complete and the effective inertial length would be greater than the scale of the initial premixing. The above consideration suggests that the existence of a vapor layer between molten fuel and coolant reduces the effective inertial mass to be accelerated and, consequently, the intermixing between fuel and coolant (for a given pressure input) would be finer with a pre-existing vapor layer than without it.

*A similar expression can be derived by considering spherical drops of molten fuel dispersed in the coolant.

A similar observation may be made for a shock wave of magnitude Δp propagating through a coarse mixture of molten fuel and coolant (compressible flow). It is assumed that the molten fuel drops dispersed in the coolant are stationary and do not interfere with the shock propagation. In the absence of gas/vapor volumes, the coolant velocity behind the shock may be approximated by $u_c = \Delta p / \rho C_2$ where C_2 is the speed of sound in the liquid coolant. When the premixed system contains void volumes such as vapor layers surrounding fuel drops, an estimate of the coolant velocity may be given by $u_{c\phi} = \sqrt{\alpha/(1-\alpha)} \times \sqrt{\Delta p / \rho}$ where α represents the void fraction. It is seen that unless the shock wave magnitude is very large (say, $\Delta p > 100$ bars), the coolant velocity is much larger with a pre-existing void volume than without it. Since the larger the coolant velocity, the finer would be the breakup of fuel drops, it may be concluded that the existence of a vapor layer promotes fine-scale intermixing between molten fuel and coolant.

Thus, we have observed that the existence of a vapor layer between molten fuel and coolant promotes fine-scale intermixing by reducing the effective inertial mass. Another role of the vapor layer would be to stabilize the premixing condition until the interaction is initiated. Without a stable vapor layer, the premixing configuration may be disrupted due to local, violent boiling and the molten fuel would tend to freeze rapidly, resulting in a solid crust which would prevent fine-scale intermixing between fuel and coolant.

The above considerations of the role of a vapor layer suggest that the premixing configuration for an energetic interaction would involve stable film boiling of the coolant prior to the initiation of the interaction. When masses of molten fuel and coolant are initially separated, the premixing would require stable film boiling upon contact. This contention seems to be supported by certain observations made in aluminum/water [8,9] and Freon 22/ water experiments [10]. Also, an examination of the SPERT-ID test [11] indicates that the fuel plates (which had initially been premixed with water because of the nature of reactor design) were blanketed by a layer of water vapor prior to the observed steam explosion. It is estimated that at the time of the explosion, 1.5 mm thick molten fuel plates were interleaved with 3 mm thick water layers with a 0.6 mm thick intervening vapor layer between them. In view of the premixing requirements discussed above, this core condition appears to be ideal for the occurrence of an energetic interaction. It is quite probable that a similar premixing condition existed in the BORAX-I destructive test [12] and in the SL-1 accident [13].

The observations made in this section are consistent with the concept that a large-mass, energetic interaction involves three stages in a manner resembling that of a chemical explosion. The three stages are (1) premixing* in which molten fuel and coolant are coarsely interdispersed on a scale small compared to the overall dimensions of the interaction system, (2) local initiation of the interaction (triggering), and (3) rapid propagation throughout the premixed system. The possibility of triggering and propagation in a premixed system had been suspected when attempts were made to explain the SPERT-ID destructive pressure pulse [11]. It is rather recent, however, that the three-stage concept received renewed attention and was further elaborated [14].

PROPAGATION IN A PREMIXED SYSTEM

A large-mass interaction is likely to involve propagation in which a local interaction initiated at one point produces interactions nearby. Recently, Board and Hall considered such a propagation mechanism in a premixed system and suggested a detonation model by analogy with chemical detonation [5]. Assuming that a stable, self-sustaining detonation has been established, Board and Hall

*The role of premixing is analogous to the preparation of chemical explosives in which energy-rich fuels and oxygen-rich compounds are uniformly intermixed.

calculated the limiting steady-state propagation velocities and shock pressures. It is doubtful, however, that such a detonating interaction could develop from a physically realizable initiating event.

A question of major interest concerns the development of a high-energy event from a local event of low energy during the propagation. Some insight into this question may be gained by considering propagation in the one-dimensional system of interleaved molten fuel and coolant layers with an intervening vapor layer between them, as shown in Fig. 5b. To do this, it is assumed that: (1) the fuel, coolant, and vapor layers have the same thickness of ℓ , (2) the sudden application of Δp causes a coolant layer to impact on the adjacent fuel layer and accelerates the composite layer of coolant and fuel of length 2ℓ until the composite layer collides with the next layer of coolant, (3) the intermixing between coolant and fuel during the acceleration is described by Taylor instability, (4) the effect of coolant vaporization during the intermixing is ignored, and (5) the heat transfer between molten fuel and coolant is controlled by thermal conduction on the fuel side so that the coolant temperature may be considered to be uniform. Based on these assumptions, one can make an order of magnitude estimate of the heat transferred during the intermixing and the resulting pressure. If it is assumed that the pressure generated corresponds to the vapor pressure of the heated coolant, a calculation for the $\text{UO}_2\text{-Na}$ system would indicate that the pressure level does not increase with each impact of a coolant layer upon the adjacent fuel layer. Under these circumstances, a low-pressure triggering event can be escalated into a high-pressure event only by continuous heat transfer after completion of the intermixing between the coolant and fuel layers, which is controlled by the scale of intermixing. It appears unlikely that the maximum pressure that can be achieved by the continuous heat transfer would exceed the vapor pressure of the coolant in thermal equilibrium with the fuel.

The effect of coolant vaporization during the intermixing may be ignored for the $\text{UO}_2\text{-Na}$ system where the contact interface temperature is close to the coolant bulk temperature and is much below the homogeneous nucleation temperature of the coolant. The assumption, however, is very questionable for certain other systems. For the $\text{Al-H}_2\text{O}$ system, for example, the contact interface temperature is close to the aluminum bulk temperature and is much above the homogeneous nucleation temperature of water. In this case, the concept of continuous explosive boiling based on spontaneous nucleation, originally suggested by Fauske [15], may be useful. If explosive boiling occurs upon impact of a water layer on the adjacent aluminum layer, a mechanism such as Taylor instability need not be invoked (in fact, may not apply) for the intermixing of the two layers. The explosive boiling would immediately generate a sufficiently high pressure to cause fine-scale mixing of the two liquids. If the explosive boiling continues, as Fauske suggests, due to repeated contacts between the cold and hot liquids, the high pressure can be maintained. Then, the escalation of a triggering event into a high-pressure event would not be controlled by any particular heat transfer mechanism such as thermal conduction in one of the two liquids and might occur after one or two impacts. In this situation, a propagating interaction could readily occur in a premixed system provided a proper triggering mechanism is available. A question of interest here concerns the magnitude of the impact pressure or energy that is required to produce significant explosive boiling (a simple contact between water and molten aluminum would merely result in film boiling without explosive violence).

CONSIDERATION OF LMFBR ACCIDENT SITUATIONS

As mentioned earlier, it would appear that a large-mass, energetic interaction involves three stages, namely, premixing, initiation (triggering) and propagation. Failure to achieve any one stage would tend to make the interaction nonenergetic. Also, the energetics of the interaction would depend on

the extent to which the individual stages are completed. Consideration of the three-stage concept for LMFBR accident situations seems to suggest that the possibility of a large-mass, energetic interaction is extremely remote in oxide-fueled LMFBR systems.

When large masses of molten fuel and coolant that are initially separated come into contact (e.g. molten fuel mass being ejected into a sodium pool), the question is, how do the two liquids premix in smaller masses prior to the initiation of the interaction. If local interactions occur immediately upon contact, producing a large amount of vapor, they would tend to separate the fuel mass from the coolant. Such premature interactions would be avoided if stable film boiling of the coolant occurs upon contact. For the UO_2 -Na system, nucleation and boiling considerations suggest that stable film boiling upon contact is very unlikely as long as the sodium wets molten UO_2 . Available experimental information seems to support this contention. For example, in laboratory dropping experiments [16,17] and in the TREAT E4 and H2 tests [18], it is believed that sharp pressure pulses and fragmentation occurred upon initial contact between molten fuel and sodium, and there was no indication of stable film boiling upon contact. Therefore, if the sodium has initially been separated from the molten fuel mass, it would appear that local, nonenergetic interactions (similar to those observed in laboratory dropping experiments) would occur upon contact, thus preventing the development of premixing condition necessary for a coherent, energetic interaction.

If masses of molten fuel and coolant are initially premixed, one needs to examine the question of initiation or triggering as well as possible propagation mechanisms. If it is assumed that some sort of triggering mechanism producing local pressure pulses is available, the question becomes one of propagation of the initiating event. In a short-period overpower transient in a nearly unvoided LMFBR core, the molten fuel and sodium are seemingly premixed because of the nature of reactor design. The situation, however, is different from that of the SPERT-ID case mentioned earlier in that the molten fuel is separated from liquid sodium by a solid layer (cladding plus unmelted outer layer of fuel), not by a vapor layer. This difference appears to be fundamentally important. No energetic interactions between molten UO_2 and sodium were observed in the TREAT S-series tests [19], where clad fuel pins were melted down in stagnant sodium in very energetic transients. Similarly, in the SPERT Capsule Driver Core tests [20], single rods of UO_2 clad with stainless steel or Zircaloy-2 were encapsulated in stagnant water and subjected to short, energetic nuclear transients. There were no indications of energetic interactions between molten UO_2 and water. These tests were of a very limited scope in terms of the number of fuel pins employed (the maximum number of fuel pins used in the S-series tests was seven). Nevertheless, the results of the tests seem to support the hypothesis that the solid cladding acts as a barrier to the intimate mixing of molten fuel and coolant [19,21]. The presence of the solid cladding will undoubtedly have an impeding effect on the initial mixing between molten fuel and sodium following pin bursts. More important, however, will be the effect on propagation of a local interaction if it should occur. In the presence of the solid cladding, rapid propagation of an interaction is extremely unlikely, since the solid cladding would prevent fine-scale intermixing between molten fuel and sodium. As a result, the interaction following pin bursts would be highly incoherent and tend to be nonenergetic. In this case, reactivity effects associated with combined fuel and coolant motion should be the primary concern.

ACKNOWLEDGMENTS

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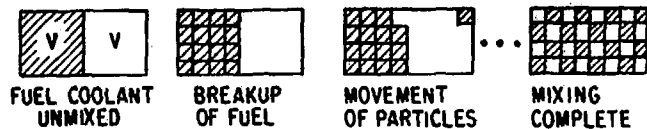


Fig. 1. One-step Mixing

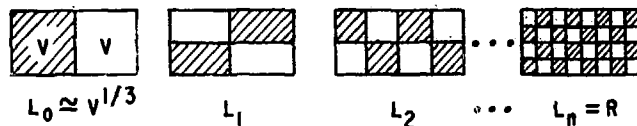


Fig. 2. Progressive Mixing

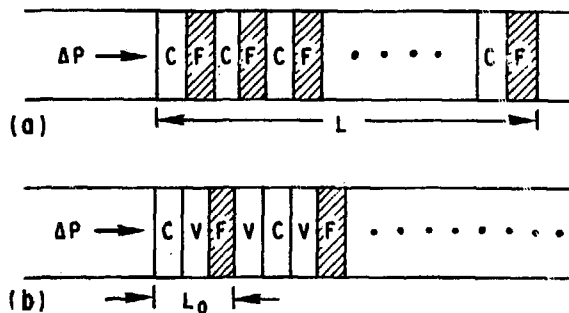


Fig. 5. Simple One-dimensional Premixed System (C=Coolant, F=Fuel, V=Vapor).

- (a) No vapor Layer Between Fuel and Coolant Layers.
(b) Vapor Layer Between Fuel and Coolant Layers.

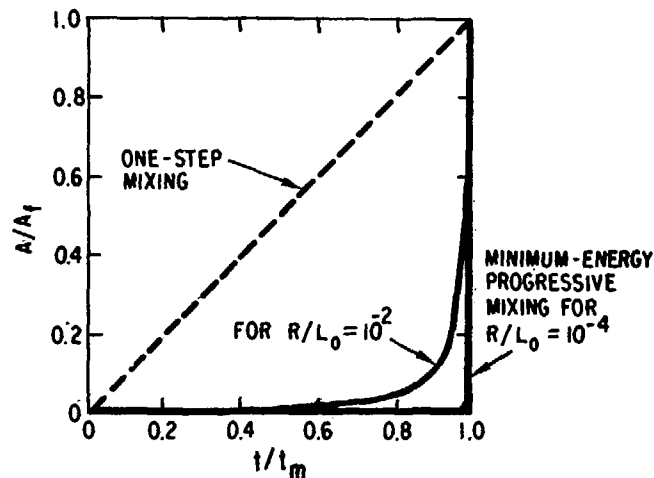


Fig. 3. Surface Area Increase During Mixing: Solid Curves for Progressive Mixing with Minimum Energy and Broken Curve for One-step Mixing.

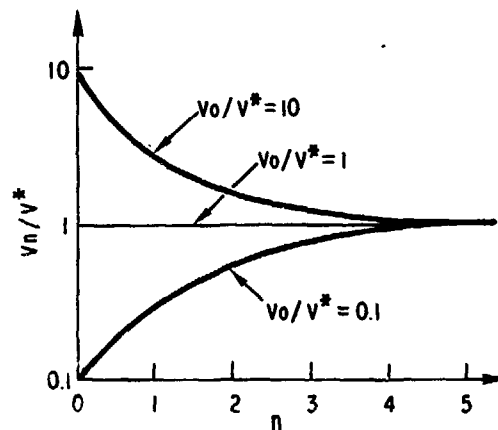


Fig. 4. Escalation of an Initiating Interaction in an Initially Unpremixed System.

VAPOR EXPLOSION EXPERIMENTS WITH SIMULANT FLUIDS

by

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ABSTRACT

A wide variety of simulant fluid vapor explosion experiments have been performed to test the predictions of the interface temperature, spontaneous nucleation model. All experiments conducted to date have been in agreement with the model.

INTRODUCTION

Vapor explosions have been shown to occur when two liquids, at greatly different temperatures come into intimate contact. Analyses of hypothetical, unprotected accidents in liquid metal fast breeder reactors have demonstrated that molten core conditions must be considered for such events. Consequently, the possibility of explosive vapor formation upon contact between molten fuel and sodium must be included in the spectrum of events to be analyzed. Several parametric models have been formulated ^{1, 2, 3} along with various mechanistic models ^{4, 5, 6} for analyzing the energetics of sodium-fuel interactions. The mechanism proposed in refs. 4 and 5 differs from the parametric models and the proposed mechanism in ref. 6, in that a specific thermal threshold is predicted, for a given fluid pair and initial conditions, below which no large scale explosive interactions will occur. This threshold is encountered when the interface temperature upon contact between the two liquids is equal to the spontaneous nucleation temperature of the liquid-liquid system. Large scale experiments ⁷ with Freon and mineral oil have demonstrated the existence of this threshold, and for this particular system, because of the affinity between Freons and oils, the spontaneous nucleation temperature is equal to the homogeneous nucleation value. The prediction of this model for an oxide fuel-sodium system is that the interface temperature upon contact is several hundred degrees centigrade below the threshold value required for explosive interactions. Consequently, large scale explosions are not predicted for such systems. Given the importance of this conclusion, additional simulant fluid tests have been conducted to study other aspects and ramifications of this mechanistic explanation in order to test the viability of the proposed model.

*Work performed under the auspices of the U. S. Energy Research and Development Administration

NONCONDENSIBLE GAS TESTS

Aluminum-water shock tube experiments⁸ have shown that small partial pressures (~ 1 kPa) of noncondensable gas can dramatically change the explosive behavior in such a system. Freon-water systems differ from the aluminum-water behavior in that neither a shock tube configuration nor contact with a container wall⁹ are required to initiate explosive vapor releases. By comparison, one would consider a Freon-water system, which satisfies the necessary thermal requirements to be more naturally explosive than aluminum and water.

The role of noncondensable gases and initial void fraction within the liquid phase, for a system which explodes in a free contact mode, was investigated for the dropping contact mode of Freon-22 into water. The experimental apparatus, Fig. 1, was essentially the same as that employed in ref. 7 with either argon gas or Freon-22 vapor injected at the base of the interaction vessel and a liquid level indicator to determine the system average void fraction. Experiments were conducted for initial water temperatures of 81°C and 85°C with average void fractions as high as 0.30. The results of these tests, in terms of maximum explosive interaction pressures, are shown in Fig. 2. While the maximum interaction pressures are somewhat lower than the highest values reported in ref. 7, they are in good agreement with the previously reported results. However, the most striking feature of the data is that noncondensable and condensable initial void fractions up to 0.3, which is the limit of bubbly flows, appear to have no significant effect on the maximum interaction pressures developed. Given these results one can conclude that if a system will explode in a free contact mode, the presence of large quantities of noncondensable gas will not prohibit the explosion.

CONSTRAINED SYSTEM TESTS

One of the intriguing aspects of the explosion energetics model presented in ref. 5 and the experimental data of ref. 7 were the maximum interaction pressures of 20 to 30 atm. for a free contact mode with Freon and water. It is of major interest to perform these experiments in a constrained geometry to determine if the values indeed represent a maximum. A system similar to that shown in Fig. 1 was used for the dropping contact mode with the addition of an orifice plate on the top of the interaction vessel. These orifice plates had a central hole of either 2.5 or 5.0 cm. The water temperature was varied from 40°C to 90°C, and the Freon-22 initial temperature was constant at -40.8°C (normal boiling point).

The respective results for the various openings in the dropping contact mode are illustrated in Fig. 3. Qualitatively, the behavior for the various constraints is identical as a function of water temperature, but the maximum interaction pressures decrease with increasing system constraint, (the overpressures shown in Fig. 3 all have rise times of < 1 msec from atmospheric to the indicated value). As illustrated by the Freon results described in ref. 7, the contacting mode definitely affects the energetics of a given interaction, and the data shown in Fig. 3 illustrate that, for the same contacting mode, the energetics of the interaction are definitely affected by the system constraint. It is also interesting that increasing the system constraint increased the interaction time, so that if the impulse of the entire interaction is calculated, a greater impulse is obtained for the more constrained system with lower pressures than for the less constrained system with the higher explosive interactions.

In addition to the dropping contact mode, a subsurface injection of Freon into a water pool was conducted under the same system constraints utilized for

the dropping mode. For these tests, the initial Freon temperature was again -40.8°C and the water temperature was varied over a range from 30 to 100°C . Measurements of pressures within the interaction vessel and force on the base of the vessel were recorded, as was the pressure within the Freon injector. The injector was a piston-type system in which the Freon was contained at its normal boiling point until immediately prior to the injection, at which time it was pressurized with argon gas from above which increased the pressure within the injector chamber rupturing the diaphragm and allowing the Freon to be injected into the water pool. The experimental measurements for these tests are shown in Fig. 4 for maximum interaction pressures as a function of the initial water temperature. For the 5- and 10 cm openings, the qualitative behavior of the system is the same as previous explosive studies carried out with Freon-22 and water, and indeed in the range of maximum pressures the rise times are less than a millisecond and the interaction is indeed explosive. It is interesting to compare the interaction pressures with the measured Freon injection pressure as illustrated in Fig. 5, which shows that even though the interaction is explosive, the maximum interaction pressure is always less than or equal to the Freon injection pressure. For the subsurface injection with a 2.5 cm dia. relief at the top of the vessel, the interaction pressures do not demonstrate the same qualitative behavior as the other interaction experiments. The measured interaction pressures essentially achieve a constant level as a function of water temperature. For this level of constraint a noted difference is achieved in the pressure-time characteristics of the interaction, in that explosive interactions are not indicated because the rise time of the pressure is many tens of milliseconds, as illustrated in the small insert of the 2.5 cm orifice data shown in Fig. 4. In this case it merely appears that violent boiling occurred and the system achieved an equilibration between the rate at which it could generate Freon vapor and the rate at which it was expelling water through the 2.5 cm dia. relief. It appears that for this particular contacting mode there is a system constraint for which an explosive interaction cannot be obtained, even though the necessary criterion described in ref. 4 is satisfied.

SUBCOOLED FREON EXPERIMENTS

To investigate the importance of the interface temperature, vapor explosion experiments were conducted with subcooled Freon-22 and mineral oil. These experiments were conducted in an A frame apparatus like that shown in Fig. 1. For these experiments, the interaction vessel contained the Freon and the oil was poured into the vessel. This contact configuration provides a better resolution of the initial freon temperature when the liquid is initially subcooled. Initial Freon temperatures were varied from -41°C to -140°C and the initial mineral oil temperatures ranged from 80°C to 220°C .

The experimental results, in terms of explosive and nonexplosive events, are given in Fig. 6, and these are compared to the predicted threshold value (60°C) for the interface temperature, spontaneous nucleation model⁵. While there is some scatter in the immediate vicinity of the prediction, the results are in good agreement with the interface temperature model. Some discrepancies are expected in the immediate vicinity of the prediction as a result of changes in the subcooled Freon temperature when several hundred millisecond delays occur before the onset of explosive events. A change of 40°C can be expected in the bulk temperature of a 1 mm diameter drop as a result of film boiling heat transfer for 500 msec. Figure 7 compares the maximum interaction pressures as a function of interface temperature for data accumulated for varying oil temperatures at a fixed Freon temperature and varying Freon temperatures at a fixed oil temperature. The agreement between the two is an additional confirmation of the dominant function of the interface temperature in vapor explosions.

SMALL DROP EXPERIMENTS

A result of the physical model proposed in ref. 5 was a drop diameter stability prediction which governed the degree of prefragmentation required before an explosive event could begin. This stability limit determined whether a drop wet the surface or remained in the film boiling state. An experiment was conducted to determine the viability of the above drop stability criteria. In this experiment, small drops of Freon-12 were impinged upon a mineral oil surface and high speed movies (5,000 pps) were taken of the resulting interactions. A schematic of this apparatus is shown in Fig. 8. The drops were generated from an acoustically oscillated hypodermic needle and fell through a heated collimator tube onto the target area on the mineral oil surface. The collimator tube temperature was sufficiently hot so that the drops were in film boiling during their transient time through the tube and this also insured the drops were saturated at atmospheric pressure when they impinged upon the mineral oil surface. A small wire ring was utilized to raise the mineral oil surface, which formed a concave region of the target area and hence kept the drops centrally located. The experimental variables were drop diameter and mineral oil temperature.

For interface temperatures less than the homogeneous nucleation value, all sized drops wet the surface and proceeded to vaporize in thin film vaporization or by entrapment and incoherent nucleation (small scale explosive interactions). For interface temperatures greater than the homogeneous nucleation value large sized drops penetrated the surface and developed their own protective vapor film, which was clearly evident when the drops returned to the surface as drops floating in their own protective vapor pockets. When these large drops either fragmented or vaporized to a small enough size, they would become trapped on the surface and vaporize completely within 1 to 2 msec. This requires a heat flux which is approximately 2 orders of magnitude greater than the pool boiling critical heat flux for the Freon. Consequently, the heat transfer rate between the liquids is enormous. While this vaporization rate is still relatively long compared to explosive interactions, this is the limit of resolution for this camera speed. At times less than this interval, one can only state that an energetic event is occurring but further resolution is impossible. In addition it should be noted that this type of experiment inherently produces drops at the limit of stability, and these have the longest time constants for energy transfer of all captured sizes at that temperature.

A summation of all these experimental results is shown in Fig. 9 along with the stability criteria for Freon-12 as a function of interface temperature, and it is seen that the experimental results are in excellent agreement with the stability predictions arising from spontaneous nucleation and thermal boundary layer considerations.

HIGH AMBIENT PRESSURE EXPERIMENTS

Another characteristic behavior predicted by the spontaneous nucleation model of ref. 5 and discussed in ref. 10 is the cessation of explosive interactions at high ambient pressures. Separate systems were constructed to investigate Freon-mineral oil and Freon-water explosive interactions under such conditions. This was done to simplify the progression of the experiments as well as to prevent system contamination that would result in switching between water and mineral oil as the hot liquids in the same system. Figure 10 illustrates the system configuration which was essentially the same for both sets of experiments. The vessels were fabricated from 10 and 15 cm pipes with electrically heated, insulated cups suspended from the upper flange. Thermocouples were provided within this cup to measure the liquid level prior to the

test as well as the initial fluid temperature. The solenoid-punch system was utilized to rupture the diaphragm at the base of the cup and initiate the test. The lower portion of the vessel in which the second fluid was maintained, was outfitted with thermocouples to measure temperature distribution within the bath and high response, piezoelectric pressure and force transducers to monitor the interaction behavior of the two liquids. This portion of the vessel was also outfitted with trace heaters and insulation for control of the liquid temperature. In addition, the gas space above the interaction zone was equipped with a pressure transducer to monitor the overall system pressure and a rupture disc and vacuum line were mounted in this same locale. The vacuum line was outfitted with a solenoid valve which was energized simultaneously with the diaphragm-punch transport cylinder which shut off the line to the vacuum system when a test was initiated.

The experimental results for Freon-22 and mineral oil are shown in Fig. 11. For an ambient pressure of 1 atm., the explosive interaction behavior is in excellent agreement with previously published results. However, for ambient pressures of 2.2 and 8.0 atm., no explosive events were observed at any combination of oil and Freon temperatures. Figure 12 demonstrates that an identical behavior was observed for Freon-22 and water. It should be noted that these ambient pressures are an order of magnitude less than the maximum interaction pressures generated for a system initially at one atmosphere. As discussed in ref. 10, it is believed that this cessation of explosive events with increasing ambient pressure is closely related to the bubble growth characteristics (inertial versus thermal controlled growth) of the system. Extensive inertially controlled growth will provide further fragmentation of liquid droplets and an incipient shock wave, whereas thermally controlled growth should provide little fragmentation and no incipient shock wave.

This general behavior is in qualitative agreement with the constrained system experiments presented above, i.e., a highly constrained system will experience pressurization as a result of vapor produced by film boiling and could force itself into a low yield or non-explosive range.

CONCLUSIONS

Several diverse experiments have been conducted to study various implied behaviors for the interface temperature, spontaneous nucleation mechanism. All of these experiments have been in agreement with the predictions of the model.

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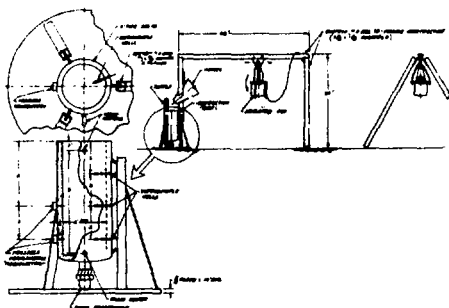


Fig. 1 Experimental Apparatus

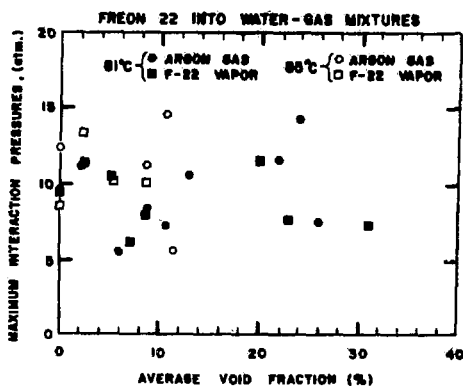


Fig. 2 Maximum Explosive Interaction Pressures

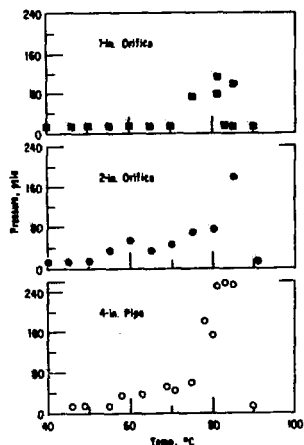


Fig. 3 Explosive Pressures, Constrained System

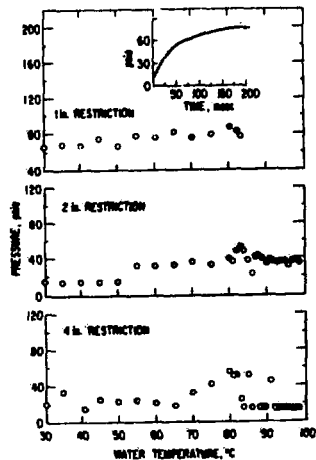


Fig. 4 Subsurface Injection, Constrained System

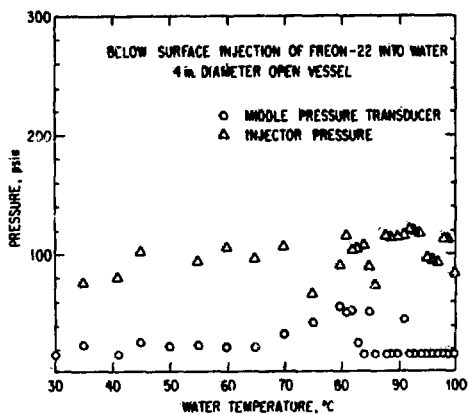


Fig. 5 Subsurface Injection, Constrained System

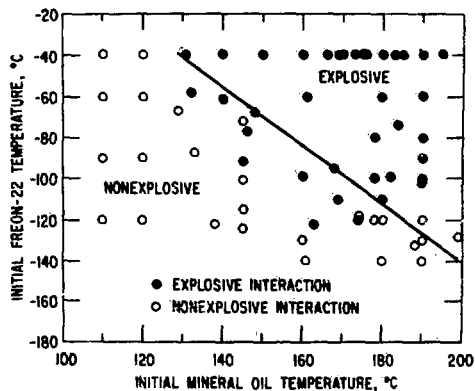
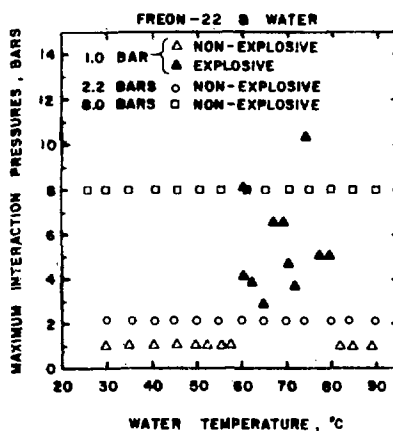
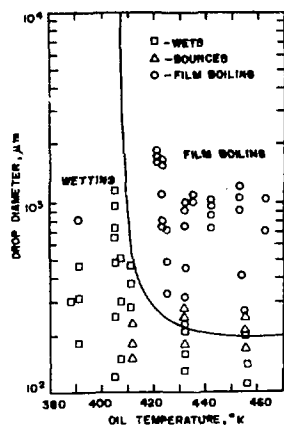
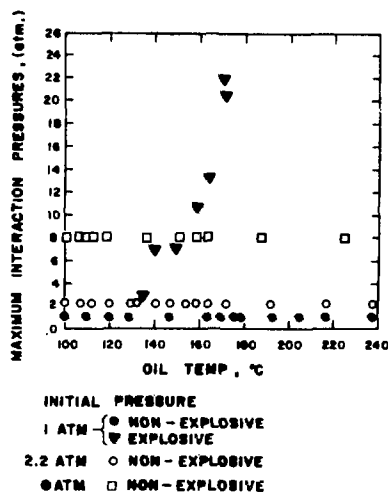
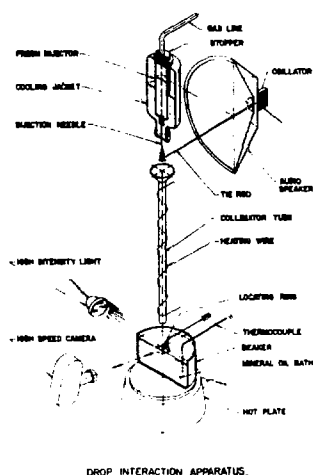
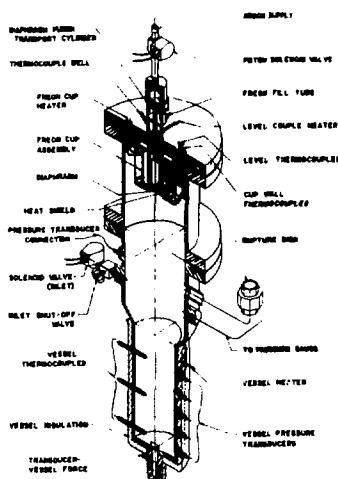
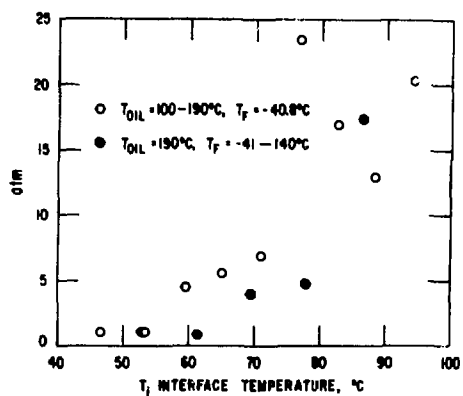


Fig. 6 Subcooled Freon-Mineral Oil Data



HEAT TRANSFER AND PRESSURE GENERATION DUE TO A TEMPERATURE DISCONTINUITY AT A LIQUID-LIQUID INTERFACE

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ABSTRACT

In the absence of a phase transition, we solve for the time evolution of the Temperature and Pressure fields for fuel and coolant in both planar and spherical geometries assuming an initial temperature discontinuity. We solve the problem analytically using Laplace transforms. We find the characteristic time for pressure relaxation is $(t/\tau)^{-1/2}$ where $\tau = \alpha/c^2$. In spherical geometry the temperature of a sphere in a medium relaxes to the temperature of the medium in a time $\sim R^2/\alpha$ where R is the radius and α the thermal diffusivity of the sphere. For a sphere of fuel in coolant or coolant in fuel, with the fuel freezing, we determine approximately the time-dependent temperature distributions and the motion of the freeze front by using a boundary layer approximation.

EQUATIONS OF FLUID MECHANICS AND HEAT TRANSFER

If we ignore viscosity and assume that the two liquids are in distinct regions I and II corresponding to two semi-infinite half planes, or a sphere inside an infinite medium we have in each medium

$$-\nabla P = \rho \frac{d\vec{v}}{dt} \quad (1)$$

$$\frac{d\rho}{dt} + \rho \vec{\nabla} \cdot \vec{v} = 0 \quad (2)$$

$$\rho T \frac{ds}{dt} = \vec{\nabla} \cdot (k \vec{\nabla} T) = \rho c_p \frac{dT}{dt} - \beta T \frac{dP}{dt} \quad (3)$$

where $c_p = T \left(\frac{\partial s}{\partial T} \right)_P$; $\beta = \frac{\left(\frac{\partial v}{\partial T} \right)_P}{V}$ = thermal expansion coefficient. At the interface we have

$$\vec{v}_1 = \vec{v}_2 = \vec{v}_{if}, \quad p_1 = p_2 = p_{if}, \quad T_1 = T_2 = T_{if} \quad (4)$$

$$k_1 \nabla T_1 = k_2 \nabla T_2, \quad \frac{\nabla p_1}{\rho_1} = \frac{\nabla p_2}{\rho_2} \quad .$$

When there is no phase change one can simplify these equations by introducing co-moving (Lagrangian) coordinates. These coordinates are defined by

$$\rho_o dx_o = \rho[x(x_o, t), t] dx = \tilde{\rho}(x_o, t) dx \quad (5)$$

in one dimension, or $\rho_o r_o^2 dr_o = \rho(r, t) r^2 dr$ in spherical geometry.

In Lagrangian coordinates

$$v(r_o, t) = \frac{\partial r(r_o, t)}{\partial t}, \quad \frac{dA}{dt} = \frac{\partial A(r_o, t)}{\partial t} \quad (6)$$

If we assume $\partial r / \partial r_o$ is small, $\rho / \rho_o \approx 1$, we get

$$\rho_o \frac{\partial \vec{v}}{\partial t} = - \nabla_o P \quad (1a)$$

$$\frac{\partial \rho}{\partial t} + \rho_o \nabla_o \cdot \vec{v} = 0 \quad (2a)$$

$$\frac{\partial T}{\partial t} - \frac{\beta T}{\rho c_p} \frac{\partial P}{\partial t} = \alpha \nabla_o^2 T \quad (3a)$$

where $\nabla_o^2 = \frac{\partial^2}{(\partial x_o)^2}$ or $\frac{1}{r_o} \frac{\partial^2}{(\partial r_o)^2}$ (rF), $\alpha = k / \rho c_p$. We next assume a liquid equation of state $dP = \rho c^2 (\beta dT + d \ln \rho)$ with β , and c^2 constants, which allows us to combine Eqs. (1a) and (2a) to yield the wave equation

$$\left[\frac{1}{c^2} \frac{\partial^2}{(\partial t)^2} - \nabla_o^2 \right] P(r_o, t) = \beta \rho_o \frac{\partial^2}{(\partial t)^2} T(r_o, t) \quad (7)$$

We next ignore the effect of the induced pressure changes on the temperature distribution and first solve for $T(r_o, t)$ from

$$\frac{\partial T}{\partial t} = \alpha \nabla_o^2 T(r_o, t) \quad (3b)$$

We then solve the wave equation with a known source for $P(r, t)$. Finally one can determine $\vec{a}(x_0, t)$, and $\vec{v}(x_0, t)$ from Eq. (1a) and then reconstruct $x = x(x_0, t)$.

Using the initial data that at $t = 0$, $T_1 = T_{1i}$, $T_2 = T_{2i}$, $P_1 = P_{1i}$, $P_2 = P_{2i}$, $v_1 = v_2 = 0$, $\frac{\partial v}{\partial x} = 0$, $\frac{\partial \rho}{\partial t} = 0$, and $\frac{\partial P}{\partial t} = 0$, one obtains by solving Eq. (3b) via Laplace transform for the semi-infinite case

$$T_1(x_0, t) = T_{if} - (T_{if} - T_{1i}) \operatorname{erf} \left(\frac{x_0}{2\sqrt{\alpha_1 t}} \right) \quad (8)$$

$$T_2(x_0, t) = T_{if} + (T_{2i} - T_{if}) \operatorname{erf} \left(\frac{-x_0}{2\sqrt{\alpha_1 t}} \right)$$

$$\text{where } T_{if} = \frac{T_{1i} k_1 \sqrt{\alpha_2} + T_{2i} k_2 \sqrt{\alpha_1}}{k_2 \sqrt{\alpha_1} + k_1 \sqrt{\alpha_2}}$$

$$\begin{aligned} P_1(x_0, t) = & P_{10} + \theta \left(t - \frac{x}{c_1} \right) \left(\frac{c_1 \rho_1}{c_1 \rho_1 + c_2 \rho_2} \right) (P_{2i} - P_{1i}) \\ & - \theta \left(t - \frac{x}{c_1} \right) \frac{\lambda \beta_2}{\gamma_2} \Delta T_1 \frac{\theta \left(t - \frac{x}{c_1} \right)}{c_1 \rho_1 + c_2 \rho_2} \exp \left(\frac{c_1 t - x}{c_1 \tau_2} \right) \operatorname{erfc} \sqrt{\frac{c_1 t - x}{c_1 \tau_2}} \\ & + \frac{\beta_1}{\gamma_1} \Delta T_1 \left[\frac{1}{2} \exp \frac{c_1 t + x}{c_1 \tau_1} - \operatorname{erfc} \frac{x + 2c_1 t}{2\sqrt{\alpha_1 t}} - \frac{1}{2} \exp \frac{c_1 t - x}{c_1 \tau_1} \operatorname{erfc} \frac{2c_1 t - x}{2\sqrt{\alpha_1 t}} \right. \\ & \left. + \left[1 - \theta \left(t - \frac{x}{c_1} \right) \right] \exp \left(\frac{t - \frac{x}{c_1}}{\tau_1} \right) + \frac{\theta \left(t - \frac{x}{c_1} \right)}{c_1 \rho_1 + c_2 \rho_2} c_2 \rho_2 \operatorname{erfc} \sqrt{\frac{c_1 t - x}{c_1 \tau_1}} \right] \end{aligned} \quad (9)$$

where $\tau_1 = \frac{\alpha_1}{c_1^2}$. The initial interfacial pressure is (if $P_{1i} = P_{2i}$)

$$P_{if}(t = 0) = \frac{\rho_1 c_1 \rho_2 c_2}{\rho_1 c_1 + \rho_2 c_2} \left[\beta_1 c_1 (T_{if} - T_{1i}) + \beta_2 c_2 (T_{if} - T_{2i}) \right] + P_{1i} \quad (10)$$

The pressure at the interface relaxes at large times $t \gg \tau_1, \tau_2$ as

$$P_1 \rightarrow P_{1i} + \frac{\beta_1}{\gamma_1} \Delta T_1 \frac{c_2 \rho_2}{(c_1 \rho_1 + c_2 \rho_2)} \frac{1}{\sqrt{\pi}} \left(\frac{\tau_1}{t} \right)^{\frac{1}{2}}$$

$$-\frac{\lambda \beta_2}{\gamma_2} \Delta T_1 \frac{c_1 \rho_1}{c_1 \rho_1 + c_2 \rho_2} \frac{1}{\sqrt{\pi}} \left(\frac{\tau_2}{t} \right)^{\frac{1}{2}} \quad (11)$$

where $\lambda = (k_1 \rho_1 c_{p1})^{\frac{1}{2}} / (k_2 \rho_2 c_{p2})^{\frac{1}{2}}$

$$\Delta T_1 = \frac{(T_{2i} - T_{1i}) k_2 \sqrt{\alpha_1}}{k_2 \sqrt{\alpha_1} + k_1 \sqrt{\alpha_2}}, \quad \gamma = -(\frac{\partial v}{\partial p})_T / v = (\rho_0 c^2)^{-1}.$$

The initial interfacial pressure $P_{if}(t=0)$ travels at the speed of sound becomes a shock once it passes the thermal wave front which is at $x_1 \approx 5\sqrt{\alpha_1 t}$. After that point we have for $x > x_1$, $v_1 = \frac{1}{\rho_1 c_1} (P_{if}^0 - P_{1i})$. Explicit expressions for P_2 , v_1 , v_2 , $x_1(x_0 t)$, $x_2(x_0 t)$ are found in Ref. 1 [1].

For the spherical case one cannot obtain an answer in terms of simple functions except at large and small times. Instead we get an answer in terms of an integral along the real axis. The major difference of spherical geometry is the existence of the time scale $\tau_R = R^2/\alpha_1$ where α_1 is the thermal diffusivity of the sphere. The center of the sphere starts heating up at $.1\tau_R$ and the sphere is nearly at equilibrium with its surroundings by $10\tau_R$. Thus if sodium is entrained in UO_2 the time delay for homogeneous nucleation should be proportional to R^2/α_1 (if the pressure relaxation time $\alpha_1/c^2 \ll R^2/\alpha_1$).

We find that (1 corresponds to sphere, 2 to medium)

$$T_1 = T_{2i} + \frac{2}{\pi} k_1 k_2 (T_{2i} - T_{1i}) \sqrt{\alpha_1 \alpha_2} \frac{R}{r} \\ \times \int_0^\infty du \exp(-u^2 t / \tau_R) (u \cos u - \sin u) \sin\left(\frac{uR}{R}\right) \quad (12) \\ \times \left\{ \alpha_1 k_2^2 u^2 \sin^2 u + \alpha_2 [k_1 (u \cos u - \sin u) + k_2 \sin u]^2 \right\}^{-1}.$$

For $t \ll \tau_R = \frac{R^2}{\alpha_1}$ one has approximately

$$T_1 = T_{1i} + \frac{k_2}{\Sigma r} R (T_{2i} - T_{1i}) \\ \times \left\{ \sqrt{\tau_M} \operatorname{erfc} \left(\frac{R-r}{2\sqrt{\alpha_1 t}} \right) - \frac{k_1}{\Sigma} (\sqrt{\tau_R} + \sqrt{\tau_M}) \frac{R-r}{\sqrt{\alpha_1}} \operatorname{erfc} \frac{R-r}{2\sqrt{\alpha_1 t}} + \frac{k_1}{\Sigma} \frac{(\sqrt{\tau_R} + \sqrt{\tau_M})}{\sqrt{\pi}} \right. \\ \left. \times 2t^{\frac{1}{2}} \exp \left[-\frac{(R-r)^2}{4\alpha_1 t} \right] \right\} \quad (13)$$

where $\Sigma = k_1 \sqrt{\tau_R} + k_2 \sqrt{\tau_M}$, $\tau_M = R^2/\alpha_2$, so for $t \leq \tau_R$

$$T_{if} = \frac{k_1 \sqrt{\tau_R} T_{1i} + k_2 \sqrt{\tau_M} T_{2i}}{\Sigma} + \frac{2k_1 k_2 (\sqrt{\tau_M} + \sqrt{\tau_R})}{(k_1 \sqrt{\tau_R} + k_2 \sqrt{\tau_M})^2} \frac{t^{1/2}}{\sqrt{\pi}} (T_{2i} - T_{1i}) \quad (14)$$

For the pressure we have

$$P_1(r_o, t) = P_{1i} + \frac{1}{2\pi i} \int_{\gamma-i\infty}^{\gamma+i\infty} e^{st} \left[E_1(s) \frac{\sinh \frac{s}{c_1} r}{r} + \frac{A_1(s) \beta_1 \rho_1 c_1^2 s \sinh \sqrt{s/\alpha_1} r_o}{r_o [s - c_1^2/\alpha_1]} \right] \quad (15)$$

where

$$A_1(s) = \frac{k_2 (T_{2i} - T_{1i}) R (\sqrt{s\tau_M} + 1)}{s \left[k_1 \sqrt{s\tau_R} \cosh \sqrt{s\tau_R} + k_2 \sqrt{s\tau_M} \sinh \sqrt{s\tau_M} + (k_2 - k_1) \sinh \sqrt{s\tau_R} \right]}$$

$$E_1(s) = \left[s \left(\frac{\rho_1}{c_2} \sinh \frac{sR}{c_1} + \frac{\rho_2}{c_1} \cosh \frac{sR}{c_1} \right) + \frac{(\rho_1 - \rho_2)}{R} \sinh \frac{sR}{c_1} \right]^{-1}$$

$$\times \left\{ \frac{\beta_2 \rho_1 \rho_2 c_2^2}{s - c_2^2/\alpha_1} \left(\frac{s}{c_2} - \sqrt{s/\alpha_2} \right) \left[R(T_{1i} - T_{2i}) + A_1(s) s \sinh \sqrt{s\tau_R} \right] \right.$$

$$\left. + s A_1(s) \frac{\beta_1 \rho_1 c_1^2}{s - c_1^2/\alpha_1} \left[\left(\frac{\rho_2}{R} - \frac{\rho_1}{R} - \rho_1 s/c_2 \right) \sinh \sqrt{s\tau_R} - \rho_2 \sqrt{s/\alpha_1} \cosh \sqrt{s\tau_R} \right] \right\}$$

Similar integrals exist for $T_2(r, t)$ [2] and $P_2(r_o, t)$. We notice that $E_1(s)$ has three poles, at $s = c_1^2/\alpha_1$, $s = c_2^2/\alpha_2$ and at $s = \gamma c_1/R$. The first two poles are the pressure relaxation times in the absence of reflections. R/c_1 is the time for the initial shock wave to reach the origin and then be reflected. $\gamma = 1.1741$ for UO_2 inside Na.

By contour distortion one can rewrite Eq. (15) in terms of the residues at the poles and an integral of the imaginary part of the integrand along the negative real axis. The latter integral can be integrated numerically and expanded to find the asymptotic behavior of $P(r, t)$ at small and large times.

FREEZING IN SPHERICAL GEOMETRY

When freezing takes place either inward or outward, one can no longer use Laplace transform techniques because of the nonlinear boundary conditions at the freeze front, which is at position $r_s(t)$, $T(r_s, t) = T_M$. For a freezing sphere, one has at the freeze front, $r = r_s$

$$k_s \frac{\partial T_s}{\partial r} \Big|_{r=r_s} = \rho \ell \frac{dr_s}{dt} + k_1 \frac{\partial T_1}{\partial r} \Big|_{r=r_s} \equiv \rho \ell \frac{dr_s}{dt} - \frac{q'(t)}{4\pi r_s^2} \quad (16)$$

where ℓ is the latent heat and the subscript s corresponds to the portion of the sphere in the solid state.

In each medium I, s, II, we convert the differential equation into an integral equation by using the boundary conditions at $r = R$, $r = r_s$. At $r = R$ $T_s = T_{2i} = T_{if}(t)$ and

$$k_s \frac{\partial T_s}{\partial r} = k_2 \frac{\partial T_2}{\partial r} \equiv \frac{-q(t)}{4\pi R^2} \quad (17)$$

Integrating the Fourier heat equation twice yields

$$T_1 = T_M + \frac{q'(t)}{4\pi k_1} \frac{r_s - r}{r_s r} + \frac{1}{\alpha_1} \int_{r_s}^r \frac{dr''}{r''^2} \int_{r_s}^{r''} r'^2 \left(\frac{\partial T_1}{\partial t} \right) dr' \quad (18)$$

$$T_s = T_{if}(t) + \frac{r - R}{k_s r R} \left[\rho \ell r_s^2 \frac{dr_s}{dt} - \frac{q'(t)}{4\pi} \right] + \frac{1}{\alpha_s} \int_R^r \frac{dr''}{r''^2} \int_{r_s}^{r''} r'^2 \left(\frac{\partial T_2}{\partial t} \right) dr'$$

$$T_2 = T_{if}(t) + \frac{q(t)}{4\pi k_2} \frac{(R - r)}{r R} + \frac{1}{\alpha_2} \int_R^r \frac{dr''}{r''^2} \int_R^{r''} r'^2 \left(\frac{\partial T_2}{\partial t} \right) dr'$$

If the medium starts freezing one gets exactly the same equation, however, now I would be the medium region and II the sphere.

These equations define an iterative procedure for generating approximate temperature profiles which are of the form:

$$\sum_{n=1}^m \frac{a_n(t) r^n}{r}.$$

The lowest approximation consists of neglecting the integral, the next being to iterate $\partial T_0 / \partial t$ where T_0 stands for the lowest approximation etc. [3]. The cubic / r profile is given by

$$T_1 = T_M + \frac{q'(t)}{4\pi k_1} \frac{(r_s - r)}{r_s r} + \frac{q'(t)}{4\pi k_1 \alpha_1} \frac{r_s}{r_s^2} \left[\frac{(r^2 - r_s^2)}{6} - \frac{r_s^2}{3} \frac{(r - r_s)}{r} \right]$$

$$+ \frac{\dot{q}'(t)}{4\pi k_1 r_s \alpha_1} \left[\frac{(r_s^2 - r^2)}{6} + \frac{r_s}{2} (r - r_s) - \frac{r_s^2}{6} \frac{(r - r_s)}{r} \right] \quad (19a)$$

$$T_s(r, t) = T_{if}(t) + \frac{r - R}{k_s r R} \left[\rho l r_s^2 \frac{dr_s}{dt} - \frac{q'(t)}{4\pi} \right] + \frac{T_{if}}{\alpha_s} \frac{R}{(R - r_s)} \\ \left[\frac{r^2 - R^2}{6} - \frac{r_s}{2} (r - R) + \frac{r_s^3}{6} \frac{(r - R)}{r R} \right] + \frac{1}{k_s \alpha_s} (R - r_s) \frac{\dot{r}_s}{r_s} \left[\rho l r_s^2 \dot{r}_s - \frac{q'(t)}{4\pi} \right] \\ \left[\frac{r^2 - R^2}{6} - \frac{R}{2} (r - R) + \left(\frac{R r_s^2}{2} - \frac{r_s^3}{3} \right) \frac{(r - R)}{r R} \right] \quad (19b)$$

$$T_2 = T_{if}(t) + \frac{q(t)}{4\pi k_2} \frac{(R - r)}{r R} + \frac{\dot{T}_{if}}{\alpha_2} \left[\frac{r^2 - R^2}{6} - \frac{R^2}{3r} (r - R) \right] + \frac{\dot{q}(t)}{4\pi k_2 \alpha_2 R} \\ \left[\frac{R^2 - r^2}{6} + \frac{R}{2} (r - R) - \frac{R^2}{6r} (r - R) \right] \quad (19c)$$

where the dot ($\dot{}$) stands for time derivative. By making the thermal boundary layer approximation in I and II (valid in I until the thermal front reaches the origin) that at $r_1(t)$, $T_1 = T_{1i}$, $\partial T_1 / \partial r = 0$ and at $r_2(t)$, $T_2 = T_{2i}$, $\partial T_2 / \partial r = 0$ one can obtain equations of the form

$$\dot{r}_1 = f_1(r_1, r_2, r_s) \quad , \quad \dot{r}_2 = f_2(r_1, r_2, r_s) \quad , \quad \dot{r}_s = f_3(r_1, r_2, r_s) \quad (20)$$

and equations for T_{if} , q , q' as functions of r_1 , r_2 , r_s . The solution of these ordinary differential equations with initial data $r_1 = r_2 = r_s = R$ then give the time dependent coefficients of the polynomial approximations. A similar boundary layer approximation holds when there is no freezing. In the "linear" approximation to the freeze problem one has for example, from $T_1(r_1, t) = T_{1i}$, $T_2(r_2, t) = T_{2i}$, $T_s(r_s, t) = T_M$, that

$$q(t) = \frac{4\pi R (T_{2i} - T_M) k_2 r_2 k_s r_s}{\left[k_s r_s (R - r_2) - k_2 r_2 (R - r_s) \right]} \quad , \quad T_{if}(t) = \frac{T_M k_s r_s (R - r_s) - T_{2i} k_2 r_2 (R - r_s)}{k_s r_s (R - r_2) - k_2 r_2 (R - r_s)} \quad (21)$$

$$q'(t) = \frac{4\pi k_1 r_s r_1}{(r_s - r_1)} (T_{1i} - T_M) \quad (21)$$

Since in the "linear"/ r approximation, $-k \frac{\partial T_s}{\partial r} = q(t)/4\pi R^2$, one has

$$\dot{r}_s = \frac{q'(t) - q(t)}{4\pi \rho l r_s^2} = f_3^{(0)}(r_1, r_2, r_s). \quad (22)$$

From

$$\left. \frac{\partial T_2}{\partial r} \right|_{r=r_2} = \frac{-q(t)}{4\pi k_2 r_2^2} + \frac{\dot{T}_{if}}{\alpha_2} \left(\frac{r_2}{3} - \frac{R^3}{3r_2^2} \right) + \frac{\dot{q}(t)}{4\pi k_2 r_2^2} \left(\frac{R}{2} - \frac{r_2}{3} - \frac{1}{6} \frac{R^3}{r_2^2} \right) = 0 \quad (23)$$

and the time derivative of

$$T_M = T_{if} + \frac{q(t)}{4\pi k_s} \frac{(R-r_s)}{r_s R} \quad (24)$$

one obtains

$$\dot{T}_{if} = \dot{T}_{if}(r_1 r_2 r_s) \quad , \quad \dot{q}(t) = \dot{q}(r_1 r_2 r_s) \quad (25)$$

From

$$\left. \frac{\partial T_2}{\partial r} \right|_{r=r_1} = 0$$

one obtains $q'(t) = q'(r_1, r_2, r_s)$. Finally from the time derivative of

$$T_{1i} = T_M + \frac{q'(t)}{4\pi k_1} \frac{(r_s - r_1)}{r_s r_1} \quad , \quad T_{2i} = T_{if} + \frac{q(t)}{4\pi k_2} \frac{(R-r_2)}{r_2 R} \quad (26)$$

we get

$$\dot{r}_1 = f_1^{(0)}(r_1 r_2 r_3) \quad , \quad \dot{r}_2 = f_2^{(0)}(r_1 r_2 r_3) \quad (27)$$

When there is no freezing, the "linear" approximation consists of

$$\begin{aligned} T_1 &= T_{if} + \frac{q(t)}{4\pi k_1} \frac{(R-r)}{rR} \quad , \quad T_2 = T_{if} + \frac{q(t)}{4\pi k_2} \frac{(R-r)}{rR} \\ q(t) &= (T_{2i} - T_{1i}) 4\pi R \frac{k_2 r_2 k_1 r_1}{k_1 r_1 (R-r_2) - k_2 r_2 (R-r_1)} \\ T_{if}(t) &= \frac{T_{2i} k_2 r_2 (R-r_1) + T_{1i} k_1 r_1 (r_2 - R)}{k_2 r_2 (R-r_1) + k_1 r_1 (r_2 - R)} \end{aligned} \quad (28)$$

$$\dot{r}_1 = 4\pi k_1 r_1^2 \frac{\dot{T}_{if}}{q} + \frac{r_1 (R-r_1)}{R} \frac{\dot{q}}{q} \quad , \quad \dot{r}_2 = 4\pi k_2 r_2^2 \frac{\dot{T}_{if}}{q} + \frac{r_2 (R-r_2)}{R} \frac{\dot{q}}{q}$$

Where $\dot{q}/q(r_1, r_2)$, $\frac{\dot{T}_{if}}{q}(r_1, r_2)$ are obtained from

$$\left. \frac{\partial T_1}{\partial r} \right|_{r=r_1} = 0, \left. \frac{\partial T_2}{\partial r} \right|_{r=r_2} = 0 \text{ that is}$$

$$\frac{-q(t)}{4\pi k_i r_i^2} + \frac{\dot{T}_{if}}{\alpha_i} \left[\frac{r_i}{3} - \frac{R^3}{3r_i^2} + \frac{\dot{q}(t)}{4\pi k_i \alpha_i R} \right] \left(\frac{R}{2} - \frac{r_i}{3} - \frac{R^3}{6r_i^2} \right) = 0 \quad (29)$$

where $i = 1, 2$. The "linear" approximation [Eqs. (28) and (29)] gives excellent results for $q(t)$ and $T_{if}(t)$ when compared with the exact answer. The curvature of the temperature distribution, however, is incorrect. The latter defect can be corrected by making the "cubic" / r approximation.

Note: Figures giving $P(r, t)$, $T(r, t)$, $T_{if}(t)$, and $q(t)$ for the case of UO_2 inside Na and Na inside UO_2 are available by request from the author.

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A MODEL FOR FUEL FRAGMENTATION DURING MOLTEN FUEL/COOLANT THERMAL INTERACTIONS

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ABSTRACT

Various physical models have been proposed to explain fuel/coolant interactions (FCI) and many sets of experimental data have been published. This paper attempts to take some of the significant results for a restricted class of FCIs and show that they are consistent with a particular cyclic model. The experiments discussed are restricted to those in which one substance is dropped into the other as this mode of contact is the most quiescent. The model which is considered is a cyclic theory of bubble growth followed by collapse. The collapse, being asymmetric, forms a jet of coolant which can penetrate the fuel and vaporize forming another bubble and disrupting the fuel surface.

1. INTRODUCTION

A fuel/coolant interaction (FCI) can occur when a hot liquid (the fuel) is brought into contact with a cold vaporizable liquid (the coolant). If the conditions are right vaporization occurs on a rapid timescale and can produce damaging effects. Various models have been proposed to explain FCIs and reviews of both theoretical and experimental aspects are given in references [1,2 and 3].

In this paper we have restricted ourselves to considering FCI and related experiments where the influence of perturbing processes is small. Hence most of the experiments relate to FCIs which occur spontaneously when the mode of contact is dropping one liquid into the other. In other types of FCI experiment, e.g. shock tubes, large hydrodynamic forces during the contact of the liquids will affect the course of the interaction [4]. In systems where coarse mixing has been achieved propagation models e.g. [5] are important. We have considered experiments which involve both simulant and possible fast reactor materials and attempt to demonstrate that they can all be explained by one cyclic model [6].

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2. EXPERIMENTAL DATA

2.1 The Temperature Interaction Zone (TIZ)

Many experiments have involved the dropping of small quantities of liquid metals as fuel simulants into water or other liquids. In their experiments with small tin drops (12 gms) in water Dullforce et al [7] have shown that explosive interactions take place only in a well-defined region of fuel temperature/coolant temperature space (see Figure 1), which they call the Temperature Interaction Zone (TIZ). The events have produced pressure peaks of several atmospheres [4] and on occasion broken glass beakers. Similar experiments by Bjornard [8] who used tin masses of about 1 gm also show the existence of a coolant cut-off temperature but lower peak pressures (maximum 2 bars) - this is due to differences in the experimental arrangement which can greatly affect the pressure records [4].

The data of Dullforce et al show that as the diagonal cut-off boundary is approached by increasing fuel or coolant temperature both the extent of the fragmentation and the time between water-entry and interaction (the dwell-time) increase. Again the experiments of Bjornard and their continuation by Bkorkquist [9] show similar behaviour. As the latter used a tin mass of about 6 gms as well as 1 gm, three different tin masses have been shown to obey the same qualitative behaviour. A schematic TIZ with remarks on possible physical processes in the different regions was given by Caldarola [10].

The presence of extremely sharp cut-offs in the TIZ is indicative of an abrupt change in the physical processes occurring - such a change could be provided by the breakdown of film boiling as several authors have suggested or the absence of a trigger mechanism necessary to initiate the interaction.

2.2 Fuel Surface Temperature at Interaction

Bjorkquist [9] has estimated the temperature of the surface of his tin drops at interaction by using published heat transfer coefficients [11,12]. It was shown that if the drop had an initial temperature higher than about 600°C it cooled to this temperature before interaction occurred (see Figure 2). The fact that a particular temperature is of importance was also noted by Zyzkowski in his experiments [13] with small copper masses (about 1/4 gm). He noted that in several experiments a thermocouple, which penetrated the copper drop when it was on the base of the vessel, indicated a temperature of about 1300°C just prior to thermal explosion. Explosions in these experiments only occurred when the copper was heated in air and an oxide layer formed on the fuel surface. It appears that destabilization of the film boiling state occurred at an internal temperature above the melting point of copper when oxide was present on the surface whereas if no oxide was present destabilization occurred below the melting point of copper and no explosion occurred.

It should be pointed out that the minimum temperature for stable film boiling, T_{min} , depends on many parameters besides the thermophysical properties of the coolant. These include the geometric configuration of the system, the thermophysical properties of the fuel surface and the relative velocity between the two materials. An experiment by Farahat [14] in which a hot tantalum sphere was quenched in sodium, showed that T_{min} decreased as the sodium bulk temperature was increased. An empirically based correlation [15] shows the same behaviour. The effect of changing the shape of the heater surface was demonstrated [16] using a copper sphere with and without a cone attached quenched in water. In the latter case the value of T_{min} was increased by nearly 250°C. T_{min} can also be increased by coating the heater surface with insulating materials [17,18]. These results are consistent with Zyzkowski's experiments [13] quoted above. T_{min} has also been shown to increase as the velocity of a quenched hot sphere is increased, in both sodium [19] and water [11].

For fast reactor materials, sodium and UO_2 (or possibly stainless steel), the thermal conductivity of the coolant is more than an order of magnitude greater than that of the fuel whereas for the simulant materials in common use (metal/water and water/Freon) the fuel thermal conductivity will be greater.

This inversion will affect the growth of bubbles of coolant vapour (see section 4.3). The ratio of fuel to coolant densities is also important in considering dynamical effects in the system.

The inverted ratio of physical properties affects the interface temperature,

$$T_I = \frac{\alpha_f T_f + \alpha_c T_c}{\alpha_f + \alpha_c}$$

where $\alpha = (k\rho c)^{1/2}$, k is the thermal conductivity, c is the specific heat, ρ the density and subscripts f and c refer to fuel and coolant. As can be seen from Table 1 for UO_2 /sodium T_I will be much smaller than T_f whereas for metal/water T_I will be close to T_f . Although this will affect the details of how the interaction proceeds it is unlikely to change the nature of the phenomenon.

TABLE 1
Table of Physical Properties of FCI Simulant Materials

System	$\frac{k_f}{k_c}$	$\frac{\rho_f}{\rho_c}$	$\frac{\kappa_f}{\kappa_c}$ *	$\frac{\alpha_f}{\alpha_c}$
UO_2/Na	0.033	9.1	0.0076	0.44
UO_2/H_2O	5.0	8.9	4.3	2.4
Stainless Steel/Na	0.4	8.4	0.10	1.1
Stainless Steel/ H_2O	37.2	8.13	37.7	6.1
Al/ H_2O	127.3	2.3	213.8	8.7
Sn/ H_2O	48.7	7.3	116.3	4.6
H_2O /Freon [†] 12	6.9	0.6	2.6	4.3

* $\kappa = k/\rho c$ is the thermal diffusivity

† Freon is the registered trade mark of Dupont

2.3 Freon/Water Experiments

Of the theories that have been put forward to explain FCIs the superheat model, suggested by amongst others Fauske (e.g. [20]) has been widely considered. This theory claims that the coolant liquid does not vaporize at its saturation temperature (T_{sat}) but is superheated until, at the spontaneous nucleation temperature (T_{spont}^{sat}), thermodynamic fluctuations reach such a level that explosive boiling takes place. T_{spont} depends on the contact angle, θ , between the two materials and has a maximum when $\theta = 0$. This temperature is known as the homogeneous nucleation temperature (T_{hom}) and is given by the Döring equation [21]. If the interface temperature at contact is below T_{spont} explosive boiling will not occur.

Experiments using Freon 22 (coolant) dropped into water (fuel) have been considered in the light of this theory [22,23]. These experiments showed (e.g. Figure 3) that the pressure produced by the interaction increased greatly at a particular fuel temperature. From high-speed film, Board et al [23] interpret these results as being due to the fact that for $T_I < T_{spont}$ the materials react instantaneously so that only a small part of the coolant has entered the water whereas for $T_I > T_{spont}$ stable film boiling occurs and the vapour layer inhibits the interaction until a much greater amount of the coolant has entered the water and can react. When the Freon is subcooled, they believe that the coolant is

heated to saturation so that the critical value of T_f depends only on the fuel temperature ($T_f = T_{\text{spoil}}$ when $T_f = 760^\circ$ for $T_f = T_{\text{sat}}$). Thus according to Board et al the importance of T_f is that it is the lower limit of stable film boiling which allows large amounts of material to react and not that pointed out by Fauske [20]. However, no evidence exists to allow this condition to be extrapolated to other systems. The identification of the lower limit of stable film boiling with T_{film} is also suggested by Schins [24]. Henry et al interpret a subsequent series of experiments [25] with Freon and mineral oil, which wet well, as exhibiting excellent agreement with the superheat theory and attribute the results with Freon/water as being due to poor wetting. Recent experiments by Armstrong and Anderson [26] with this latter system, however, have confirmed the results and observations of Board et al concerning $T_f > T_{\text{spoil}}$ but not T_f as a sole function of T_f .

2.4 Sodium/ UO_2 Experiments

As pointed out above, for a sodium/ UO_2 system the interface temperature is much lower than the fuel temperature. Using Henry's correlation [27] to predict T_{min} it appears that when liquid UO_2 and sodium come into contact a stable film boiling state is unlikely to occur as T_f is less than T_{min} . Direct liquid-liquid contact will occur immediately followed by thermal interaction and fragmentation of the small mass of UO_2 which has already penetrated into the sodium. This suggests that only small masses of UO_2 will be involved in the interaction and a coherent and hence energetic thermal interaction is unlikely. Experimental results carried out in various laboratories seem to support this view of the dropping contact mode [10].

Armstrong [28] has dropped UO_2 into sodium and shown that fragmentation occurs and particles of fuel are ejected from the crucible, but claims 'no explosions were observed'. However when sodium is injected into UO_2 violent interactions have occurred [29]. Thus it appears that if the sodium can be held for a time it can be heated such that a sufficiently high pressure can develop so that eventually it escapes explosively. The injection experiments suggest that in some circumstances violent fragmentation can occur and it is worth mentioning that the UO_2 drop experiments are not unequivocal as fast rise times for the pressure were noted in ref. [28].

In the French JEF experiments [30] a fuel pin was heated electrically in stationary sodium. After pin melting there was a non energetic FCI. Figure 4 shows the mechanical energy obtained during the interaction as a function of sodium temperature. A maximum was observed at 730°C . A similar maximum was observed by Farahat [14] in a completely different experiment. Here a hot sphere made of tantalum was cooled in a pool of liquid sodium and the pressure disturbances due to boiling were measured by means of a pressure transducer. The pressures produced had a maximum of 750°C . These results are comparable with the increase of fragmentation with coolant temperature found in the pin drop experiments [7].

2.5 Film of FCI Experiments

High-speed film of the interaction between liquid metal drops and water has been obtained by various workers. In the cases of Board et al [31], Dullforce et al [7], Bjornard [8] and Bjorkquist [9], the description of the events is similar: an interaction of small size is seen to occur on a edge of the drop, throwing out small particles of fuel into the coolant. There follows a series of similar events of increasing size until all the molten metal is included; the debris now being an approximately spherical cloud around the original position of the fuel. In the film of Dullforce's experiments part of a presumably spherical front can be seen expanding and contracting within the dispersed debris.

The number of cycles that can be observed is greater for the higher mass experiments. Typically in Dullforce's film eight or nine cycles of expansion and contraction can be seen, whereas in the films of Bjorkquist four or five cycles is more typical. Zyzkowski's films [13] of very small copper drops in water, however, show usually only a single event, which disrupts the whole mass.

The series of expansions and contractions in the film of Dullforce et al and Bjorkquist can clearly be seen to be increasing in scale as the interaction proceeds: the interaction is clearly not a large event followed by a series of decaying pulses.

3. DESCRIPTION OF THE CYCLIC MODEL

Cyclic models of FCIs have been suggested by various authors e.g. [31]; the particular one we are considering was described by Buchanan and Dullforce [6] and depends on the growth and collapse of vapour bubbles at or near the liquid surface. The bubble grows until it reaches cooler coolant when collapse begins. During the collapse phase the asymmetry in the system, in this case the presence of the fuel surface, causes an asymmetry in the shape of the collapsing bubble. This asymmetry becomes a high velocity jet of coolant which is directed at the fuel surface and can penetrate it. Rapid spreading of the coolant occurs within the fuel and the coolant is vaporized producing another bubble. Provided sufficient hot fuel is available to vaporize enough of the coolant, successive bubbles will grow to increasingly larger radii and hence produce successively greater pressures on collapse. The ratio of pressures in successive cycles depends on whether the coolant jet is vaporized heterogeneously or homogeneously and is given by equation 4.5 of reference [32], for water, as

$$\frac{P_i - P_o}{P_{i-1} - P_o} = \begin{matrix} 6.673 \text{ heterogeneous} \\ 2.899 \text{ homogeneous} \end{matrix}$$

where P_o is the system pressure. The kinetic energy of the jet produced on collapse is 0.439 times the energy of the bubble (see equation 14.4 reference [32]) and so bigger bubbles lead to more energetic coolant jets. The synergistic nature of this process is important as it allows the mechanism to build up from a minor initial perturbation to a sufficiently large event in which the whole of the fuel is involved.

However it is necessary to have an initial perturbation which allows entry into the cyclic process and, as yet, this has not been identified. It seems that the triggering has two very well-defined stages. Firstly direct contact between the two liquids, without an insulating layer of vapour, occurs; secondly some phenomenon, which we will call the 'X-mechanism', takes place which provides the entry into the cyclic process of bubble growth and collapse. The 'X-mechanism' may be intimately connected with the breakdown of film boiling. As the films indicate, it must occur at or near the fuel surface.

4. DISCUSSION

4.1 Trigger: 1st Stage - Direct liquid-liquid contact

Evidence from many experiments indicates that direct liquid-liquid contact is necessary before an FCI can take place. The TIZ (Figure 1) itself suggests that a different physical process is occurring above and below the diagonal cut-off line. There are no spontaneous interactions if the experiment is carried out with fuel and coolant temperatures that are in the top right hand region of the two-temperature diagram (TIZ). However Board et al have shown that it is possible to produce an interaction between tin and water when above the TIZ diagonal cut-off by providing an external trigger [33]. They demonstrated two methods: suddenly increasing the ambient pressure or striking the reaction vessel. The suggestion is that a film boiling state exists and the disturbance causes breakdown of the film allowing direct contact between the liquids. The Freon/water experiments [34], as mentioned above, showed that larger pressures resulted from inhibiting the interaction until large amounts of coolant are involved.

The fact that a particular surface temperature is found at interaction as was detailed in section 2.2 lends weight to the concept that different phenomena occur above and below this point. Reynolds et al [35] showed that the upper boundary of the TIZ could be accounted for by changes in the nature of the vapour layer around the drop and in particular the nature of the breakdown

of this layer. Using observed dwell times the theory predicts that the vapour film breaks down at a particular fuel temperature in agreement with ref. [12].

The necessity for direct liquid-liquid contact as the first stage in the development of an FCI, while not proved, seems to be in accord with the observational evidence as in UCLA experiments [36] where such interactions were noted.

4.2 Trigger: 2nd Stage - 'X-mechanism'

Once the contact between fuel and coolant has been achieved the interaction must then be triggered into the cyclic process of bubble growth and collapse. This stage of the triggering process we are calling the 'X-mechanism'. The system is highly unstable at this stage and different 'X-mechanisms' may be operative in different experiments. The requirement of an 'X-mechanism' is that after direct liquid-liquid contact occurs it causes the system to enter the cyclic bubble growth and collapse process. It can do this in 3 ways: by breaking small fuel particles from the fuel surface so that the contact area and hence heat transfer are increased and the first bubble grows; by allowing coolant to get beneath the fuel surface; by directly growing the first bubble. Some possible 'X-mechanism' are considered below:

4.2(a) Release of Dissolved Gas. It has been suggested [37] that FCIs are due to the explosive release of dissolved gas in the fuel on quenching. Disruption of steel due to oxygen being forced out has been observed [38] when the temperature falls so quickly on quenching that it becomes locally supersaturated with oxygen. The experiments with metal/water systems have been conducted in inert atmospheres e.g. [7] with short heating times and it is unlikely sufficient gas could be dissolved in this time as inert gases are virtually insoluble in metals [39]. It is possible however that small amounts of gas could be dissolved, sufficient to disrupt part of the surface on release. The local throwing out of fuel particles noted in films of metal/water interactions fits well with this explanation. However this 'X-mechanism' is unlikely to be operative in the Freon/water experiments as the water, which is the fuel, will not have a large amount of gas dissolved in it.

4.2(b) Liquid-liquid instabilities. Liquid-liquid instabilities, tearing off a small part of the fuel surface or mixing a little coolant below the fuel surface [40], have been suggested as an FCI mechanism and Flory et al [41] show photographic evidence for Helmholtz rings on the surface of solidified liquid drops. However the physical properties of the fuel which generally determine the size of the instability change with temperature so that one would expect greater effects at higher temperatures. In some experiments dwell-times are much shorter than the timescale that would be expected for instabilities.

4.2(c) Stress Cracking of a Solid Shell. This model postulates that a surface shell of the fuel freezes and in attempting to contract at a faster rate than the liquid fuel core the shell is stressed to the extent that it cracks [42]. This results in either liquid fuel squirting out into the coolant or coolant dribbling down the cracks into the liquid fuel core. This theory has been considered in detail by Knapp and Todreas [43] and they conclude that it is unlikely to be effective in the case of metals as they undergo plastic rather than brittle deformation. But for ceramic materials e.g. UO_2 stress cracking could occur on quenching.

4.2(d) Localised Superheating. As was mentioned in section 2.3 models in which it is postulated that the coolant superheats so that vaporization occurs explosively at a temperature above the saturation temperature have been put forward. As pointed out by Fauske [20] this model would only be applicable when the coolant is the bulk material if small drops of coolant can penetrate beneath the fuel surface. Thus we have to postulate some fluid-fluid instability as in 4.2(b). Local superheating would also seem unlikely if a vapour film existed around the rest of the drop as the film would provide nucleation sites.

4.2(e) Bubble Growth during Vapour Film Breakdown. Stevens and Witte [12] have noted the existence of two different mechanisms for the breakdown of film boiling on the surface of a moving solid silver sphere in water. These they call the progressive instability, with a timescale of 50 - 100 msecs and the precipitous instability, with a timescale of the order of $\frac{1}{2}$ msec. The former mechanism occurs only in nearly saturated water and the latter only in sub-cooled water. Thus the method of breakdown of film boiling exhibits a marked change as the coolant temperature increases as is observed in FCIs. It is conceivable that the precipitous instability is so violent as to disrupt a liquid heated surface leading to an increased surface area and hence enhanced heat transfer. Reynolds et al [35] have shown that the upper boundary of the TIZ corresponds reasonably to the boundary separating regimes of thin and thick vapour films around the hot fuel drop. If we postulate that different kinds of instability are connected with the different vapour thicknesses then it would seem reasonable to suggest that this could be a tenable 'X-mechanism'.

4.3 Cyclic Bubble Growth and Collapse State

The existence of a synergistic cyclic process is supported by the films of Dullforce et al [7] and Bjorkquist [9] which, as mentioned above, show an interaction increasing in time. The production of a coolant jet by asymmetric collapse of a vapour bubble has been experimentally noted [44]. Buchanan has also investigated the conditions for penetration of the jet into the fuel [45]. The film evidence of collapsing and expanding fronts within the roughly spherical debris cloud in the films strongly suggests growth of bubbles followed by their subsequent collapse.

The experiments with metal/water systems can adequately be explained by this model. Depending on the region of 'two-temperature space' in which the experiment is performed there is or is not an interaction, which defines the TIZ. The boundaries of this region depend on the nature of the heat transfer process between fuel and coolant. If a vapour film is stable for a long time a solidified fuel surface will have formed before direct liquid-liquid contact occurs and prevent the coolant jet penetrating. On the left hand TIZ boundary the fuel is close to its melting point and will solidify unless the trigger can occur very soon after contact with the water. Experiments [7] show that dwell-times near this boundary are very short. The observation of less cycles of expansion and contraction as the mass of fuel decreased can be explained because the heat energy available in the fuel will be dissipated after a smaller number of cycles and further escalation will be prevented.

The degree of fragmentation of the fuel, and hence the energy in the interaction, increase as the upper diagonal boundary is approached. This can be explained as the number of bubble growth and collapse cycles will be smaller as the fuel temperature decreases as the available heat energy in the fuel is less. The smaller the number of cycles the lower the degree of fragmentation. The increase of fragmentation as the coolant temperature increases occurs because the size to which a vapour bubble can grow increases as the coolant temperature increases and hence the bubble collapse will be more energetic. In fact a maximum in the collapse energy of a bubble is to be expected. As the coolant temperature rises the bubble volume (V) increases whilst the difference between ambient pressure and the coolant saturation pressure (ΔP) falls. Thus the energy of the bubble collapse ($V \cdot \Delta P$) will have a maximum [10,46] which explains the results shown in figure 4 and may be reflected also in the relationship between the degree of fragmentation and the coolant temperature.

The model can also explain the apparent lack of violent fragmentation in UO_2 /sodium interactions. Due to the difficulty of setting up the conditions for a coherent interaction as a stable film boiling stage is unlikely to be achieved interactions between UO_2 and sodium are likely to be localized. However the size to which a sodium vapour bubble can grow is probably much smaller than in water [46]. Thus the energy in the collapse is smaller and the energy of the coolant jet is smaller. Thus bubble growth and collapse is less energetic in

sodium than in water in the sense that more cycles will be required in sodium than in water to achieve an equal level of energy from the collapsing bubble. Hence one expects that when sodium is the coolant a less violent interaction will occur, although fragmentation can occur by 'chipping' at the fuel surface. It is unclear whether or not it is possible to completely explain the Freon/water experiments by this cyclic model; a propagation model may be applicable.

5. CONCLUSIONS

We have considered significant FCI experiments, involving both simulant and reactor materials for the restricted class of near quiescent contact, in the context of the cyclic bubble growth and collapse model. Previous models have quoted a limited set of experiments in support but in this paper it has been shown that a larger set of experiments can be explained consistently using this model. In particular, the model can explain the seemingly low energetic interactions observed in experiments using fast reactor materials.

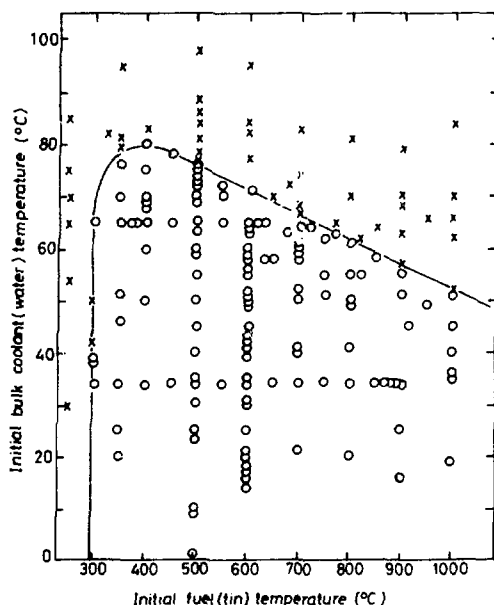
The 'X-mechanism', which is the trigger beginning the cyclic process is still undetermined and various suggested mechanisms were considered. It is likely that different mechanisms can trigger the process in different experiments but elucidation of these is required to achieve a complete understanding of the FCI phenomenon.

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FIGURE 1



The Temperature Interaction Zone for 12 grams of tin dropped through 3 cm into boiled distilled water

o indicates an interaction

x indicates no interaction

(from ref. 6)

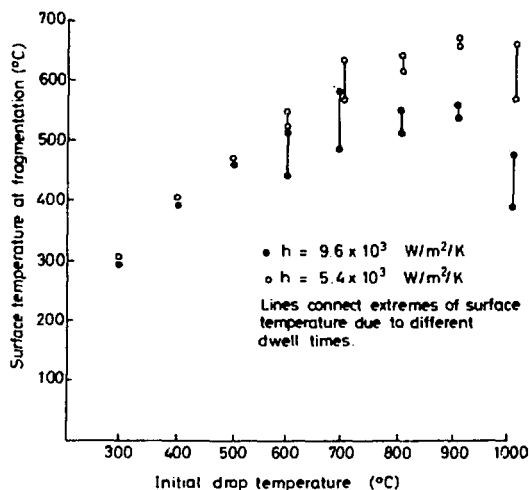


FIGURE 2
 Plot of the Surface Temperature at Interaction against the Initial Temperature for 0.8 grams of tin dropped into distilled water at 295K (from ref. 8)

FIGURE 3
 Plot of Observed Peak Pressure against Fuel (water) Temperature for Freon-22 dropped into water (from ref. 22)

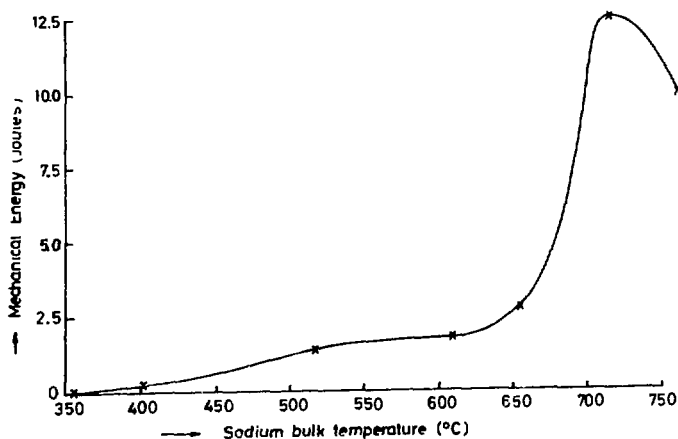
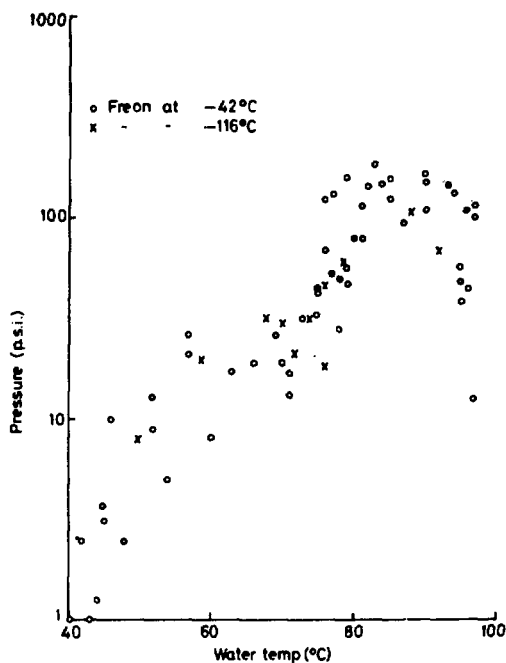


FIGURE 4
 Plot of Mechanical Energy against Bulk Sodium Temperature in the JEF experiments (from ref. 28)

THEORETICAL AND EXPERIMENTAL INVESTIGATION OF FUEL-COOLANT INTERACTIONS IN A SHOCK TUBE CONFIGURATION

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ABSTRACT

Experiments in a shock tube configuration were performed using molten stainless steel and UO_2 in combination with water, respectively sodium. The following data were measured as a function of time:

- a) reaction force acting on the interaction crucible
- b) pressure of the vapour in the interaction space
- c) growth of vapour volume
- d) ejection velocity

The experiments demonstrated that the process of "vapour explosion" in constrained configurations is dominated by successive ejection and reentry cycles. A physical model and a corresponding calculation code has been developed in order to simulate FCI in shock tubes.

According to the experimental results the model describes the FCI as a sequence of cycles each of which consists of three phases:

- a) fuel-coolant-contact
- b) ejection and reentry of the coolant
- c) impact and fragmentation

INTRODUCTION

Spert type experiments and foundry accidents demonstrated that "thermal vapour explosions" due to mixing of molten metal and water are likely to occur. Since even the experts are not able to predict definitely the conditions met in the reactor when it runs out of control, it is reasonable to assume pessimistically that a direct contact accident is indeed possible. Calculations of the maximum mechanical energy yield due to a fuel coolant interaction revealed "unrealistic high values". However, neither the postulated mechanisms have been observed, nor the necessary conditions have been established in experiments using power reactor materials.

This statement is not in conflict with the observed violent Al-water interactions, since for several reasons such interactions are far away from simulating power reactor FCI.

EXPERIMENTAL INVESTIGATION

Test facility

The test apparatus (Fig. 1) consists of a reaction chamber (A), a channel (B) and an expansion vessel (C). The reaction chamber is separated from the channel by a fast opening three way valve. Two test facilities of the same basic design are used for experiments with either water or sodium.

During the test the following data are recorded simultaneously:

- pressure history at different points along the channel
- reaction force acting on the crucible
- displacement of the coolant liquid-vapour interface
- velocity of the coolant flow
- coolant temperature
- cover gas pressure

The mechanical work can be calculated from the pressure and ejection history.

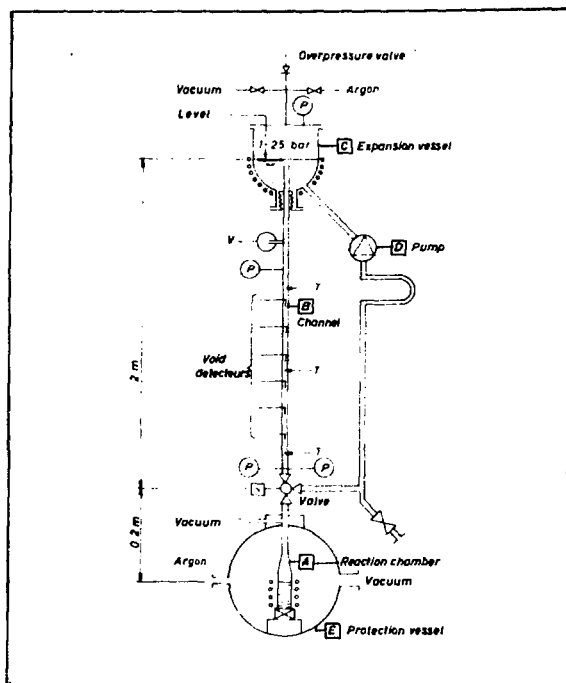


Fig. 1 : Test apparatus fuel-coolant interaction

Experimental Results

Examples of experimental results are compiled in Table I. The Al_2O_3 and UO_2 -tests were performed with small quantities of hot material. It is debatable whether such experiments are representative and whether the results can be used for the analysis of full scale reactor situations.

Furthermore, the results of the Al_2O_3 experiments might be affected by chemical processes between Al_2O_3 and Na. The stainless steel-water experiments were performed with larger quantities of steel so that geometrical boundary effects are less important.

Table I : Examples of test results

Hot material	Quantity	Coolant	Hot material temperature	Coolant temperature	Cover gas pressure	Pressure peak	Conversion factor
Al_2O_3	1 gr	Na	2040°C	556°C	1.41 bar	1.41 bar	< 1%
Al_2O_3	1.2 gr	Na	2040°C	643°C	1.24 bar	1.83 bar	< 1%
Al_2O_3	1.16 gr	Na	2040°C	713°C	0.3 bar	8.6 bar	< 2%
UO_2	4.6 gr	Na	2880°C	678°C	1.275 bar	1.85 bar	< 1%
Steel	134 gr	H_2O	1500°C	20°C	1.0 bar	3.9 bar	< 1%
Steel	120 gr	H_2O	1500°C	20°C	1.0 bar	9.8 bar	< 1%
Steel	134 gr	H_2O	1550°C	20°C	1.0 bar	10.6 bar	< 1%

Fig. 2 shows the pressure and voiding pattern of the interaction between molten Al_2O_3 and Na of 713°C , under cover gas pressure of 0.3 bar. The interaction bursts into a pressure peak of 8.6 bar; the coolant is ejected completely from the test channel.

The reentry is characterized by violent boiling without significant pressure generation. About 1.3% of the thermal energy stored in the Al_2O_3 has been converted into mechanical work.

The UO_2/Na -experiment, shown in Fig. 3, was performed under the same test conditions, except the sodium was subcooled with respect to the saturation temperature ($T_{\text{Na}} = 650^\circ\text{C}$, $T_s = 764^\circ\text{C}$). The maximum pressure peak measured did not exceed 1.85 bar. A typical feature of the interaction was again the repeated evaporation and condensation with decreasing pressure peaks until thermal equilibrium has been reached.

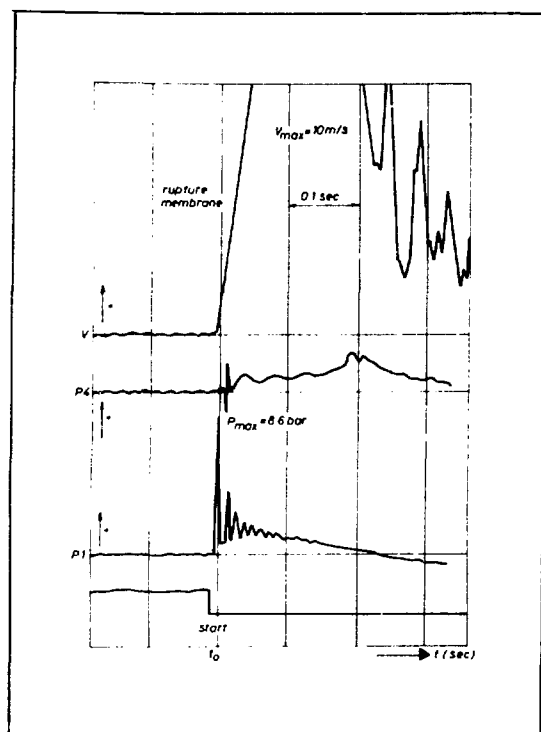


Fig. 2: $\text{Al}_2\text{O}_3/\text{Na}$ interaction—pressure and ejection pattern ($P_1 = 15\text{ cm}$ down stream; $P_4 = 150\text{ cm}$ down stream interaction zone)

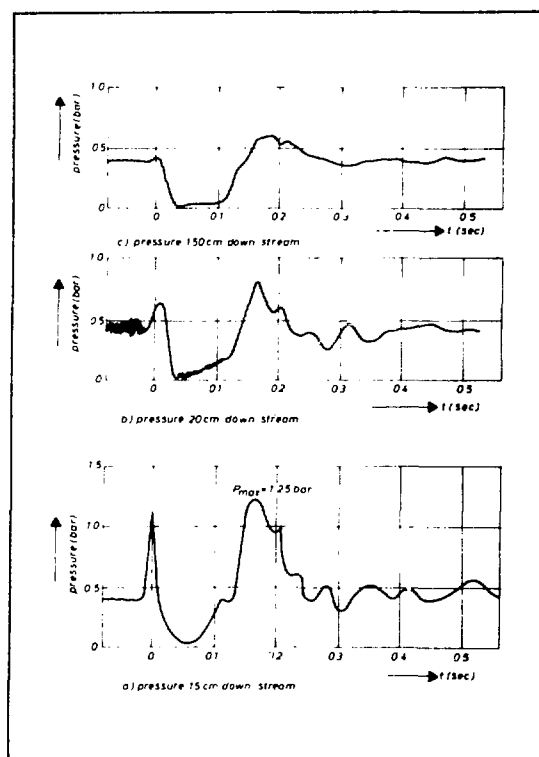


Fig. 3: UO_2/Na interaction—pressure measured down stream the interaction zone

The stainless steel/water experiments show a similar interaction pattern, but higher pressure peaks. The typical pattern of the interaction of molten stainless steel and water is shown in Fig. 4. The diagram shows the reaction force measured below the crucible and the pressure in the coolant measured above the interaction zone. The measured pressure in the coolant exhibits an oscillating pattern with decreasing pressure peaks.

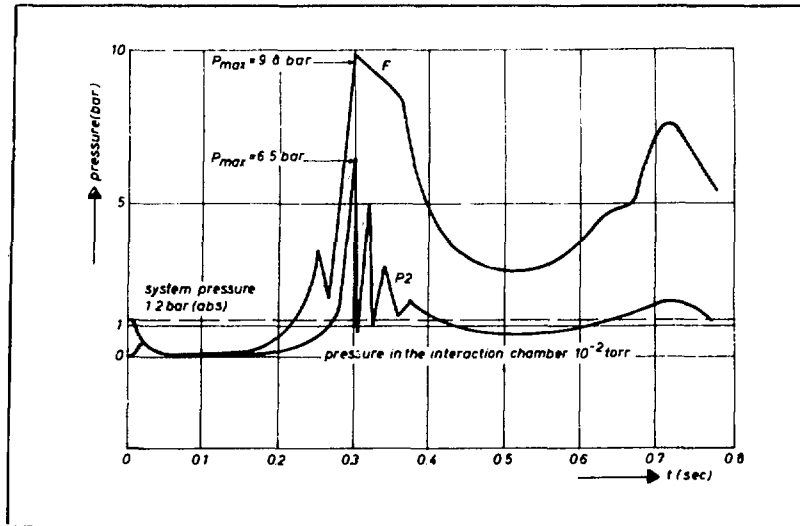


Fig. 4 : Stainless steel water interaction pressure pattern

THEORETICAL APPROACH

The experiments in the shock tube geometry demonstrated that the process of "vapour explosion" in constrained configurations is characterized by successive evaporation and condensation cycles. According to the experimental results the interaction model KAMI has been developed. The model divides each cycle into three phases:

1. Fuel-coolant-contact

At the beginning of the interaction, intimate contact between fuel and coolant is assumed. The heat transfer from fuel to coolant is calculated by solving the Fourier-equation, thereby taking into account the fuel's latent heat of solidification. The hydrodynamics of the contact phase are treated using the well known acoustic approximation.

During the contact phase, the initial conditions for the subsequent ejection are established, namely:

- The temperature profile in the coolant, including a superheated boundary layer
- The nonzero initial velocity of the coolant column (corresponding to the momentum transferred to the coolant by the shock wave and the reflected rarefaction wave)

2. Ejection and reentry of the coolant

The ejection of the coolant starts with a finite initial velocity; the direct heat transfer is cut off by the vapour formed between fuel and coolant.

During the first few milliseconds of the ejection phase, the coolant motion is further accelerated by the difference between the pressure in the vapour space and the outer system pressure. The pressure in the vapour volume is given by the saturation pressure of the coolant at the temperature of the coolant surface. As heat is removed from the superheated layer by evaporation and by heat conduction to the bulk of the coolant, the coolant surface temperature and the associated vapour pressure decrease during the ejection. After a time interval - comparable to that of the contact time - the vapour pressure falls below the system pressure. The upward motion of the coolant column is decelerated and then inverted: the coolant starts reentering.

During reentry, the vapour condenses on the coolant surface. As a consequence, the coolant surface is reheated and the pressure in the vapour volume increases again, resulting in a cushioning of the falling coolant column before impact.

A typical computed interaction pattern (pressure and coolant displacement) is shown in Fig. 5. The boundary conditions are: 1 bar system pressure and highly subcooled coolant (678°C); shock tube of 9 mm diameter and 2000 mm length.

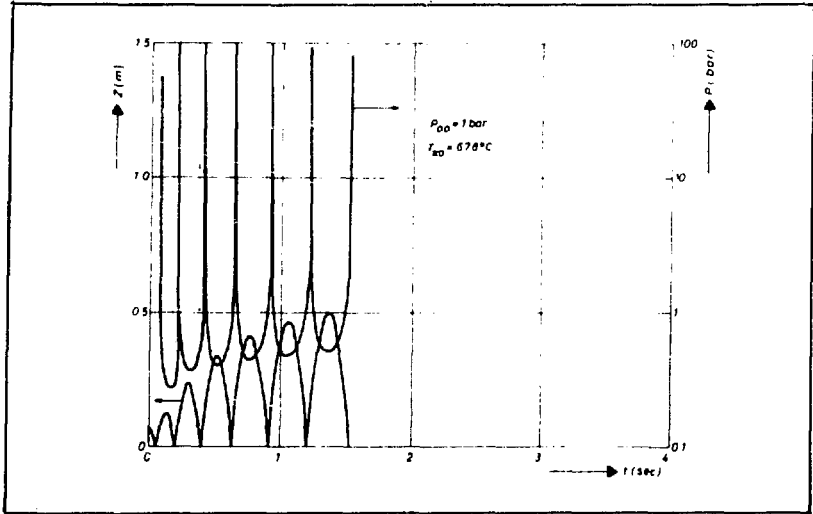


Fig. 5 : UO_2/Na interaction—pressure and ejection history

The increase of the maximum ejection height from one interaction cycle to the next one reflects an important feature of the model: At the beginning of a new interaction cycle, the heat which has been stored in the coolant during predeccessing cycles has been dissipated, resulting in a flat temperature profile in the coolant column. Due to the small gradients, this profile is varying only very slowly; thus it can be regarded as a bias, over which the transient temperature profile associated to the momentary heat transfer processes is superimposed. Evidently, the influence of all predeccessing cycles is accumulated in the biasing temperature profile.

In the KAMI code, the described facts are treated quantitatively by the consistent application of the Fourier-equation throughout the whole course of the interaction.

The interaction pattern shown in Fig. 6 gives a further illustration to this feature. Here, the

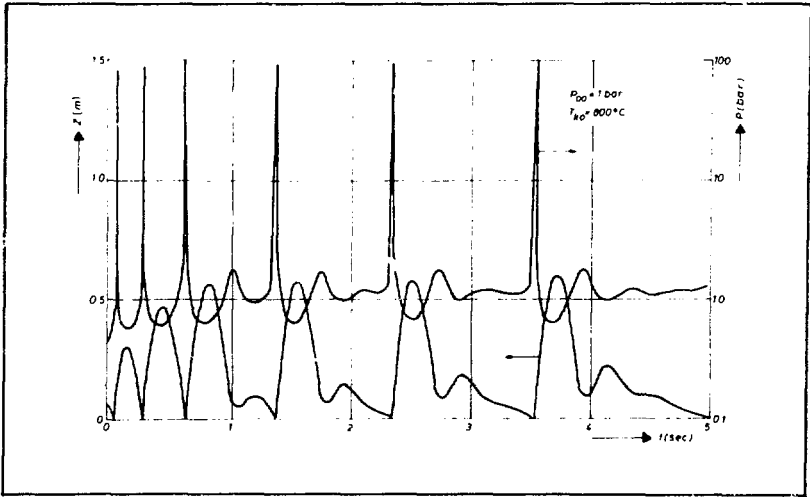


Fig. 6 : UO_2/Na interaction—pressure and ejection history

initial coolant bulk temperature (800°C) is fairly close to the saturation temperature ($\approx 890^{\circ}\text{C}$, corresponding to the static pressure ≈ 1.15 bar at the lower end of the coolant column); The "bias temperature" reaches the saturation temperature after only three cycles.

As a consequence, the latent heat of vaporisation which is transferred by condensation to the surface of the reentering coolant column, cannot be dissipated fast enough to the bulk of the coolant: the falling coolant column is completely cushioned and then reejected; finally, it approaches the fuel rather slowly according to the rate at which the latent heat of vaporisation is dissipated.

3. Impact and fragmentation

In the proposed physical model the fragmentation of the fuel is not an arbitrary input parameter. The two dimensional representation of the ejection and reentry process readily entails the fragmentation mechanism.

During ejection, the wall friction induces a radial velocity profile in the coolant flow. As a consequence, the transition from ejection to reentry motion sets on in the outer radial zones. During reentry, the outer zones are thus preceding the central zones.

Finally the different radial zones successively impinge on the fuel, thereby transferring a part of their kinetic energy. The transferred energy is available to fragment the fuel and to dislocate the particles. Evidently, fragmentation is a continuous rather than an instantaneous process. The described nonuniform impact supplies a fragmentation mechanism which is the most likely one in constrained configurations.

The impact of the coolant on the fuel marks the beginning of a new contact phase and thereby the beginning of a new interaction cycle.

DISCUSSION OF THE RESULTS

Experiments with $\text{Al}_2\text{O}_3/\text{Na}$, UO_2/Na and stainless steel/ H_2O in constrained configurations did not display violent interactions. The pressure peaks measured did not exceed 11 bar; the half width times of which were in the range of milliseconds.

The interaction patterns observed were characterized by repeated ejection and reentry cycles. The conversion factor, i.e. the ratio of mechanical work to thermal energy stored in the hot material, was smaller than 2%.

Similar low energy yields were observed in pool type experiments (not reported here) with stainless steel and water. The pressure peaks measured there did not exceed 1 bar.

The constrained geometry of the shock tube restrains to a certain extent the penetration of the coolant into the hot material and the intimate mixing of the two components before evaporation, which influences the process into the direction of a mild interaction.

A physical model has been developed to describe fuel coolant interactions in shock tube geometry. A principal feature of the code is the consistent application of the Fourier-equation throughout the whole interaction process.

The two dimensional representation of the coolant flow provides furthermore the basis for the fragmentation mechanism, which therefore needs not to be introduced as an input parameter.

When comparing the experimental results to those predicted by the model, only very poor agreement can be stated, as far as the pressure peaks are concerned: the calculated peaks exceed the measured ones by about one order of magnitude.

On the other hand, good agreement between theory and experiment is found in the description of the interaction pattern and in the time intervals of the successive interaction cycles.

It is open to discussion whether shock tube experiments are representative for fuel coolant interactions occurring in reactors.

Beyond any doubt the shock tube offers the best possibilities to measure the mechanical work of vapour expansion. Not known, however, is whether the preexplosive phase of impact and mixing of fuel and coolant is representative.

CONCLUSIONS

It is very difficult to achieve perfect mixing of large quantities of fuel and coolant in constrained configurations.

The fragmentation of the fuel (excluding coolant entrapment) is caused essentially by the impact between coolant and fuel and transfer of mechanical energy to the fuel. The subsequent behaviour of the system is determined by the fraction of cold liquid that is vaporized, and which depends on the amount of heat which can be transferred during the contact to the coolant.

In constrained geometries the multiple ejection-pattern is a typical feature which cannot be identified as vapour explosion.

The fuel coolant interaction model (KAMI-code) takes into account the experimental evidence. It may indicate a step towards a more realistic comprehension of the Fuel Coolant Interaction.

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EFFECTS OF FUEL-SODIUM INTERACTION IN TOP TESTS WITH ELECTRICALLY HEATED FUEL PINS

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ABSTRACT

To simulate TOP accidents experiments are performed in an out-of-pile sodium loop using electrically operated UO_2 fuel pins. A single pin or a seven-pin bundle is heated from steady-state operation in a power transient of 300 to 400 ms duration so that up to 50 g of molten UO_2 penetrate into the cooling channel.

Immediately after the pin has become defective narrow pressure pulses occur with amplitudes up to about 70 bar. They seem to be produced by collapsing of sodium vapor bubbles. Gas overpressures of about 2 to 3 bar in the fuel pin yield the highest amplitudes. Another type of pressure profile was observed, above all at a very low gas pressure in the fuel pin. It might be caused by evaporation of sodium penetrating into the defective zone. There is relatively small mechanical work done at the coolant during these events. A strong blockage of the cooling channel was observed within the damaged zone.

INTRODUCTION

Transient overpower simulation tests were taken up in an out-of-pile loop at the Karlsruhe Nuclear Research Center about one year ago. It was not our goal to simulate the fuel pin behavior proper during very steep reactivity ramps. In our experiments the power transient has to melt and overheat oxide fuel starting from the centre line of the fuel pin and fast enough to keep a solid shell of fuel until the molten fuel penetrates into the coolant preferably at a single spot.

We started with experiments in inert gas and in sodium capsules in the course of which we learnt the suitable technology for the test operation, the instrumentation, the measurement recording and processing. Then with our sodium loop the interest has been concentrated on studying the influence of different operating parameters, such as coolant conditions, gas pressure in the fuel pins and cooling channel geometry, on the development of fuel-coolant interaction.

In the existing tests the sodium temperature and the static pressure within the test section have been kept constant while the coolant velocity has been varied between 1 and 7,5 m/s and the gas overpressure in the fuel pins

between 0 and 32 bar. Up to now eleven experiments with single pins were performed in different annular cooling channels as well as six experiments with 7-pin bundles having a reactor specific cooling channel geometry.

EXPERIMENTAL SETUP

A special fuel rod was developed for these experiments (Fig. 1). The 80 mm long fuel zone consists of a single UO_2 cylinder 5 mm in diameter. It is insulated from the stainless steel cladding tube (outer diameter 7 mm) by an 0.4 mm wide gas gap and 0.3 mm thick Al_2O_3 sleeves. Since the sleeves have a good contact with the cladding tube, they are relatively cold during the experiment and so the insulation property is maintained. The UO_2 cylinder is electrically heated by direct current passage which has no fixed potential with respect to the cladding. The fuel pin can be filled with argon of a given pressure which is kept constant automatically until the start of the power transient.

Fig.2 shows the flowsheet of the sodium circuit. The sodium flow is directed upwards in the test section. The cooling channel length is 500 mm. The sodium temperature is set by use of an electric preheater. The maximum value achievable is close to 700°C . The hydrodynamic resistance of the line connect-

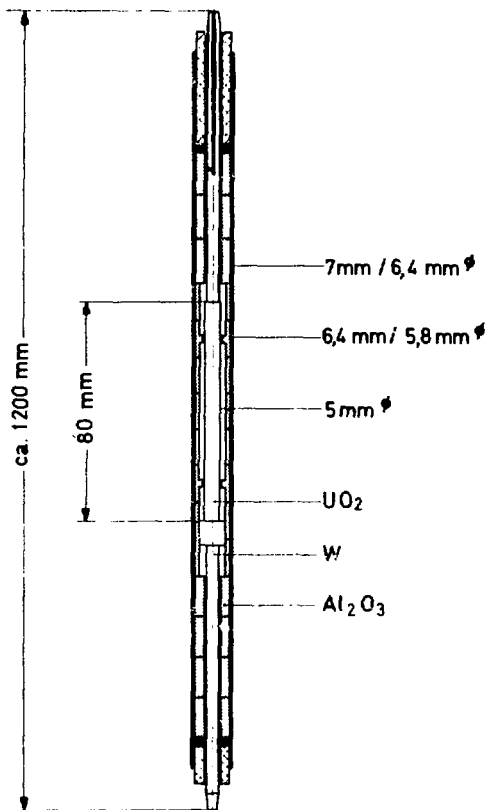


Fig. 1 Layout of the Fuel Pin for TOP Simulation Tests

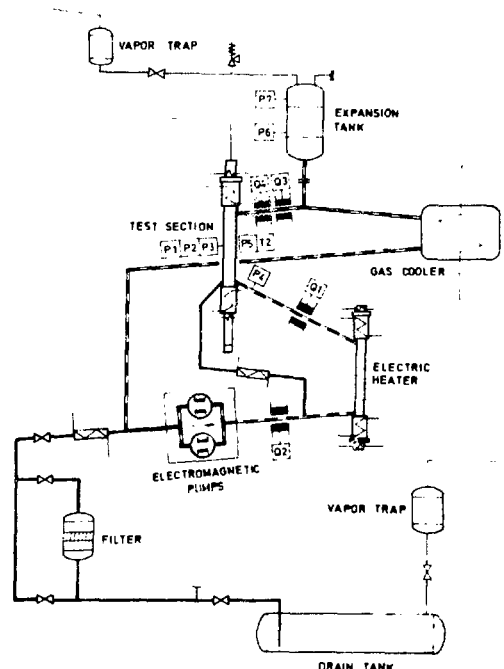


Fig. 2 Schematic Diagram of the Sodium Test Loop

ting the test section with the expansion vessel has been so rated that it roughly corresponds to that in the upper part of a FBR fuel element. The pressure in the expansion vessel can be set up to 16 bar so that also the sodium boiling temperature can be varied. The electric device for experiments to be performed with 7-pin bundles comprises a separate system of power supply and control for each fuel pin / 1 /.

In the measurement recording device 28 different signals are recorded on magnetic tape. Besides the electric and thermal operating data the following measured values are the most significant for the experiments:

- pressure in the cooling channel measured with 3 transducers oriented radially to the cooling channel at different levels
- pressure at the inlet and outlet of the test section
- sodium flow at the inlet and outlet of the test section

For these measurements pressure transducers are used which had been developed at the Karlsruhe Nuclear Research Center. These transducers use strain gauges attached to a diaphragm. Their effective measurement frequency is more than 40 kHz. The sensitivity to effects from external electrical sources is low since the measurement system is fully insulated.

The measured analogous signals are converted by an analogue-digital conversion system and recorded on magnetic tape by means of a processor. For further processing of the data the computer program SEDAP (System for experimental data processing) / 2 / is used. By this system about 60 mathematical operations can be applied to the data files.

RUN OF AN EXPERIMENT

Prior to an experiment the sodium circuit is heated to more than 550°C.

This brings down the electrical resistance of uranium dioxide to such a level that the power generated at 270 Volt is sufficient to heat the fuel further. Subsequently, steady-state operation is set yielding a central temperature of over 2200°C at about 100 W/cm rod power.

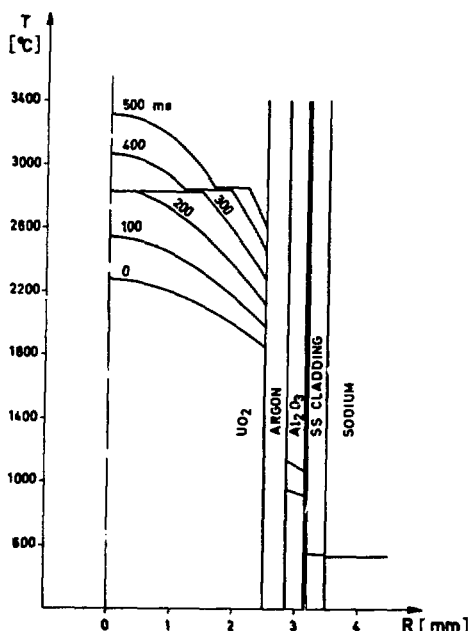


Fig.3 Time History of the Temperature Profile in the Fuel Pin (calculated)

After the desired sodium temperature has been set - 520°C in the previous experiments - the power transient is initiated by switching over to the maximum voltage. During 300 to 400 ms about 50% of the fuel are molten and heated in the center to more than 3000°C (Fig. 3) /3/. The linear rod power rises to about 4000 W/cm during this process. The total input of electric energy during the power transient is about 1 kJoule/cm fuel pin length.

Then liquid UO_2 penetrates the insulation and the cladding at the lower end of the fuel cylinder. This brings the electric potential of the lower electrode to that of the stain-

less steel cladding. At the upper fuel end, where the full voltage difference with respect to the cladding tube now gets effective, a short circuit generally takes place so that the heat production in the fuel becomes very low.

Immediately after the pin has become defective the molten fuel flows very quickly into the sodium together with filling gas. Possible events of thermal interaction normally occur in quite a short period of time. Therefore the measured data are recorded for about 1.5 s. During this interval about 2.5 million data are recorded on magnetic tape.

In 7-pin experiments the single pins are first brought successively into the steady-state condition of operation. By a very careful harmonization of the individual operating values we succeeded in making largely identical the power excursions of the 7 pins. The times of failure occurred within the period from 25 to 60 ms.

TEST RESULTS SO FAR OBTAINED

The fuel-coolant interaction becomes apparent by the occurrence of pressure pulses and flow rate variations. Fig. 4 shows the typical course of pressure close to the reaction zone as well as the flow rate variations at the inlet and outlet of the test section after the pin has become defective.

During the first contact between the fuel and the sodium a considerable number of discrete pressure pulses can be observed both in the single rod and in bundle tests. The coolant flow shows substantial variations caused by the

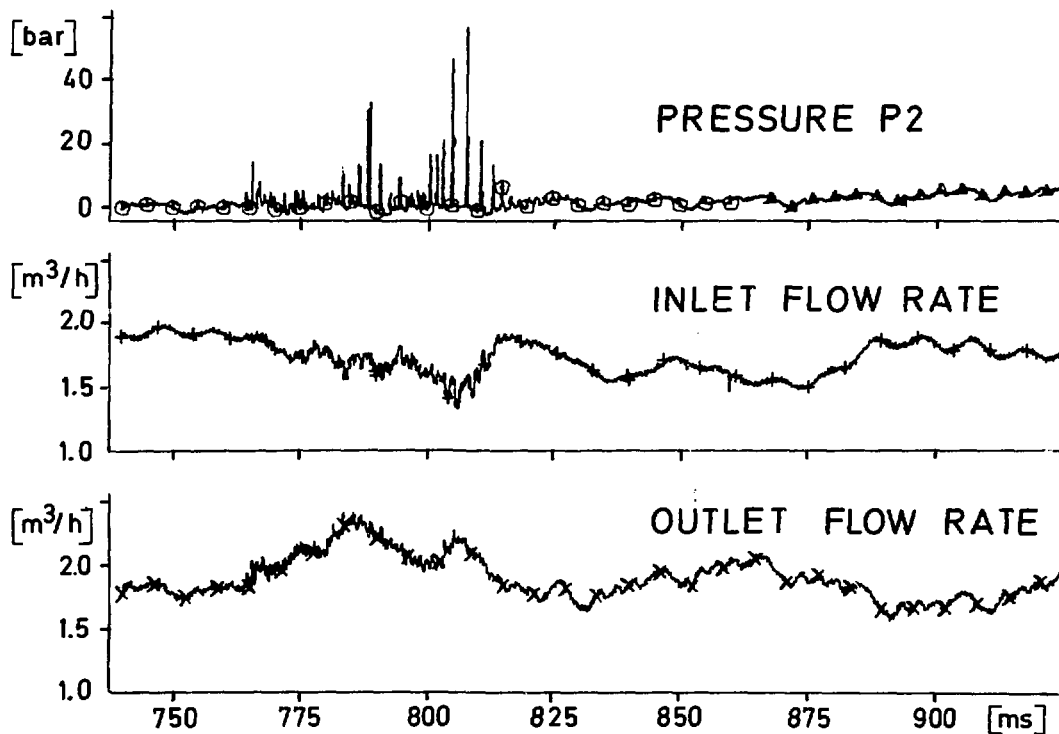


Fig. 4 Typical Measured Data during Fuel Coolant Interaction

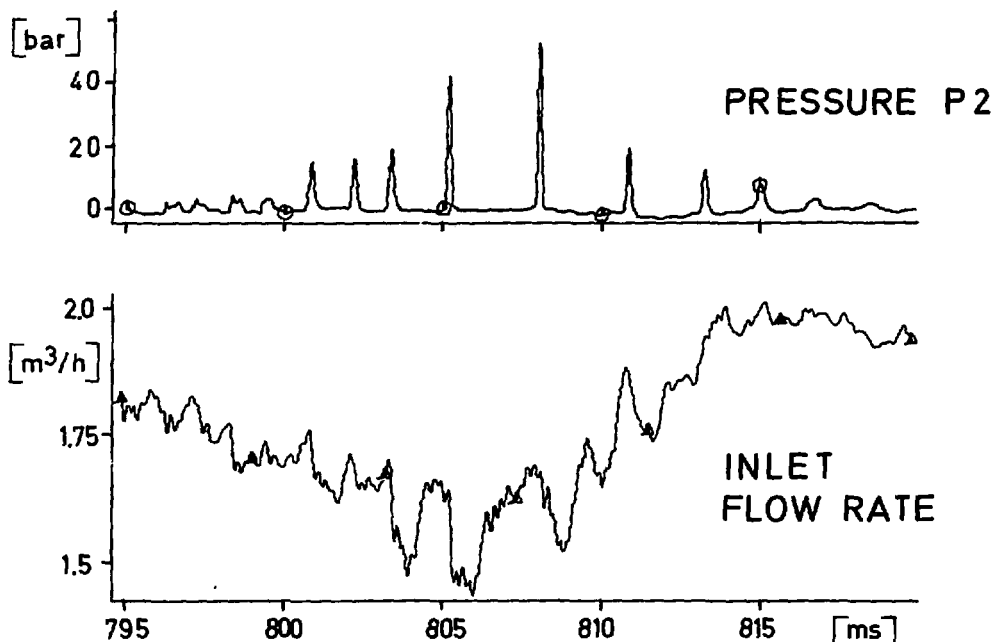


Fig. 5 Pressure and Inlet Flow Rate on an Expanded Time Scale

occurrence of bubbles. On Fig. 5 a 20 ms interval is plotted on an expanded time scale, which gives a clearer impression of the profiles. In addition, small and fast flow variations correspond with single pressure pulses, at which one has to take into account the transition time of the pulse.

The pressure pulses have quite high amplitudes and their half width is in all experiments about 0.1 to 0.2 ms. A possible interpretation for the origin of these very steep pressure pulses is in our opinion the collapsing of sodium vapor bubbles in the coolant flow with a temperature of 520°C. In the existing tests the static pressure within the test section was about 2 bar corresponding to a sodium boiling point of 962°C.

We have made the following observations regarding the height of these pressure pulses: At a very low gas overpressure in the fuel pin the maximum amplitudes reach 10 to 20 bar, whereas for an overpressure of about 2 to 3 bar amplitudes have been measured up to 70 bar. One of the reasons should be the differing volume of collapsing sodium vapor bubbles. Both the outlet velocity of the liquid fuel and its temperature can play a part in this process. Both values increase with increasing inner gas pressure. As a matter of fact, it has appeared that the duration of the transient and hence the fuel temperature rise with the inner gas pressure.

On the other hand, such pressure pulses were no more observed at an inner gas pressure of e. g. 32 bar. It can be assumed that the gas escaping from the fuel pin in larger quantities impedes the fast heat transfer between the fuel and the sodium.

The number of pressure pulses depends on the mass of the fuel coming into contact with sodium. This was clearly shown when we proceeded from single pin experiments to 7-pin bundles. The overpressure of the filling gas in the fuel pins has an indirect influence. At very low overpressure when the duration of the power transient is comparatively short, the penetrating quantity of fuel is small. At about 2 to 3 bar overpressure again we find a maximum regarding the number of pressure pulses.

Within the range under consideration the coolant velocity did not exert a clear influence on the pressure pulses. At any rate we could not find any correlation between the amplitudes of the pressure pulses and the coolant velocity.

The effect of different sodium temperatures together with the steady-state pressure in the cooling channel still wait for an examination. We hope that our assumption made on bubble collapsing as a cause of these pressure pulses can be supported by these investigations.

In the experiments with low gas pressure inside the pins the observation was made that the pressure pulses described above are followed by further pulses having different profiles (Fig. 6). Comparatively flat pressure rises of few bars height and several milliseconds duration occur which are overlayed by several narrow pressure peaks. Synchronous flow oscillations can be observed. These events perhaps could be explained by sodium entering the damaged zone and evaporating quickly. The vapor recondenses as single bubbles in the subcooled sodium flow.

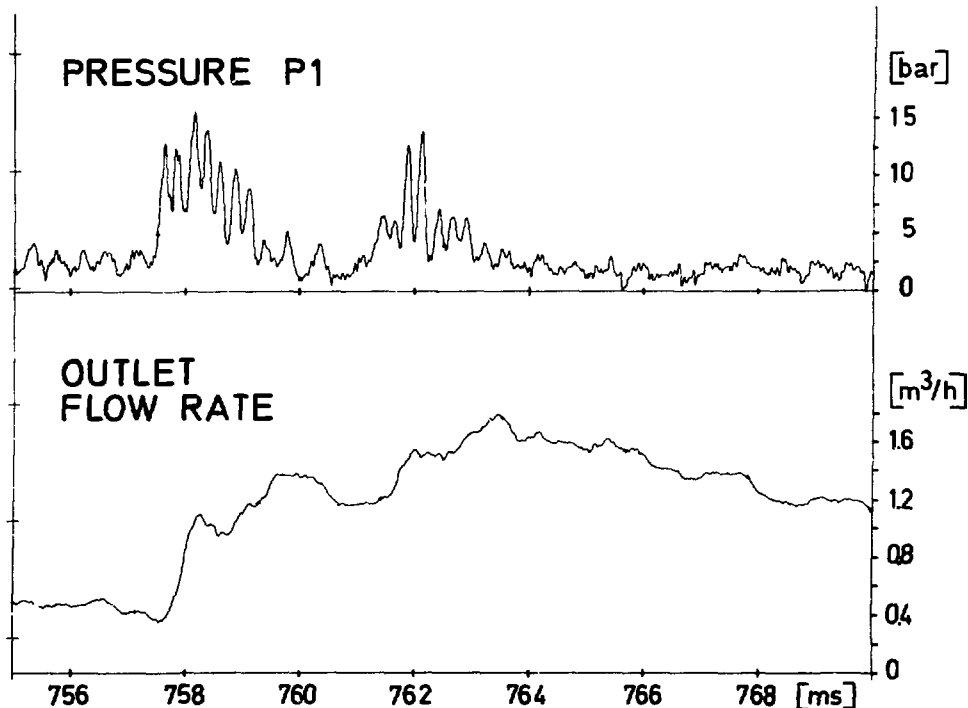


Fig. 6 Pressure Profiles and Resulting Flow Rate in a Later Phase

Regarding the effect of the pin damage on the coolant flow area in the test section we observed the following:
In experiments with single pins surrounded by a relatively large cooling channel most of the molten material is washed away and does not cause a permanent disturbance of the flow.

On the other hand, in 7-pin bundles having a reactor specific cooling channel geometry relatively grave destructions take place at the fuel pins (Fig. 7). Considerable deformations and deposits of solidified material cause a strong blockage reducing the coolant flow rate in one case to about 20%.



Fig. 7 View of the Damaged Region (Seven Pin Bundle)

In the course of test evaluation also the energy conversion efficiency should be determined. Mechanical work at the coolant is done both by expansion of the fuel pin filling gas and by the pressure pulses. The contribution of the work done by gas can be subdivided as follows:

- increase of the kinetic energy of the sodium columns
- increase of the potential energy of the sodium columns
- friction work
- work to overcome the pump pressure head
- compression work at the gas plenum in the expansion vessel.

In the analytical description the coolant can be considered as incompressible since the expansion of the filling gas is a relatively slow process. The influence of the narrow pressure pulses was suppressed by filtering the pressure and flow records.

The pressure pulses originating from the reaction zone propagate as pressure waves within the whole sodium circuit. Besides they generate short, fast variations in the values measured by the flowmeters since the propagation of pressure waves is accompanied by displacement of the liquid particles. The analytical description of the mechanical work of pressure pulses is based on the theory of plane wave propagation in compressible media.

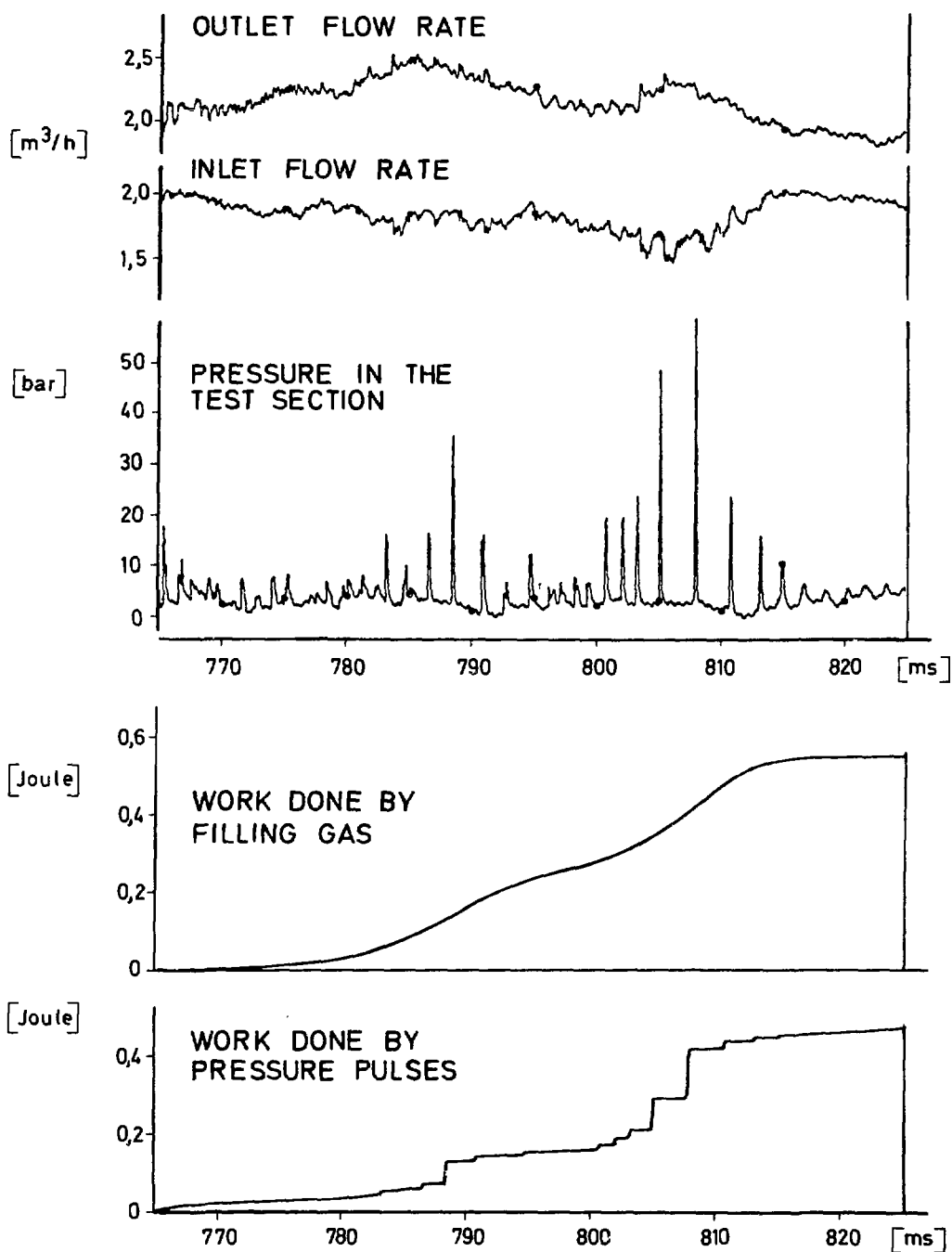


Fig. 8 Mechanical Work versus Time for a Typical Single Pin TOP Experiment

In Fig. 8 both the work of the filling gas and of the pressure pulses have been plotted versus time for a single fuel rod experiment. In the first case the smooth plot is characteristic, whereas the discrete pressure pulses result in an abrupt rise of mechanical work. In this example with a filling gas overpressure of 3 bar in the fuel pin the two contributions are about 0.5 Joule each. The work of a single 50 bar pressure pulse of the first type is only 0.1 Joule. The second type of pressure profiles has an energy content higher by the factor 2 to 3 due to its larger width.

By relating the total mechanical work to that amount of energy which is required to heat up the ejected fuel from the sodium boiling temperature to more than the UO_2 melting temperature, an energy conversion efficiency of about 0.04% is obtained. This value seems to be well comparable with results obtained in other experiments of this type.

CONCLUSION

We think that our out-of-pile facility can make a valuable contribution to the study of the fuel-coolant interaction in power transients. There is relatively small expenditure involved for the individual experiments. The interpretation of the very fast events is rather difficult on the basis of measured values and post-mortem investigations. More experiments including parameter variations have still to be carried out and additional evaluations will be necessary. The following statements can be made with respect to the results presently available:

A larger number of steep pressure pulses with symmetrical shape are measured with their half widths always at about 0.1 to 0.2 ms. In our opinion there are arguments that they are caused by the collapsing of sodium vapor bubbles.

The overpressure of the fuel pin filling gas influences the amplitude of the pressure pulses. At some 2 to 3 bar there seems to be the maximum of pressure peaks attaining about 70 bar. At 32 bar filling gas pressure no pressure pulses of this kind could be found.

The increasing amount of ejected molten fuel enhances the number of pressure pulses but not the amplitudes of these pulses.

The mechanical work done at the coolant during fuel-coolant interaction is very small. We find values of the order of 0.1 Joule/g of molten fuel, leaving aside the expansion work of the filling gas.

Experiments performed with 7-pin bundles reveal the generation of a strong cooling channel blockage.

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Aerosols and Fission Product Release

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ON THE STATE OF THE ART IN AEROSOL MODELLING FOR LMFBR SAFETY ANALYSIS

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ABSTRACT

The problems of aerosol formation, aerosol transport behavior, aerosol retention systems and (eventually) release to the environment from core disassembly accidents and other sources are of high significance in LMFBR safety. Beginning with the early models developed in the mid 60's much work has been done since to improve understanding of aerosol behavior in LMFBR containments. Present modelling theories comprise essentially the US code HAARM-2 and the German code PARADISEKO-3. The status of aerosol modelling, the verification by experimental programs and the remaining uncertainties are discussed.

INTRODUCTION

In the case of large core disruptive accidents in a LMFBR several aerosol sources will be formed releasing radioactive material into the inner and outer containment space. In the early studies [1] the primary aerosol source from vaporized core materials was identified as the most important problem of activity release. Since, according to today's discussion, several aerosol sources have to be taken into account the modelling of aerosol behavior has become more complex.

In the following the state of the art on aerosol modelling is discussed. Emphasis is put on the problem that existing and future improved models must be experimentally verified and must be developed consistently with theoretical aerosol physics.

AEROSOL SOURCES AND CONTAINMENT SYSTEM

The significance of aerosol sources which might occur in heavy LMFBR accidents and should be investigated and thoroughly understood depends on

- the course of accident
- the materials involved and
- the lay-out of the containment system.

There are essentially two processes of aerosol formation which play an important role in LMFBR safety analysis, the accidental vaporization of core materials and subsequent recondensation and the accidental vaporization of

coolant material (including trace constituents) and subsequent recondensation (Na-aerosols) or chemical reaction (Na_2O -/NaOH-/ Na_2CO_3 -aerosols). Depending on many parameters of the accident considered the various aerosol sources will combine and form as function of time a "mixed aerosol source" consisting of different chemical compounds, having variable physical characteristics (density, size, etc.) and therefore having different aerosol behavior. The occurrence of specific aerosols can be influenced by containment system design. For example, aerosols containing NaOH can be oppressed by avoiding in the containment water vapor and heated concrete structures which release water at higher temperatures. On the other hand emphasis on tank integrity would shift present assumed aerosol releases from the primary system to other release patterns.

In the course of the SNR-300 development four aerosol sources have been identified which are given in Fig. 1.

FIGURE 1

Aerosol Sources in Accident of IMFER

Source:Origin	Composition	Time of Formation	Initial Particle Diameter	Remarks
core disassembly	UO_2 - PuO_2 , fission products, steel, sodium	instantaneous	$<0.08 \mu\text{m}$ (increasing)	high initial concentration
Na-residual O_2 reaction	Na_2O	instantaneous	$0.1 \mu\text{m}$ (increasing to ca. $0.4 \mu\text{m}$)	
Na-evaporation from core catcher	Na	long term (days)	$0.1 \mu\text{m}$ (increasing to ca. $0.3 \mu\text{m}$)	recondensation aerosol with trace fission products and fuel
H_2O -evaporation from concrete	NaOH	long term (days)	?	

These sources have different chemical composition, different physical characteristics and time functions of formation. In respect to the formation time we have two time regimes identified namely the instantaneous sources (minutes) and the long term sources (days). Despite the fact that all aerosol sources represent very different activity concentrations all must be taken into account for dose calculations due to their different formation time and concentration versus time behavior. Investigations on the source strength, particle characteristics and other properties of these aerosols have been carried out at GfK and are reported at this conference [6], [7]. It should be emphasized again that these characteristic properties might change with the change of containment system design.

AEROSOL MODELLING

In modelling behavior of these aerosol systems in closed low leakage containment systems we should bear in mind the two groups of aerosol sources (instantaneous and long term). Studies on the instantaneous source were initiated early in the 60's and led to the computer codes HAA-3 B [8] and PARISEKO-III [3]. Both dealt mainly with the instantaneous source. Both codes were tested against experiments on aerosol concentration time function in smaller vessels. PARISEKO-III was tested and improved by the TUNA program [2] where UO_2 aerosols, produced by the exploding wire technique, were investigated in a 2.2 m³ vessel with a nitrogen atmosphere of varying gas temperature. The most important results of the TUNA program have been reported in [9]. These results led to the incorporation of essentially four aerosol processes in the PARISEKO code, namely

- coagulation (Brownian and gravitational)
- sedimentation
- thermophoresis and
- diffusion.

Another important result was the necessity to introduce form factors into the PARISEKO theory. These form factors describing the deviation from behavior of a spherical particle in comparison to the real irregularly shaped particle related to the aerosol process considered (f =collision form factor relates to coagulation, K =dynamic form factor relates to sedimentation, thermophoresis and diffusion) appeared to be the focal point in the aerosol modelling theory. This is illuminated by Fig. 2. The solid curves are the envelopes of the range representing all experiments carried out in TUNA at room temperature. The best fit was achieved by using $f=8.2$ and $K=3.5$ which is at least for K in accordance with comparable literature data (see [9]). The same form factors were also applied to experiments in TUNA at elevated temperatures showing good agreement between experiment and theory. Also for small chamber (0.02 m³) experiments the same K and f values gave the best agreement.

It should be mentioned, however, that in all PARISEKO calculations for the particle material the theoretical density was assumed. This is not in agreement with the experimental findings for UO_2 aerosols forming usually a porous structure. There exists, however, certainly a close correlation between particle form factors and particle density supposing a combination of both parameters might be more suitable to aerosol theory. Certainly the form factors are of great importance in aerosol modelling and cannot be neglected if the behavior of irregularly shaped particles is described.

In the US the HAA-3 B code [8] was developed and improved in recent years. The latest version, the HAARM-2 code, includes essentially the same elementary aerosol processes and uses the same integro-differential equations. H. Jordan [4] made a comparison between HAARM-2 and PARISEKO-III. He identified three essential differences between both codes, namely

- HAARM-2 accounts for the temperature and pressure dependence of the Knudsen number (ratio between particle radius and mean free path of carrier gas molecules) and viscosity, PARISEKO-III does not.
- PARISEKO-III includes two shape factors (K and f), HAARM-2 includes the apparent reduced density of a porous particle only.
- HAARM-2 employs the theoretical solution for the collision efficiency of a spherical particle in viscous motion. PARISEKO-III takes the collision efficiency as an input constant.
- HAARM-2 assumes the particle size to be lognormally distributed for the whole period of time under consideration allowing very short computation times. PARISEKO-III makes no restrictions on the particle size distribution at the expense of long computation times.

The discussion of the advantages and disadvantages of these different

features of both codes is beyond the scope of this paper. However, the last point (assumption of lognormal size distribution throughout the considered time period in HAARM-2) poses the problem of proving the validity of this assumption. Concerning the complex aerosol system discussed in the first chapter multimodal distributions should be expected which then cannot be modelled by HAARM-2. For simple systems with one instantaneous aerosol source in relatively small volumes, however, both codes seem to apply fairly reasonable as can be seen from Fig. 3 (taken from [4]).

A final test whether the theoretical assumptions made in a code for aerosol modelling are correct and lead to a description of the real accidental aerosol system with sufficient accuracy can be made only by comparison with experiments. In the frame of the FAUNA program, starting early 1977 at Karlsruhe, a larger vessel (about 200 m³) will be available to make the necessary experiments to answer that problem. Preliminary calculations applying the PARADISEKO-III code to the sodium burning experiments in the CEN Cadarache (Cassandre program, see [5]) have been carried out. One typical result is given in Fig. 4 where the sodium-oxide aerosol concentration time function is compared with a PARADISEKO-III calculation [10]. The experiment was performed in the 400 m³ containment facility at Cadarache employing the burning of 40 kg of sodium. Here, of course, other form factors than for UO₂ aerosols should apply which are given in Fig. 4. It is important to note that the time dependent source as experimentally measured was introduced into PARADISEKO-III code. It is not fully understood whether the typical result of PARADISEKO-III curves being always by some factor above the experimental curves relates to the form factors assumed ($f=1.0$ and $\kappa=1.0$) or to the neglect of gravitational coagulation which was not taken into account in these calculations. Since the first comparisons between PARADISEKO-III calculations with time dependent aerosol sources in large volumes [10] revealed differences more than a small factor much more work has to be done on aerosol modelling. It is true, that the theoretical results lie at higher values than the experimental ones, therefore, from the safety point of view on the safe side. However, with the increasing complexity of the aerosol systems under considerations this might not anymore be defensible for real LMFBR containments.

SUMMARY

Concerning the behavior of nuclear aerosols in a LMFBR post-accident atmosphere the present state of knowledge can be described as follows:

- The time function of an instantaneously formed aerosol concentration in a contained system can be satisfactorily described by existing codes (PARADISEKO-III and HAARM-2), if some from standpoint of aerosol physics reasonable assumptions (form factors etc.) are made.
- Since the aerosol system in a core disassembly accident is formed of several sources (consisting of UO₂, PuO₂, Na, fission products, Na₂O etc.) the interaction of these sources must be taken into account. More experimental information on the chemical and physical characteristics of these sources and the developing aerosol system (mixed aerosols) is necessary.
- PARADISEKO-III is capable to calculate aerosol behavior of several timely overlapping sources. Experimental confirmation of these results is not yet sufficient and should be provided for large containment volumes.
- Present existing codes have been developed on the basis of small vessel results only. It is known that in large vessels thermal convection and condensation of sodium vapor plays a role. The influence of these effects needs to be investigated and incorporated in present aerosol modelling theories.

ACKNOWLEDGEMENT

A number of arguments presented in this paper are based on discussions

with colleagues in the Laboratory of Aerosol-Physic and Filter-Technology and abroad. In particular the valuable contributions of H. Bunz, H. Jordan, L.D. Reed and J. Fermandjian are greatly appreciated.

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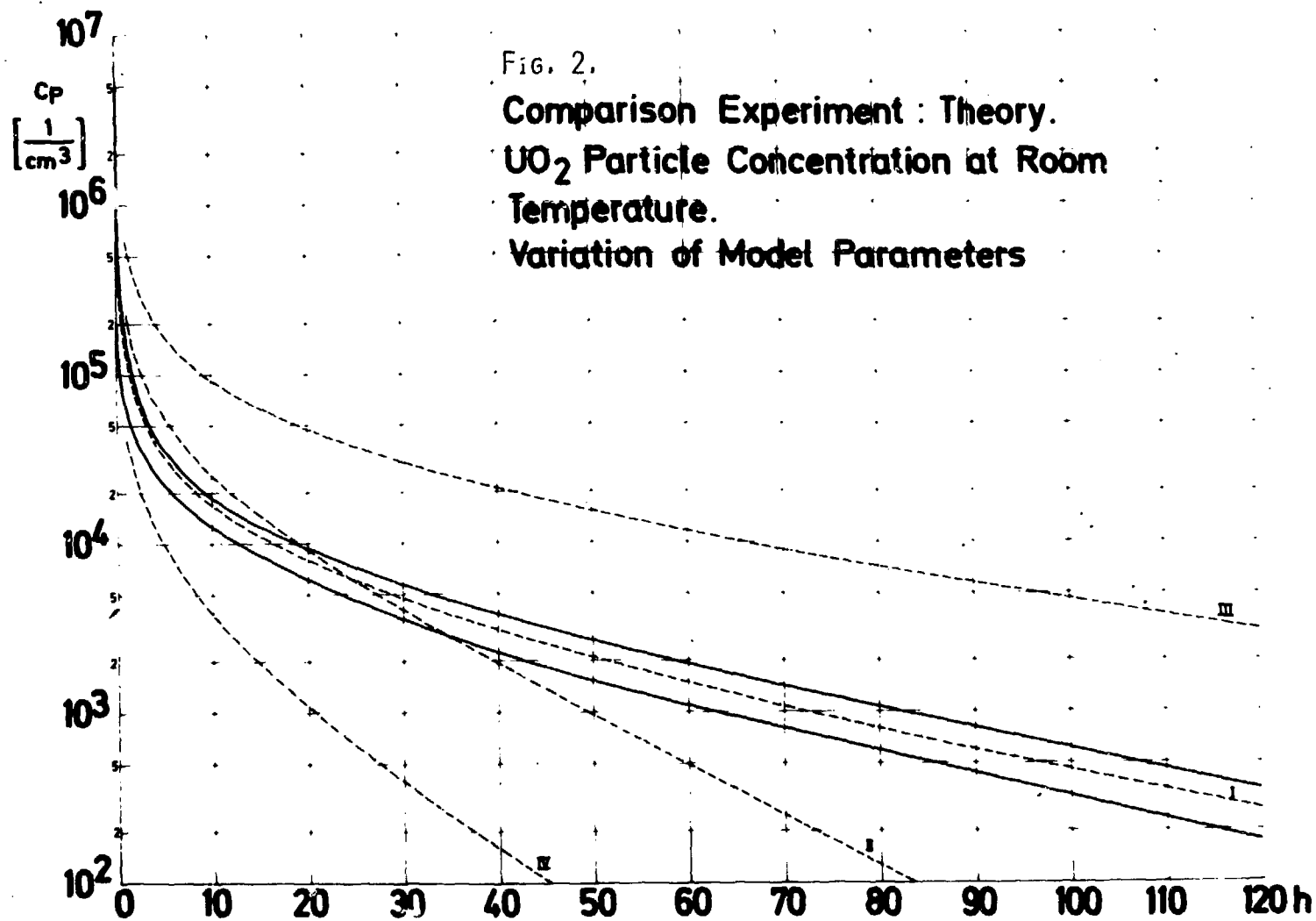
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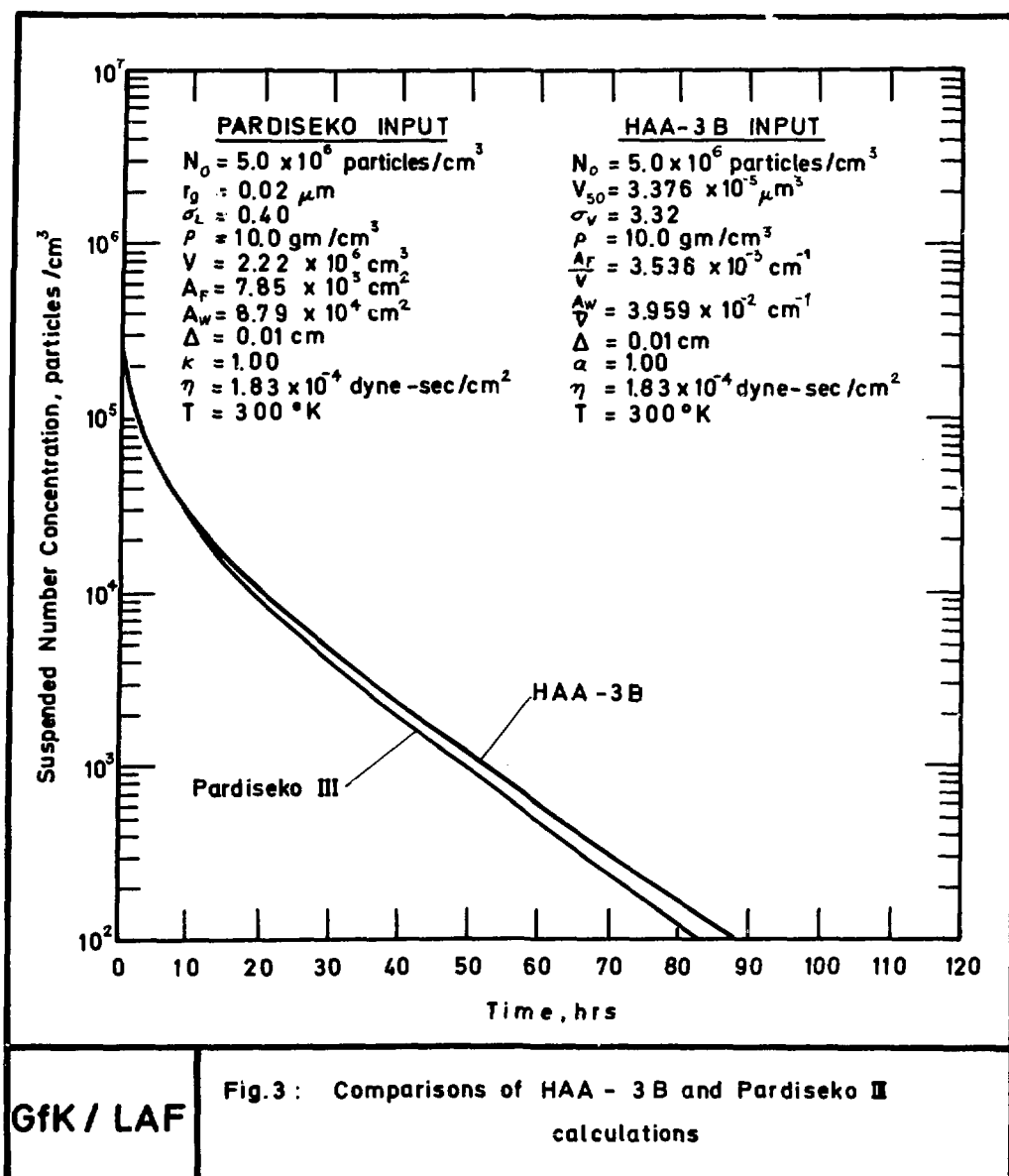
Fig. 2.

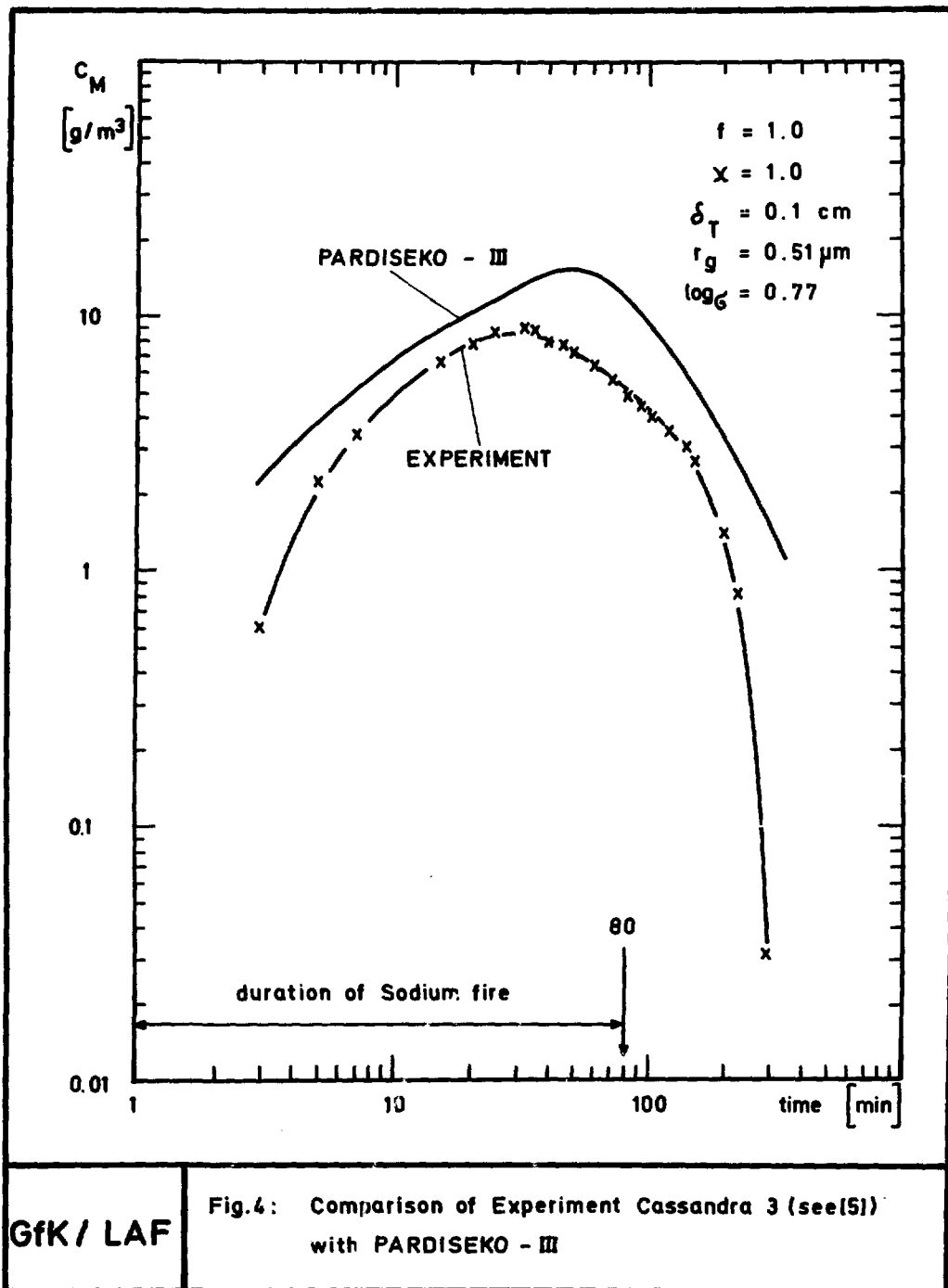
Comparison Experiment : Theory.

UO₂ Particle Concentration at Room Temperature.

Variation of Model Parameters







DEPOSITION OF AEROSOLS FORMED BY HCDA DUE TO DECAY HEAT TRANSPORT IN LMFBR INNER CONTAINMENT ATMOSPHERES

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ABSTRACT

The role of heat transfer related processes governing aerosol behaviour after core melt down in LMFBR containments is discussed. Useful and experimentally supported equations for calculation of aerosol decay due to thermophoresis or condensation coupled settling are given. The important aspect of spatially homogeneous distribution of aerosol in the containment space is treated.

Comments are made about existing computer models for safety evaluation of nuclear aerosols.

INTRODUCTION

After a core disruption or core melt down of a nuclear reactor, heat transfer by the inner containment atmosphere influences aerosol behaviour in various ways. As aerosols comprise a very important part of the source term of radiological risk of these hypothetical accidents, it is worthwhile to consider the heat transfer related aerosol processes. These processes are:

- stirring of the containment atmosphere by natural convection leading to drag forces on the particles,
- diffusiophoresis (aerosol transport in a vapour concentration gradient),
- photophoresis (aerosol transport in a radiation field),
- thermophoresis (aerosol transport in a temperature gradient),
- particle growth by condensation.

In this paper these processes will be treated, although some of them only introductory. Also other aerosol processes commonly used in computer codes, will be compared and influences of heat transfer related processes will be discussed.

STIRRING OF CONTAINMENT ATMOSPHERE

In nuclear aerosol safety it is generally assumed that the aerosol contents of the safety containments are well mixed apart from boundary layers at the walls where aerosol deposition takes place. It is of utter importance for the aerosol deposition rate whether in "reality" the aerosol particles are transported to these boundary layers by thermal convective flows or other, slower processes. When the containment atmosphere is insufficiently mixed the description of the three-dimensional aerosol transport becomes rather complex. Inhomogeneous spatial aerosol distribution can be either due to stable strati-

fication of the confined atmosphere or due to aerosol particle settling velocities too large compared to the thermal convection flows.

Stable stratification arises when the heat source is in the upper part of the containment or when the lower part is strongly cooled, two unreal cases. However, stable atmospheres have been also observed in some cases of vessels with a heated pool of liquid on the floor. Study of 12 different vapour/gas systems in such vessels has revealed that stable stratification arises only when $(M_d - M_g)X_d$ is positive and very large. M_d and M_g are the molecular masses of the vapour and gas, resp., X_d is the mole fraction of vapour in equilibrium with liquid at gas temperature. The complete limiting condition predicting the stability of an enclosed atmosphere above a heated liquid can be found in [1]. From this it may be concluded that the atmosphere of inner containments of sodium-cooled reactors is stirred under post-HCDA conditions (where material producing decay heat will collect in the lower parts of the containment).

The second case of inhomogeneous spatial aerosol distribution (dominating settling rate) occurs when gravitational forces on aerosol particles are larger than the drag force from the stirring natural convection, i.e. for coarse aerosol particles. For an aerosol filled containment heated locally with power Q , a diameter $d_{\max}$ of aerosol particles can be defined which aerosol is just well-stirred. Combination of the equations of Stokes, Blasius and Newton leads to the following relation for $d_{\max}$:

$$d_{\max} = 3 \left(\frac{\eta}{\rho} \right)^{\frac{1}{2}} \left(\frac{\epsilon Q}{\lambda l g} \right)^{\frac{1}{4}} \quad (1)$$

For a reactor containment under post-accident conditions $d_{\max}$ can be calculated to be some hundred microns. Since such coarse particles are not expected, the containment will be well-stirred with respect to the confined aerosol. This means that atmospheres of containments under post-accident conditions are well-stirred due to convective decay heat transfer.

DIFFUSIOPHORESIS AND PHOTOPHORESIS

Current reactor design aims at covering of a dispersed core by coolant, which means transfer of a considerable part of the decay heat by evaporation. At the cold walls where subsequent condensation takes place, diffusiophoresis may be important.

Photophoresis can become important when heat transfer is mainly by radiation which will be the case at higher temperatures. Deposition of radiation absorbing particles will be at cold walls viewed by the heat source.

Equations describing diffusiophoresis and photophoresis are available ([2] and [3] resp.), however, these subjects are not treated in this paper.

THERMOPHORESIS

As shown, the atmosphere of a locally heated containment is well-stirred. Therefore, transport of heat and aerosols is mainly due to thermal convection. At the boundary layers near the heat sinks, the cold walls, heat transfer is by diffusion. The resulting temperature gradient gives rise to thermophoresis of aerosols from hot to cold, with a velocity proportional to this gradient. The aerosol will decay exponentially with a decay constant β_{th} due to thermophoretic deposition

$$\beta_{th} = \frac{\alpha}{\lambda} \frac{Q}{V} = 0.5 \frac{Q}{V} \quad (2)$$

From Stetter's theory [4] on the relation between α and the diffusion coefficient of the gaseous phase and from simple gas kinetic theory it may be con-

cluded that $\frac{\alpha}{\lambda_g} = 0.5 \text{ cm}^3 \text{ W}^{-1} \text{ sec}^{-1}$ [5]. The value of $\frac{\alpha}{\lambda_g}$ is almost independent of aerosol material and particle size, and does not vary with temperature.

At the last ANS conference on Fast Reactor Safety [5] evidence was presented of the validity of (2) for a wide range of containment volumes (75 litres up to 20 m^3) and geometries. Recently, we performed experiments in the SAUNA-vessel (about 150 litres, to be described later on) in order to explore the validity of the mentioned temperature independence of equation (2). In fig. 1 the measuring data from SAUNA which cover the range from 90°C up to 700°C , is plotted together with the measuring data presented earlier. Also these new data fit the earlier relation. In all cases of low to moderate initial aerosol mass concentration (some 0.1 g m^{-3} or less) an exponential decay is observed, which is in good accordance with the size independence of thermophoresis. The aerosols used were all polydisperse aerosols mainly from Exploding Wires.

Equ. (2) describing aerosol mass decay due to HCDA decay heat transfer by the containment atmosphere is a very simple tool for aerosol safety calculations and is therefore useful in this field.

AEROSOL DECAY IN CONTAINMENTS WITH A HEATED POOL OF COOLANT

The inner containment of LMFBR's must be filled with a gas inert to sodium in order to prevent important oxidation of sodium in case of HCDA. Therefore, under post-HCDA conditions the inner containment will contain the dispersed core immersed in hot sodium which may even boil initially in absence or failure of core catcher cooling.

The atmosphere inside an enclosure with a heated layer of liquid on the floor is supersaturated with vapour of this liquid. This is a general experience from studies on a large number of such liquid/gas systems. The supersaturation, although experimentally difficult to measure, can be observed simply from the fog formation after introduction of aerosol particles. This fog formation also explains the increased removal rate of aerosol in condensing atmospheres compared to the rate in dry atmospheres (fig. 2). The aerosol particles act as condensation nuclei and may grow to sizes with relatively large gravitational settling rates due to continuous material transport from the evaporating liquid to the particles. This process of aerosol removal by condensation can be quantified in the following way. The growth of the aerosol particles due to the continuous condensation on them is given by

$$c_m \frac{dR^3}{dt} = E \quad (3)$$

in which

$$E = \frac{3Q_v M_m}{4\pi\rho \Delta H_v V} \quad (4)$$

where m = average mass of original aerosol material in one droplet. Increase of droplet radius due to condensation leads to an enhanced settling rate in agreement with the observations such as represented in fig. 2. In addition to this, there are visual observations at special illumination with a laser beam on the boundary layer near the floor which confirm this.

According to Stokes' law the mass concentration decreases according to

$$\frac{dc_m}{dt} = -AR^2 c_m \quad (5)$$

where

$$A = \frac{2\rho g}{9\eta n} \quad (6)$$

An exact solution of (3) and (5) is not possible. An approximate solution can be obtained using the following conservative assumptions:

- the radius of the droplets formed immediately after the initial fog formation is equal to zero (no gravitational settling up to that point of time),
- before condensation starts, the aerosol formed by the excursion has a negligible settling rate,
- Q_v is no function of time (Q_v can be given a conservative value).

This yields

$$c_m^{2/5} = \{c_m(0)\}^{2/5} - Gt \quad (7)$$

with

$$G = \frac{2}{5} \left(\frac{5}{3} E\right)^{2/5} A^{3/5} \quad (8)$$

(4) and (6) give E and A.

The usefulness of (7) was explored by means of experiments with aerosols in condensing vapour of either water or sodium. Table I gives a survey of the various aerosol vessels used. The SAUNA-rig was specially built for aerosol experiments in condensing sodium vapour (fig. 3). The SAUNA cylindrical aerosol vessel of about 150 litres contains about 10 litres of sodium on the floor and is filled with nitrogen. This pool of sodium can be heated up to 750 °C; higher temperatures, including boiling temperatures, are under consideration. Exploding Wire aerosols can be introduced by flushing the content of the formation vessel (EW chamber) into the aerosol vessel. Due to traces of oxygen and water in this aerosol, traces of additional sodium oxide aerosol will be formed too. The radiotracer techniques used for aerosol analysis, however, were specific for the Exploding Wire material. The sodium oxide aerosol being of low mass concentration has not been taken into account. No influence on the aerosol decay has been observed.

Recent experiments at E.C.N. using aerosols mainly from Exploding Wires, have yielded measuring data showing that $c_m^{2/5}$ decays linearly with time (figs. 4 and 5). Table II lists a number of experiments carried out in air/water or nitrogen/sodium systems. In all cases the aerosols, produced from Exploding Wires, were introduced into the supersaturated atmospheres and the mass concentration decay measured by filter sampling followed by radiotracer analysis. It may be concluded that the aerosol decay obeys equation (7) and that the system fulfils the requirements of mentioned assumptions as well.

Using flame scintillation counting, the decay was recorded of sodium oxide aerosol formed in SAUNA by flushing in nitrogen, in this case without Exploding Wire aerosol. Also these data shows a linear decay of $c_m^{2/5}$ (fig. 6).

A typical feature of the aerosol decay in condensing vapour is the finite life time of the aerosol, which is very interesting from the point of view of reactor safety. A certain period of time $t_{\max}$ after the aerosol is formed in a supersaturated atmosphere there is essentially no aerosol particle left, as has been observed visually.

$t_{\max}$ can be calculated from

$$t_{\max} = \frac{5}{2} \left(\frac{5}{3} E\right)^{-2/5} A^{-3/5} c_m(0)^{2/5} \quad (9)$$

Assessment of Q_v is one of the problems to be solved. For the particular case of condensing vapour Q_v was calculated from the G-values obtained from a number of experiments in various vessels (see Table II). Equations (4), (6) and (7) were used for this computation. As shown in this table, Q_v/Q (being the part of the total heating power dissipated into the vessel and used for condensational growth of the particles) increases for increasing vessel volume and Q. Therefore, a safe lower limit of 0.1 % can be taken for Q_v/Q from these data. Another unknown in (9) is the magnitude of m. However, (9) in fact contains

both $c_m(0)$ and m as their ratio which ratio is the initial aerosol number concentration $c_n(0)$. For $c_n(0)$ a value of 10^6 cm^{-3} can be taken independent of the real initial number concentration. Within a period of about 10 minutes, any aerosol of high initial number concentration will have decayed to 10^6 cm^{-3} due to coagulation. This transformation is also attractive in view of the deviation from the $2/5$ -th power decay observed at low heating powers (fig. 4) which must also be attributed to coagulation. Assuming a point of time of aerosol formation later than the HCDA event, solves conservatively this problem of the observed anomaly.

Using the given model it is now possible to calculate the aerosol lifetime $t_{\max}$ in the inner containment building of an LMFBR after an HCDA. The decay heat power is taken to be 5 MW (300 MWe reactor, e.g. SNR-300) of which 10 % is assumed to be dissipated to the inner containment atmosphere and of which 0.1 % is used for condensational particle growth. Further: $\Delta H_v(\text{Na}) \approx 10^5 \text{ J mole}^{-1}$, $V \approx 10^4 \text{ m}^3$ and $h \approx 10 \text{ m}$. This yields a $t_{\max}$ -value of about 30 minutes. Including the coagulation period of also 30 minutes, this means that about one hour after the HCDA essentially no aerosol particle formed by the HCDA is present in the inner reactor containment.

MODELS FOR AEROSOL BEHAVIOUR IN CONTAINMENTS UNDER POST-HCDA CONDITIONS

Various computer codes exist describing aerosol behaviour in LMFBR safety containments under post-HCDA conditions, like e.g. HAA-3A [6], PARDESEKO-3 [7], ABC [8]. There are a number of simplifications and approximations in these codes making their applicability to reactor safety doubtful.

Firstly, for two reasons aerosol particles are assumed to be droplets: simplicity of calculation, and lack of knowledge about the relation between aerodynamic behaviour and microstructure of non-spherical fluffy particles. This neglect of the dynamic shape factor κ plays a role in two important size dependent processes of these codes: coagulation and gravitational settling. Recently, we have found for Exploding Wire aerosols [9], consisting of aggregates of more than 5×10^3 primary particles per aggregate (representative for aerosols formed by HCDA) that the dynamic shape factor reaches a final maximum value amounting to about 15. This means that HCDA aerosol particles have an aerodynamic diameter considerably smaller than assumed hitherto on the basis of the droplet model which implies the use of volume equivalent diameters. The HAA-code uses a correction factor for non-sphericity which can be interpreted as a dynamic shape factor κ of $\sqrt{3}$. This is not only a relatively small κ -value but also the variation of κ during coagulation is not taken into account.

Secondly, the high density, decay heat producing dispersed core will collect in the lower parts of the inner containment. This results in heating of horizontal surfaces which generally are assumed to be available for gravitational deposition. Thermophoretic forces counteracting gravity will hamper deposition of particles on these surfaces. In order to investigate the importance of such conditions, decay constants for aerosol removal in a vessel with a heated floor were measured. Fig. 7 gives the results. With increasing power of the floor heating the decay constant initially decreases (aerosol stability increases) due to hampering of gravitational settling. At higher levels of heating power, the aerosol deposition is enhanced because thermophoretic deposition becomes overwhelming. From these experiments, it is clear that more attention has to be paid to the limited availability of the floor of containments for aerosol deposition due to floor heating.

Thirdly, experiments carried out for testing computer models must be performed under conditions as real as necessary. This implies among others that experiments using high aerosol mass concentrations require additional stirring in order to keep the aerosol spatially homogeneously distributed. Most realistic stirring can be achieved by means of local heating of the aerosol

containment. However, in that case long term behaviour of the aerosol will be influenced by thermophoretic deposition. Altogether, this is another argument for application of thermophoretic deposition or other heat transfer related processes in models for aerosol behaviour in containments under post-HCDA conditions. Computer models for aerosol safety evaluation must be tested by experiments under conditions simulating the actual accident.

SUMMARY

1. Heat transfer related aerosol transport processes (stirring, thermophoresis, condensation) play important roles in the fate of aerosols under post-HCDA conditions.
2. It is shown that natural convection will keep the aerosol stirred under various conditions.
3. It is certainly preferable to apply thermophoretic deposition to modelling for nuclear aerosol safety because of its simplicity: absence of temperature, aerosol material, particle size and concentration effects and simple relation to containment volume and decay heat power.
4. Condensation of coolant vapour on aerosols and subsequent settling of the droplet mist, can be made useful for incorporation into nuclear safety evaluation of aerosols under wet conditions.
5. Comments are made about existing computer codes based on aerosol models closely coupled to coagulation combined with gravitational settling. Among others incorrect assessment of the dynamic shape factor of "real" HCDA aerosol is indicated.

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PRINCIPAL NOMENCLATURE

c = concentration	X = mole fraction
d = particle diameter	α = proportionality constant according to [4] and [5]
g = gravitational acceleration	β = decay constant
h = height	ϵ = coefficient of thermal expansion of gas
ΔH_v = latent heat of vaporisation	η = gas viscosity
k = heat transfer coefficient	κ = dynamic shape factor
l = characteristic length (about $\sqrt[3]{V}$)	λ = thermal conductivity
M = molecular mass of condensing vapour	ρ = density
Q = (decay) heat power	
Q_v = heat released per second due to condensation on aerosol	
R = particle radius	
t = time	
V = containment volume	

Subscripts

d = diffusional, vapour
g = gaseous
m = mass
th = thermophoretic

TABLE I

Survey of Aerosol Vessels used

Name	Shape	Volume (m ³)	Height (m)	Wall material
PERVEX	box	0.15	0.6	Lucite, brass floor
100	vertical cylinder	1.2	1.5	steel
200	box	1.0	1.0	steel
300	box	0.075	0.5	stainless steel
ENAK	vertical cylinder	20	4.2	aluminium
GRACE	"	0.3	1.2	glass
SAUNA	"	0.15	0.6	Incoloy

TABLE II

Q_v Values calculated using (7) from Aerosol Experiments
performed in condensing Vapour in various Aerosol Vessels

Aerosol vessel	$Q(W)$	G ($kg\ m^{-3}$) ^{2/5} sec^{-1}	$Q_v(W)$	$\frac{Q_v}{Q} \times 100\%$	vapour/gas
ENAK	8×10^3	7.8×10^{-7}	45	0.56	water/air
ENAK	4×10^3	5.5×10^{-7}	18	0.45	water/air
ENAK	2×10^3	4.0×10^{-7}	8.5	0.42	water/air
ENAK	10^3	2.7×10^{-7}	3.2	0.32	water/air
GRACE	600	3.0×10^{-6}	2.8	0.46	water/air
GRACE	300	1.9×10^{-6}	0.87	0.29	water/air
GRACE	200	1.6×10^{-6}	0.57	0.28	water/air
GRACE	100	1.2×10^{-6}	0.28	0.28	water/air
200	900	1.2×10^{-6}	2.1	0.23	water/air
200	600	1.0×10^{-6}	1.3	0.22	water/air
200	400	8.3×10^{-7}	0.82	0.20	water/air
200	200	5.5×10^{-7}	0.29	0.15	water/air
PERVEX	60	1.5×10^{-6}	0.067	0.11	water/air
SAUNA	2×10^3	1.8×10^{-5}	480	24	sodium/N ₂
SAUNA	10^3	10^{-5}	110	11	sodium/N ₂
SAUNA	500	2.4×10^{-6}	3.0	0.6	sodium/N ₂

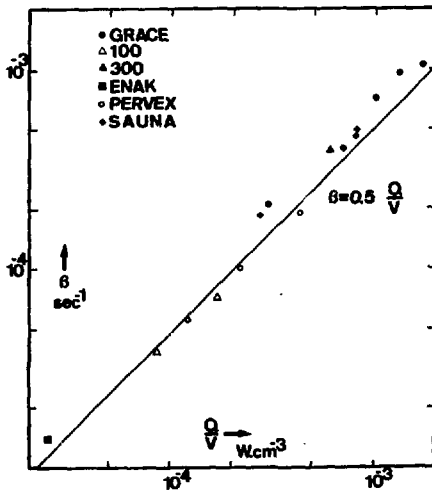


Fig. 1 Decay constants β of aerosols for various dry vessels and heating geometries as a function of specific heating power Q/V .

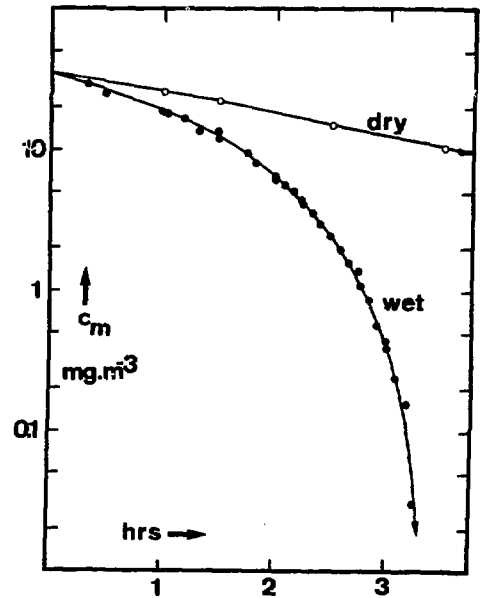


Fig. 2 Mass concentration decay (c_m in $mg\ m^{-3}$) of gold aerosol in pervex with water layer on the floor (•) and under dry conditions (○). 80 W floor heating power.

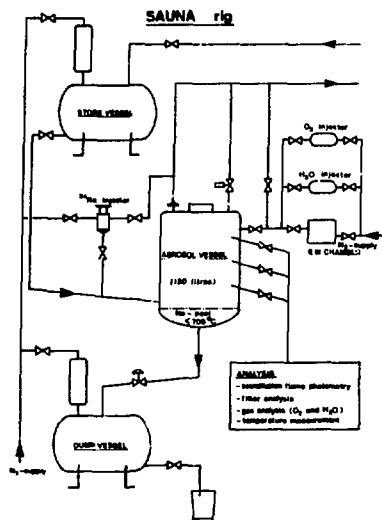


Fig. 3 SAUNA rig.

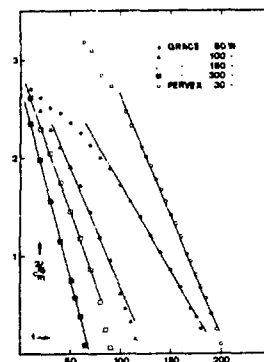


Fig. 4 Decay of aerosol mass concentration (c_m in mg m^{-3} ; t in minutes) in GRACE and PERVEX for various heating power levels of water layer.

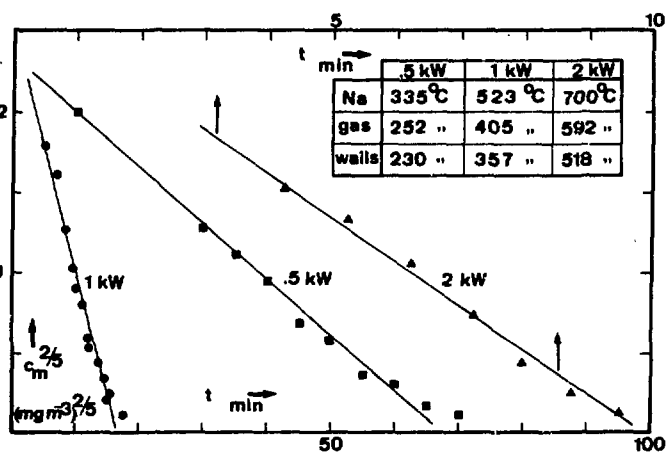


Fig. 5 Decay of aerosol mass concentration (c_m in mg m^{-3}) in sauna with heated sodium pool at various heating powers.

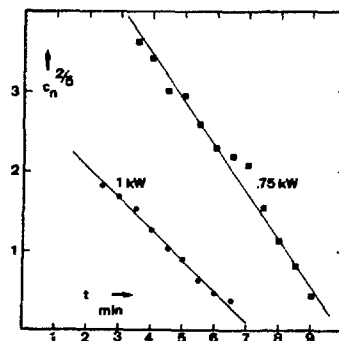


Fig. 6 Decay of aerosol number concentration (c_n in arbitrary units) in SAUNA with heated sodium pool at two levels of heating power.

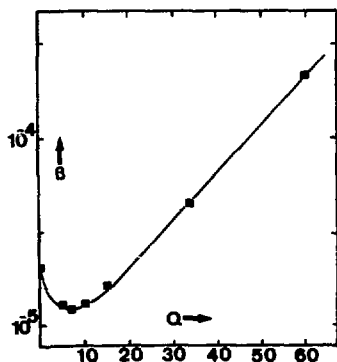


Fig. 7 Decay constants β (in sec^{-1}) of gold aerosols in PERVEX. The floor was heated with a variable power Q (in Watts).

FUEL PARTICLE AND FISSION PRODUCT RELEASE FROM LMFBR-CORE CATCHER

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ABSTRACT

During a postulated severe incident with core rupture at a sodium cooled fast reactor major quantities of fuel particles may be suspended in the liquid sodium contained in the core catcher. The release of fuel and sodium from a contaminated pool at 500 and 900°C was investigated by laboratory and technical experiments. Sodium vapor was released from a boiling sodium pool into Nitrogen atmosphere with a rate of 85 kg Na/m²·h. Decontamination factors for Uranium release from sodium pools were measured between 10³ and 10⁴ for sodium containing 1 weight percent UO₂ powder. The effect was found to be not strong dependent on temperature between 500 and 900°C.

INTRODUCTION

In case of a severe accident (Bethe-Tait) with core disrapture of a sodium cooled fast reactor the core may melt through the reactor tank and contained in an ex-vessel core catcher. There it is covered with hot or boiling sodium.

During the process of core meltdown considerable quantities of fuel particles and fission products may be suspended or solved in liquid sodium. Some of the fission products and presumably also fuel particles or reaction products of fuel sodium reaction are subsequently vaporized into the gas space and then released into the reactor containment.

For estimation of activity concentration in the containment it is important to know what quantities of sodium, fuel and fission products are released from hot sodium pools.

Even at core disrapture and temperatures as high as 6000°C it is assumed that most of evaporated or molten fuel is still in oxidized state [1-7]. Some of the evaporated fuel recondenses to particles and suspenses in the sodium, another part of the molten fuel may be suspended in sodium mechanically. Conservative calculations have shown that up to 7 - 8 kilograms of fuel might be suspended in one cubicmeter of sodium.

The release of fuel from sodium into the gas phase is possible in two ways: first, particles are ejected mechanically from sodium, for example by

*delegated from Hitachi Research Laboratory, Japan

collapsing bubbles or second, after fuel-sodium reaction as a compound with higher evaporation rate than UO_2 .

Since several years sodium uranates are well known. The reaction product Na_3UO_4 is known to be stable in the system $\text{Na}-\text{UO}_2-\text{Na}_3\text{UO}_4$ at oxygen concentrations of 0.4 - 0.9 ppm in sodium and temperatures above 500°C [27]. The theoretical density is measured to 5.6 g/cm^3 , the melting temperature is 1420°C .

There have been only few calculations in the past on fuel release from burning sodium pools. It was estimated that 10^{-5} of PuO_2 contained in the pool may be released into the environment [37].

To clarify this problem laboratory and technical scale experiments were performed to investigate the release of sodium, fuel and fission products into the nitrogen atmosphere of the inner containmen of a LMFBF reactor.

EXPERIMENTAL FACILITIES

At the laboratory scale experiments different quantities of uranium dioxide powder (mean particle diameter 20 micron) were suspended in about 100 grams of sodium and heated up to 500 to 900 degree centigrade. The evaporated sodium and fuel was either transported by a gas stream of Argon or by diffusion through a heated (200°C) pipe in two cold-traps cooled with liquid nitrogen. At the end of the open loop was a filter. The gas stream was measured by a gas-meter. A block diagram of this test facility is shown in Fig. 1.

The technical scale experiments were performed in the 4 cubicmeter closed stainless steel vessel "NABRAUS". A diagram of this facility is shown in Fig.2. Metallic sodium was molten in a burning pan in nitrogen atmosphere and mixed mechanically with uraniumoxid powder. The pan was then heated up to about 500°C . During the experiments probes of aerosols were drawn at different places of the vessel. The mass concentration of sodiumoxid aerosols was determined by washing out the aerosols in water with subsequent titration. Behind the scrubber a filter was placed to deposit particles not washed out by the scrubbers.

The uranium content of aerosol samples respectively the uranium content of sodium in the cold traps was determined by fluorometric technique. By this technique it was possible to measure an uranium content as low as 10 ng.

For both series of experiments the concentration of fuel in each (aerosol-) sample is compared with the concentration in the initial hot sodium pool and discussed in terms of decontamination factors DF which are the ratios of the fuel concentration in pool to the released fuel concentration in sodium aerosol:

$$DF = \frac{\left(\frac{m_U}{m_{Na}}\right)_{\text{pool}}}{\left(\frac{m_U}{m_{Na}}\right)_{\text{Aerosol, vapor}}}$$

EXPERIMENTAL RESULTS AND DISCUSSION

Three laboratory scale experiments have been carried out under boiling conditions: two with Argon gas flow, one without gas flow. Four experiments were carried out at 500°C and different Argon gas flow rates. Experimental results are placed together in table 1. All experiments were performed with

100 gram of sodium and either 1 or 10 gram of uranium dioxide suspended in sodium.

Decontamination factors were found nearly reproducible to 1500 ± 500 at sodium boiling conditions. For the first three experiments initial Uraniumdioxide concentration in reaction vessel did not change too much during experiments. No significant change of decontamination factor was found between the distillation experiments (1 and 2) and forced transport by Argon gas flow. A comparison of decontamination factor of experiment 1 and 2 with experiment 3 shows that the concentration of Uraniumdioxide in sodium has no strong effect on decontamination factor.

From the experiments 4 to 7 it can be concluded that decontamination factor does not change substantially with temperature.

From the experimental results it is possible to get a reference to the vaporization rate of sodium at boiling conditions. Analysing the experiments of table 1 the evaporation rate of sodium was found to be about $85 \text{ kg Na/m}^2 \cdot \text{h}$ at 900°C and about $300 \text{ g Na/m}^2 \cdot \text{h}$ at 500°C .

Separate experiments were performed to check the influence of oxygen (Na_2O_2) content of sodium on decontamination factor. No influence could be measured. Sodium-Uraniumoxide suspensions were preheated at 800°C five hours before each experiment. Longer preheating did not influence decontamination factor.

It is interesting to note that without gas flow most Uranium was found in the pipe and the first cold trap. With gas flow a great quantity of Uranium was found in the filter behind both cold traps.

Results of one technical scale experiment are shown in table 2 standing for two additional experiments with similar results. The oxygen content of the atmosphere above the sodium pool was in the first part of the experiment lower than 0.1 ppm. In the course of the experiment oxygen concentration was increased to 0.5 % and kept constant. Decontamination factor did not change substantially and was measured between 1000 and 5000. Later on the oxygen content was increased to 21 %. The decontamination factor increased to values about 10^4 . Temperature varied during the experiment between 460 and 650°C . Also at these experiments a large portion of the sampled Uranium was deposited in the filter behind the wet scrubbers but nearly no sodium was found in the filter.

It seems to be probable that sodium and Uranium evaporate separately from the pool and only parts of the Uranium particles are attached to sodium aerosols by coagulation. Therefore, it might be reasonable to calculate the Uranium content of atmosphere above a hot sodium pool. These values are evaluated from experimental results and shown in col. 5 of table 2. The airborne Uranium concentration is nearly constant for all samples and measured to $0.01\text{--}0.04 \text{ mg}_U/\text{m}^3$. Increasing temperature of pool surface for example by pool burning increases rapidly sodium aerosol formation but not as much the Uranium release as pointed out by higher decontamination factor measured at 21 % oxygen content of atmosphere.

In the range of error no significant temperature dependence of Uranium releases from sodium pool was found. This points to a evaporating compound boiling at much higher temperatures than sodium. We believe that sodium uranate Na_3UO_4 is responsible for Uranium release from contaminated pools.

From the increase of sodium aerosol concentration above the pool sodium evaporation rate can be estimated. At about $450\text{--}500^\circ\text{C}$ sodium evaporation rate was calculated to be $200 \text{ gr Na/m}^2 \cdot \text{h}$. Because of proportionality of evaporation

rate and vapor pressure this value is in good agreement with the value derived from laboratory scale experiments at boiling conditions.

SUMMARY

- Sodium evaporation rate into inert atmosphere was found to be $200 \text{ gr Na/m}^2 \cdot \text{h}$ at 500°C and $85 \text{ kg Na/m}^2 \cdot \text{h}$ at 900°C .
- A hot sodium pool contaminated with Uraniumdioxide particles releases Uranium into inert atmosphere above the pool with a decontamination factor of 1000-5000.
- No significant temperature dependence of decontamination factor between 500 and 900°C was measured.
- Sodium and Uranium evaporates separately into the atmosphere.

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TABLE I
Release of Uranium from a Sodium Pool Contaminated with UO_2 Particles

Experiment Nr.	Na g	UO_2 g	Temp. °C	Argon Flowrate l/min	Na trapped g	^{235}U trapped μg	DF
1	100	8,48	890	-	40,0	1580	2000
2	100	8,68	890	-	23,9	1230	1400
3	100	0,76	890	0,6	45,0	394	1100
4	100	0,84	500	17,0	12,3	42	2100
5	100	0,72	500	10,0	2,0	5	2470
6	100	0,95	500	1,0	0,9	4	1750
7	100	10,25	500	10,0	2,8	104	2450

TABLE II
Uranium Release from Contaminated Sodium Pool
(Containing 2000 gr Na, 20 gr UO_2 Particles)

Sample Nr.	Time min.	Oxygen Conc. %	Temp. °C	Aerosol Conc. mgNa/m^3	Uranium Conc. mgU/m^3	DF
1	5	0	360	67	-	-
2	20	0	470	1326	-	-
3	35	0	530	2910	-	-
4	50	0	550	8560	0,03	2800
5	61	0	550	6500	0,01	4500
6	75	0	480	2580	0,01	2500
7	85	0,5	460	3260	0,03	1000
8	92	0,5	460	3370	0,03	1000
9	105	0,5	470	3900	0,04	1000
10	115	21,0	610	7800	0,01	10000
11	120	21,0	670	9700	0,01	10000

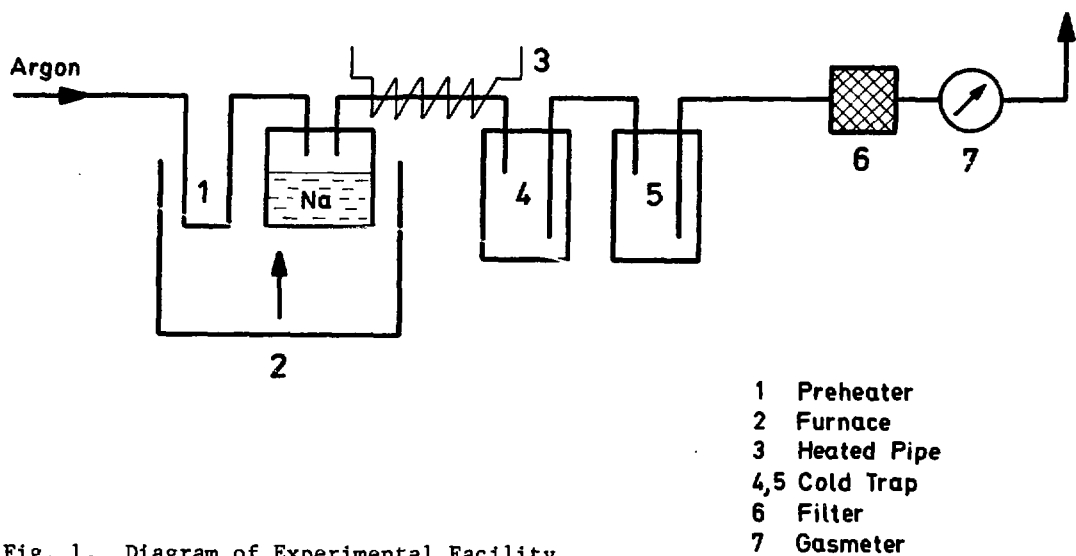


Fig. 1. Diagram of Experimental Facility

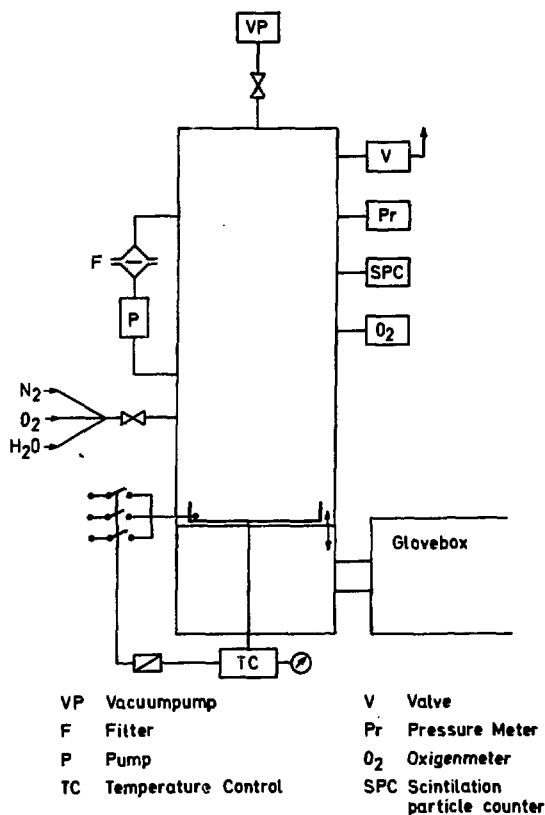


Fig. 2. Diagram of NABRAUS Test Facility

DEVELOPMENT AND APPLICATION OF CAPACITOR DISCHARGE
VAPORIZATION TECHNIQUE FOR FUEL AEROSOL STUDIES*

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ABSTRACT

Investigations into the behavior of LMFBR fuel as it responds to the levels of energy deposition projected for prompt-critical excursions have been limited because of the difficulty of experimentally achieving the resultant high thermodynamic states in a laboratory. In order to conduct "source-term" assessment and fuel aerosol behavioral experiments, the Aerosol Release and Transport program at ORNL has been developing a Capacitor Discharge Vaporization (CDV) technique for using electrical energy, via the discharge from capacitors, to achieve HCDA-like energy states in UO_2 . This paper reports the details of the technique, the developmental test results that have demonstrated feasibility, and a brief description of the proposed experiments that will use the CDV system for safety related studies.

INTRODUCTION

The defence-in-depth safety philosophy for Liquid Metal cooled Fast Breeder Reactors (LMFBR) requires assessment of the radiological consequences of hypothetical core disruptive accidents (HCDA) notwithstanding their low probability of occurrence. Such assessment involves determination of the quantities of radionuclides that move out of the core, escape from the primary vessel, and become available for release from the secondary line of containment. The characteristics and transient behavior of the released material are of interest as well as the quantities released.

It is customary to recognize two major paths for release accompanying an HCDA - for core material that rises up through the sodium and escapes through the primary vessel head, and for material that falls and melts its way through the vessel bottom. The Aerosol Release and Transport (ART) program [1,2] at Oak Ridge National Laboratory is currently concerned with assessing the radiological source term and transient aerosol behavior that results via the first path - the rising HCDA bubble.

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THE ART PROGRAM

The ART program is an experimental and analytical project structured to make investigations into three main areas related to HCDA source term assessment and aerosol behavior:

1. conversion of the core into transportable material as a function of the amount of energy deposited in the fuel,
2. transportability and attenuation within the primary vessel sodium and behavior in the cover gas space, and
3. transient behavior of the released radionuclides accompanied by the burning of sodium in the secondary containment.

The program is being conducted in four separate test facilities designated CRI-III, CRI-II, FAST, and NSPP.

In the CRI-III facility, UO_2 (as a surrogate for fuel) will be placed into a variety of initial HCDA-like states. The UO_2 will then be allowed to flash as it undergoes essentially unconstrained expansions into an atmosphere of argon. The unconstrained expansions should provide vapor yields that approach adiabatic maximums and the rapid quench should result in the smallest primary aerosol sizes. The objectives would include establishing the quantities of fuel of all phases that are made airborne and transportable, determining the primary aerosol sizes and distributions, and determining the characteristics and transient behavior of the airborne material. Some of the tests would add sodium to allow fuel-coolant interaction and would add sufficient amounts of stainless steel to investigate the influence of cladding in the source mixture.

The bigger, CRI-II, facility is restricted to lower temperatures. However, it is located in a confinement area and can be used at low activity levels to investigate how traced fission product simulants are released and travel with the aerosols, and to investigate possible effects of a radiation environment on aerosol behavior.

The Fuel Aerosol Simulant Test (FAST) is a UO_2 /sodium facility for investigating the under sodium transport and attenuation of the expanding and rising HCDA bubble. It is basically a 1/10 scale version of the reactor primary vessel. Electrical energy stored in capacitors will be used to create small scale disassemblies of UO_2 fuel simulant samples under a substantial pool of sodium. The objectives will be to study the source as it interacts with the sodium, the bubble dynamics and growth, the transport and attenuation in the sodium, and the transient behavior in the cover gas space.

The fourth facility, the Nuclear Safety Pilot Plant (NSPP), uses a 40 m³ vessel in which sodium pool and spray fires will accompany the simultaneous generation of U_3O_8 aerosols (through burning of uranium). In the NSPP, sodium oxide and uranium oxide mixture aerosol behavior will be established as the aerosol undergoes transient conditions similar to those projected to exist in the LMFBR secondary containment. Of interest will be to validate aerosol behavioral models including the effects on the aerosol behavior of concentration level, relative time of introduction of the various species, pressure/temperature transients, convection currents, and the presence of moisture.

To conduct some of the ART program experiments, it appeared necessary to have a means of placing fuel samples (or UO_2 as a surrogate) into the high thermodynamic states that are projected to exist following a prompt-critical excursion. Consequently, the early phases of the ART program have concentrated on developing a technique based on the "exploding-wire" method of creating aerosols from conducting metals via the discharge of electrical energy stored in capacitors. The remainder of this paper, then, will discuss the development of this Capacitor Discharge Vaporization system at ORNL and present some of the preliminary results.

CDV DEVELOPMENT

The use of capacitor discharge techniques to produce aerosols from fuel materials was originally reported at Karlsruhe in 1968.[3] Their intent apparently was to produce aerosols for behavioral studies in confined volumes - and they were able to vaporize a fraction-of-a-percent of their fuel samples for that purpose. A slightly different view is taken here. In the ART program, the CDV system is considered as an experimental tool for placing samples of LMFBR fuel into the appropriate high thermodynamic states. The laboratory samples can then be used in a variety of safety related experiments including aerosol behavioral studies. That is, in the ART program, the electrical energy serves as a replacement for the nuclear energy. The design objective has been to premelt UO_2 and then deposit ~ 1000 to 2000 joules/gram over millisecond time scales.

The high electrical resistance exhibited by fuel at low temperatures and the negative temperature coefficient (the resistance decreases about six orders-of-magnitude when heated from room temperature up to several thousand degrees Kelvin) has led us to choose the following operational procedures: The samples are first preheated by means of a constant power electrical preheater until a substantial portion of the mass is molten (ideally we would like to premelt the entire sample). The preheater is then removed from the circuitry and a capacitor storage bank with appropriate switches and inductance chokes is applied. The CDV system can therefore be considered as having four components; the fuel sample and holder, the sample preheater, the capacitor energy delivery system with associated charging and discharge control circuitry, and the diagnostic instrumentation.

Prototype Sample Configuration

Fuel simulant sample assemblies used for most of the CDV development tests have been made up of axial stacks of fuel-sized UO_2 pellets contained between spring-loaded tungsten electrodes. The pellet stacks are enclosed by loose refractory packing (UO_2 powder or microspheres) that serves as thermal insulation. The refractory packing is held in place by a surrounding quartz or ceramic containment tube. The electrodes fit into the containment tube so that structural rigidity is maintained but sufficient clearance is permitted for thermal expansions. This basic assembly is mounted coaxially in a stainless steel holder webbed to allow the disassembly to occur generally in radial directions. One electrode is attached to an axial copper conductor electrically isolated from the stainless steel holder by rigid ceramic insulators. The other electrode is spring loaded and is attached to the stainless steel holder by flexible conductors. The holder itself is flanged for mounting inside a test vessel. See Fig. 1.

The use of quartz for the containment tube will allow some pyrometry observations of the sample during the experiments, will provide electrical insulation when the system is used under sodium, and will prevent the pressurized sodium from penetrating into the sample prior to capacitor discharge.

Sample Preheating System

The fuel sample is first preheated to melting prior to the capacitor bank discharge by means of a constant power preheater. The ORNL design can provide any chosen power up to 4000 watts (100 amp maximum). During a typical test, the sample has a steady power of 50-80 watts/cm applied to it until the system stabilizes. Then 200-300 watts/cm are applied for 30 to 60 sec. The latter power period brings the sample temperature to the melting point and provides the melting energy. An auxiliary circuit is included that will allow the application of a high voltage, low current capability to warm the sample from room temperature where it has a very high resistivity ($> 10^6 \Omega$) to a temperature where the resistance is low enough ($\sim 10^{-4} \Omega$) to allow the main preheater to be used.

Because of the inverse temperature-resistance characteristics of fuel, the preheater has been designed to operate at constant power which helps provide

stability and control. Control at constant power is accomplished by an integrating circuit that multiplies the load voltage and current signals. The resulting instantaneous power signal is then electronically compared to a reference setting. The firing angle of the silicon controlled rectifiers is reset to achieve a null balance. For a 1000 watt difference, 90% error closure can be obtained within 0.08 sec.

The Capacitor Energy Delivery System

The capacitor energy delivery system has been designed for flexibility. The energy storage system has four modules. Each module consists of ten capacitors (rated 3 kJ each at 2500 volts), an Ignitron for firing control, and blocking diodes for module isolation. An internal safety resistance bank is incorporated to dissipate the stored energy if required. Each module can be charged at a rate of 35 kJ per minute to a preset voltage with a safety cutoff at 3000 V. Any combination of modules can be used and firing can be simultaneous or sequential with preprogrammed delays.

The energy discharge rate through the samples can be varied about a factor of three by changing sample length and module voltage. To reduce the energy insertion rate by over an order of magnitude, we can use adjustable inductors with each module.

Our tests have shown that we produce about a gram of airborne material from our prototype samples for each 25 kJ of energy deposited. Consequently, we presently have the installed capacity to produce about 5 grams of aerosol.

CDV Diagnostic Instrumentation

The instrumentation and diagnostics discussed here relate only to the CDV system and its application and covers the time period from initial preheat through sample disassembly. Three time periods are of interest; the high preheat phase, the capacitor discharge phase, and the short disassembly phase.

For the high preheat phase (seconds), the sample is monitored for uniformity of heating and containment integrity by a television camera. Voltage, current, power, and temperature (by optical pyrometer) are recorded on a strip chart and stored on data tapes.

During the capacitor discharge phase (milliseconds), the sample is monitored by a color movie camera operating at 10,000 frames per second. Voltage, current, vessel transient pressure, and the acceleration of the spring loaded electrode are recorded on data storage oscilloscopes. These signals can be tape recorded at high resolution and subsequently digitized for data processing.

For the disassembly phase (microseconds), we have available a two-channel storage oscilloscope which digitizes analogue input data as rapidly as 0.5 microseconds per point. It has a 4096, 12 bit, mainframe memory with a triggering feature that allows data to be stored both before and after the triggering event (which, in our case, may be containment failure). Its application will be to record the transient temperature of the expanding vapor-aerosol system during disassembly using a photoelectric device as the primary sensor.

Development Testing

The interactions between sample assembly design, thermal preconditioning, capacitor characteristics, and energy discharge rates have been investigated in our development testing program prior to finalizing the system design described above. These tests were conducted to establish the feasibility of the CDV technique, and to optimize the design for maximum efficiency. These feasibility and development tests were conducted at an existing facility through an interagency agreement with the Arnold Engineering Development Center (AEDC), a U.S. Air Force facility located at Arnold Air Force Station, Tennessee.

Two capacitor bank configurations were used. One, when charged to 2600 V (74,000 joules), produced a nominal 15 MW power pulse with a delivery time up to

3.3 msec. The other, when charged to 2600 V (63,000 joules), provided a nominal 5 MW pulse with a delivery time up to 10 msec.

Forty experiments were completed at the AEDC facility. The parameters investigated included: (a) containment tube strength changes, (b) refractory packing insulation changes in both material and thickness, and (c) the addition of stainless steel as filings or as cladding around the pellet stack and (d) other special purposes tests.

High-speed (20,000 frames/sec) motion pictures of sample disassembly were obtained with each test along with electrical and pressure data, serial post-shot filter samples of the airborne material, fallout and plateout collector samples, and grid samples for scanning-electron photomicrographs.

Table I summarizes results from four typical successful experiments. More details of all the tests are available in the ART Quarterly Progress Reports and a report of the CDV development now in preparation.[4]

Table I. Results of Selected CDV Tests

	Test Number			
	14	24	30	37
Energy input (kJ)	20.4	23.2	25.1	28.7
Aerosol Yield (g)	0.78	0.90	0.84	0.62
Percentage Yield	10.0	8.1	9.0	5.7
Comments	Normal Disassembly	Lower Electrode Expelled-Disassembly Otherwise Normal	Normal Disassembly	UO ₂ Ejected Around Electrode No Disassembly

In all the tests in Table I, the packing insulation consisted of 30/50 mesh UO₂ microspheres. The ID of the containment quartz was ~1 cm with wall thicknesses ranging from 2 to 4 mm. The aerosol yield is shown as total grams and as a percent of the initial pellet mass (tests using ThO₂ for packing insulation have demonstrated that > 85% of the aerosol is produced from the pellet region and the remainder from the insulation).

In the first three of these tests, the quartz containment ruptured - followed by rapid system expansion. Test 37, however, was assembled with larger than normal clearances between the electrodes and the quartz. This apparently allowed the high pressure, high temperature UO₂ to escape around the electrodes without rupturing the quartz.

The observed yields (order of 10% of the sample mass) reported in Table I are lower than values that are predicted from adiabatic expansion calculations.

The special tests that included stainless steel in the vaporization process, either as cladding or as filings added to the sample, have been inconsistent. One test with stainless steel cladding and ThO₂ insulation yielded 1.8 grams of airborne aerosol which consisted of ~65% UO₂, ~30% ThO₂, and ~5% stainless steel. However, two similar experiments yielded much less.

We had anticipated that packed powder insulation would provide better thermal insulation (as opposed to UO₂ microspheres) thereby flattening the radial temperature gradient across the pellets at termination of the preheat phase. Flattening of this initial temperature profile before capacitor discharge was expected to result in more uniform energy deposition and greater aerosol yield. However, the powder insulation experiments disassembled after less energy input and produced less aerosol than did the microsphere insulation tests. We suspect that the greater void space available in the microsphere packing allows some expansion of the liquid UO₂ thereby providing more time for energy deposition before disassembly.

The packing insulation we have used is electrically conducting. Consequently, some portion of the capacitor discharge can be diverted from the pellet stack and pass through the insulation. Our limited tests have indicated that an optimum insulation thickness to minimize this diversion of current and still thermally protect the quartz appears to be between 2 and 4 mm. Follow-on experiments will be designed to more closely determine the fraction of energy that is diverted through the insulation. Computer simulations and our present tests have indicated this to be less than 20% of the total.

In successful disassembly experiments, average power input rates from 4.6 to 15.3 MW corresponding to ~0.35 to 1.15 MW per gram of sample have been obtained. Times-to-sample-disassembly have varied from 0.65 to 5.5 milliseconds. These times and powers are consistent with those of postulated HCDA's.

The CDV development test at AECD were also used to make preliminary assessments of disassembly produced aerosol characteristics and behavior inside the CDV test vessel. Electron microscope examination of plateout samples of the aerosols have indicated that the primary particle sizes (D_{50}) range from <0.005 to $0.02\text{ }\mu\text{m}$. Settling rates interpreted from serial filter sample data and the Stokes relations indicate effective mass median aerodynamic diameters of about 0.4 to $0.6\text{ }\mu\text{m}$ for the aged aerosols as they settle out under the influence of gravity.

We believe our development tests using UO_2 as a fuel surrogate have demonstrated the feasibility of using electrical energy discharged from capacitors to place LMFBR fuel into thermodynamic states appropriate for making safety studies relevant to HCDA source term assessment and aerosol behavior.

The feasibility studies were conducted at an existing facility remote from ORNL and, thus, did not allow for complete characterization of the CDV technique. Our follow-on experiments with the newly installed system at ORNL will be better equipped to further develop the technique, to more precisely define the energy deposition quantities and distributions, and to more clearly establish the states reached by the samples.

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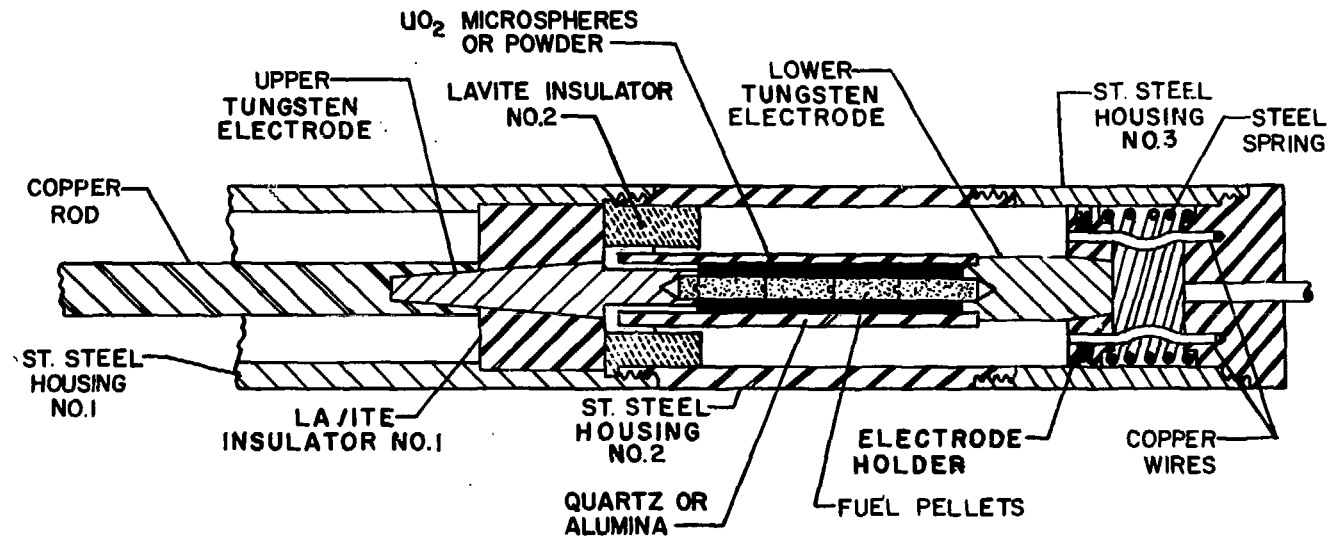
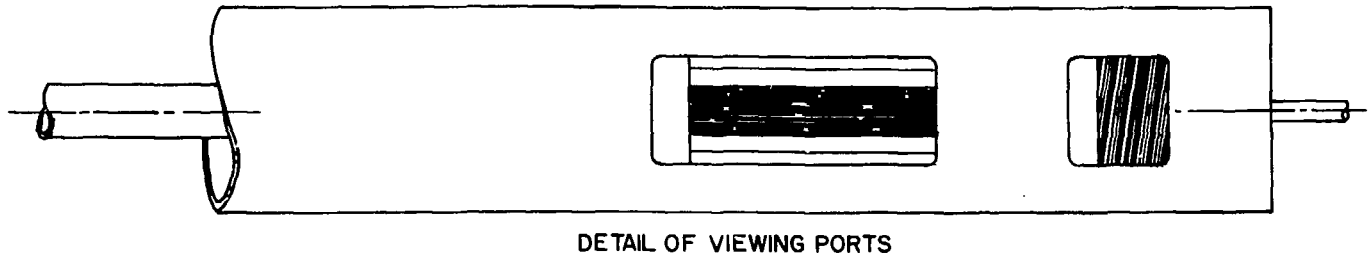


Fig. 1. Prototype sample configuration

SOME POTENTIAL REDUCTIONS IN THE RELEASE OF RADIOACTIVITY UNDER LMFBR ACCIDENT CONDITIONS*

by

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I. INTRODUCTION

The reactor system and containment of a LMFBR can be expected to be very effective in preventing the spread of radioactivity following an accident. With current leak data measurement technology, a reactor containment building must be considered to have 0.1 v/o leakage/day. Since this is the limit of sensitivity, this much gas leakage can be accommodated by flow through a single, short length hole having an equivalent diameter of about 1 mm. Recent tests at Atomics International have shown that holes this small in diameter are quickly plugged if aerosols are present to any extent. Since, in addition, radioactive volatiles will be mostly trapped in the sodium pool, there is little release of dose producing radioactivity. The most hazardous fission product vapor is iodine. It has a high fission yield and concentrates in the relatively small thyroid gland. In a series of recent tests, we have shown that most of the iodine vapor released under sodium with Xe and Kr gas will react chemically and be trapped in the sodium pool before it can escape through broken reactor head seals. Thus, neither significantly radioactive aerosols or gases can escape the outer containment in appreciable amounts following LMFBR accidents.

This paper discusses in Part II the removal of iodine from gas bubbles rising through a sodium pool. In Part III, experiments on the plugging of small capillaries by sodium oxide aerosols are described.

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II. IODINE REMOVAL FROM BUBBLES RISING THROUGH A SODIUM POOL

Any elemental iodine produced as a fission product in a fuel pin will tend to migrate to the gas plenum above the fuel along with xenon and krypton gas. The iodine vapor will be a minor constituent because xenon has a higher fission yield and probably is more easily released from the fuel than iodine. Following a break in the plenum, the iodine will be carried in gas bubbles through the sodium pool. As each bubble rises, some of the iodine will be transported to the bubble wall where it will be removed by the formation of NaI, which is soluble in sodium. Sodium vapor will also diffuse into the gas bubble and form NaI aerosol within the bubble. Removal of NaI aerosol from within a bubble is probably not very efficient because diffusion of aerosols is slow.

Experiments were conducted to measure the amount of iodine (either elemental or NaI) which reached the cover gas after being released along with Xe and Kr gas beneath a sodium pool. The tests were conducted in a pressure-coded stainless steel vessel 43 cm diameter by 1.8 meter high. The vessel was filled with 145 Kgm of sodium providing a 1.4 meter sodium column. A schematic of the iodine test apparatus is shown in Figure 1.

The head of the test vessel could accept various test nozzles and was provided with a bubble collection apparatus. The bubble generator consisted of a removable end section of .64 cm OD tubing, to which could be connected either a 0.48, 1.27 or 3.8 cm ID nozzle. The bubble generator was connected to a separate reservoir containing Xe, Kr and gaseous I_2 . The Xe to Kr ratio was 10, iodine concentration relative to Xe + Kr was about 0.3. As shown in Figure 1, bubbles could be generated at various sodium depths. Flow rate was controlled by a needle control valve. Initially, the released bubbles were collected from the cover gas space above the sodium. However, the amount of I_2 in this large volume was greatly reduced by wall plate-out or reaction with sodium vapor before it could be sampled. In later experiments, the released bubbles were collected in an inverted funnel located at the surface of

the sodium column. The collected gas passed upward through a tube where it was mixed with an N_2 purge gas and then passed to a charcoal filter train. The purpose of the purge gas was to maintain a continuous flow through the filters prior to bubbling so that pressure could be properly adjusted. The flow rate of purge gas was automatically controlled to maintain the liquid sodium at a constant level. Volumetric measurements of the gases which passed through the filter train were made with a wet test meter.

Under conditions of the experiment, the injection pressure ranged from 1 to 2 atmospheres. The I_2 pressure was 165 torr. The concentration of iodine relative to Xe + Kr was, therefore, 0.4 to 0.2. (In a reactor if all iodine, xenon, and krypton were released from the fuel, the concentration of iodine would be about 0.06.)

In order to confirm that the calibrated amount of iodine was actually injected, a duplicate collection funnel was constructed except that sodium interface was replaced by a solid stainless steel plug. Xe, Kr, and I_2 were injected into the funnel and collected in filters and activated charcoal. By measuring the change in weight of the filters and activated charcoal due to iodine collection, it was determined that at least 99% of the original iodine injected was collected.

Iodine attenuation tests were conducted using 0.48 and 1.27 cm diameter nozzles positioned at sodium depths ranging from 2.5 to 137 cm. Twenty tests were conducted at low sodium temperatures, 104 to 230°C, to minimize the amount of sodium vapor in the rising bubble and in the cover gas space. Fourteen tests were conducted at sodium temperatures of 450°C, to show the effect of I_2 reaction with sodium vapor within the rising bubble and in the cover gas.

The mass of iodine collected on the millipore and charcoal filters and the activated charcoal filter train was determined by radioanalysis of activated I-127. Standards for radioanalysis were prepared in the laboratory and consisted of crystalline iodine and NaI. Samples of both standards were provided for the range of mass collected, e.g., 1×10^{-4} to 3×10^{-1} g. The sample train filters were irradiated in the AI L-85 research reactor and analyzed using a gamma detector and 4000-channel analyzer. Statistical error was less than $\pm 10\%$.

The experimental results are plotted in Figures 2 and 3 in the form of log attenuation factor versus sodium depth. The attenuation factor is the ratio of the mass of iodine injected to the mass collected. The attenuation factors show considerable scatter (much greater than the statistical error). Perhaps this occurs as a result of variations in internal turbulence from bubble to bubble. Nevertheless, a conservative lower limit can be defined for the attenuation factor below which experimental points have not been found. (See Figures 2 and 3)

The 0.48 cm and 1.27 cm diameter nozzles produce bubbles having diameters which are believed to bracket those expected following loss of cladding integrity. Because buoyant gas bubbles rise in sodium at a terminal velocity of about 0.3 m/sec, the attenuation may also be given as a function of time (see upper legend in figures).

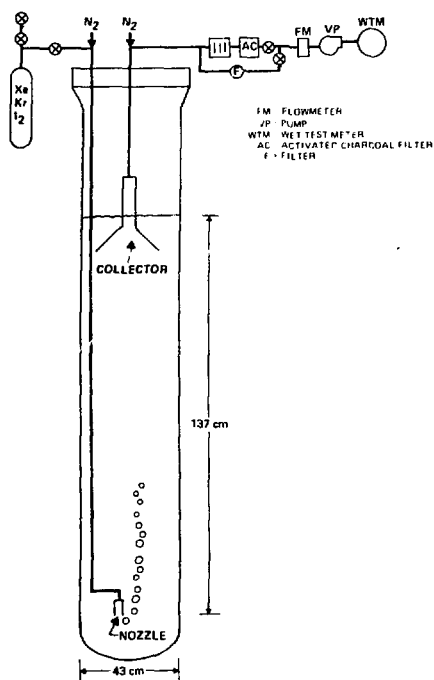


Figure 1. Iodine Attenuation Test Apparatus

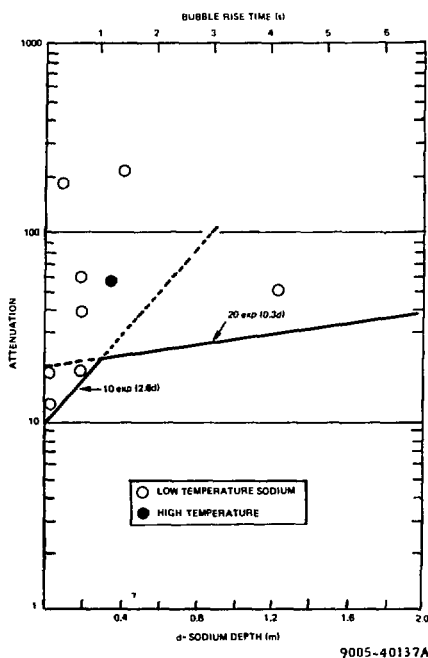


Figure 2. Iodine Attenuation Factor vs Depth, 1.1 cm Bubble Diameter

III. PLUGGING OF SMALL CAPILLARIES BY SODIUM OXIDE AEROSOLS

Aerosol transport through capillaries representative of 0.1 v/o leaks in containment vessels is being investigated. All experiments were conducted in a vessel about 1.0 meter diameter. Sodium pool fires were used to generate aerosols at concentrations up to $50\mu\text{gm/cc}$. The tests were conducted in an environment in which the dew point varied from approximately -23°C to $+13^{\circ}\text{C}$. The various vessel pressures ranged from 1.03 to 1.68 atmospheres.

Simplified diagrams of the sampling trains are shown in Figures 4a and 4b. During the initial test run, the valves to the dry air source remained closed and the ball valve between the capillary and vessel was opened. At the same time an aerosol concentration sample was taken at another main vessel sampling port. As the aerosol passed through the capillary, the gas flow rate and total volume of flow were recorded. A gauge monitored the pressure drop across the filter. If plugging of the capillary occurred, the ball valve was closed and the plugged capillary exposed to the air source. The gas pressure required to unplug the capillary was then observed and recorded. When, and if, the pressure differential across the capillary reached 0.68 atmospheres without unplugging the capillary, the test was terminated. Some tests were terminated before plugging or immediately following plugging in order to visually inspect the condition of the capillary and to note the plug location.

One method employed to determine the size of the particulates emerging from the capillary is shown in Figure 4b. A portion of the particulates were deposited on cover slips which were removed and photographed. The particulates were sized from the photographs. The particulates inside the vessel were sized using an impactor.

Given a certain pressure differential or flow rate, there is an aerosol concentration above which plugging occurs and below which no plugging occurs. On a plot of aerosol concentration versus pressure

differential, the two distinct regions of plugging and no plugging can be seen. The separation line is straight for pressure differentials of 0.068 atm or less and may curve up for other pressure differentials. The line of separation depends on the diameter of the capillary.

With "dry" aerosol, plug formation occurs at the entrance of the capillary. The plugs have a hemispherical shape. Their length inside the capillary is at most a few capillary diameters. These plugs are porous, and gaseous flow through a capillary can be monitored after the plug has been formed. In some cases, plug formation inside the capillary may also occur.

The mass of aerosol that flows through a capillary before complete plug formation is essentially independent of aerosol concentration and flow rates. It is dependent on the capillary diameter.

A quantitative theory for plug formation is at present not available. However, since the plugs are usually formed at the entrance of the capillary, have a hemispherical shape, and a length of a few capillary diameters, it is believed that the plug is formed by an impaction type phenomena. In the flow of gas through a mouthpiece, essentially the geometry of the present experiments, a stagnant zone, sometimes referred to as a vena contracta, results near the entrance. Particles can readily enter the stagnant zone as a result of inertia to motion along a curved path. The plug then builds up inside the stagnant zone.

Experiments with glass and plastic capillaries show that fluffy agglomerates grow along the wall of the capillary. During formation, the particles appear to attach to the wall or to other particles already attached on the wall. Large aggregated particles detach from the wall and escape (see Figure 5). Thus the aerosol transport is dictated by the detachment of particles from the wall, not by coagulation theory. We are then dealing with a problem of resuspension of large aerosol aggregates.

IV. CONCLUSIONS

Neither radioactive aerosols nor vapors can readily escape from reactor containment because of attenuation provided by natural processes. Consideration of these factors will generally reduce the site boundary dose following an accident to negligible amounts.

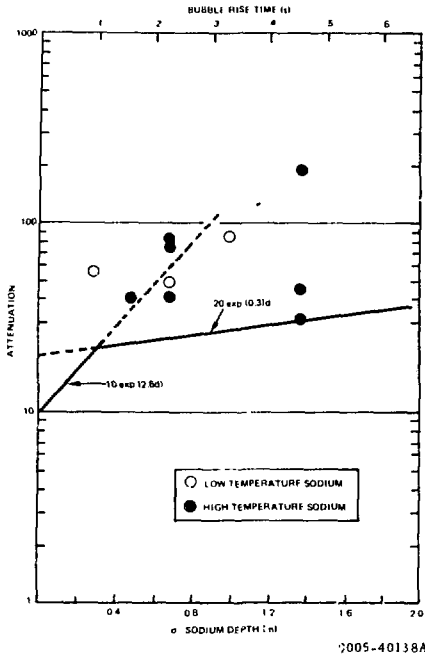


Figure 3. Iodine Attenuation Factor vs Depth, 2.3 cm Bubble Diameter

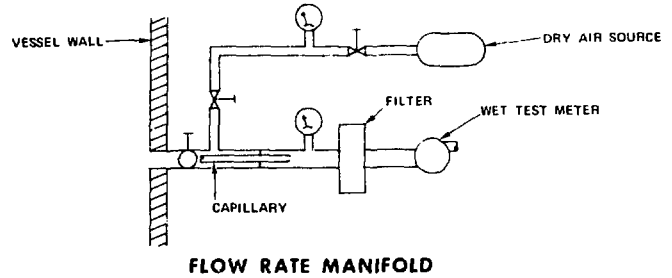


Figure 4a. Flow Rate Manifold

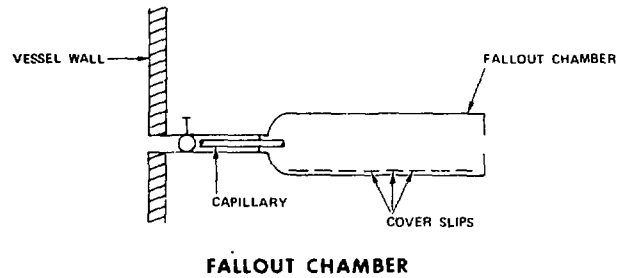


Figure 4b. Fallout Chamber

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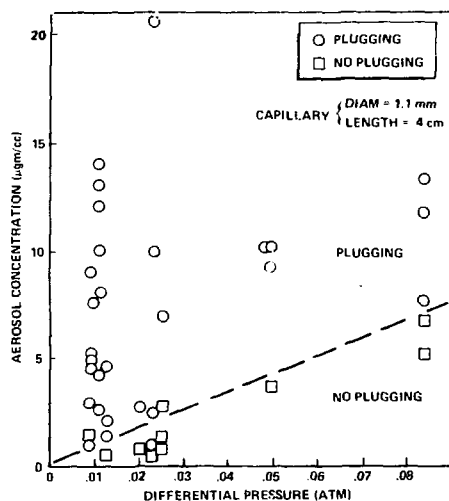


Figure 6. Conditions for Plugging

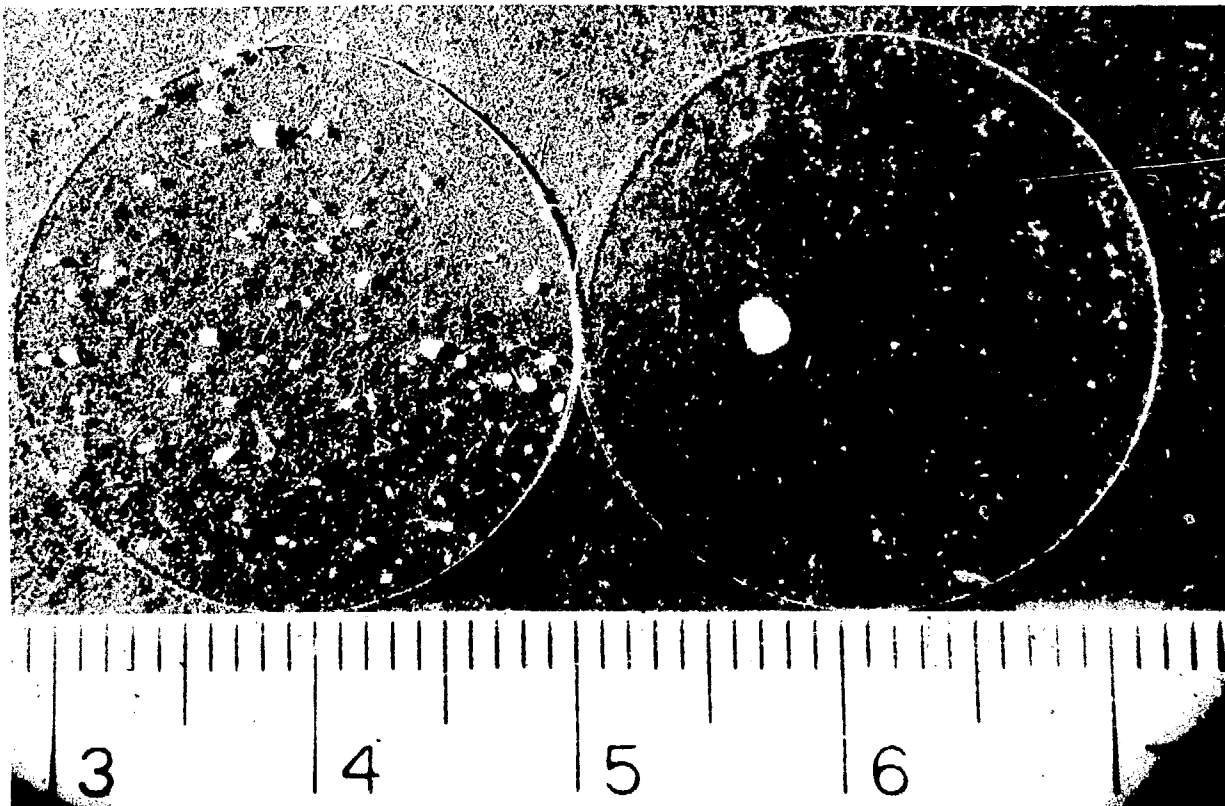


Figure 5. Typical Fallout of Particulates Emerging
from Capillary. Single Large Particulate
Is 2000 μm , Others
50 to 700 μm

SODIUM FIRE STUDIES IN FRANCE
SAFETY TESTS AND APPLICATIONS ON AN LMFB

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ABSTRACT

The risk of sodium fires in an LMFB requires thorough analysis, and the possible consequences of an accidental fire must be accurately determined. Not only must means of prevention and detection be perfected, but techniques must be developed to limit the damage caused by a fire : extinguishment, aerosol containment, protection of reactor structures. This paper describes the program currently undertaken by the CEA's Nuclear Safety Department covering these problems ; it discusses the major results obtained as well as their application to the SUPER-PHENIX reactor.

INTRODUCTION

The risk of sodium fires in a French-designed LMFB exists above all in the secondary circuit and, to a lesser extent, in the auxiliary circuit (fuel storage drums and related cooling circuits). The primary sodium is fully contained in the main vessel or - in case of leakage - in the safety vessel. Only an accident in the core itself is liable to penetrate the upper barrier and to project sodium into the volume covered by the air dome. The possibility of a contaminated fire must thus also be examined.

The amount of secondary sodium liable to be released may range from a few kilograms to several tons if leakage occurs in the sodium lines themselves. The pressure in the secondary circuit is not very high (approximately 5 bars) and drops rapidly in the event of major leakage so that the release and spread of sodium results in a pool fire. The fundamental problem is thus to understand this type of fire and its extinguishment.

The problem is quite different, however, for primary sodium fires. Sodium passage through narrowly restricted volumes under high pressure raises the likelihood of spray fires with virtually instantaneous combustion. The dangers in this case result from the thermal effects of this combustion, the effect of the corresponding pressure and the radiological hazards created by the contaminated aerosols. In order to ensure environmental protection against radioactive contamination, it is important to ascertain the effectiveness of the filters with regard to aerosols, and to determine whether fire-induced contamination is likely to be particulate and thus trapped by the filter, or if gases may be released which would require special equipment. The latter problem is particularly important in the case of iodine.

I - THE TESTING PROGRAM

In response to these imperatives, the Nuclear Safety Department has undertaken the program of tests described in Table I. The test facilities used at Cadarache have been described in references (1) (2) (3). Essentially they consist of :

- a 400 m³ concrete caisson fitted with a large number of temperature, pressure and oxygen consumption measuring instruments as well as aerosol sampling equipment ;
- several steel enclosures, ranging in size from 22 m³ to several hundred liters ;
- chemistry, granulometry and gamma-spectrometry laboratories.

For 1976, this program has been allocated a budget of about seven million francs and employs 15 engineers and technicians.

In addition to the data in Table I, programs are under development for the areas of protective clothing, sodium fire detection, study of the chemical evolution and health hazards of a sodium aerosol cloud.

TABLE I

Experimental sodium fire research and equipment testing program

SCOPE OF BASIC PROGRAM PHASE	DETAILED OBJECTIVES	STATUS AS OF JUNE 1976	MAJOR RESULTS
STUDY OF LARGE SODIUM POOL FIRES (refer to paragraph II.1 below)	-Determination of ignition temperatures and combustion rate -Study of possible sodium boiling and fire acceleration -Determination of combustion temperatures and pressures in the enclosures -Study of smoke products	Completed: see references (1) (2) (3) and (12) -13 experiments carried out in 400 m³ caisson with from 15 to 300 kg of sodium	-Inflammation is possible at low temperature (140°C) -No risk of sodium boiling even at high initial temperature (> 800°C) -Computer codes capable of predicting pressure and temperature -Numerous findings on aerosol granulometry and on concentration variations during fire
EXTINGUISHMENT OF LARGE POOL FIRE (paragraph II.2)	-Development of a fixed mechanical device acting automatically at the onset of sodium leakage -Equipment testing under conditions representative of major leakage at high flow rates	Completed -7 tests carried out in 400 m³ caisson, using 1 ton of Na at 550°C, at flow rates of up to 1 ton/minute	-Development and testing of a 9 m² movable-shutter smothering pan. Reduces burnt mass to 50 kg when 1 ton of Na is spread. Aerosol release is limited to approx. 10 kg
EXTINGUISHMENT WITH POWDER COMPOUNDS (paragraph II.3)	-Development of an effective non-corrosive powder compound	- 1st phase completed. A large scale open-air test has been carried out with 260 kg of sodium	-Development of alkaline carbonate based powder. Pound-per-pound it is 4-5 times more effective than the best currently used products. It is completely non-corrosive on reactor structures

Table I (cont'd)

STUDY OF REACTION BETWEEN SODIUM AND CONCRETE	-Determination of resistance to Na fire of ordinary concrete. -Examination of various special sodium-resistant concretes. -Study of possible coatings to protect the concrete	Completed except for further data on hydrogen release during sodium-concrete reactions. See reference(4)	-No concrete withstands sodium fire, not even special types (alumina or phosphatic concrete) -Standard steel sheeting provides effective protection ; may be completed as required by thermal barrier (e.g. sand)
BASIC STUDY OF SODIUM POOL FIRE	Study of combustion kinetics according to : - sodium pool geometry - water and oxygen content of atmosphere - sodium temperature	1st phase (geometrical effects) completed. 5 tests carried out in a 22 m³ steel enclosure.	Pool thickness determined beyond which sodium is no longer involved in combustion: critical thickness is approximately 25 cm
STUDY OF SODIUM SPRAY FIRES AND SODIUM JET FIRES	- Study of temperature and pressure resulting from spray or jet fire in a sealed enclosure. - Development of mathematical model for prediction of temperatures and pressures.	15 experiments with spray fires and 8 experiments with jet fires.	Maximum pressure attained during testing : 3.6 bars.
STUDY OF CONTAMINATED SODIUM FIRE (paragraph II.4)	- Assessment of quantity of fission products released, and determination of contaminant form (gas or particles).	- 2 experiments carried out on sodium contaminated with Iodine 131	Iodine is completely fixed by sodium oxide aerosols and trapped by ventilation filters.
STUDY OF FILTRATION AND PRE-FILTRATION OF SODIUM OXIDE AEROSOLS	- Assessment of filter effectiveness with regard to aerosols. - Development of solution to filter clogging problem .	- 2 tests carried out on test loop fitted with a cyclone and high-efficiency filters	- Filter efficiency > 10³ for sodium oxide aerosols - Cyclone agglomerator traps 95 % of aerosol mass and prevents filter clogging.

II - MAJOR TEST RESULTS

II.1 - Large sodium pool fires

Thirteen tests were carried out in a 400 m³ caisson in order to assess the characteristics of pool fires, using up to 300 kg of sodium in several of these experiments. The results contributed to the development of a computer code (3) (6) for predicting pressure and temperature versus time in the event of a sodium fire. Figure 1 shows the satisfactory correlation between calculated and experimental results. This code is similar to the SOFIRE II code (5), except as follows :

- the combustion rate varies according to the oxygen content and temperature of the air, and may be calculated from the initial combustion rate ; it is independent of the sodium pool temperature and thickness. Based on test findings, the initial combustion rate is estimated at 36 kg per hour per m².
- the code allows for heat transfer by radiation and convection between the aerosol-charged air and the enclosure walls. The SOFIRE II code allows only for heat transfer by convection.

A large number of analyses carried out during testing showed that the aerosols consist of very fine Na₂O₂ peroxide particles less than one micron in diameter when released. The diameter distribution is characterized by a normal-logarithmic curve with a median diameter of between 0.2 and 0.5 µm and a standard deviation of approximately 2. There are three distinct phases in the evolution of aerosol concentration versus time (refer to Figure 2) : during the initial phase the aerosol generation exceeds the rate of sedimentation ; this is followed by a period of equilibrium and by a phase of diminishing concentration whose onset corresponds to the end of the fire. The maximum concentration level is roughly proportional to the combustion surface area : 10 g/m³ for a 1 m² combustion area, 19 g/m³ for 2 m² and 45 g/m³ for a surface of 4 m². These high concentrations tend to favorize aerosol agglomeration and sedimentation. Concentration variations in time may be calculated from a relation defined in reference (3), in which variables include the mean aerosol release rate from the sodium pool and the geometrical characteristics of the enclosure. Figure 2 shows the results obtained when this relation was applied to one of the tests.

II.2 - Sodium fire extinguishment by means of a smothering pan

The consequences of sodium leakage in a circuit may be minimized by the installation of recovery devices designed to prevent sodium combustion. Two principles seemed applicable at first : either cooling the sodium or starving its oxygen supply. However, as the tests described above showed that the combustion rate is relatively independent of the sodium temperature, research was concentrated on designing a smothering pan system. Seven tests were conducted using such a device, resulting each time in design improvements. Approximately one ton of sodium at 550°C was used for each test, while the leakage rate ranged from 100 kg to 1 500 kg per minute. The first two tests used a device with a single central slot, and substantiated the oxygen depletion principle. The system then evolved into a movable shutter device which confers virtually perfect sealing of the sodium from air while at the same time simplifying sodium recovery (refer to Figures 3 and 4). This system was designed to withstand the thermal stresses resulting from the very extensive flow of high-temperature sodium. The following results were obtained with such a system :

- less than 50 kg of burned sodium out of one ton released.
- the combustion rate was 10 times lower at the onset of the fire, which was extinguished after about 20 minutes.
- the quantity of aerosols released was reduced by a factor of 20.
- the sodium temperature in the pan dropped immediately.

Figure 5 compares the temperature of a 4 m² pool without the system, and that of a 9 m² pool with a smothering pan. In the first case the temperature reached 730°C after one hour, while after the same time in the second experiment the temperature had dropped to 350°C.

These tests were supplemented by interconnecting several smothering pans to increase the overall system retention capacity and to cover the entire surface subject to possible leakage. The results obtained using four coupled pans represent a significant improvement over the performance of a single unit : for one ton of sodium leakage, only 10 kg of sodium burned and only 1 kg of sodium aerosols were released.

II.3 - Extinguishment by means of powder compounds

Extinguishing powders are a complementary technique to the use of mechanical smothering devices described above. However, as was pointed out during the Hanford meeting in May, 1972 (7), the powders currently used in various countries are not satisfactory : generally of only mediocre efficiency, even the least unsatisfactory powders raise the problem of corrosion by chloride ions. The Nuclear Safety Department set out to satisfy this basic requirement, and in 1972 undertook in collaboration with the National Center for Scientific Research (C.N.R.S.-Center for High Temperature and Combustion Chemistry Research at Orléans-la-Source) a program of basic research on inhibiting sodium combustion. This effort led to the development of a powder compound based on alkaline carbonates which has been submitted to large-scale testing at Cadarache. The most significant test involved the discharge of 260 kg of sodium at 550°C into a 2 m² steel tank ; the fire was allowed to reach the maximum combustion rate, then 100 kg of the powder were spread uniformly over the fire by means of a specially designed grid system. The fire was extinguished 15 seconds after the spreading began : smoke release was completely checked, and sodium temperature dropped. Half an hour after the extinguishment a few isolated flames appeared from fine cracks in the crust formed when the powder came into contact with the sodium. These isolated flames were easily controlled by spreading an additional 5 kg of powder. Thus a total of 105 kg of powder were adequate to ensure complete fire extinguishment.

The research which led to the development of this new product may be summarized as follows :

- the initial idea was to improve the combustion-inhibiting role of the crust formed on the sodium pool (8). The objective was to obtain a homogeneous (and therefore liquid or pasty) insulating layer, chemically inert with regard to sodium.
- work was thus concentrated on mineral compounds with low fusion points which would melt on contact with the sodium - i.e. eutectic : a chart of binary, tertiary and quaternary eutectics was drawn up and eutectics diagrams analyzed (9). About thirty laboratory tests were conducted on such compounds as : K_2CO_3 , Na_2CO_3 , Li_2CO_3 - Li_2CO_3 , K_2CO_3 - Li_2CO_3 , Na_2CO_3 - Na_2CO_3 , NaCl, ...etc.
- it was also proposed that a lubricant, such as graphite or molybdenum disulfide, could be added to the compound so as to favorize the formation of a protective film.

About forty compounds were studied in all, and some 700 laboratory fire tests were carried out. The technical problems of powder storage, transport and spreading are currently under study, and it is hoped that a fully operational product will be available in 1977.

II.4 - Contaminated-sodium fires

A pool-fire experiment was conducted on 3.2 kg of sodium contaminated with iodine 131. This is the first in a series of experiments designed to study the behavior of the major fission products (¹³¹I, ¹³⁷Cs, ⁹⁰Sr) during a sodium fire. As noted by A.W. CASTLEMAN (10) this behavior cannot be predicted by theoretical considerations alone : in view of the complexity of the phenomena involved, research must be carried out under actual fire conditions. The purpose of the initial experiment was both to measure the quantity of iodine released from the burning sodium pool and to study iodine behavior in the air-aerosol medium. It was particularly important to determine whether or not iodine is fixed by the sodium oxide aerosols, and thereby to assess the consequences on power station filtering and purification systems.

A 4.4 m³ sealed enclosure was used to contain the fire. The sodium was first contaminated with 43.4 mCi of Iodine 131 - which corresponds to a specific sodium activity of 13.5 $\mu\text{Ci/g}$. The sodium was heated to 500°C and then driven by compressed argon into an 0.125 m² pan located in the enclosure where it burned for nearly 1 hour until virtually all available oxygen was depleted. During combustion, aerosols and gases were sampled at two points in the vessel through "May Pack" filter systems, consisting of :

- a glass fiber inflow filter designed to hold back the aerosols
- two silver-plated copper " knit mesh " filters for molecular iodine
- two charcoal filters impregnated with triethylene diamine for more penetrating forms of iodine
- a glass fiber safety back-up filter.

These filter elements were subsequently analyzed by gamma spectrometry. Table II indicates the iodine activity retained by each May-Pack element at both sampling points.

TABLE II

Iodine 131 activity retained by May-Pack filter elements
(Sampling was conducted between the 10th and 15th minutes, and between the 20th and 25th minutes after the outbreak of the fire).

	MAY PACK n° 1		MAY PACK n° 2	
	10 - 15 min	20 - 25 min	10 - 15 min	20 - 25 min
Glass fiber (inflow)	3.76 μCi	21.4 μCi	5.18 μCi	15.6 μCi
Knit Mesh 1	6.9×10^{-4}	7.5×10^{-4}	2×10^{-5}	2×10^{-3}
" 2	1.9×10^{-4}	1.1×10^{-4}	4×10^{-5}	2.8×10^{-4}
Charcoal 1	5.9×10^{-4}	4.2×10^{-4}	5×10^{-4}	2.2×10^{-4}
2	8×10^{-4}	6×10^{-4}	9×10^{-4}	1.9×10^{-4}
Glass fiber (back up)	3.2×10^{-4}	1.7×10^{-4}	4.6×10^{-5}	2.3×10^{-4}

It may be observed that virtually all the iodine is retained by the inflow filter in the form of aerosols : the activity levels recorded from the remaining May Pack elements are negligible - corresponding to the instrumentation detection limit. The vessel was rinsed after the fire and the rinsing solution analyzed for sodium and iodine. It was found that, of the 43.4 mCi of iodine initially present in the sodium, a total of 11 mCi were released. The mean iodine content in the aerosols was 20 μCi per gram of sodium, which is greater than the initial specific activity of 13.5 $\mu\text{Ci/g}$. This result is comparable with MOREWITZ's findings for a 15 g sodium fire where similar iodine enrichment was observed in the aerosols (11). It is thus clear that LMFBRE safety design must allow for the release of a significant portion of the iodine contained in the sodium during a fire. This iodine, however, is fixed by the aerosols and retained by standard ventilation filters.

III APPLICATIONS TO THE SUPER-PHENIX REACTOR

Pressures and temperature in the various reactor enclosures - particularly for the dome above the reactor slab - were calculated using the computer code developed during the test program described above.

The consequences on the dome were thus calculated for a 50 ton leak of 550°C sodium from the secondary circuit, causing a pool fire over an area of 300 m². Allowing for the oxygen available in the dome, 3700 kg of sodium would be burned, the pressure would reach a maximum of about 0.9 bar after a few minutes, and the maximum wall temperature of 130°C would be reached about two hours after the occurrence of the leak.

Smothering pans are provided over the entire floor area of the secondary sodium galleries (refer to Figure 6). The pans, with a total capacity of 30 m³ per circuit, are interconnected by spillways. A system of retention tanks has been provided below the smothering pans, connected to a recovery tank in the steam generator building : this system is designed to protect the concrete in case of pan leakage or overflow - which is nevertheless highly unlikely - and to evacuate any excess sodium. The smothering pans will greatly reduce the mass of burned sodium, the quantity of aerosols released and the temperature of the recovered sodium.

Sodium fire protection in the steam generator building depends first on fast circuit drainage in the event of leakage : it will be possible to drain the SUPER-PHENIX secondary circuit in less than 8 minutes. Moreover, funnel-shaped metal floor panels will be provided below the main piping and connected to a 200 m³ recovery tank designed to contain the maximum potential leakage. The sodium storage tanks will include no low-level tapping points ; in spite of this precaution, the floor beneath the tanks is lined with sheet metal and a 5 cm thick sand layer is added to ensure thermal insulation of the steel plate and the concrete base. A fixed spreading system for the newly developed extinguishing powder compound will be provided to complete the safety installations.

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FIG.1. SURPRESSION ET TEMPERATURE MOYENNE DE L'AIR. (*overpressure and mean air temperature.*)

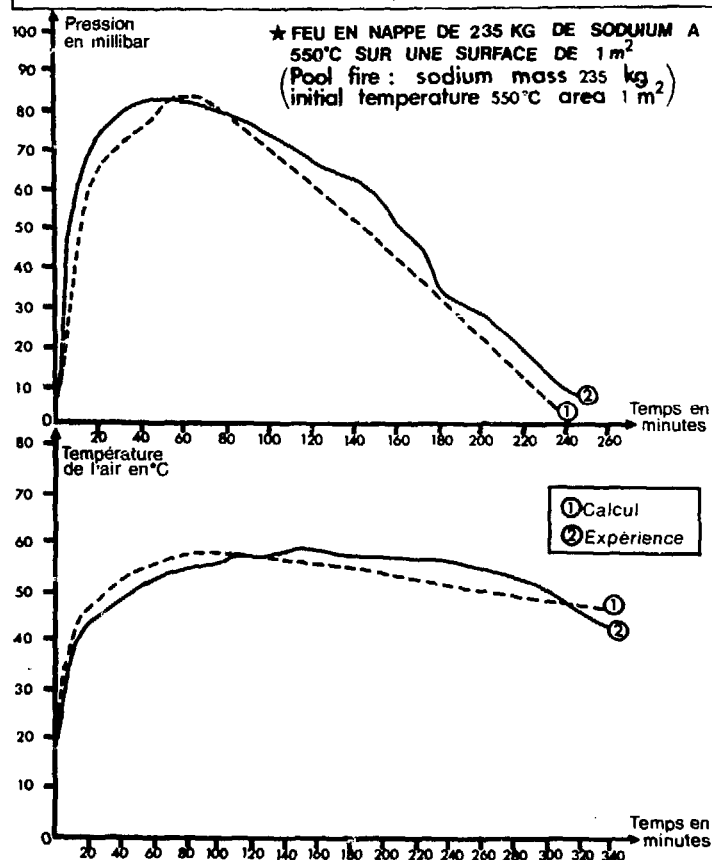


FIG.2. EVOLUTION DE LA CONCENTRATION MOYENNE D'AEROSOLS. (*mean aerosol concentration versus time.*)

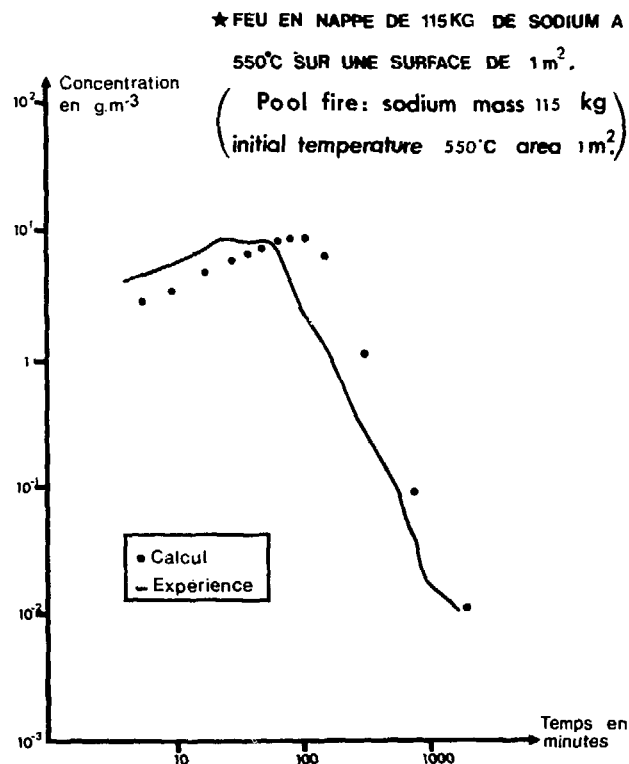


FIG.3. BAC ETOUFFOIR. (smothering pan)

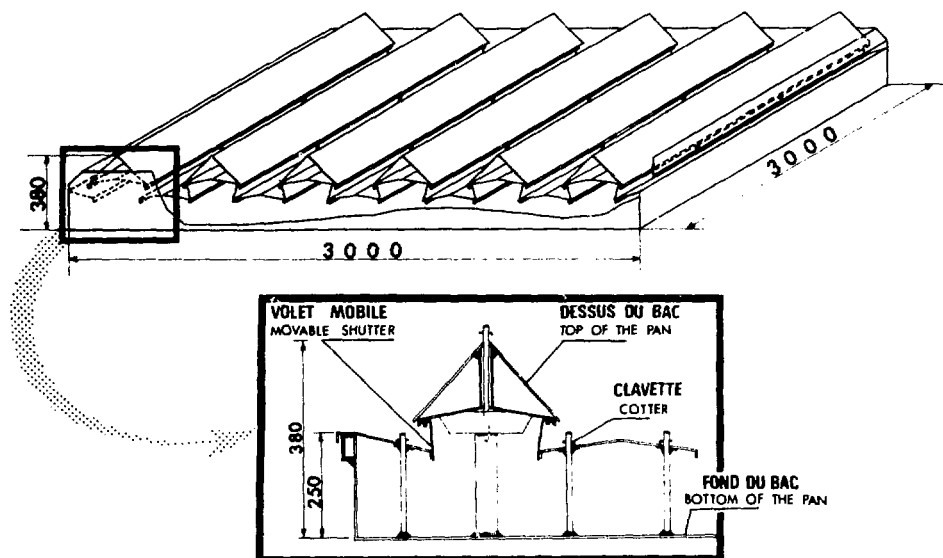
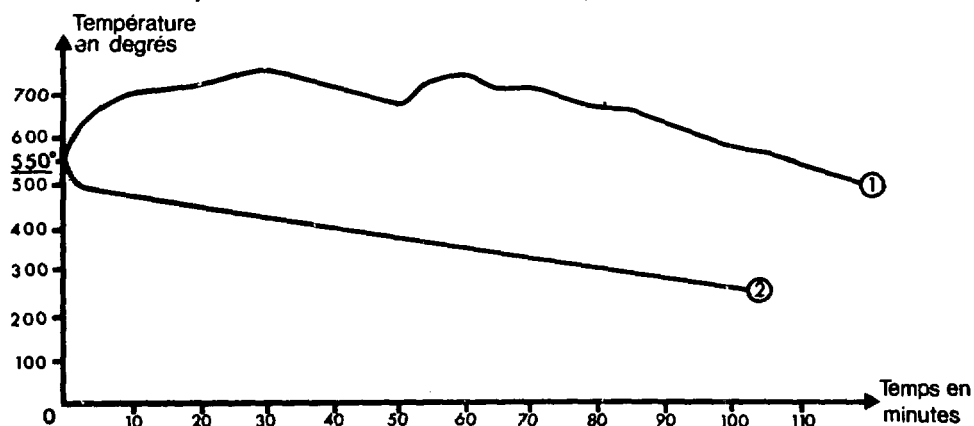


FIG.4. DETAIL D'ASSEMBLAGE DES ELEMENTS
Detail of component assembly

FIG.5. TEMPERATURE MOYENNE DU SODIUM EN FONCTION DU TEMPS. (mean sodium temperature versus time)



- ① { FEU EN NAPPE DE 300 KG A 550°C SUR 4m² SANS COUVERTURE
Pool fire without cover (mass 300 kg; initial temperature: 550°C; area: 4 m²)
- ② { FEU EN NAPPE DE 1000KG A 550°C SUR 4m² AVEC COUVERTURE
Pool fire with cover (mass 1000 kg; initial temperature: 550°C; area: 4 m²)

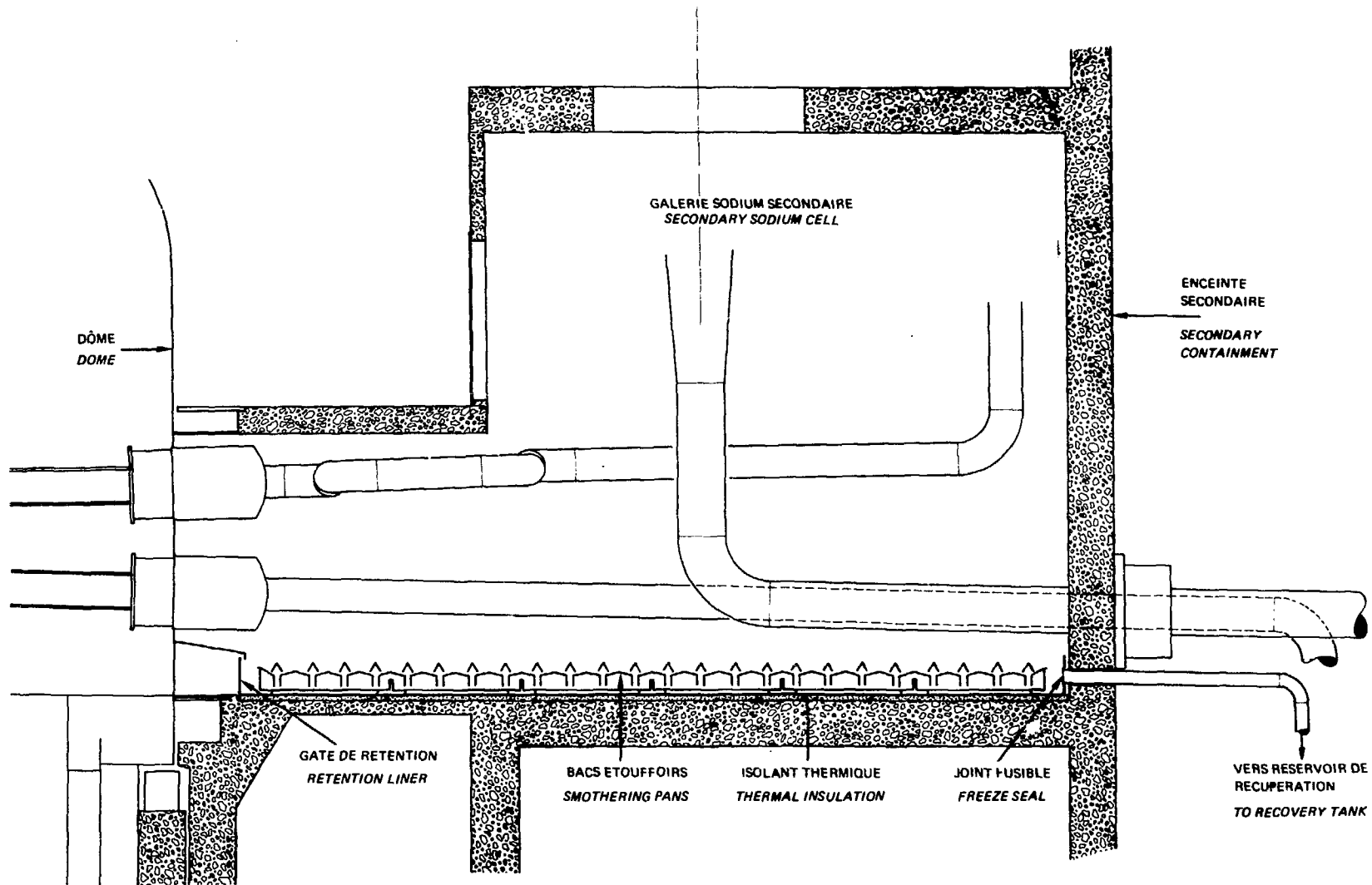


FIG. 6

IMPLANTATION DES BACS ÉTOUFFOIRS DANS LES
GALERIES N° SECONDAIRE
SMOTHERING PANS IN SECONDARY SODIUM CELL

RELEASE OF FISSION PRODUCTS FROM CONTAMINATED SODIUM FIRES

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ABSTRACT

Leakes in the primary coolant system of a LMFBR and also serious incidents with tank rupture may entail the escape of fission products into the containment of the reactor. For incident analysis it is important to know the retention capability of sodium for the different fission products. The release of Cesium and Strontium from pools contaminated with 100 to 1000 ppm was investigated by experiments. The Cesium content of airborne aerosols depends on oxygen concentration: at 21 % oxygen concentration the Cs content of sodium-oxide aerosols is 3 times and at 0.5 % 15 times as high as the initial Cs concentration in the pool. Strontium content of aerosols over burning contaminated sodium pools is 10^3 times smaller than the Strontium pool concentration.

INTRODUCTION

Errors in manipulation as well as leakages in the primary coolant circuit of a sodium cooled fast breeder reactor may entail the escape of fission product contaminated sodium into the inner containment. Considerable amounts of airborne radioactive sodium and sodiumoxide aerosols due to evaporation and reaction of hot sodium with oxygen will be formed.

Also during a postulated serious incident at the reactor in which the reactor tank ruptures and molten fuel as well as liquid sodium flow into the core catcher provided outside the tank, where they cool down, major fission product releases from the sodium into the inertized atmosphere of the inner containment (0.5 % residual oxygen) must be anticipated. In the course of this incident fission products are released from the melting fuel into the sodium.

Therefore, for incident analyses and radiological considerations it is important to know the retention capability of the sodium coolant with respect to different fission products of radiological significance and to investigate their release rate from sodium under very different conditions, especially concentration of fission products in sodium, pool temperature and composition of ambient atmosphere.

It can be derived from theoretical considerations [1] in which compounds and states the fission products must be expected in the sodium system after a serious incident has taken place.

Based on calculations by Catton and Erdmann [2] the fission products can be divided into three groups with regard to their behavior during release in an incident:

The first group includes e.g. Zr, Ba and the lanthanides, i.e. substances which form oxides as stable as e.g. UO_2 and PuO_2 .

The second group comprises fission products, most of them occurring as metals such as Ru, Ag, etc.

The third group contains fission products, most of them gaseous at 3.000 K (noble gases, halogens, Cs, Rb, Cd).

By analogy with the calculations by Gabelnick and Chasanov [3] expected concentrations, states of aggregation and oxidation of some fission products pertaining to the three groups are compiled in table I.

During the incident a major part of fission products are dissolved and suspended, respectively, in sodium and might form compounds with the sodium. Due to its high rate of production, its evaporation rate and its long half life the Cesium fission products (especially Cs-137) takes a particular significance for radiological incident considerations. Therefore, under a long-term research project, the release of fission product Cesium from hot sodium was investigated experimentally and the behavior of the fission product bearing sodiumoxide aerosol formed above the sodium was observed. In a second serie of experiments the release of Strontium, respectively, Strontiumoxide contaminated sodium pools during burning was investigated.

EXPERIMENTAL SETUP

The tests were performed in the large sodium fire vessel of about 4 m^3 volume of the NABRAUS test facility. The vessel can be evacuated so that any desired atmosphere can be realized. A survey of the function of the vessel and the measurement instruments connected is given by the block diagram in Fig. 1.

TABLE I

Oxidation State of Fission Products at 3000 K, Concentration
in 10^{-5} Moles/ccm. Smear Density = 5 Grams/ccm
[from Gabelnick, Chasanov]

Element	Yield	Condensed Phase		Vapor Phase	
		Metal	Oxid	Metal	Oxid
Zr	19,6	$1,1 \cdot 10^{-7}$	19,6	$5,7 \cdot 10^{-14}$	$3,2 \cdot 10^{-7}$
Sr	4,0	$1,2 \cdot 10^{-5}$	4,0	$3,0 \cdot 10^{-6}$	$3,3 \cdot 10^{-6}$
Ru	21,3	21,3	$1,2 \cdot 10^{-4}$	$2,3 \cdot 10^{-4}$	$9,3 \cdot 10^{-5}$
Cs	16,9	9,7	0,24	6,9	0,06
J	1,23	—	—	1,23	—
U	1410	—	1410	$1,7 \cdot 10^{-11}$	0,03

Metallic sodium and metallic Cesium were brought into the fire pan via a glovebox serving as a lock in an inert atmosphere (N_2) and melted in nitrogen atmosphere by heating of the pan. They were subsequently mixed mechanically. In most of the tests about 0.2 g (100 ppm) of metallic Cesium was mixed with 2-3 kg of sodium. After the melt had solidified the desired atmosphere (0-21 % O_2) was set above the fire pan and the melt was reheated after sampling. Except violent sodium-oxygen reaction (fire) the temperature of the melt could be controlled and kept constant over the whole test (about 1 hour). In all tests strong aerosol formation was observed above the pool, beginning at about 250°C. During the tests sodiumoxide aerosol samples were taken at different points of the test vessel at given time intervals by washing in water. The aerosol mass concentration was determined by titration of the sodiumhydroxide solutions formed in the process of washing (with hydrochloric acid).

The Cesium content of the samples was determined by neutron activation analysis. In some tests the aerosol size distribution was measured using a scintillation particle counter. The oxygen concentration was kept constant during one test each. The experiments on the Strontium release from contaminated pool fires were performed in the same test facility under similar conditions. 0.5 and 5 gram of Strontium as SrO powder were mixed with 5 kg of sodium in the pan before burning. The sample technique was the same as for the Cesium experiments. Strontium content of aerosols was determined by atom-absorptionspectrometry (AAS).

EXPERIMENTAL RESULTS AND DISCUSSION

Especially for incident calculations, the size distribution of the sodiumoxide aerosols at a sodium fire and the size distribution of the sodium condensation aerosols in an inert atmosphere directly above a sodium pool command special interest. Fig. 2 shows the measured average diameter ($d_{50\%}$ -value). The smallest particles were observed at the beginning of the test both in air and in inert atmosphere. As the particle concentrations increase, coagulation processes take place which entail a greater average particle diameter. With the source remaining constant, separation processes dependent on the particle size are responsible for the establishment of an equilibrium. The average diameter of the aerosols generated in a sodium fire can be assumed to be 0.4 μm , that of condensed sodium aerosols in inert atmosphere 0.3 μm .

The aerosol mass concentration over the sodium pool increased in all tests performed in the vessel at a relatively fast rate and attained aerosol concentrations between 3 and 20 g/m^3 , subsequently, aerosol concentration decreased slowly to concentrations around 1 g/m^3 . Mass concentrations of several grams per cubicmeter were attained also at oxygen concentrations smaller than 1%.

The Cesium content of the samples shows a development similar to the mass concentration with the maximum Cesium concentration in the aerosol generally attained before attainment of the maximum value of the airborne sodium concentration. This effect gets particularly obvious in tests carried out with a low oxygen content. Fig. 3 shows the time dependence on the aerosol mass concentration and the Cesium content of the aerosol as a function of the time for different oxygen concentrations. A multiple of the initial Cesium concentration in the pool was measured in the aerosol.

Table 2 shows some selected experimental results. For different oxygen concentrations the maximum sodium and sodiumoxide concentration, respectively, the maximum Cesium content measured in the aerosol and the Cesium content of the sodium pool before and after the test have been indicated. The results of measurements show that for a low oxygen concentration a maximum enrichment of Cesium is obtained in the aerosol. The aerosol may contain more than ten times

the amount of Cesium present in the sodium pool. The Cesium content of the aerosol falls with increasing sodiumoxide aerosols production caused by an increase of the oxygen content in the atmosphere. As has already been shown in earlier tests performed under this program [4] the rate of sodiumoxide aerosol production increases from 1.5 kg Na/m²·h for 0.5 % O₂ to 20 kg Na/m²·h for 21 % O₂ at an initial pool temperature of roughly 500°C.

Whilst for a low oxygen content most of the Cesium and sodium evaporate from the melt due to their vapor pressure and form condensation aerosols, a comparatively greater amount of sodium seems to evaporate in normal air by local heating of the melt caused by the reaction heat generated in sodium oxidation. This effect causes a more intense formation of Na₂O₂ at the points exposed to fire. Table 2 shows that in case of sodium fires in air the aerosol might contain the triple amount of sodium present in the pool. Independent of the oxygen concentration the airborne Cesium content had its maximum between 3.5 and 4.5 mg Cs/m³ in all the tests.

It can be concluded from the tests that sodium and Cesium evaporate independent of each other and that this process is governed exclusively by the temperature and vapor pressure, respectively. Due to its higher vapor pressure as compared to sodium, the aerosol gets enriched in Cesium so that gradually the Cesium content in the pool is substantially reduced. This fact is reflected in the Cesium analysis of reaction residues present in the pan after the fire (last column in table 2).

Balance calculations yield for a sodium pool with a Cesium content of 100 ppm at 500-700°C a Cs evaporation rate of 5 g Cs/m²·h. According to the experimental results this evaporation rate seems to be proportional to the Cesium concentration in sodium: for 500 ppm and at identical temperatures a rate of 25 g Cs/m²·h was measured.

The Strontium content of aerosols in the second series of burning experiments was very low and quite near to the detection limit of analyzing technique. Aerosol mass concentration had a development similar to mass concentration at Cesium experiments. The Strontium content of aerosols was measured three orders of magnitude lower than pool concentration. Even at 5 g Sr in 5 kg Sodium (100 ppm) in the pool maximum Strontium content of sodiumoxide aerosol was smaller than 1 ppm: at aerosol concentrations of 15 g/m³ the maximum Strontium concentration was determined to 2·10⁻⁶ g/m³.

SUMMARY

The following experimental results can be emphasized:

- Above hot sodium pools aerosol concentrations up to 5 g/m³ in inert atmosphere were observed.
- Cesium content of aerosols over contaminated sodium pools is up to three times higher than pool concentration at 21 % oxygen content of atmosphere and 15 times higher in inert atmosphere.
- Cesium evaporates independent from sodium out of the pool with a rate of about 5 g Cs/m²·h at 100 ppm and is proportional to Cesium concentrations in sodium pool.
- Strontium content of aerosols released from burning contaminated sodium pools is at least by a factor of 10³ smaller than Strontium concentration in pool.

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TABLE II

Cesium Content of Aerosols above Contaminated Sodium Pools

[O ₂] [%]	Cs - Content of Sodium Pool [PPM]	Maximum Aerosol - Concentration [MG/M ³]	Maximum Cs - Content of Aerosols [PPM]	MG CS/M ³	Cs - Content of Residue in Pool [PPM]
0	100	4040	1700		14
1	100	3940	1300	4,5	14
11	100	7600	480	3,6	
21	100	21400	200	4,2	37
22	100	11700	300	3,5	
21	200	11000	310	3,3	50
21	500	18000	1500		58

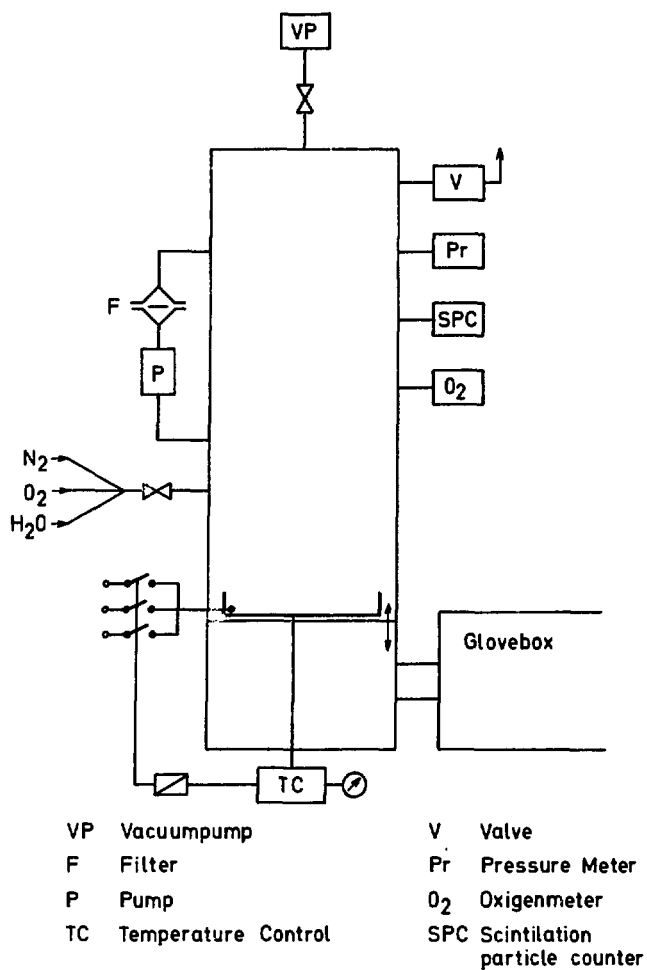


Fig. 1. Diagram of NABRAUS Test Facility

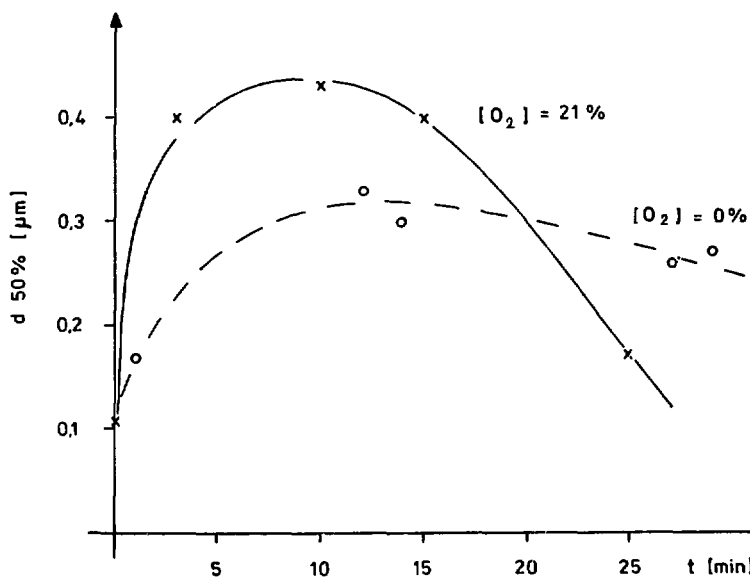


Fig. 2. Particle Diameter of Sodium- and Sodiumoxide Aerosols

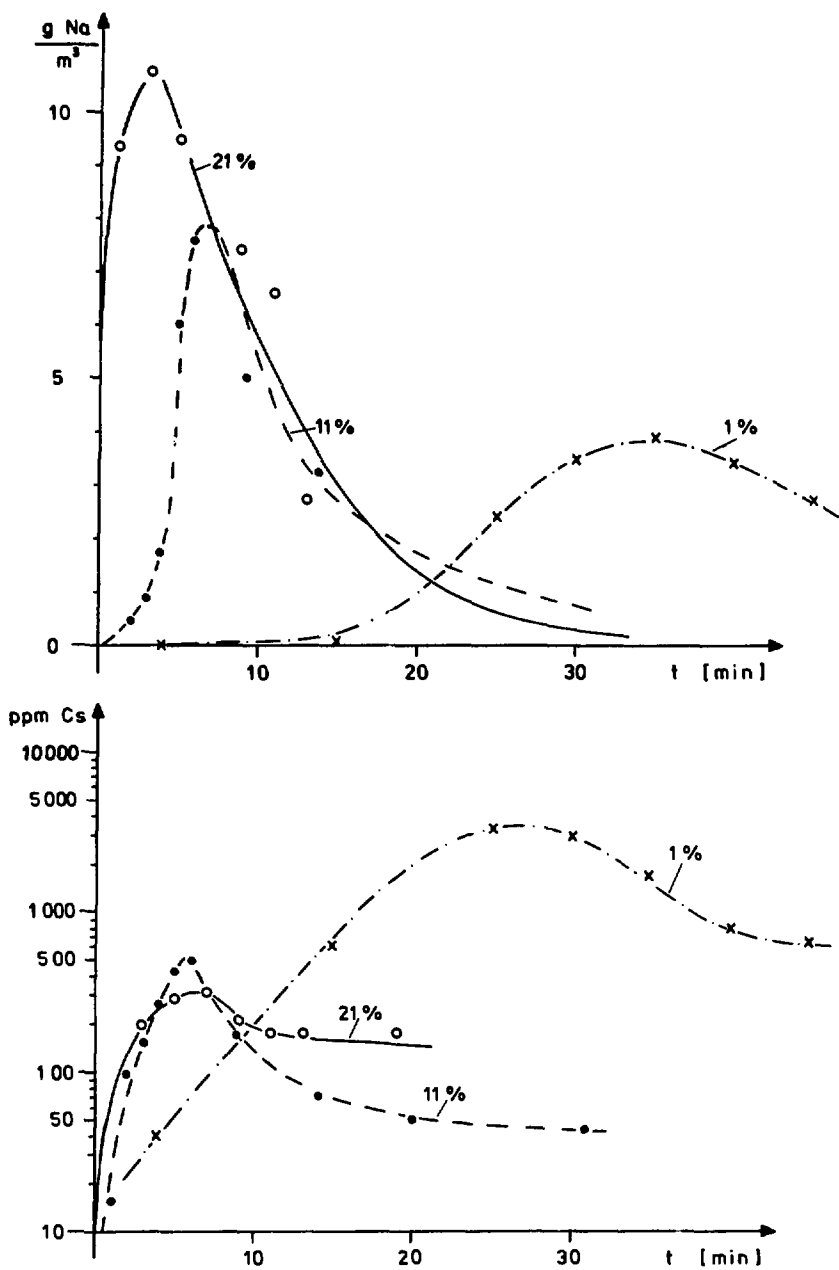


Fig. 3. Aerosol Mass Concentration and Cesium Content of Aerosols above Contaminated Sodium Pools (100 ppm Cs in pool) at Different Oxygen Concentrations of Atmosphere

SPOOL-FIRE -- AN IBM 370/195 CODE FOR
THE ANALYSIS OF COMBINED SPRAY
AND POOL BURNING OF SODIUM

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ABSTRACT

A code for the computation of the pressure and temperatures within a containment building as a result of a sodium fire is described. The fire is assumed to occur in two distinct phases: a spray and a pool. The spray fire is conservatively assumed to occur instantaneously. The pool fire model used is the SOFIRE-II model developed by Atomics International. The code calculates all important temperatures, accounting for heat loss from the building to the environment, as well as leakage of the containment atmosphere under conditions of internal overpressure. The IBM CSMP methodology is used to solve the set of ordinary differential equations which model the system. The use of CSMP allows the user considerable flexibility in modifying the models employed.

INTRODUCTION

Almost since the advent of liquid-metal-cooled reactors, a major safety problem unique to those systems, the combustion of the liquid-metal coolant in air, has received a great deal of attention worldwide. These efforts have included theoretical as well as experimental determinations of pressures and temperatures following a liquid-metal fire, studies of fire fighting techniques and fire prevention, etc.

For the purposes of a reactor safety analysis, accidents are usually postulated wherein liquid-metal coolant is assumed to leak from its piping system. If the surrounding atmosphere contains oxygen, the resulting combustion of the liquid metal leads to a temperature and pressure increase within the volume containing the fire. Subsequent events depend upon whether the coolant contains radioactivity, whether atmosphere cooling systems are provided, whether the volume is sealed or vented, etc.

In general, sodium fires are classified as spray or pool fires. The former may be characterized by the rapid ejection of sodium into air, resulting in a large surface/volume ratio and rapid burning. The latter may result from a slow spill of sodium onto the floor and lead to relatively slower burning. A literature survey covering both types of fires is given in reference [1].

Although both pool and spray fires have been studied to a greater or lesser degree over the years, it seems intuitively obvious that in a realistic situation involving, say, a pipe break, the actual fire will exhibit characteristics of each. For example, during the sodium-ejection phase, a relatively high burning rate may be expected, but after unburned sodium comes to rest on the floor, a less violent burning, characteristic of the pool-type fire, might be expected to occur. In an effort to study this combined spray-pool fire, current models of each type of fire were combined into a single code called SPOOL-FIRE (Spray-Pool Fire).

The incentive for this work was the need to develop design criteria for the containment building of an advanced reactor to be used for the transient testing of LMFBR subassemblies in sealed experiment loop configurations. The design basis accident for the containment was postulated to be a test loop rupture resulting in sodium expulsion into the air-filled building. Since the initial expulsion is presumed to occur under high driving pressures from the test transient, it was considered likely that the in-pile sodium would be ejected rapidly, followed by a slower expulsion of the sodium in the external piping system. While SPOOL-FIRE cannot give the safety analyst the fraction of the expelled sodium which burns as a spray, it can be used to perform parametric analyses and provide guidance to the designers in this critical area. Ultimately, the goals of this analysis are to:

- (1) provide functional requirements to the containment designer, and
- (2) calculate radioactivity releases to the environment via containment leakage.

MODEL DEVELOPMENT

Spray Fire

General

A number of attempts have been made over the years to calculate the effects of a sodium spray fire [1]-[3]. Simple upper-bound calculations were made by Humphreys in support of the EBR-II safety analysis [4]. Later, Baker and Tevebaugh performed more thermodynamically precise calculations [5]. A recent publication has reviewed two of the current models used to calculate spray fire consequences [6].

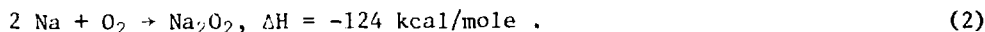
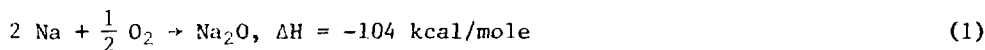
Notwithstanding the accumulation of literature on this subject, our approach has been to follow Humphreys' lead in making simple but conservative upper-bound calculations that would be defensible before any safety review body, and to use the more sophisticated techniques and supporting experiments to argue that the model is indeed conservative. Accordingly, the current model is based on the following assumptions:

- (1) The quantity of sodium ejected as a spray is assumed to react instantaneously and stoichiometrically with the oxygen in the surrounding atmosphere.
- (2) The heat of reaction is apportioned among the reaction product, the nitrogen in the atmosphere, and the remaining oxygen, in accordance with their heat capacities, and assuming a uniform equilibrium temperature for the mixture.
- (3) The pressure in the enclosure increases according to the perfect gas law, with a correction for the oxygen-depletion effect.

The nomenclature used in this paper is given in Table I.

Chemical Reactions

There are two major chemical reactions possible:



The peroxide reaction predominates where the reaction is sodium-limited, whereas the monoxide reaction tends to occur with an excess of sodium [7]. In keeping with the criterion of conservatism, we have considered the peroxide reaction to predominate, irrespective of the thermodynamics. We have also ignored the reaction of sodium with water vapor since, in a sodium-limited situation, more energy is liberated from the oxygen reaction than the water reaction, even when one includes the subsequent reaction of liberated hydrogen with oxygen [7].

Equilibrium Temperature

From equation (2), each gm-mole of sodium which burns results in a heat release of 62 kcal, or 1.128×10^7 J/kg. The equilibrium temperature (T_E) is found by solving the following equation:

$$1.128 \times 10^7 \text{ WSOD} = \sum \int_{T_{o,i}}^{T_E} W_i C_i dT_i . \quad (3)$$

The temperature-dependent specific heats for nitrogen and oxygen were derived from Glassner's work [8]. The specific heat of Na_2O_2 was taken as constant at $1298 \text{ J kg}^{-1} \text{ K}^{-1}$ [4]. The calculation of T_E from equation (3) requires the determination of the roots of a fourth-order polynomial. This calculation was performed using the KAPL code ROPE [9] as a SPOOL-FIRE subroutine.

Peak Pressure

As noted above, the containment pressure following the spray fire is computed simply from the perfect gas law. The number of gas moles in the building are reduced by the number of moles of oxygen which were consumed in the combustion process. Subsequent pressure reduction due to leakage from the building under overpressure is treated below.

Pool Fire

SPOOL-FIRE uses the pool-burning model developed by Atomics International and incorporated in the code SOFIRE-II [10]. Conventional equations of heat transfer by conduction, convection, and radiation are used to perform heat balances and compute temperatures. The SOFIRE-II model is based on computing a sodium burning rate which is proportional to the rate of diffusion of oxygen to the pool surface. The reader is referred to the AI report for a discussion of the pertinent equations and models [10].

The major departure from the SOFIRE-II models in this work is in the computation of containment temperatures as a function of time. The equations and models for these calculations are discussed below.

TABLE I
SPOOL-FIRE Nomenclature

a	exponent for leakage [see equation (16)], dimensionless
A	heat transfer surface through containment wall, m^2
Ap	sodium pool surface area, m^2
C	specific heat, $Jkg^{-1} K^{-1}$
COX	oxygen/air mass ratio
FCMB	fraction of heat of combustion which goes directly to sodium pool
h	free convection heat transfer coefficient, $Wm^{-2} K^{-1}$
H_{O_2}	oxygen transport coefficient, $msec^{-1}$
HTCAPG	heat capacity of containment atmosphere, JK^{-1}
IUP	input integer parameter. IUP = 0 if the Na pool bottom loses heat to the ambient. IUP = 1 if the heat loss is to the building atmosphere.
k	thermal conductivity, $Wm^{-1} K^{-1}$
$K_{j,k}$	conductance between heat transfer nodes j and k, $Wm^{-2} K^{-1}$
LEAK	leak rate from containment, sec^{-1}
n	number of concrete or sodium nodes
OVERP	containment overpressure, kPa
P	pressure, kPa
q	heat generation rate, $J sec^{-1}$
Q	heat generated by sodium combustion, $J sec^{-1}$
R	constant used to determine leak rate based on containment leak rate specification [see equation (16)], $sec^{-1} kPa^{-a}$
RCMB	stoichiometric combustion mass ratio (Na/O_2). RCMB = 1.44 for peroxide and 2.88 for monoxide
SOD	weight of sodium burned in pool, kg
t	time, sec
T	temperature, K
TGZERO	initial containment gas temperature, K
TSZERO	initial liner temperature, K
V	building volume, m^3
W	total weight of material, kg
WSOD	weight of sodium burned as a spray, kg
X	material thickness between heat transfer nodes, m

Greek Letters

Θ	heat of combustion, $Jkg^{-1} Na$. $\Theta = 1.128 \times 10^7$ for peroxide and 9.476×10^6 for monoxide
ρ	density, $kg m^{-3}$

Subscripts

1,2, . . . n	number of a concrete or sodium heat conduction node
a	ambient
b	sodium pool bottom
c	concrete
E	equilibrium
g	containment atmosphere
G	gas gap between liner and concrete
i	component, material, or node index
n	the last concrete or sodium node
N_2	nitrogen
Na	sodium
o	initial
O_2	oxygen
P	constant pressure
v	constant volume
w	containment liner

Containment Model

General

Since SPOOL-FIRE was constructed specifically for utilization in the design of a new reactor facility, no attempt was made to generalize all the possible containment options, e.g., sealed vs. vented. However, as will be seen below, the mechanics involved in problem solution are greatly simplified through the use of the IBM CSMP simulation methodology [11]. As a result, the user can make code modifications relatively simply by adding or modifying differential equations at will to describe a particular physical situation not considered in the present model.

Containment Structural Model

The containment model is shown schematically in Fig. 1. The basic structure is concrete with an internal steel liner to achieve low leakage of radioactivity under accident conditions. Although the concrete may be assumed to be either reinforced or prestressed, neither rebar nor prestressing tendons are treated in the model. An air gap, currently taken as 0.63 mm but easily changed, is included between the liner and concrete.

Containment Thermal Model

The model allows the computation of all significant temperatures in the system following a sodium fire. The temperature of the containment atmosphere (hereafter referred to as gas), consisting mainly of nitrogen and unreacted oxygen, is of primary interest since, as noted earlier, this temperature is used directly to calculate the building overpressure, which in turn is used to calculate the leakage of radioactivity to the environment. However, other temperatures are no less important. For example, one must assure that concrete temperatures do not exceed about 360 K so that water evolution can be disregarded. Similarly, the liner temperature is crucial for the structural analysis which must establish acceptable liner stress levels under both normal and accident conditions.

The gas temperature is calculated as a function of time by considering the heat input (from the fire and hot sodium) and heat loss to the containment structure. The former is calculated as described above, with the "equilibrium" gas temperature following the spray fire used as the starting temperature for the time-dependent pool fire.

The heat loss from the gas is assumed to occur by convection to the containment liner and conduction through the liner and concrete to the ambient which is assumed to be at a constant temperature.

The heat generated by the pool-burning process is given by

$$Q = \theta \rho_g (\text{COX}) (\text{RCMB}) H_{\text{O}_2} A_p, \quad (4)$$

which leads to the following heat generation split between the air and the sodium in the pool:

$$q_{\text{Na}} = (\text{FCMB}) Q \quad (5)$$

$$q_g = (1 - \text{FCMB}) Q \quad (6)$$

The heat transfer equations used are standard transient equations which consider all modes of heat transfer. For materials having a poor thermal diffusivity, e.g., concrete, any number of heat transfer nodes may be employed to improve accuracy. The lower surface of the floor supporting the burning sodium pool is assumed to be inside the building, so that heat transferred through the pool and floor is deposited in the containment atmosphere. As an option, the user may allow the floor to transfer heat to the outside ambient.

Containment Leakage Model

As noted earlier, one of the goals of this analysis is to calculate the leakage of radioactivity from the containment building under a driving pressure created by the energy from a sodium fire. In order to do this, the code calculates the leakage rate from the building as a function of overpressure, and integrates this leakage rate over time, so that one can compute total integrated releases and doses for any period of time of interest. For example, 10CFR100 requires that a 2-hr dose be calculated at the boundary of the exclusion area [12]. The radiation source term for this case can be calculated by simply looking at the integrated leakage fraction for $t = 7200$ sec, and applying that fraction to the radioisotope inventory inside the building and available for release at $t = 0$. Of course, this method is conservative, in that no credit is taken for source depletion due to fallout or rainout or radioactive decay during containment or transport.

The basic equation used in this calculation is

$$\text{LEAK} = R(\text{OVERP})^a. \quad (7)$$

Implicit in equation (7) are the following assumptions:

(1) The leak rate characteristic of the containment does not change over time, that is, the flow-pressure drop relationship is applicable over the entire pressure range.

(2) The gas density is independent of the pressure.

With respect to assumption (1), there are a number of possible models for calculating gas flow through leaks. These have been discussed in some detail by Maccary *et al.* [13]. They also show that there is no right model for all cases, that in fact, each containment is likely to have its own peculiar leakage characteristic, which is a composite of more than one model. A safety analyst does not know a priori what that characteristic will be when the actual structure is leak-rate tested. Hence, one is generally forced to make conservative assumptions.

Assumption (2) is a good approximation as long as the leakage rate is low. In other words, the gas density remains essentially constant in a constant-volume process. Nevertheless, a correction is made in the pressure calculation to account for pressure reduction due to gas leakage.

For the purpose of the analyses described herein, the constant a was taken as 0.5, which would be characteristic of turbulent flow. Although Maccary *et al.* show that orifice flow gives higher leak rates than turbulent flow at the same pressure assuming equal leak rates at the peak pressure, orifice flow does not seem, intuitively, to be a likely mode of flow through a steel-lined concrete vessel. The turbulent flow model is also believed to be inapplicable, but was nevertheless chosen as a compromise between the most pessimistic and the most likely. In any event, the code permits the user to vary any of the leak-rate parameters to suit particular requirements.

CODE DEVELOPMENT

As noted earlier, SPOOL-FIRE takes advantage of the IBM Continuous System Modeling Program (CSMP) [11] to solve the set of differential equations described above. CSMP is designed to meet the needs of scientists and engineers who want to analyze physical phenomena or solve engineering problems without having to spend a great deal of time programming numerical methods for the solution of systems of ordinary differential equations. Further, CSMP allows the differential equations to be solved with a minimum of programming skill. Finally, since the user has only to write the applicable differential equations, it is a relatively simple matter to change the model and the equations at will without concern for the method of solution. For a complete description of CSMP, including all the various output options, simulation options, etc., the user is referred to reference [11].

The code ROPE [9] is treated as a subroutine to solve for the "equilibrium" gas temperature following the spray fire. Standard CSMP input of WSOD and V in the "Parameter" section automatically invokes the ROPE calculation of equilibrium temperature and the corresponding pressure. The former then becomes the initial condition for the gas temperature computation in the subsequent pool fire calculations.

SPOOL-FIRE is available through the Argonne Code Center.

SAMPLE PROBLEM

SPOOL-FIRE was used to obtain the pressure-time history and total integrated leakage fraction for a postulated containment design basis accident (DBA). This DBA consisted of a failure of an in-pile test loop resulting in sodium expulsion into the air-filled containment building. Of the 3629 kg of sodium expelled, 907 kg were assumed to burn as a spray, with the remainder coming to rest in a pool with a surface area of 46.5 m^2 . The building free volume was $34,547 \text{ m}^3$. For conservatism, all of the heat of combustion was assumed deposited in the gas ($\text{FCMB} = 0$), and heat loss from the gas by radiation was neglected.

The results of this calculation are shown in Figs. 2-4. Figure 2 gives the pressure-time history inside the building following the sodium expulsion. Note that, in this case, the peak pressure occurs at $t = 0$, i.e., the peak pressure results from the spray fire. The subsequent heat addition rate from the pool fire is less than the rate of heat loss from the gas, resulting in a monotonically decreasing pressure. This situation could easily change if certain of the key parameters were changed. It is therefore not possible to qualitatively generalize the consequences of a combined pool-spray fire.

It should also be noted that pressure-decay is rather slow even after all the sodium is burned. This is because the hot pool containing sodium oxide continues to provide a source of sensible heat to the containment atmosphere.

As noted earlier, the liner and concrete temperatures are important to the designer. The calculated liner temperature history is given in Fig. 3. In this case, the liner temperature peaks at $t = 16,000 \text{ sec}$ at a value of 342 K . Obviously, the concrete temperature will be less than 342 K (in this case the peak concrete temperature was calculated to be 333 K) so that in this example the structural integrity of the concrete is assured.

Finally, the integrated containment leakage fraction is shown on Fig. 4. At $t = 733,585 \text{ sec}$, $\Delta P = 0$ and the leakage fraction $= 1.46 \times 10^{-3}$. Clearly, out-leakage will cease at this point. The problem will terminate when $\Delta P = 0$ or at a specific time, whichever occurs first.

REMARKS ON THE USE OF SPOOL-FIRE

The great flexibility of SPOOL-FIRE from the user's point of view is counter-balanced by, and in fact derives from, the many technical judgments required of the user. We have not performed sensitivity analyses to determine the relative importance of particular parameters. Any substantial use of the code would require such analyses, so that the user would be in a position to defend the calculations for a specific case.

For example, SPOOL-FIRE allows the user the option of choosing which chemical reaction to consider for the pool-burning phase. The sample problem considered the peroxide reaction. Since the SOFIRE model assumes the burning rate is determined by the rate of arrival of oxygen at the pool surface, the use of the peroxide reaction implies a lower rate of heat addition, but a greater total heat addition, for a sodium-limited fire, than does the monoxide. The user would have to experiment with the code to decide which of the two cases is the appropriate or more conservative one to use.

A particular feature of CSMP is the availability of eight integration schemes, any of which can be specified by the user. For a particular problem, the user should experiment with the integration method, since precision and computer time will vary with the method chosen. Further, for fixed step-size integration methods, it may be advantageous to run more than one problem using different step sizes to cover the entire range of interest. For the sample problem, we found that the method based on Simpson's Rule was fastest, and the results practically indistinguishable from those of more sophisticated methods, when the problem was run in two parts with different integration step-sizes. A total running time of about 4-6 minutes was typical.

ACKNOWLEDGMENT

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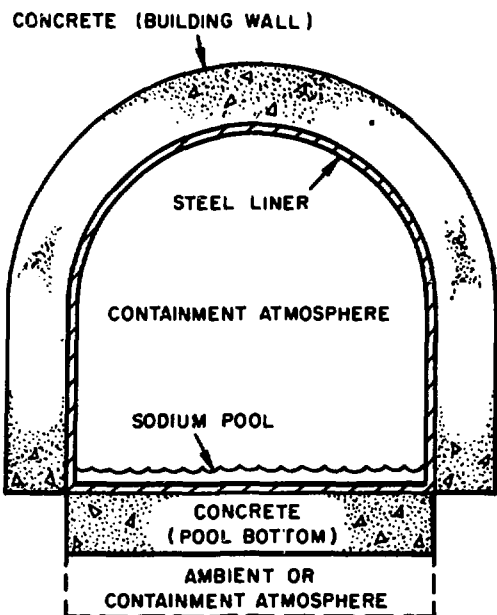


Fig. 1. SPOOL-FIRE Geometry Schematic

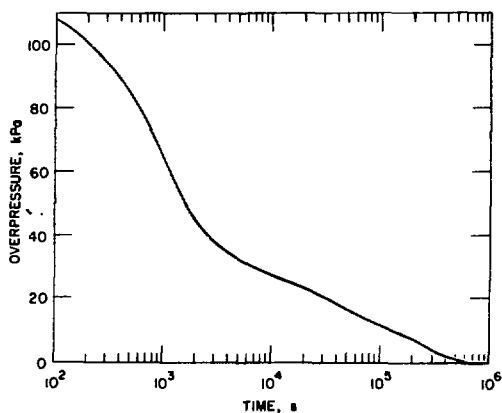


Fig. 2. Containment Overpressure vs Time

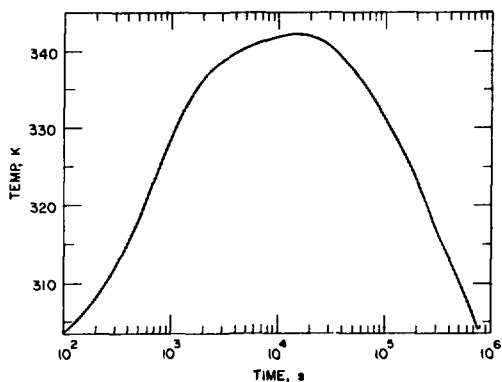


Fig. 3. Containment Liner Temperature vs Time

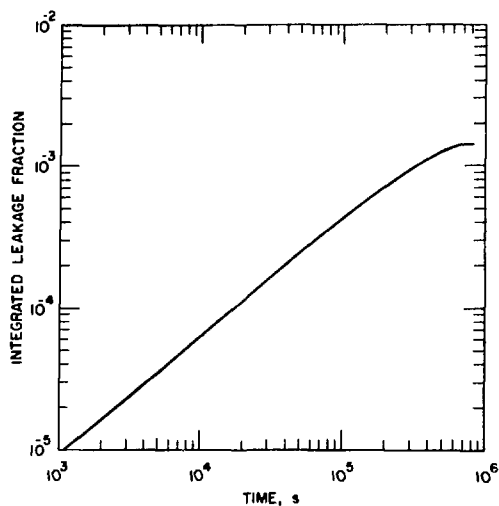


Fig. 4. Integrated Leakage Fraction from Containment

SODIUM SPRAY RELEASE ACCIDENT ANALYSIS FOR FAST REACTOR SAFETY STUDIES

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ABSTRACT

A computer code has been developed to model the effects of postulated sodium spray release from LMFBR piping, although this is an event of extremely low probability. The calculation method utilizes gas convection and droplet combustion theory to calculate the pressure and temperature effects in a range of 0-21 mol% oxygen with humidity. Droplet motion and large aggregate sodium surface area result in rapid release of combustion and sensible heat causing a nearly adiabatic pressure rise which peaks in several seconds. This analytical tool has indicated reasonable agreement with prototypic test data for a range of oxygen and water vapor concentrations, cell volumes and droplet sizes.

INTRODUCTION

A method has been formulated to calculate the heat and mass transfer, droplet motion, gas convection and combustion thermodynamics for postulated accidental release of sodium spray droplets within an LMFBR containment space. The method is an advance from earlier evaluations¹ which utilized instantaneous reaction models for sodium combustion and limiting adiabatic heat absorption in the containment atmosphere. The present method has progressed from a static gas heat transfer convection model,² to a one-dimensional gas convection model³ and then to a quasi-two-dimensional model in cylindrical coordinates to represent circulation within the containment cell atmosphere. It has progressed from the low oxygen case (0-2 mol%)³ to the present 0-21 mol% oxygen model. Additional advancements are in the treatment of smaller sodium droplets and the water vapor reaction.

Sodium spray release consequences are essentially limited to the short term pressure and temperature rise in the cell gas volume. Metal clad insulation modules surrounding the pipe provide a secondary barrier. The effect of these must be neglected in order to postulate a piping leak which produces a spray of droplets. A total of nine experiments with 1.2 and 3.8mm leaks in horizontal and vertical insulated sodium pipe sections were performed by Huber, et al.⁴ A minimum of 3 min was required for sodium travel 1m axially and leakage at the end of the insulation module. In two of the nine cases, sodium was absorbed by insulation or otherwise blocked from further leakage.

For a given sodium spray ejection rate the peak cell pressure will vary nearly as the inverse of the droplet size. A narrow crack is requisite to form-

ing small droplets whose diameter is on the same order as the crack width. In the other extreme a jet emerging from a large break may disperse into a shower of droplets after impacting on a nearby surface.

Experimental test data has been utilized to verify the calculation procedure for oxygen concentrations at 0-21 mol% and droplet diameters at 0.34-16mm. This technique has been utilized in safety evaluations for the Fast Flux Test Facility and the U.S. demonstration plant (CRBR).

THEORY

The mathematical model of sodium spray burning, known as the SPRAY code, describes a cylindrical gas filled steel vessel, the walls of which are perfectly insulated on the exterior. A time varying spray release of liquid sodium droplets is introduced into the vessel to simulate a failure of LMFBR sodium coolant piping. Previous theory^{2,3,5} is reviewed briefly while new developments are presented in more detail.

Droplet Mechanics

Droplet trajectory is considered in the vertical direction only since the horizontal component is small and has minor effect on droplet velocity. The motion is then calculated by summing the gravitational, droplet drag, buoyancy and mass rate-of-change forces:

$$\frac{d}{dt} (mv) = f_{\text{gravity}} - f_{\text{drag}} - f_{\text{buoyancy}} - f_{\Delta \text{mass}} \quad (1)$$

Since the gas/sodium density ratio is small the buoyancy term can be neglected and since droplet mass is decreasing slowly due to reaction, the last term can be neglected. Deviation of the drag coefficient for the liquid droplet from that for a solid sphere is negligible when related to the surface tension number⁶ for the present range of conditions.

Gas Convection

The cylindrical geometry has been selected to facilitate an axial flow model which is combined with thermal mixing crossflow when turbulent conditions exist (see Figure 1). Gas motion is determined by summing the forces of thermal buoyancy, droplet drag and flow friction:

$$\frac{d}{dt} (mv) = f_{\text{buoyancy}} - f_{\text{drag}} \pm f_{\text{friction}} \quad (2)$$

Thermal buoyancy imposes an upward force on the hot spray zone gases due to the cool outer region while droplet drag applies a downward force on the spray zone gases. Under varying conditions either thermal buoyancy (mode 1), or gas entrainment due to droplet drag (mode 2) may be dominating and in either case flow friction will always oppose the direction of motion.

Heat Transfer in the Gas and Vessel

Heat transfer from sodium jet to impact surface is included and can be significant for heat removal during the first few seconds of sodium release. Heat will be transferred to the vessel wall from the outer gas region by convection; typically this will be small during the pressure rise time but will account for half the released heat in ~5 minutes. Sodium not consumed in the spray flight will form a pool on the floor of the vessel transferring the bulk of its heat to the floor by conduction, with some going to the gas and walls by convection and

radiation. Sodium pool burning has a small effect on cell pressure during the short term of the spray transient and is not included in the current model.

Droplet Reaction Model

Heat and mass transfer from the sodium droplet (see Figure 2) has used the correlation of Ranz and Marshall.⁷ Eq. (3) correlates heat transfer and Eq.(4) correlates mass transfer. Actually there are

$$Nu = 2 + 0.6 Re^{1/2} Pr^{1/3} \quad (3)$$

$$Sh = 2 + 0.6 Re^{1/2} Sc^{1/3} \quad (4)$$

three applications for Eq. (4): sodium vapor, oxygen, and water vapor, from which the mass transfer coefficients $k'_i = cD \cdot Sh/d$ are obtained.

Where: c = molar density of gas = P/RT
 D = diffusion coefficient
 Sh = Sherwood Mass Transfer No.

The molar flux equations under forced convection conditions⁸ in spherical coordinates for the single droplet model, for each of the three components are:

$$N_i = Y_i (N_i + N_n) - \frac{Sh}{2} \cdot cD_i \frac{dY_i}{dr} \quad (5a,b,c)$$

Where: $i = \begin{cases} \text{Na, sodium vapor} \\ \text{O, oxygen} \\ \text{H, water vapor} \end{cases} \quad N_n = \text{O, nitrogen}$

D is the respective diffusion coefficient, Y the mole fraction and N the molar flux. When sodium vapor, oxygen and water vapor exceed a trace concentration in nitrogen the mass transfer coefficients must be corrected for high transfer rates by:

$$f_i = \frac{1-Y_i}{Y_i} \cdot \ln \frac{1}{1-Y_i} \quad \text{and} \quad k'_i = f_i \cdot k'_i \quad (6)$$

Values of f range from 0.995 when $Y = 0.01$ to 0.89 when $Y = 0.21$.

The solutions to Eq. (5) in terms of molar flux are multiplied by the reference area (droplet surface) to obtain molar flow:

$$W_{Na} = 4\pi r_A \Delta_{AB} \cdot K_{Na} \cdot \ln \frac{1-Y_{Na,B}}{1-Y_{Na,A}} \quad (7a)$$

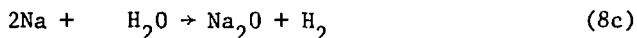
$$W_O = 4\pi r_A \Delta_{BC} \cdot K_O \cdot \ln \frac{1-Y_{O,B}}{1-Y_{O,C}} \quad (7b)$$

$$W_H = 4\pi r_A \Delta_{BC} \cdot K_H \cdot \ln \frac{1-Y_{H,B}}{1-Y_{H,C}} \quad (7c)$$

Where: $\Delta_{AB} = \left(\frac{1}{r_A} - \frac{1}{r_B}\right)^{-1}$ and $\Delta_{BC} = \left(\frac{1}{r_B} - \frac{1}{r_C}\right)^{-1} = r_B \quad (r_C \rightarrow \infty)$

The reactants vanish at the burning zone and therefore the concentrations become zero at r_B (see Figure 2): $Y_{1,B} = 0$.

The reactions considered at the burning zone are:



The choice of monoxide (8a) or peroxide (8b) reaction is not in the theory but is available for modeling experimental data for which the fraction of monoxide and peroxide has been determined. To satisfy stoichiometry we have the following molar flow balance at the burning zone:

$$W_{\text{Na}} = \chi_{\text{O}} W_{\text{O}} + \chi_{\text{H}} W_{\text{H}} \quad (9)$$

Where: χ_{O} = moles Na/moles O_2 from Equations (8a,b)

χ_{H} = moles Na/moles H_2O from Equation (8c)

Since hydrogen is both produced and consumed at the burn zone, it is not transported by molar flow.

When Equations (7a,b,c) are substituted into Equation (9) the location of the burning zone is obtained:⁹

$$\frac{r_B}{r_A} = 1 + \frac{K_{\text{Na}} \ln \frac{1}{1-Y_{\text{Na},A}}}{\chi_{\text{O}} K_{\text{O}} \ln \frac{1}{1-Y_{\text{O},c}} + \chi_{\text{H}} k_{\text{H}} \ln \frac{1}{1-Y_{\text{H},c}}} \quad (10)$$

Thus far we have assumed that the reaction was instantaneous. We now introduce a reaction rate constant which is in effect an efficiency applied to the burning rate:

$$\text{For oxygen:} \quad \epsilon_{\text{O}} = \left(1 + \frac{D_{\text{O}}}{V_{\text{O}} \Delta_{\text{BC}}}\right)^{-1} \quad (11a)$$

$$\text{For water:} \quad \epsilon_{\text{H}} = \left(1 + \frac{D_{\text{H}}}{V_{\text{H}} \Delta_{\text{BC}}}\right)^{-1} \quad (11b)$$

The rate constant V is determined from experimental data and has the units of velocity. With r_B known the sodium molar flux or burning rate referenced to the droplet surface area can be calculated. For sodium reacted with oxygen:

$$N_{\text{Na},\text{O}} = \epsilon_{\text{O}} \cdot \frac{r_B}{r_A} \cdot \chi_{\text{O}} k_{\text{O}} \cdot \ln \frac{1}{1-Y_{\text{O},c}} \quad (12a)$$

and for that sodium reacted with water vapor:

$$N_{Na,H} = \epsilon_H \cdot \frac{r_B}{r_A} \cdot \chi_H^{k_H} \cdot \ln \frac{1}{1-Y_{H,c}} \quad (12b)$$

Droplet Statistics

A log-normal distribution of droplet sizes has been assumed in reporting results in terms of mass mean diameters for experimental data. For use in calculations the surface mean diameter is desired to correctly model the surface effective heat and mass transfer formulations. Heat capacity of equal sodium masses is independent of the droplet size and therefore is independent of the volume mean. The relation between mass (volume) mean and surface mean is:

$$d_s = d_m \cdot \exp(-1/2 \ln \sigma_g^2) \quad (13)$$

where σ_g is the geometric standard deviation. An average $\sigma_g = 1.3$ was obtained for spray nozzle applications,¹⁰ while a value of 2.6 was obtained for jet impact tests¹¹ by analysis of test data. The result when the substitution for σ_g is made in Eq (13) is a reduction factor of 0.97 for spray nozzle droplets and a factor of 0.63 for jet impact.

BURNING RATE EXPERIMENTS

Sodium droplet stationary burning experiments in air were performed by Richard, et al.¹² for initial diameters of 1, 2, and 3mm at 500-600°K. Similar tests were performed at Atomics International (AI)¹³ for droplet diameters of 6-7mm at 700-800°K. Sodium burning rates (mg/sec) were plotted versus 1/T and extrapolated to zero at the melting point of sodium (371°K) as suggested by vapor phase burning theory. Utilizing this correlation at a common reference temperature of 600°K, Richard's data indicated a burning rate/diameter ratio of ~1.0. At high humidity the AI data indicated a ratio of ~0.9 and at low humidity a ratio of ~0.6 was obtained. Sodium burning tests were performed with commercial spray nozzles at AI¹⁴ for mean droplet diameters of 0.45mm at 400°K and low humidity. This indicated a burning rate/diameter ratio of ~0.012. When extrapolated to gas temperatures of 600°K by the 1/T method a ratio of ~0.06 is obtained. In similar tests by Huber, et al.⁴ droplets of ~1.5mm diameter were sprayed at an average 500°K with a burning rate/diameter ratio of ~0.07. This spray test data indicates a burning efficiency of ~10% compared to single droplets assuming that burning rate is not enhanced significantly by the free fall velocities of these small droplets. Refer to Table I. This efficiency is projected into the calculational model by means of Equations (11a,b) with the reaction rate constant $V_O = V_H = 2.5$ cm/sec.

Evidence from the tests of Richard, et al.,¹² and AI¹³ indicates that ignition delays and flame spreading times for single droplets are ~0.8-40 sec. Since droplet flight times in spray tests are on the order of a few seconds, either of these time delay phenomena could explain burning efficiency when comparing spray tests with single droplet burning.

CALCULATION VERSUS TEST RESULTS

Calculations were performed for a selection of spray tests which had suitably defined parameters (see Table II). A total of sixteen tests performed since 1970 were selected and these represent the range of oxygen concentrations from 0-21 mol%. Another important parameter, mass mean droplet diameter, varied from 0.34-16mm, but the high oxygen cases had only small droplets, 0.34-0.45mm. The ratio of sodium injected to vessel volume varied from

0.07 kg/m³ for air tests to 0.90 kg.m³ for low oxygen tests. Water vapor concentration varied from 0.01-1.6 mol% throughout the test series.

TABLE I
BURNING RATES FROM SODIUM DROPLET COMBUSTION EXPERIMENTS

Type of Test	Single Drop	Single Drop	Droplet Spray	Droplet Spray
Reference	Richard ¹²	AI ¹³	AI ¹⁵	Huber ⁴
Droplet Diameter (mm)	1, 2 and 3	6-7	0.45	1.5
Droplet Motion	Stationary	Stationary	Descending	Descending
Temperature (°K) ^a	523-623	700-800	400	500
Burning Rate/Diameter ^b	0.95-1.00	0.6-0.9	.06	.07
Burning Efficiency Compared to Single Drop	-	-	6-10%	7-12%

a. Average temperature of gas surrounding the droplet.

b. Burning rate referenced to 600°K by 1/T method, see text.

The summary of test versus calculated data as shown in Table II compares peak gas pressure, oxygen consumption and sodium consumption. Reasonable agreement was obtained for high oxygen (21 mol%) and low oxygen (~1 mol%). Calculated results were generally lower than the observed pressure for those cases where the sum of oxygen and water vapor concentration was 3-5 mol%.

A parametric study was performed for spray test J-8 by varying oxygen concentration and droplet diameter. Results are shown plotted in Figures 3 and 4. Pressure increases almost linearly with oxygen concentration but does not become zero when oxygen is zero due to release of sensible heat (Figure 3). Pressure varies inversely with droplet diameter as shown in Figure 4.

CONCLUSION

A theory of sodium droplet reaction with oxygen and water vapor has been developed for a common burning radius surrounding the droplet. Instantaneous reaction theory agrees reasonably well with single droplet burning rates. Modification of the theory to include a reaction rate constant results in a model which predicts gas pressures and consumption of reactants reasonably well at ~10-21 mol% oxygen and at ~1 mol% oxygen. In the intermediate region, ~5 mol%, there is an under-prediction in the model in cases where the humidity is high. There is some evidence to suggest that the presence of water vapor produces this anomaly - possibly a catalytic effect which may be non-linear with water vapor concentration. A systematic experimental study of the effect of water vapor on sodium spray reaction is desirable.

TABLE II

SODIUM SPRAY EXPERIMENTAL DATA AND CALCULATION RESULTS FROM SPRAY CODE

Test No.	Ref.	Initial O ₂ Conc. (mol%)	Initial H ₂ O Conc. (mol%)	Vessel Volume (m ³)	Initial Sodium Temp. (°K)	Sodium Injected (kg)	Mean Droplet Diameter (mm)	Maximum Pressure Rise (atm) ^a		O ₂ Consumed (mol%)		Na Consumed (%)	
								Test	Calc.	Test	Calc.	Test	Calc.
LTV-1	14	.35	.4	17.8	815	9.66	16.0	.081	.031	0	.002	-	.02
LTV-3	14	.6	.3	17.8	755	15.28	16.0	.076	.036	0	.004	-	.02
J-3	11	.015	.8	62.3	805	4.08	4.6	.051	.067	0	0	-	.40
J-4	11	.015	.8	62.3	805	5.13	5.3	.040	.068	0	0	-	.34
J-7	11	.8	.8	62.3	810	2.36	3.7	.048	.032	0	.003	-	.78
J-8	11	.8	.8	62.3	810	2.72	3.7	.034	.037	0	.004	-	.77
STV-9	15	2.0	1.0	.89	810	.101	.34	1.56	.35	2.0	.23	36.0	6.0
STV-10	15	2.0	1.6	.89	810	.068	.34	1.16	.28	1.18	.17	35.0	8.3
STV-11	15	2.0	.2	.89	810	.166	.34	.42	.48	.09	.38	1.2	5.0
J-6	11	4.8	.8	62.3	805	5.58	4.1	.29	.095	.7	.04	-	20.5
CH	15	5.0	.2	.89	810	.158	.45	1.10	.51	2.2	.67	32.0	8.8
CL	15	5.0	.01	.89	810	.065	.45	.22	.29	.21	.25	6.6	9.0
B	15	10.0	.02	.89	810	.152	.45	.54	.63	.75	1.05	12.0	18.3
AH	15	21.0	.9	.89	810	.130	.45	.97	.94	2.2	1.4	42.1	38.0
AL	15	21.0	.03	.89	810	.181	.45	.79	.91	2.7	2.1	29.0	32.5
STV-7	15	21.0	.4	.89	810	.078	.34	1.08	.87	1.17	1.27	40.7	49.8

^a. Pascal = Atm . 1.013 E+5

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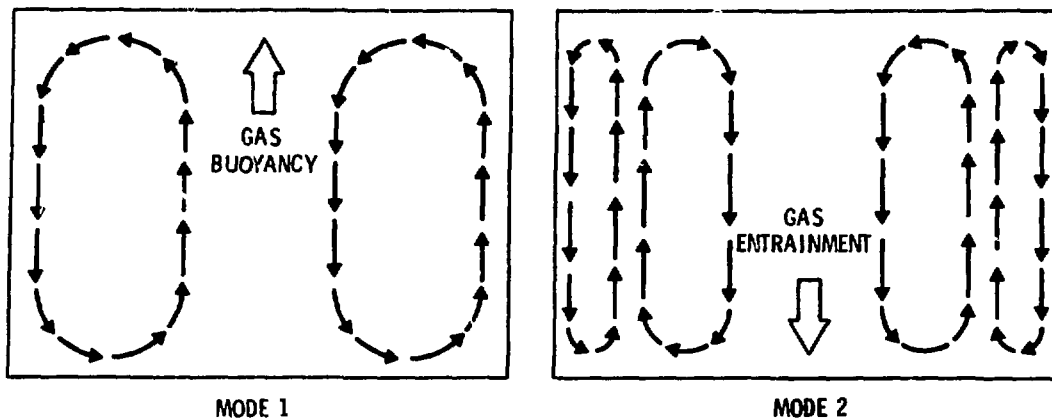


Fig. 1. Gas Convection Model for Cell

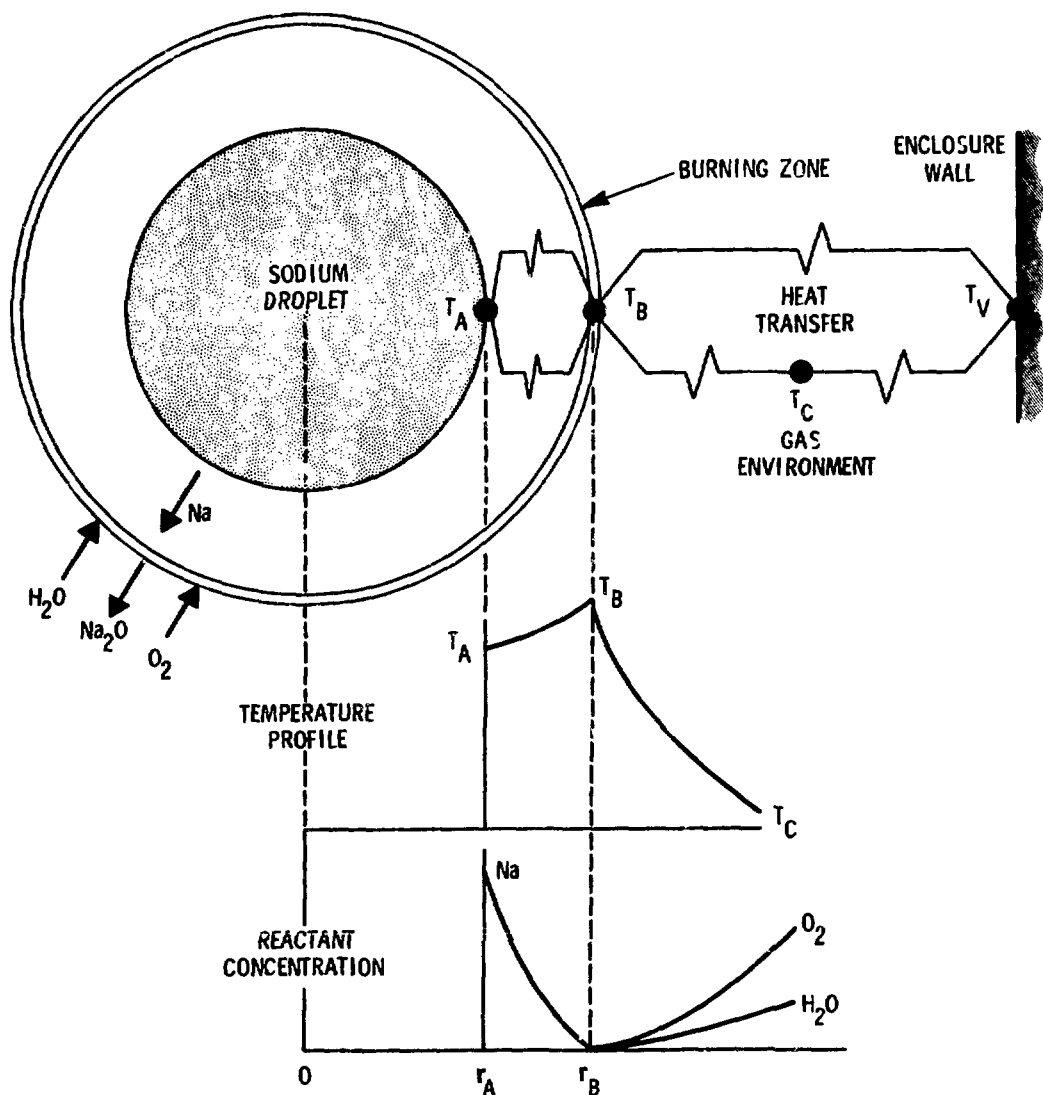


Fig. 2. Reaction Model for Sodium Droplet

HEDL 7609-86.1

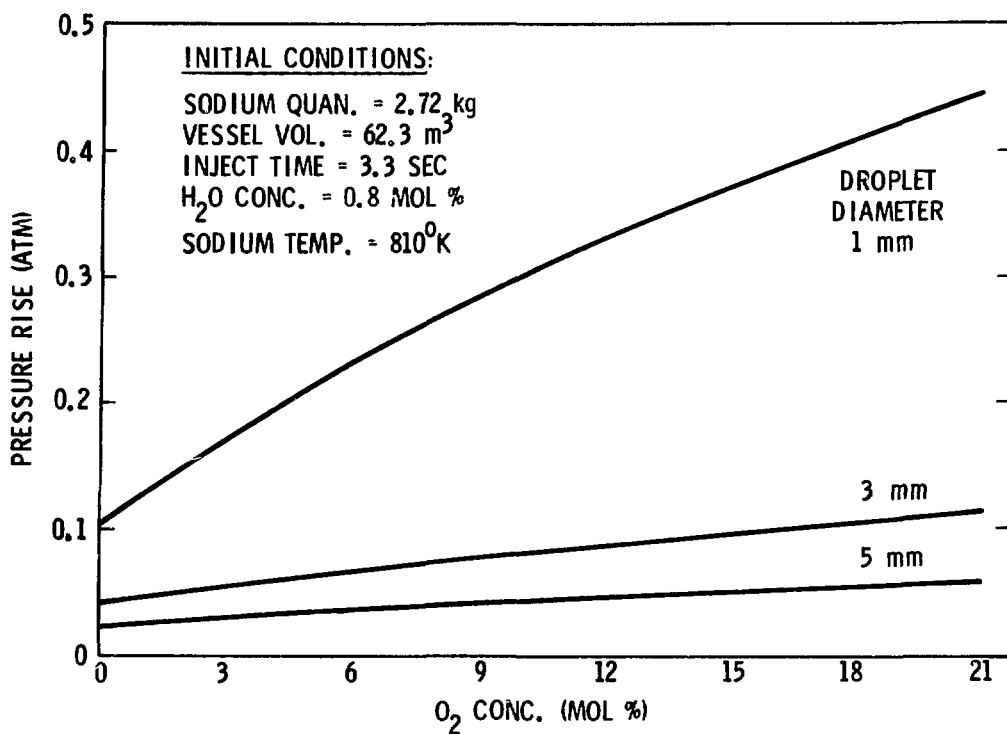


Fig. 3. Peak Pressure vs Oxygen Concentration

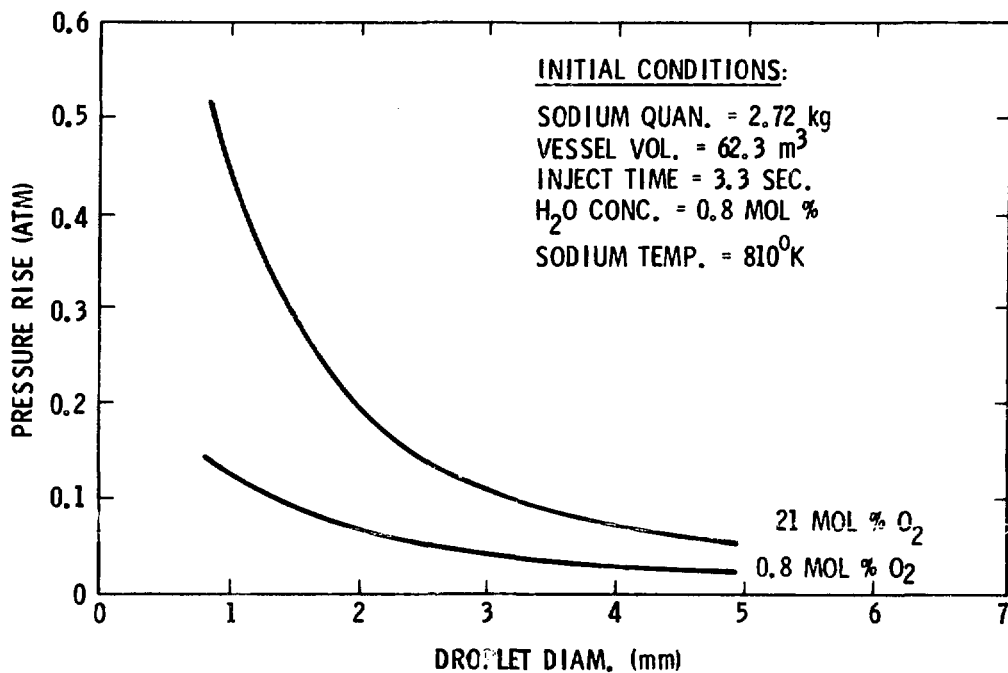
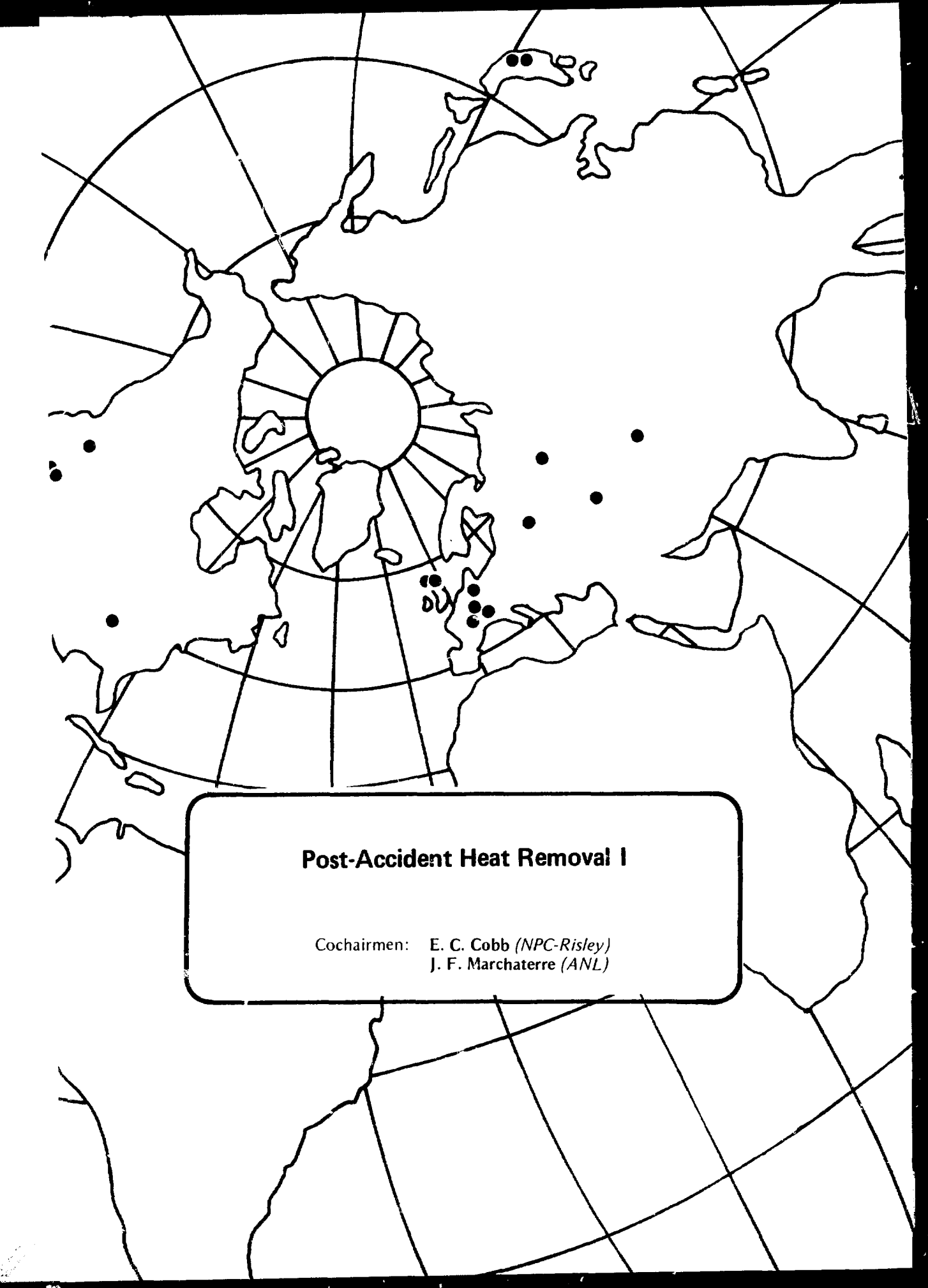


Fig. 4. Peak Gas Pressure vs Droplet Diameter

HEDL 7609-86.2



Post-Accident Heat Removal I

Cochairmen: E. C. Cobb (*NPC-Risley*)
J. F. Marchaterre (*ANL*)

A MODEL FOR EVALUATING PROBABILITIES
OF PRIMARY SYSTEM RETENTION OF CORE
DEBRIS AND TIME OF RELEASE
IN LMFBR RISK ASSESSMENTS

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ABSTRACT

An analytical model is presented that can probabilistically analyze the capability of the primary coolant system to retain core debris after a core disruption accident. For a given set of conditions at the termination of a core disruption, the model provides 1) the probability of a failure of the reactor vessel and the primary system piping resulting from non-coolable core debris, 2) the probability distribution of the time of major fuel releases from the primary system due to the system boundary failure, and 3) a characterization of events within the primary system that may affect the spatial distribution of radionuclides. The model is under development and has been applied to selected reactor designs and accidents. These applications provide insight into the reliability of the primary system to retain core debris, the range of accident outcomes, and the accident risks.

INTRODUCTION

The development of a computer code model to assess the probability of core debris retention within the primary coolant system and the probability distribution of the time to major fuel releases from this system is part of an effort to develop risk assessment methodology for LMFBR's. The probabilistic debris retention analysis was developed for incorporation within a larger probabilistic model to assess the risk of consequences to the public resulting from core disruptive accidents. The probabilistic debris retention model, the subject of this paper, provides an analytical tool for evaluating the effect of design changes on the capability of the primary coolant system to retain core debris after a core disruption. The overall risk model, not discussed herein, provides a means of evaluating the risk worth of primary system debris retention reliability. In conjunction with the overall risk

model, the probabilistic debris retention model becomes an instrument for comparing the risk modifications obtainable by primary system design changes with the risk modifications resulting from other alternatives.

The development of the general risk assessment model and the probabilistic debris retention model has progressed to the point where risk sensitivity to perturbations within a specific design concept can be analyzed; however, further evaluation and verification of the validity of the debris retention model and the overall risk model are prerequisites to implementation in the design process.

ACCIDENT DESCRIPTION

Accident sequence events up to the time of a possible core debris attack on the primary system structures include the accident initiating event, the engineered safety system responses, and any resultant core disruption. The core disruption influences the primary system debris retention by affecting the extent of core damage, the quantity and direction of debris ejection from the core region, and the formation of flow blockages.

The debris retention analysis begins with a description of the accident events described above. The fuel-steel mixture ejected from the core region is assumed to interact with sodium, resulting in the fragmentation of the core materials into small particles. The melted core debris remaining within the core region is also assumed to particulate. Most of the core debris particles will settle out of the sodium and form debris beds. Some of the smallest particles may remain suspended in the sodium coolant. The distribution of the debris depends on the initial locations of the particles, the size of the particles, and the forces exerted on the particles by movement of the surrounding liquid coolant.

Once the fuel particles settle into debris beds, the decay heat is removed from the fuel to the surrounding sodium and steel, unless the heat generation is high enough to cause extensive vaporization of the liquid sodium in the particulate bed and subsequent formation of a molten pool of core debris. The heat generation rate within the debris beds depends on the decay heat of the fuel and fission products that are not dissolved in sodium or released to the cover gas region.

During and after the formation of the high temperature debris pool, heat transfer from the debris into the adjacent structures may cause damage and failure of the structure supporting the debris. Failure of such structures leads to debris relocation. If the failed structure is not part of the primary system boundary, the debris pool is assumed to particulate on contact with any underlying sodium. If the failed structure is part of the primary system boundary, the fuel and sodium are released from the primary system. The release of sodium may cause other debris beds to dry out and become molten. If most of the coolant escapes from the primary system, the fuel remaining in the primary system will melt through the system boundary within a short time. The debris retention analysis continues until it is predicted that all primary system debris will remain in a stable and coolable configuration or until it is predicted that there will be no further significant fuel release from the primary boundary.

MODEL DEVELOPMENT

In developing the debris retention model, four areas were selected as contributing to the uncertainty in primary boundary integrity and to the predicted consequences of a failed boundary. These areas of uncertainty are 1) the initial debris and system conditions, e.g., fraction of fuel ejected or number of loops operating; 2) the accident event involved, e.g., it is uncertain whether molten core debris in a piping elbow will flow into the reactor vessel; 3) the analytical models for predicting the outcome of an accident event, e.g., the model for predicting debris bed critical heat flux; and 4) the model parameters, such as physical properties. The relative contribution to accident risks of the identified sources of uncertainty within each area were compared using available literature and scoping analyses and the most significant sources were selected for probabilistic treatment. All other variables, parameters, and models were treated deterministically.

The criteria used to select the analytical models for the accident events treated probabilistically within the debris retention model are 1) the model is available in published literature, 2) the model was developed as a realistic (versus conservative) predictor of the outcome of an accident event, 3) the model does not require a great number of calculations, 4) the uncertainties in the ability of the model to predict the outcome of the event are quantitatively described, and 5) these uncertainties can be easily included within the analysis. Such models do not exist for all accident events of interest. This required a compromise in the selected models in some instances and development of new models in other instances, particularly if available models either contained very conservative assumptions or were not representative of the expected accident events.

The uncertainties in the debris retention model were represented as probability distributions. When available literature did not contain a quantified probability distribution for an accident model, parameter, etc., the distribution was estimated. In some instances, a statistical comparison of published experimental data and model predictions was used to estimate the uncertainty. Differences between the experimental situation and the expected accident environment were considered when the uncertainty estimate was based on experimental data. Uncertainties were also estimated from similarities between the phenomenon for which the uncertainty estimate was needed and other phenomena for which uncertainty estimates exist. Subjective judgement was applied in assigning the uncertainty estimate when a quantitative basis for the uncertainty was incomplete or lacking.

There are several possible approaches to propagating the uncertainties through the debris retention analysis. The initial approach of this model was to calculate the results of all combinations of a set of equally probable discrete values for the input data and the modeling uncertainties. As the model was developed, more variables were treated probabilistically, and the total number of combinations became extremely large. As the number of combinations increase, this method becomes less practical because of the cost of the increased computer processing time. Methods were developed for grouping some combinations into a singular combination, but this also failed to bring the computer processing time down to an appropriate level. An alternate scheme was investigated in which the probability distributions of the debris retention model are combined into a new probability distribution at each step of the accident analysis. This new probability distribution is input into the next step of the analysis. This method was discarded because of the difficulty of handling the many interdependent variables of the debris retention model.

Monte Carlo methods are now used within the debris retention model to sample each probability distribution independently. This method has allowed probabilistic treatment of more phenomena at a reasonable cost. Computer processing time remains reasonable when using Monte Carlo methods as long as one does not attempt to establish extremely low probabilities of boundary failure. So far the debris retention model has only been applied to low probability initiating accidents that can lead to severe consequences. The model has not predicted extremely low probabilities of primary system failure for any of these applications. However, if extremely low probability of failure applications do arise they might be better handled by biased Monte Carlo methods.

MODEL DESCRIPTION

As mentioned earlier, the debris retention model is concerned with primary system events extending from the core disruption to the termination of fuel release from the primary system boundary. In order to simplify the analysis, several constraints were placed on the type of accident events analyzed. The analysis is conducted only for core disruptions which do not significantly alter the geometry of primary system structures which may support non-coolable core debris. It is assumed that nuclear recriticalities occurring during the time span covered by the debris retention model do not significantly affect the ability of the primary system to retain the fuel. The model also does not analyze the propagation of damage within the core after a partial disruption of the core. Several other less important constraints are not discussed here but can be implied from the model description.

Figure 1 shows the flow of the computer aided debris retention analysis. This figure indicates which areas of the analysis are modeled probabilistically and which areas are modeled deterministically. The initial accident events were emphasized for probabilistic treatment because of the sensitivity of the model predicted outcome to the uncertainties of these initial events. Even though many of the accident events modeled in the latter part of the code are treated deterministically, the variables input into these deterministic models are probabilistic and, thus, the analysis is probabilistic. The code is divided into subroutines to facilitate replacement of analytical models for specific accident events as new or improved models become available.

The input and output of the debris retention model are shown in Fig. 2. The inputs are generated by analyses of accident events occurring prior to and during the core disruption. The calculation by the model of the flow within the primary loops is based on two of the inputs: the probabilities of the number of loops operating in a flow mode and the characterization of coolant flow blockages. The heat removal system state (failed or operational) is used in the calculation of the coolant temperature. If any fuel is ejected from the reactor vessel during the core disruption, the fuel inventory within the primary system is reduced accordingly.

The output of the code is a discrete probability distribution of the time of a major fuel release from the primary system boundary, including the probability of no major release, i.e., infinite time to boundary failure. For each time category in this probability distribution the output includes a set of expected values for variables which characterize primary system accident events. These variables are used as input to subsequent analyses within the overall accident risk assessment. The fuel coolant interaction product is defined as the product of the amount of molten fuel that interacts with sodium times the number of such interactions.

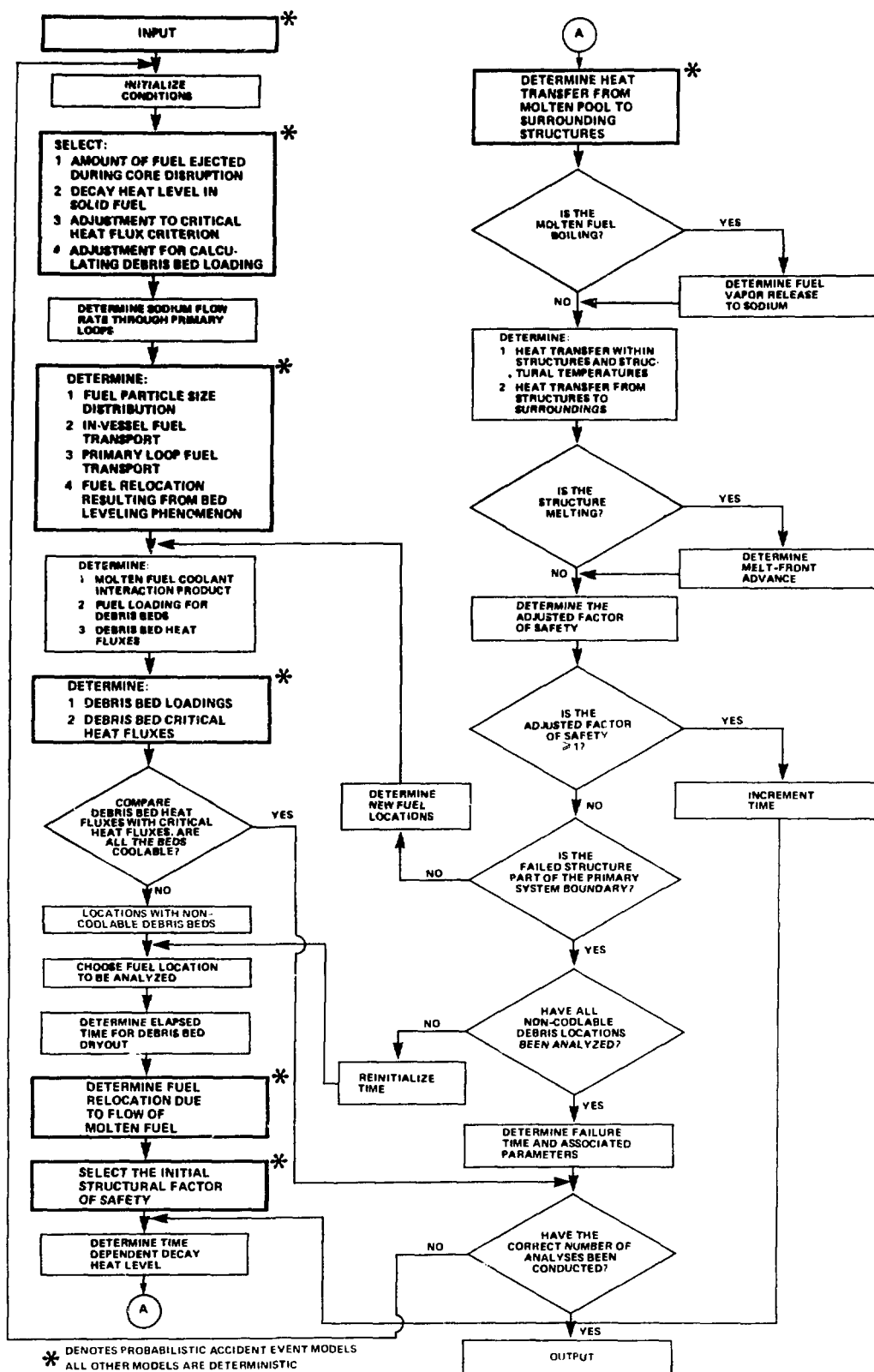


FIGURE 1. DEBRIS RETENTION MODEL FLOW CHART

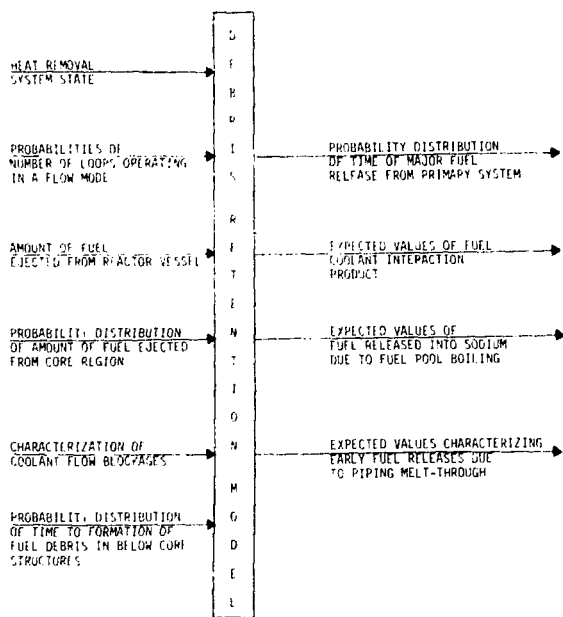


Figure 2 Model Inputs and Outputs

The major aspects of the debris retention model are briefly discussed below. The model probabilistically treats the amount of fuel ejected from the core during the core disruption phase of the accident. The upward ejected fuel is assumed to be uniformly mixed in the reactor vessel upper plenum. The fuel particle size distribution is generated by a probabilistic function. The sodium flow within the primary loops is calculated by the model and is based on the model inputs. The transport of the fuel particles is based on a comparison of the particle settling velocities in sodium and the local sodium flow velocities, while the effectiveness of particle transport by sodium is treated probabilistically. The locations of the debris beds and the amount of fuel contained in these beds are determined from the fuel transport analysis. Fuel relocation between debris beds due to the debris bed leveling phenomena is analyzed probabilistically.

The code calculates the maximum local loading of fuel particles in the debris beds. Based on this fuel particulate loading and a probabilistic decay heat level, the maximum heat flux from the debris bed is determined. The uncertainties in the decay heat level are based on uncertainties in the total decay heat produced by all of the fuel and fission products, the fraction of this decay heat remaining in the fuel particles, and the elapsed time from criticality termination to debris bed formation. The heat flux from the debris bed is compared with the probabilistic critical heat flux of the bed to determine if bed dryout is predicted. The critical heat flux for a debris bed is based on a probabilistic critical heat flux criterion for a given debris bed mass loading and a probabilistic debris bed mass loading. The uncertainty in the debris bed mass loading represents uncertainties in the geometry of the bed and of the location and amount of steel particulate intermixed with the fuel particulate.

If non-coolable debris beds are predicted, the model evaluates the consequences of these non-coolable beds. There is a separate analysis for each non-coolable bed location and the model conducts these analyses in a specific sequence. The sequence is such that when a structural failure at one location may influence the debris retention capability of another location, the former location is analyzed first. Once the core debris has become molten, the possible relocation of this debris as a result of molten fuel flow is treated probabilistically. The decay heat level in the molten pool is adjusted for the elapsed time of molten pool formation.

A transient heat transfer analysis and a structural load carrying capability analysis is conducted by the code for each molten debris pool location. At locations where the fuel pool is expected to reside for a long time, the heat transfer analysis will switch from a transient analysis to a quasi-steady

state analysis after the initial thermal transients have subsided. The heat transfer from the molten pools into the steel structures is treated probabilistically while the heat transfer within and out of the structures is treated deterministically. The structural heat transfer is assumed to be one-dimensional.

The ability of a structure to support the molten pool is based on a structural factor of safety. This factor of safety is defined as the ratio of the mechanical stresses at which failure will occur to the actual stresses on the structure. The structure is assumed to fail when this ratio is less than one. The model reduces the probabilistic initial factor of safety as structural thicknesses decrease (due to melting) and as structural temperatures increase. The reductions in the factor of safety represent the reduced load carrying capability of thinner structures and the decrease in the strength of steel with increased temperature and with high heat fluxes.

MODEL APPLICATIONS

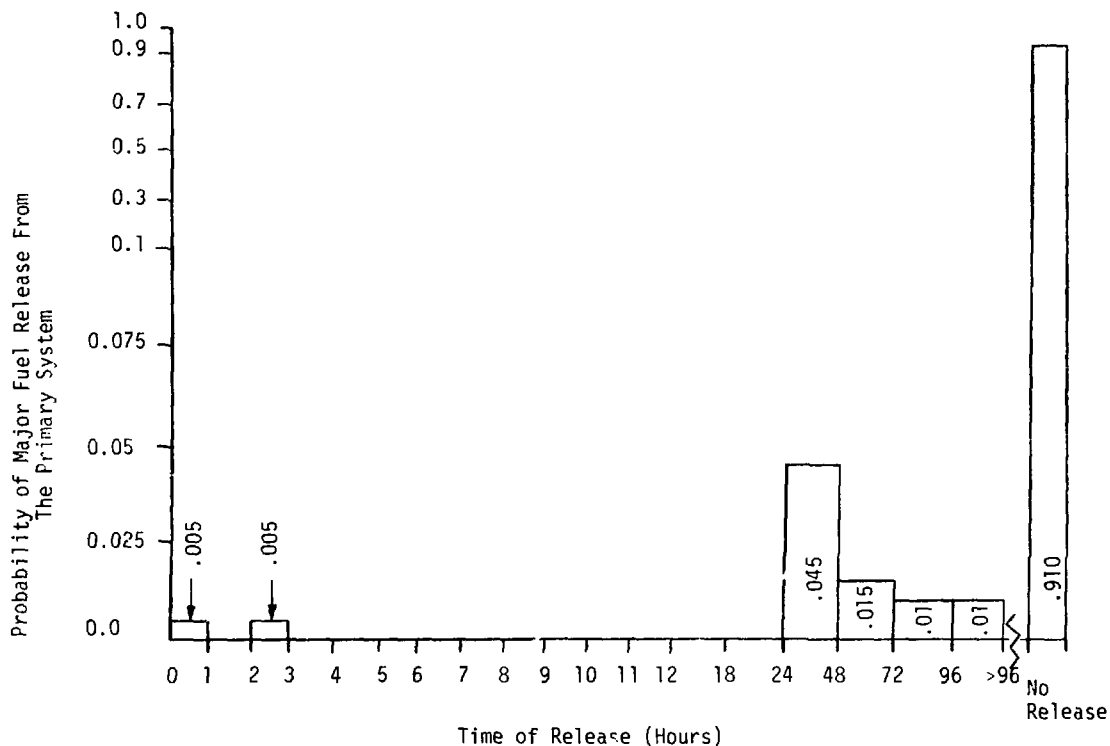
The debris retention model has been developed as part of a risk assessment methodology development program. Test applications of the model within risk assessments of existing reactor designs permit evaluation of the model for completeness and a comparison with existing deterministic safety analyses. An initial simplified version of the model was applied to an early LMFBR reactor design. The model predicted high probabilities (greater than 0.99) of primary system failure after a core disruption in which 100% of the fuel was involved. These results are qualitatively consistent with deterministic analyses of the same reactor configuration for an accident with 100% core involvement.

The most recent version of the model was applied to a reactor configuration similar to the reactor for the Fast Flux Test Facility. Two emergency events were selected for this risk assessment application. Thirty-three separate cases were identified for the analysis of these two events. The cases were selected by assuming the debris retention analysis is a function of 1) the flow mode (main pump, pony motor, or natural circulation) for the primary loops, 2) the state of the emergency heat removal system (functioning or failed), and 3) the extent and severity of core disruption. The results of one of these cases is shown in Fig. 3.

The histogram in Fig. 3 shows the predicted probabilities of a major fuel release from the primary system as a function of time. The early failure of the primary system to retain the fuel is a low probability event which results from a large quantity of fuel reaching the core support dome in a short time after the accident initiation. This, in combination with several other low probability events, leads to a failure of the core support dome and subsequent failure of the primary system boundary. The late failure of the primary system boundary is the ultimate result of a piping elbow failure and the loss of sodium from the primary system. The piping elbow failure is caused by the accumulation of core debris into a non-coolable debris bed in the piping elbow. This non-coolable debris leads to a molten fuel pool and melt-through of the piping elbow.

In addition to generating the probabilities used in the histogram, the model also provides further information for each of the time categories describing primary system events that occur prior to the major fuel release. These descriptions are used in the consequence evaluation and as feedback to the core disruption analysis. The primary system event descriptions shown

in the computer printout of Fig. 3 are 1) the expected value of the fuel/coolant interaction product (defined as the sum of the products of the number of such interactions times the fraction of fuel inventory involved), 2) the expected value of fuel vapor released from molten debris pools to overlying sodium expressed as a fraction of the total fuel inventory, 3) the probability of melt-through of the primary piping occurring before the major fuel release (early fuel release), 4) the expected value of the time, in seconds, of this piping melt-through, and 5) the expected value of the amount of fuel (expressed as a fraction of total inventory) released from the primary system as a result of the piping melt-through.



TIME OF FUEL RELEASE	TIME CATEGORY PROBABILITY	E. V. OF FUEL COOLANT PRODUCT	E. V. OF BOILED FUEL VAPOR	PROB. OF EARLY FUEL RELEASE	E. V. OF EARLY FU. REL. TIME	E. V. OF QNTY OF EAR FU REL
1 ST HOUR	0.5000E-02	0.1983E 01	0.1619E-02	0.	0.	0.
2 ND HOUR	0.	0.	0.	0.	0.	0.
3 RD HOUR	0.5000E-02	0.1694E 01	0.	0.	0.	0.
4 TH HOUR	0.	0.	0.	0.	0.	0.
5 TH HOUR	0.	0.	0.	0.	0.	0.
6 TH HOUR	0.	0.	0.	0.	0.	0.
7 TH HOUR	0.	0.	0.	0.	0.	0.
8 TH HOUR	0.	0.	0.	0.	0.	0.
9 TH HOUR	0.	0.	0.	0.	0.	0.
10 TH HOUR	0.	0.	0.	0.	0.	0.
11 TH HOUR	0.	0.	0.	0.	0.	0.
12 TH HOUR	0.	0.	0.	0.	0.	0.
12 TO 18 HOUR	0.	0.	0.	0.	0.	0.
18 TO 24 HOUR	0.	0.	0.	0.	0.	0.
2 ND DAY	0.4500E-01	0.3507E 01	0.5493E-04	0.1000E 01	0.1111E 04	0.2290E-01
3 RD DAY	0.1500E-01	0.3589E 01	0.	0.1000E 01	0.1127E 04	0.2772E-01
4 TH DAY	0.1000E-01	0.3896E 01	0.1870E-04	0.1000E 01	0.1110E 04	0.2497E-01
>4 TH DAY	0.1000E-01	0.3393E 01	0.1414E-04	0.1000E 01	0.1160E 04	0.2343E-01
NO RELEASE	0.9100E 00	0.2935E 00	0.	0.	0.	0.

Figure 3 Model Results for an Example Application

CONCLUSIONS

A detailed description of the models and calculation techniques employed within the computer code will be published in the near future. Model development is expected to continue as the model is applied to various reactor designs within the context of overall risk assessments.

Applications of the debris retention model show that the uncertainties in the modeling of accident events produce a range of predicted accident outcomes. Although we have not yet conducted a thorough sensitivity study for the debris retention model, the probability of primary system debris retention in the most recent risk assessment application (FFTF) appears to be most sensitive to uncertainties in the modeling of the following accident events:

- The fuel particle size distribution after a molten fuel/coolant interaction
- The transport of the core debris particles by sodium
- The geometry of core debris beds within the primary piping system, particularly for debris in the piping elbow at the reactor vessel exit
- The critical decay heat level for fuel particles in a core debris bed. Decay heat levels above this critical level produce more heat within the bed than can be transferred to sodium and, thus, cause the formation of a molten debris pool.
- The heat flux from a molten pool of core debris to adjacent steel structures

Increased understanding of the accident events along with further efforts to quantify accident event uncertainties should reduce modeling uncertainties and, therefore, uncertainties in the predicted accident outcome. However, uncertainties in the predicted outcome of a core disruptive accident will continue even after further research into accident events because of intrinsic uncertainties in certain accident events and the impracticality of conducting prototypic experiments for some of the accident events. It is assumed within the debris retention model that the probabilities of accident events other than those included in the model are very small and will have a negligible effect on the accident outcome probabilities. Quantified estimates of the probabilities of accident events other than those utilized in the model could be used in developing an upper limit to the predicted debris retention reliability or these probabilities could be added to the model to improve the prediction of the accident outcome.

What are some long term uses of the debris retention model if its development is continued? The model could be used for evaluating design alternatives within the constraint of a debris retention probability criterion, or a risk criterion if employed within a general risk assessment model. The debris retention model could also be used to conduct sensitivity studies of the models for the various accident events with the purpose of indicating which areas of research and development to pursue for the most fruitful payoff in improving the predicted reliability of the primary system to retain core debris. It has been shown by Atcheson and Temme [1] that sensitivity studies conducted with probabilistic models may yield different conclusions from conventional deterministic sensitivity studies. These authors explain that in

some instances the true sensitivity is only apparent from the results of a probabilistic sensitivity study.

ACKNOWLEDGEMENTS

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CONSIDERATIONS OF THE THIRD LINE OF ASSURANCE -
POST-ACCIDENT HEAT REMOVAL AND CORE RETENTION
IN CONTAINMENT

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ABSTRACT

LMFBR safety through defense in depth is provided by four lines of assurance (LOA 1 to 4). Primary emphasis in design is on LOA-1 or prevention of accidents, but the LMFBR can also be shown to possess inherent safety characteristics in the second, third and fourth lines. These lines would limit core damage, retain radioactive materials in the primary system cells and mitigate the consequences of very low probability events. The main concern of this paper is with LOA3. Functional requirements and pertinent considerations of core retention and decay heat removal are given. The scenario of a postulated accident is described from the stage of core debris dispersion to the final stage of debris settling in either the reactor vessel or at the bottom of the reactor cell. Parametric studies for conservative scenarios which assume liner failure and subsequent chemical reactions have been performed for early-sized LMFBRs. The containment response appears to be highly dependent on the structural integrity of the reactor cell liner and concrete dehydration rates in the case the liner has failed. Radiological assessments indicate that 10CFR100 guidelines can be met for all considered cases.

LINE OF ASSURANCE CONCEPT

At present four lines of assurance are considered for LMFBR safety [1]. The objective is to provide defense in depth against uncontrolled release of radioactive materials to the public environs. The lines of assurance (LOA) are:

- LOA1: Prevention of accidents
- LOA2: Limitation of core damage
- LOA3: Containment of accidents in primary system cells
- LOA4: Attenuation of radiological materials.

LOA1 comprises the main safety systems of the reactor such as the control and shutdown system, the decay heat removal system and inherent safety features etc.. These systems maintain an adequate power/coolant relationship in the

reactor core during all operating conditions and shutdown. LOA2 is designed to prevent the propagation of single unit core assembly failures towards a major accident. LOA3 and 4 limit the potential release of radioactive materials from the containment building to levels below the specifications of the Code of Federal Regulations. In general the inherent retention capability of the reactor cell and containment is sufficient to meet these requirements.

PREVIOUS CONSIDERATION OF ACCIDENT CONSEQUENCES AND SAFETY ASSURANCE

The safety approach of LMFBRs has progressed from early concepts which eliminated "any conceivable" cause of accidents and investigated "maximum consequences" of whole core disruption to a full consideration of all lines of assurance. Early consequence evaluations were influenced by the EBRI meltdown which occurred in 1955. In this incident a central part of the core melted and sunk to the lower part of the core during a planned power transient when a thermocouple failed. Detailed data on the accident scenario were obtained. The observation indicated among other things that uranium from the core was dispersed both upward and downward from the center of the core, steel and metallic fuel materials were mixed and freezing occurred in the blanket. The dispersal role of the liquid metal coolant became apparent. No explosive forces developed.

Information on the kinetic energy release during a core meltdown accident was obtained from the GODIVA experiments [2] where three pieces of 93.7% U-235 in form of a sphere with 55 kg mass and no reflector encountered an accidental prompt burst with low kinetic energy release (1.4% of total energy) [3]. Accident analysis models which were developed later incorporated such information. Up to the FFTF design, these models were based on the assumption that the upper half of the core could melt and fill the space usually occupied by sodium at the lower half. The reactivity insertion would depend on gravity collapse of all of the fuel in the core at the worst possible moment [4]. The burst behavior of the core was modelled to be much like that of GODIVA.

These sequences were believed to be conservative with respect to the total energy generation in accidents. Such assumptions were generally acceptable for the reactors with small fuel masses (up to 2200 kg). As the reactor cores became larger, the need arose for a mechanistic treatment and better physical understanding of hypothetical core disruptive accidents. Thus complex analytical techniques, e.g. SAS and VENUS [5,6] and extensive experimental programs such as TREAT [7] and SLSF [8] evolved. These programs, focused on the initiation phase of core disruptive accidents, provided further strong evidence that violent core disassembly considered in earlier LMFBR safety analysis is improbable. Attention of the experimental and analytical program has consequently shifted toward core disruptive sequences involving core meltdown rather than core disassembly. This development work focuses on LOA3.

Meanwhile, it is now generally believed that core disruptive accidents can be made sufficiently improbable by design that they need not be part of the design basis. Thus the current design efforts for commercial-sized reactors (1200 to 2000 MWe) focus strongly on LOA1 and LOA2.

Of the five fast reactors built in the United States prior to the Clinch River Breeder Reactor only two included specific provisions for LOA3, the Southwest Fast Oxide Reactor (SEFOR) [9] and FERMI [10]. For SEFOR a retention tank designed to retain the entire primary sodium was located below the reactor vessel. The tank consisted of steel sheet metal surrounded by an insulation layer of calcium silicate. The decay heat from the entire core could be transferred from the tank to the surrounding containment structure without

damaging these barriers.

Fermi utilized two design features to increase the capability for molten core retention and post-accident heat removal. A zirconium plate was inserted in the flow inlet plenum to resist penetration of molten core debris and promote debris spreading into a non-critical layer. Additionally, a graphite coated crucible was placed below the lower reactor vessel head. The graphite protection layer included 5% boron to assure neutronic shutdown.

FFTF has been shown to be capable of permanent core debris retention of 90% of the active core in a dispersed form and complete decay heat dissipation [11].

OBJECTIVES OF THE THIRD LINE OF ASSURANCE

The retention of radioactive materials in the primary cells of the reactor containment building or preferably in the reactor vessel after a large core disruptive accident, is the main concern of LOA3.

Successful retention of radioactive materials within the cell structure following failure of LOA1 and LOA2 would depend on the control or accommodation of the following parameters:

- a. the temperature and pressure in the reactor building,
- b. material interactions and heat generation by chemical reactions,
- c. the resistance of containment materials and seals and degradation of these components,
- d. initial accident conditions, including accident energetics, and
- e. the fission product distribution.

Several concepts for retention have been considered [12,13] including:

- a. systems with active cooling that prevent boiling of sodium and material interactions and degradation, and
- b. passive systems with limitation of pressure buildup and limitation of chemical reactions.

Both systems are capable of meeting functional requirements in the early stages after core disruption. It appears that the active cooling and retention systems would have an advantage for long term retention of radioactive materials. Concrete containments without cooling devices could leak after prolonged exposure to high temperatures due to material degradation and cracking.

POST-MELTDOWN SCENARIO

Hypothetical low probability events with maximum consequences such as a loss of flow (LOF) accident or transient overpower (TOP), coupled with the failure to scram have been used as a basis for safety considerations of early sized LMFBRs (1100 Mwt size). These events envelop the consequences of other mechanistic sequences such as the propagation of fuel assembly blockage without scram and a large gas bubble passing through the core without scram. An LOF scenario can lead to the most severe meltdown conditions.

In a LOF type accident, the decrease in sodium flow would cause sodium boiling in the core region, flow reversal, coolant channel voiding, cladding

and fuel melting and relocation under the influences of gravity and vapor flow shear forces. Neutronic shutdown would occur by dispersal of fuel away from the active core region. The axial direction is favored for material flow due to low inertial and thermal resistance forces. Massive structures prevent dispersion in the radial direction. Transient freezing of molten core debris in contact with a cool lower support structure would promote vapor driven ejection in the upward direction.

In the absence of adequate heat removal the frozen core debris would remelt due to internal heating. The heat sources are produced by absorption of β and γ radiation emitted from fission products, Np-239 and U-239. γ radiation represents a significant mechanism for energy transport which promotes the melting process of steel support structures and advance of core debris in the downward direction.

In contact with liquid sodium, fuel and steel debris will fragment into small sized particles [14] with average size fuel particles of approximately 200 μm and steel particles of 400 μm . The fragmentation is primarily due to thermal and mechanical interactions including hydraulic forces and thermal stress cracking [15]. For small amounts of fuel debris complete fragmentation has been observed [14]. The fragmentation process of large masses of debris is associated with uncertainties. Unfragmented masses of debris in contact with inadequately cooled steel structures may lead to local structural failure.

In order to determine the decay heat dissipation in the reactor vessel and the response of structures some knowledge of the debris settling time, the final geometry and location is required. The settling characteristics of heat generating particles in recirculating flow are not well understood. The heat delivered from the particles to the surrounding fluid is too small to create significant buoyant forces [16]. Gravitational, inertial and fluid drag forces determine the particle motion relative to the fluid. Settling times have been studied for stagnant pools indicating characteristic times for complete settling within 600 seconds for a pool of 9 m depth [17]. However, this time can change since a small particle volume fraction of 2.6×10^{-6} is sufficient to cause onset of fluid motion and forces on the particles. The residual fluid flow after the accident also affects the settling time.

It has been shown experimentally that layers of internally heated particles which have settled at the bottom of a pool of fluid would be stable up to a certain critical depth. If this depth is exceeded, the vapor generation rate is greater than the rate at which gravity can maintain a steady flow of sodium through the bed so that sodium dryout would occur [18,19]. For typical core sizes of early sized LMFBR, it can be shown that 8 to 10% of the core would be stable as a particle bed at the bottom of the reactor vessel; however, 2 to 5% in a molten form could lead to vessel meltthrough.

For the event of large accumulation of core debris (10 to 100%) at the bottom of the reactor vessel, heat transfer analysis indicates that the debris would be in a molten state. If the steel and fuel separate immediately due to density differences, no boiling would be encountered. For a homogeneous mixture of fuel and steel the maximum layer temperature can be determined from Jahn's investigations [20] as 3200°K. This temperature is well below the boiling point of fuel (3600°K), but lies above that of steel (3080°K). Therefore, in a homogeneous mixture, steel boiling may occur. This would increase the upward heat transfer and sodium boiling leading to further pressurization of the lower vessel.

Core debris in contact with the bottom of the reactor vessel would penetrate through the steel wall. Vessel failure may occur due to steel structure melting and creep rupture. A two-dimensional model was applied to study these processes and to establish conditions under which the vessel could be kept intact. Characteristic failure times of 12 to 60 minutes have been found for meltthrough failure if heat removal from the bottom of the vessel is by thermal radiation only. High pressures exerted on the vessel would cause structural failure prior to melting. Calculations show the time to failure can be significantly increased if the vessel temperature is maintained below the melting point.

Considerable improvement of the retention capability within the reactor vessel can be accomplished by placing a single tray for debris collection in the sodium plenum underneath the core support structure. Decay heat transferred in the downward direction would be dissipated by natural convection under each tray. A core debris catch tray is included in the PFR design. Stagnation point effects at the center of the tray are reduced by injecting cold sodium at this point [21]. In the FFTF and SNR-300 the cooled bottom plate of the flow inlet plenum possesses significant inherent retention capability for core debris. In-vessel retention can be achieved by limiting core damage (LOA2) and limiting the amount of debris penetrating into the lower part of the vessel to approximately 20% (for early-sized LMFBRs).

The conditions existing shortly after failure of the reactor vessel and guard tank structure are highly uncertain. In general, conservative assumptions are made for evaluating the transient containment response and radiological consequences following these events. It is possible that a stream of debris falling from the guard vessel onto the reactor cell floor may locally fail the liner. This would expose the concrete to chemical attack by core debris and sodium. The anticipated chemical reactions are exothermic, generate both gases and aerosols and consume primary system sodium.

Sodium oxide, sodium hydride and sodium hydroxide are products of the water/sodium reaction. However, at temperatures above 412°C as expected in the reactor cell, the equilibrium of the reaction would shift to the formation of sodium oxide and sodium hydride. The latter would exist only under sufficiently high hydrogen pressures. The consecutive reactions of end products can be of importance if additional heat generation and sodium consumption take place. An example for this would be the formation of sodium carbide as a result of the sodium/carbon reaction where the carbon is generated by sodium/limestone or dolomite aggregate interaction.

Calculations indicate that conductive heat transfer into the concrete wall of the reactor cell, vessel internals, and sodium is not sufficient to prevent continuous sodium boiling [13]. Complete sodium boiloff from the reactor cell would occur within approximately 100 hours. This time may be on the order of 28 hours in the case of complete liner failure.

Concrete structures show a degradation of material strength when exposed to high surface heating rates. This degradation is expected to proceed slowly due to the low thermal conductivity of concrete. The core debris is expected to spread into a thin layer at the bottom of the reactor cell. No penetration into the concrete structure would be expected if a particle layer forms. A molten layer will cause limited melting attack. Transport of hydrogen to the air-filled containment may lead to the autoignition of the mixture [22]. Controlled hydrogen burning or air purge could be employed to prevent an explosive interaction.

The transient containment response after reactor vessel and guard tank failure has been studied in detail. The areas of main interest included

concrete dehydration, containment pressure and temperature time histories, and radiological consequences.

CONCRETE DEHYDRATION

Parametric studies on core meltdown consequences have revealed a high sensitivity to the amount of water present at the accident site. This water is contained in the concrete structures in form of free water, adsorbed (gel) water, interlayer water and chemically bound water. Significant amounts of water can be released to the reactor cell due to heating of these structures, resulting in exothermic reactions with sodium, sodium consumption and undesired gas generation. Consequently, efforts have been made to study and model the water release phenomenon and to predict the distributions of the temperature, pressure, and moisture content within the concrete as a function of time.

A model based on thermodynamic principles was developed which considered water transport in both the liquid and vapor form. The concrete was treated as a porous material. Heating its surface results in expansion of gases and evaporation of water within the pores. Therefore, pressure and species concentration gradients are created causing mass flow and species diffusion, respectively. If heating proceeds, a stage is reached where the hot portion of the concrete completely dries out the water. This zone expands upon further heating. Recondensation of vapor occurs at lower temperature zones and enhances the rate of heat transfer.

The above model has been coded and successfully tested with preliminary experimental results obtained by HEDL. Calculations indicate that the rate of water release from the heated surface is fairly insensitive to heating conditions (within certain ranges). A typical water release-time history is shown in Figure 1. The pressure distribution at 1, 4, 11 and 24 hours after surface heating is depicted in Figure 2. At elevated temperatures pore pressures generate stresses that may exceed the tensile strength of concrete and spallation may occur. From the present study it is evident that the mechanism of energy transport in concrete is not merely conduction but one of convection and phase change too. Water is released to both the heated and unheated side of the concrete.

TRANSIENT CONTAINMENT RESPONSE AND RADIOLOGICAL CONSEQUENCES

The thermal-mechanical response of the containment of an early-size LMFBR (1100 MWt) was investigated for a range of conditions assumed to exist after a postulated core disruptive accident.

A four cell model that included the reactor cell, the primary heat transfer cells, the head compartment area and the containment cell, was employed in this study. Typical dimensions of the model are shown in Figure 3. The CACECO Code developed by HEDL was used to determine pressure and temperature time histories in the individual cells. The following assumptions were made:

- a. The reactor core penetrates through the reactor vessel and guard tank within 1000 seconds after termination of the postulated accident.
- b. The containment and cell structures consist of limestone aggregate concrete, which releases H_2O and CO_2 upon heating by core debris and sodium.
- c. Free pressure communication exists from the reactor cell into the primary heat transfer system (PHTS) cells and the head access area (HAA) cell.
- d. The leak rate from the PHTS cell and HAA cell to containment cell is 100%

volume per day at 0.7 and 1.05 atm, respectively. The containment cell leaks 0.1% volume per day to the outside atmosphere at 0.7 atm. The degradation of concrete due to exposure to high temperatures does not effect these rates.

- e. An air purge is activated in the containment cell whenever the hydrogen concentration exceeds 4% volume. This presents a conservative lower limit for hydrogen autoignition in air.

In the case the reactor cell liner would stay intact a maximum reactor cell temperature of 1225°K and pressure of 1.9 atm. would be obtained. The sodium inventory would be boiled off within 96 hours. If the bottom liner of the reactor cell should fail initially, the cell pressure would increase to 2.2 atm. and sodium would be evaporated and consumed by chemical reactions within 66 hours. The severest reactor cell condition resulted from initial bottom liner failure and side wall liner failure 10 hours later. The peak pressure in this cell would reach 2.6 atm. and sodium would disappear within 49 hours. The range of reactor cell pressures and temperatures is shown in Figure 4.

It was observed that the PHTS presents a major heat sink for all cases considered. Heat transport to these cells is by sodium vapor flow which condenses at the cell walls. The cell pressure and temperature time histories are sensitive to the rate of water release from the reactor cell concrete structure. In the case of a low release rate no hydrogen purge in the containment cell would be required. Additionally, the temperature and pressure increase would occur at a slower rate. Variations in the time to sidewall liner failure, time to vessel and guard tank failure, and sodium reflux showed little change in maximum reactor cell temperatures and pressures. The time to complete loss of sodium from this cell would be more significantly affected.

The radiological consequences were evaluated for the described accident scenarios with the COMRADEX Code [23]. Examination of the total 2 hour site boundary and 30-day Low Population Zone (LPZ) dose for case without any filtration of release products shows the doses are well below the 10CFR100 guideline limit. Maximum doses were obtained for the case with initial bottom liner and 10 hours delayed side wall liner failure. For this case the 30 day LPZ thyroid dose is approximately 15% of the 10CFR100 limit. Insertion of a filter lowers this value to 3% of the allowable guideline limit.

CONCLUSIONS

In-vessel debris retention supported by a strong second line of assurance (limitation of core damage) is preferred over ex-vessel retention. The latter involves complex analyses of material interactions and predictions of the behavior of concrete structures. These evaluations are presently associated with large uncertainties due to the lack of conclusive experimental information. Interactions in the reactor cell could be minimized in insulating the concrete structure with a layer of refractory oxides such as Al_2O_3 . This insulation layer would also reduce water release into the reactor cell following liner failure. The steel reinforcement can be designed to minimize concrete spallation. The evaluations indicate that for early-sized LMFBRs the ex-vessel retention of core debris in the reactor cell is possible without the use of active heat removal systems. Dose rates for the worst scenario assumed stayed well below 10CFR100 limits.

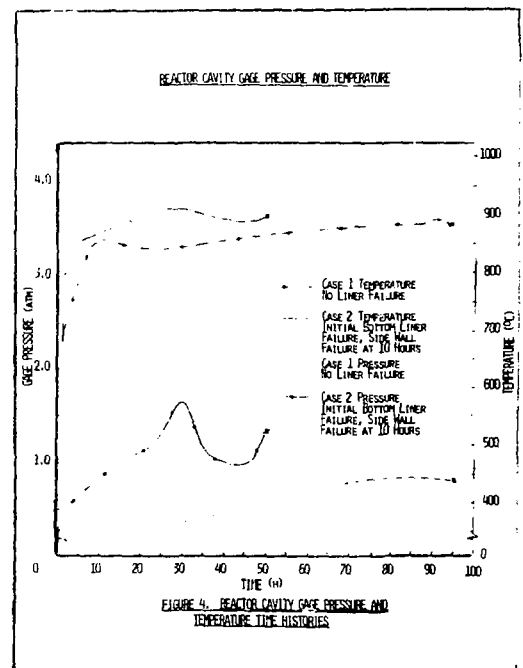
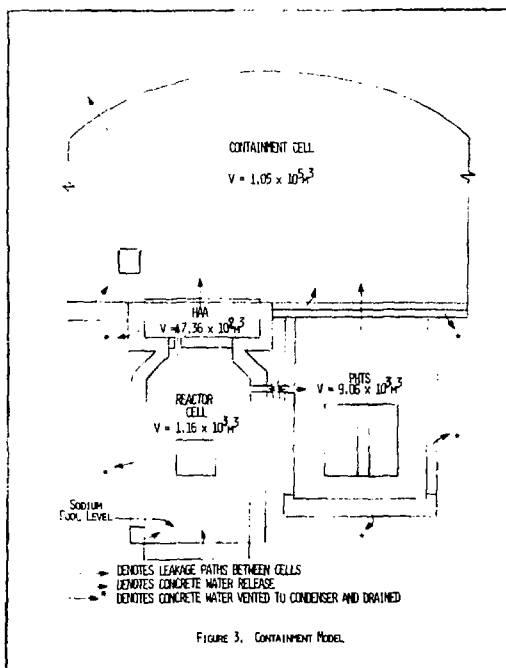
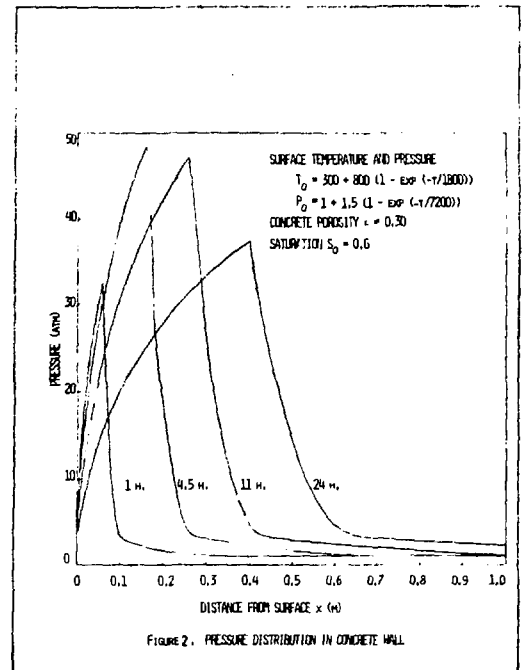
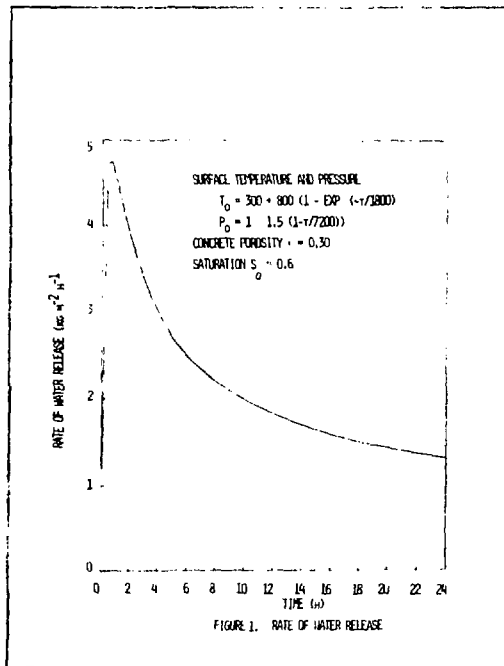
ACKNOWLEDGEMENTS

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POST-ACCIDENT FUEL CONTAINMENT IN THE GAS-COOLED FAST BREEDER REACTOR

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ABSTRACT

The upward heat removal capability in the 300 MW(e) GCFR demonstration plant following a hypothetical core meltdown accident has been investigated. It was assumed that no forced helium circulation is available, and a delay of 30 minutes has been assumed before the loop isolation valves in the Core Auxiliary Cooling System (CACS) are opened. Following opening of the isolation valves, a natural convection helium flow is established which provides heat transport from the melt surface to the water cooled CACS heat exchangers. The analysis showed that no melting of internal components would occur for system pressures greater than 6 atm and that no melting in the radial blanket assemblies would occur for system pressures greater than 14 atm.

INTRODUCTION

Analyses of the post-accident fuel containment (PAFC) capability and of the associated post-accident heat removal (PAHR) phenomena in the event of a core meltdown in the gas-cooled fast breeder reactor (GCFR) have been underway at General Atomic (GA) and at Argonne National Laboratory (ANL) for several years. At GA this work has included analytical evaluations in order to determine the important phenomena for a range of potential conditions regarding initial conditions, operation of cooling systems, etc. This paper reports on an assessment of the natural convection capability for upward heat removal from a melted core pool on the lower thermal shield.

Previous analyses [1] indicated that for the current design of the lower thermal shield, downward heat removal from the core melt must be provided by the liner cooling system in order to prevent PCRV liner overheating, and upward heat removal is required to prevent melting of the central cavity internals. However, several hours are available to establish the required cooling capacity. In the downward direction an enhanced liner cooling system would be capable of removing the downward heat flow which begins to increase to above normal levels after about six hours. In the upward direction the operation of only one core auxiliary cooling loop appears adequate to prevent cavity internal structures from reaching the melting temperature even under depressurized conditions. Although these preliminary conclusions were encouraging, the analysis upon which they were based assumed that active heat removal systems are available.

As a next phase, an investigation of the consequences of not having an active heat removal system inside the PCRVR was made. For this analysis, neither the three main loop circulators nor the three core auxiliary helium circulators are assumed to be operational and the only means of upward heat removal is by natural convection of helium. Only a continuous cooling water supply to the PCRVR liner cooling tubes and to the three core auxiliary heat exchangers is assumed.

INITIAL CONFIGURATION AND ASSUMPTIONS

The initial configuration of a 300 MW(e) GCFR reactor cavity following a postulated core meltdown accident is shown in Fig. 1. A partial core meltdown, where the upper axial blanket and the radial blanket remain in place is assumed. The dropped core debris, assumed to be molten, is accumulated above the lower thermal shield. The events leading to this configuration are assumed to have occurred instantly at the beginning of the accident. Following a total loss of forced circulation, the helium loop isolation valves would normally be closed, such that natural convection coolant flow paths can only develop within the central cavity. After a 30 minute delay, the isolation valves in the core auxiliary cooling loops are assumed to be mechanically opened and a natural convection flow path develops through the auxiliary cooling loops as shown in Fig. 1. The helium flows upward through the central portion of the reactor cavity which forms the hot leg with an average temperature T_h , and then downward through the auxiliary heat exchangers to the outer radial shield annulus which forms the cold leg with an average temperature T_c . The effective loop height h is measured from the lower end of the outer radial shield to the mid-height of the auxiliary heat exchangers.

The two-dimensional computational model as well as the initial conditions, are shown in Fig. 2. For simplicity, irregular geometrical shapes have been replaced by regular ones. The perforated grid plate and the blanket rod bundles have been represented by equivalent solid masses. The radial shields and the radial blanket assemblies are assumed to consist of one material having the weighted property values of the actual materials. The molten core debris is assumed to separate into two distinct layers with the molten stainless steel from cladding and assembly ducts overlaying the molten fuel. Volatile fission products are assumed to have escaped into the reactor cavity where they flow with the helium to the auxiliary heat exchangers. The volatile fission products in the unmelted blanket are not released to the helium. The non-volatile fission products are distributed in the oxide fuel and the molten stainless steel. Based on the isotope-by-isotope decay heat rate for the first 30 hours which covers the time span of the present analysis, 80% of the decay heat (from the non-volatiles) is generated in the oxide region, and the remaining 20% is generated in the molten stainless steel region. Based on previous analyses [1], two thirds of the heat generated in the oxide layer is assumed to flow upward into the steel layer. A uniform heat flux passing into the bottom of the molten stainless steel layer, constitutes the lower boundary of the computational model. The sides of the stainless steel layer are assumed to be insulated. Internal free convection within the molten stainless steel layer is treated as an equivalent heat conduction with an assumed effective thermal conductivity based on previous analysis [1]. All the reactor internal surfaces emitting or reflecting thermal radiation are assumed to be gray and diffuse. Emissivity of the molten stainless steel surface is assumed to be the same as that of solid stainless steel. The helium, partially mixed with aerosols, is assumed to be transparent.

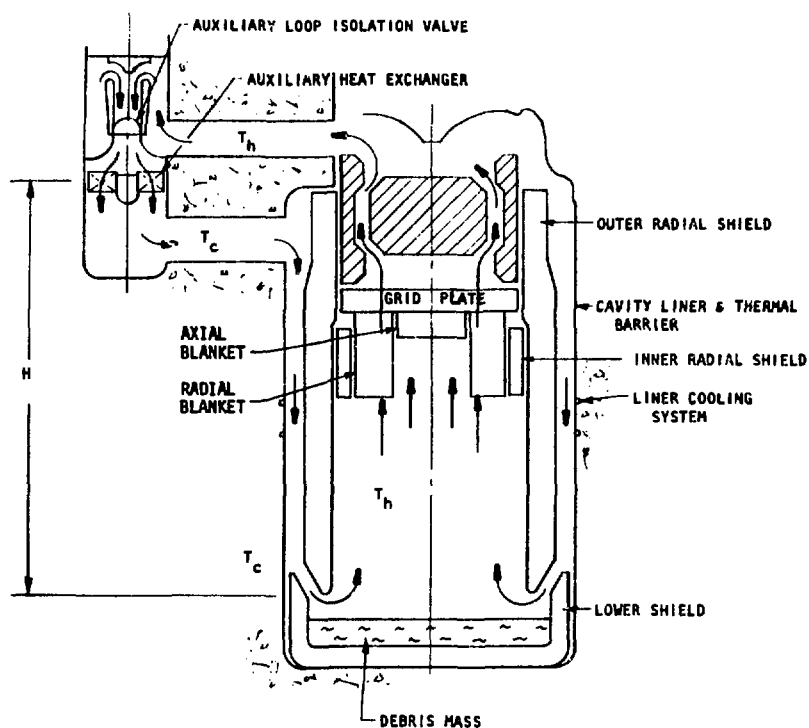


Fig. 1. Initial configuration and natural circulation flow path

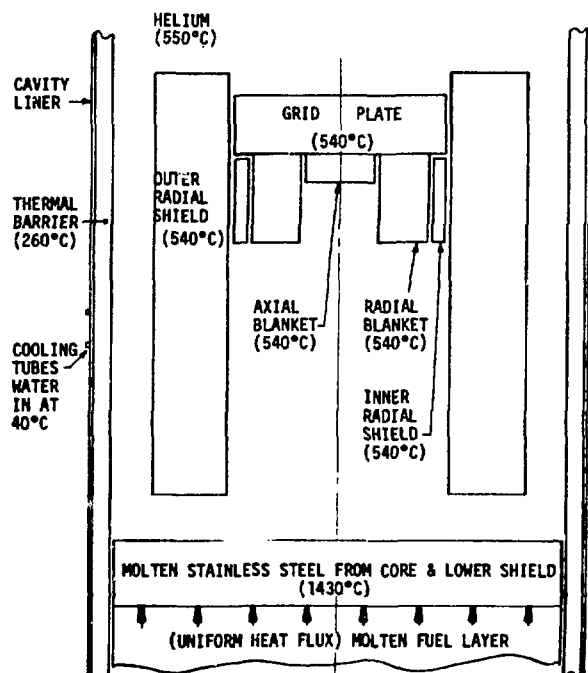


Fig. 2. Computational model with initial temperatures

ANALYSIS

Radiative Heat Transfer

Radiative heat transfer from the core debris surface to the reactor internal structures serves as a major heat transfer mechanisms for upward decay heat removal. The cavity region affected by thermal radiation is divided into 4 sub-regions of regular geometry which are separated by fictitious gray surfaces. The apparent emissivity of these fictitious surfaces is found by considering the separated regions as cavities facing each other [2]. Geometric view factors for each region are calculated based on standard view factor algebra. Modified radiative interchange factors in a gray enclosure are then found by Hottel's method [3]. An equivalent radiative conductance between any two surfaces is then established and the non-linear effects are accounted for by including a temperature dependence of the equivalent conductance.

Natural Circulation of Helium

The natural convection of helium is induced by density differences between the hot helium in the core cavity and the cold helium in the annulus formed by the outer radial shield and the thermal barrier over the effective height of the gas columns. The helium mass flow $\dot{m}$ can thus be termed by

$$\dot{m}^n = \frac{H \cdot p^2}{\beta R} \frac{1}{T} \left(\frac{1}{T_c} - \frac{1}{T_h} \right) \quad (1)$$

where H is the effective height of the gas columns, p is the helium pressure of the system, R is the gas constant, T is the average loop temperature, T_h and T_c are the hot and cold leg temperature, respectively, and β is the loop pressure drop constant which is determined by the nominal pressure drop at nominal flow, pressure and temperature conditions for decay heat removal in the auxiliary cooling loops. It is expressed by

$$\Delta P_o = \beta \frac{\dot{m}_o^n T_o}{P_o} \quad (2)$$

where the subscript "o" refers to the nominal condition. The constant n, which appears in both Eqs. (1) and (2), is 2.0 if $\dot{m} \geq \dot{m}_o$ and 1.8 if $\dot{m} < \dot{m}_o$. The smaller value of n takes account of Reynolds number dependency of the loop friction coefficient.

Heat Convection at Reactor Internal Surfaces

The helium flow induced by natural convection can be laminar or turbulent, and either developing or fully developed along its flow path. Formulas for the heat transfer coefficients at various reactor internal surfaces are given in standard text books [4,5,6]. For most flow conditions the Nusselt number is well represented by the relation

$$Nu = a Re^m Pr^n \quad (3)$$

Where Re and Pr are Reynolds and Prandtl numbers, respectively; and a, m and n are constants which depend on the type of flow.

Helium flow through the grid plate is treated as flow through circular tubes. Because of the small length to diameter ratio, the entry-length effect is included. For both the laminar and turbulent range. Nusselt numbers are given by Kays [4]. In the axial and radial blanket regions a flow parallel to a bank of staggered circular rods is modelled. For this type of flow, Kays [4] has treated both the laminar and the turbulent cases and plotted the average Nusselt numbers as functions of the pitch to diameter ratio. Along the inner and lower surfaces of the outer radial shield a boundary layer flow along a flat plate is modelled. Heat transfer solutions for this type of flow are well-known. For the upper surface of the core debris, a stagnation flow condition may be assumed. Equations for the Nusselt number are given by Kays [4].

For the first 30 minutes, when no helium circulation through the core auxiliary cooling loops is assumed, local natural convection is considered in the central region of the lower cavity above the core debris surface, and also in the annulus between the outer radial shield and the thermal barrier. Both sides of the outer radial shield as well as the thermal barrier surface are considered as cooled vertical plates. The portion of the debris surface facing the central cavity is treated as cooled horizontal plate facing upward. The lower surfaces of the inner radial shield and the remaining core are treated as cooled horizontal plates facing downward. The space between the lower end of the outer radial shield and the debris surface is considered as a horizontal gas space. Nusselt numbers for these types of flow are represented by the relation

$$Nu = C Ra^n \quad (4)$$

where Ra is the Rayleigh number and c and n are constants. Experimental data for these types of flow using air as the medium have been collected by McAdams [6].

Heat Transfer in the Auxiliary Heat Exchangers

The auxiliary heat exchangers are of the helically wound cross flow type. The normal direction of the helium flow is upward through the auxiliary heat exchanger and countercurrent to the overall water flow inside the tubes. However, under the reversed flow condition due to natural circulation of helium, these exchangers become parallel flow heat exchangers. The heat transferred from the helium to the water is the product of the overall heat transfer coefficient, the heat transfer area, and the log mean temperature difference, i.e.,

$$q = U \cdot A \cdot \Delta T_{lm} \quad (5)$$

For a fixed water inlet temperature and flow rate the heat balance between the helium side and the water side determines the water outlet temperature. Incorporating this relation into Eq. (5) gives a transcendental equation for the cold helium temperature at the exit of the heat exchangers.

RESULTS AND DISCUSSIONS

Numerical results were obtained using the NUTAK computer code [7], a multi-dimensional network type thermal analysis computer program. Input data required for the computation are given in the Appendix.

The upward flowing portion of the decay heat in the molten pool and the cold helium temperature at the exit of the auxiliary heat exchangers were pre-calculated and the results were prepared as input data to the NUTAP computer code. Results of the decay heat calculation are given in Fig. 3 which shows the total decay heat flowing upward, the decay heat in the core debris melt, the decay heat from the volatile fission products, and the decay heat in the radial blanket. The cold helium temperature, as a function of the helium mass flow and the hot helium temperature at the inlet of the auxiliary heat exchanger, is shown in Fig. 4. A simple linear relation between the cold and hot helium temperatures was observed.

Calculations were made for various depressurization conditions. The lowest residual pressure allowable without melting of any reactor internal structure was found to be 6 atm. The temperature history at various locations is shown in Fig. 5 for a coolant pressure of 6 atm. It can be seen that the temperatures at all surface locations increase during the first 30 minutes when no helium is circulating through the core auxiliary cooling loops. Sudden temperature drops are observed at 30 minutes when the auxiliary loop isolation valves are mechanically opened. The debris surface, assumed molten at the time of accident initiation, reaches its maximum temperature of 1640°C after 2 hours. The surface temperature at the lower end of the outer radial shield reaches the melting point after 2 hours and begins to decrease after 5 hours. Temperatures at other locations are well below the melting point; except in the radial blanket where the temperature increases continuously due to decay heat generation. Only a small helium flow rate is developed through the radial blanket assemblies, because of the high flow resistance compared to the remaining portion of the core elements. After cladding and assembly wall melting in the radial blanket occurs, the blanket assemblies would drop into the molten pool and thereby reduce the flow resistance in the central cavity and consequently more helium flow will be naturally induced. Therefore, convective cooling is improved, resulting in lower temperatures for the structures. This configuration was not considered in the present analysis.

Temperatures at all locations have passed their maximum values within 15 hours, except for the grid plate temperature which continues to increase for another 5 to 10 hours but would not reach the melting temperature. Maximum temperatures at various locations are shown in Fig. 6 as a function of the system pressure. It can be seen that no melting of the radial shield surface would occur for system pressures greater than 6 atm, and no meltdown of the radial blanket would occur for system pressures greater than 14 atm.

Helium temperatures as a function of time are shown in Fig. 7 for various system pressures. Because of the low specific heat capacity of helium, maximum temperatures are reached early in the 30 minute period while the isolation valves are closed. Helium temperatures in the annulus between the outer radial shield and the thermal barrier are much lower than those in the central cavity because of the comparatively larger cooling surface in the annulus. This temperature difference initiates the helium flow at the time when the auxiliary loop isolation valves are mechanically opened. At this moment, a sharp temperature drop can be observed. The temperature difference between the hot leg and the cold leg is also reduced due to the helium circulation. In Fig. 8, the naturally induced helium flow and the percentage of helium flow passing through the radial blanket are shown as a function of the system pressure. The helium flow increases almost linearly with system pressure. For a given system pressure, the helium flow is nearly constant from 1 to 10 hours after the accident initiation. For system pressures

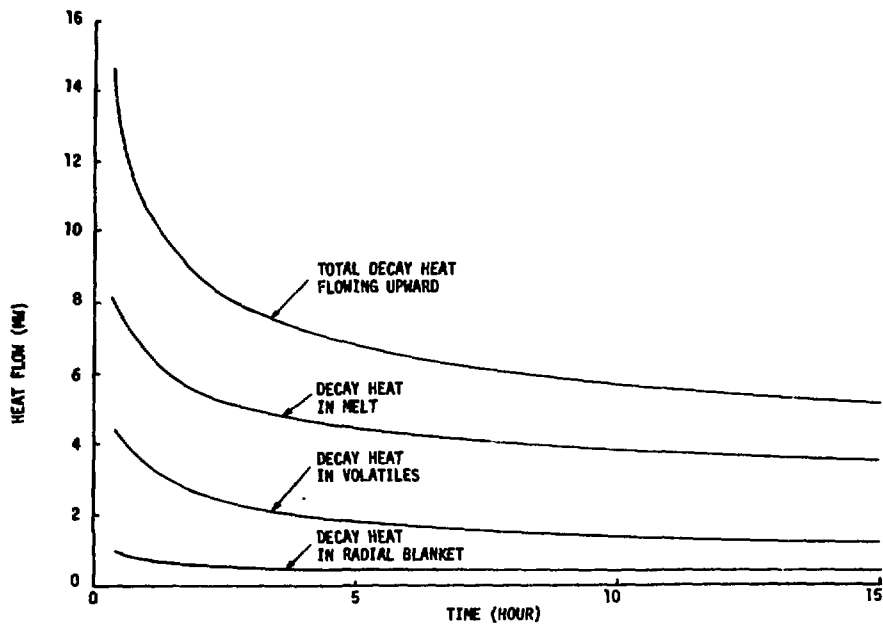


Fig. 3. Distribution of upward flowing decay heat vs. time

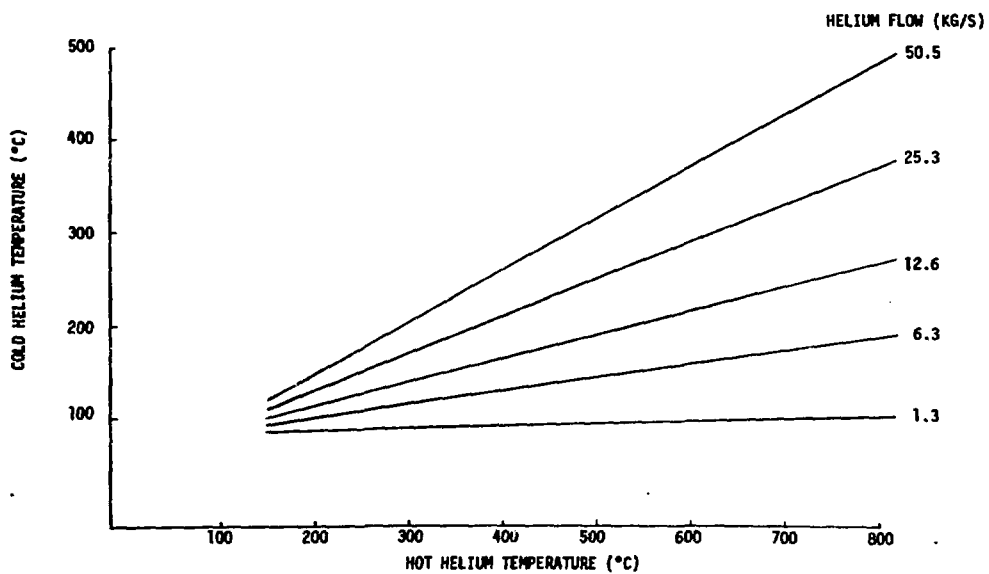


Fig. 4. Hot and cold leg temperatures for various mass flow rates

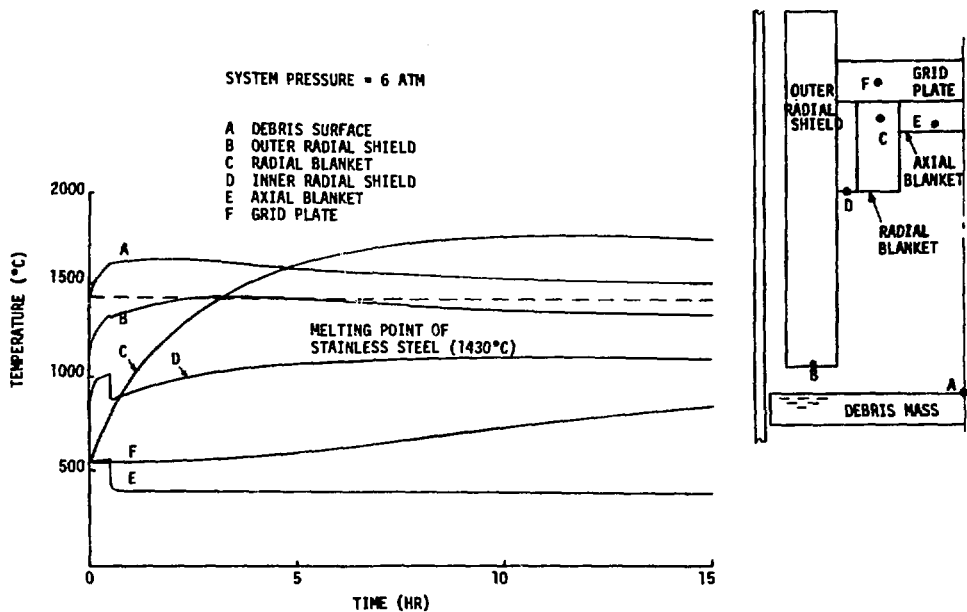


Fig. 5. Surface temperature vs. time at various locations for a system pressure of 6 atm

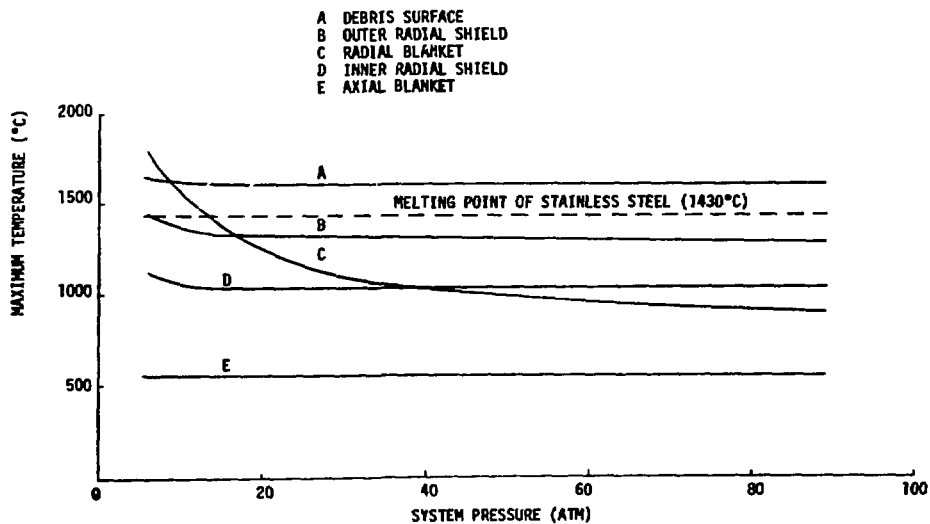


Fig. 6. Maximum temperatures at various locations versus system pressure

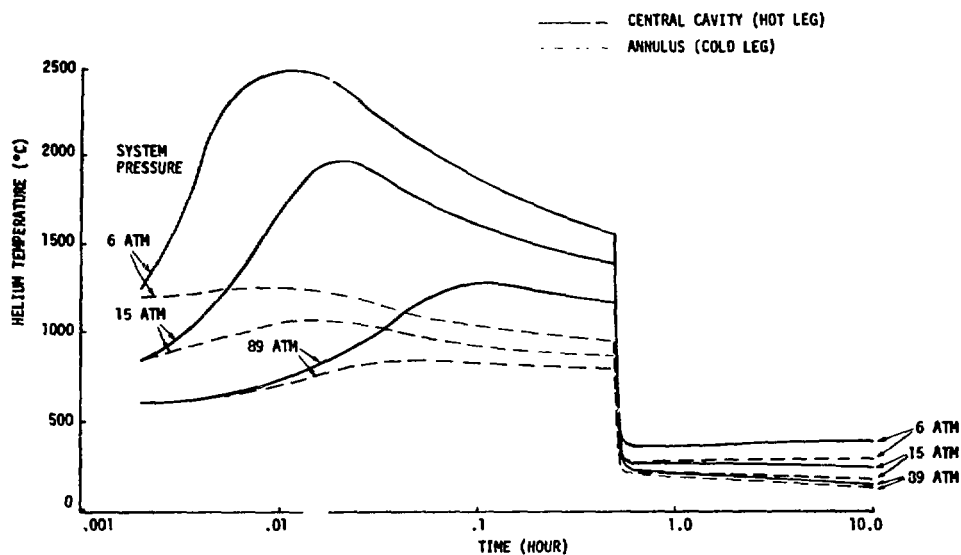


Fig. 7. History of helium temperature for different system pressures

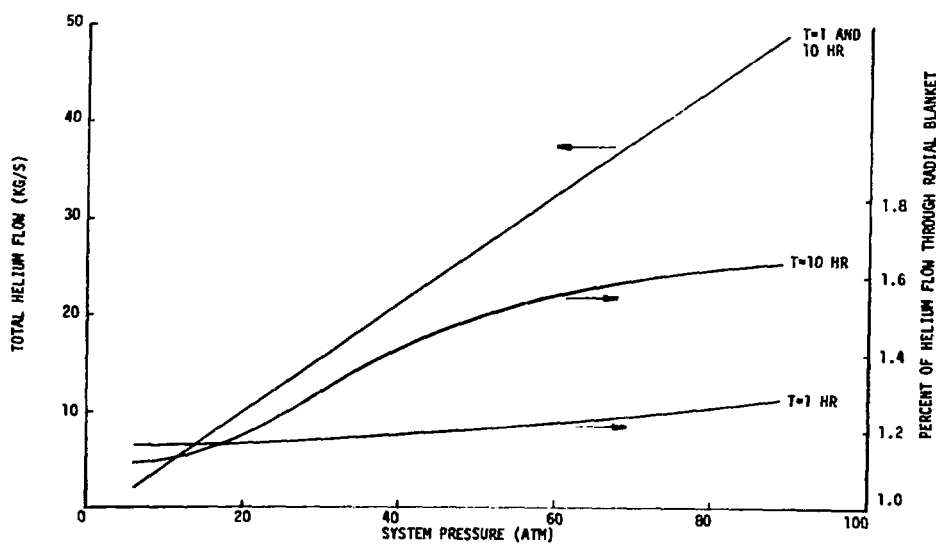


Fig. 8. Natural convection induced helium flow rate vs. system pressure

lower than 17 atm, the percentage of helium flow passing through the radial blankets decreases with time from 1 to 10 hours, because the blanket temperature continues to increase during this time.

CONCLUSIONS

Natural helium convection has been shown to be effective in transporting the upward flowing decay heat from a core melt on the central cavity floor to the core auxiliary cooling system heat exchangers for system pressure greater than 6 atmospheres. Therefore, forced convection helium circulation is not necessary for upward heat removal following a pressurized core meltdown in the GCFR demonstration plant. However, two requirements are necessary to prevent melting of the radial thermal shield at the lower end: 1) the CACS loop isolation valves must be opened within about 30 minutes, and 2) the PCRVR pressure relief valves must maintain system pressure above 6 atm.

For fully depressurized conditions, natural convection of helium will not prevent melting of the lower portion of the radial thermal shield. Probabilistic accident analyses have shown that a core meltdown caused by an accidental primary system depressurization is of significantly lower probability than a pressurized core meltdown accident.

Forced water circulation in the CACS heat exchangers was assumed for this analysis. Future investigations are planned to determine the capability of heat rejection from the CACS heat exchangers by natural convection.

APPENDIX: INPUT DATA

Physical Properties

Physical properties of stainless steel (Type 316), graphite, and fuel and blanket materials were taken from Ref. [8]. Other physical properties used in the analysis are the following:

Thermal conductivity of helium = $3.32 \times 10^{-3} T^{0.674}$ (W/m-°K)

Thermal conductivity of thermal barrier (Kaowool)
= $0.1082 + 3.226 \times 10^{-4} T$ (W/m-°K)

Density of Kaowool = 240 (kg/m³)

Specific heat of Kaowool = 837 (J/kg-°K)

Thermal conductivity of irradiated graphite = 26 (W/m-°K)

Emissivity of stainless steel surface = 0.6

Reactor Cavity

System pressure at normal operating condition = 89 atm

Material of radial shield = 50% graphite and 50% stainless steel by volume

Outer radial shield thickness = 0.69 m

Outer radial shield height = 10.36 m

Annular space between outer radial shield and thermal barrier = 0.25 m

Effective loop height for natural convection = 11.6 m

Size of debris mass (stainless steel) = 6 m (dia.) x 0.34 m (thickness)

Thickness of thermal barrier (Kaowool) = 0.1 m

Liner cooling water temperature = 40°C

Heat transfer coefficient in liner tubes = 363 (W/m²-°K)

Auxiliary Heat Exchanger

Nominal helium flow per loops = 5.05 (kg/s)

Nominal system pressure = 1.77 atm

Water inlet temperature = 82°C

Water flow = 19.4 (kg/s)

Heat transfer area = 106 m²

ACKNOWLEDGMENTS

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POST-ACCIDENT HEAT REMOVAL FOR GAS COOLED FAST REACTORS

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ABSTRACT

The Karlsruhe Nuclear Center and the German firm Kraftwerk Union are working at the design of a 1000 MWe GCFR plant, which has many design features in common with the 300 MWe demonstration plant proposed by General Atomic Company. In a GCFR the absence of large reactivity excursions and the downward direction of the cooling flow ensures that during a gross melt-down accident the fuel is falling on the base of the reactor cavern contained in the Prestressed Concrete Reactor Vessel (PCRVR). Various PAHR solutions for the GCFR based on ZnCl_2 , lead or water have been investigated, however at present the best solution appears to be a core-catcher based on borax, which dissolves the oxide fuel and thus decreases the volumetric heat production in the melt and the heat flux to the water-cooled liner of the PCRVR. Furthermore the 8.7 meter thick concrete slab forming the base of the PCRVR could be used as external core-catcher. Calculations show that to melt this slab it would require 230 days. To use the PCRVR as an external core-catcher, it would be necessary to cool the outer surface of the lower part of the concrete pressure vessel, in order to maintain intact a sufficiently thick layer of concrete.

INTRODUCTION

The Karlsruhe Nuclear Center and the German firm Kraftwerk Union are working at the design of a 1000 MWe GCFR plant, which has many design features in common with the 300 MWe demonstration plant proposed by General Atomic Company in U.S.A. The nuclear steam supply system consists of eight main loops totally housed in a telephone-dial-type prestressed concrete vessel (PCRVR) the central cavity being occupied by the reactor core. In addition to the eight main loops the PCRVR houses four auxiliary loops as a back-up for emergency conditions.

Safety studies account for a substantial fraction of the work that is currently being carried out [1]. The Design Basis Accident is postulated to be initiated by a sudden and complete failure of the seal of a steam generator plug. The resulting leak area is the largest one possible with the proposed vessel design. The associated pressure decay constant is 100 second. A detailed reliability analysis for the Design Basis Accident [2] lead to the conclusion that the probability that the decay heat cooling system, formed by the 8 main coolant loops and the 4 auxiliary loops, would not be capable of maintaining the fuel can maximum temperature below 1200°C in the first 3000 hours after be-

gin of the accident is less than 10^{-4} per demand. If we assume that the chance of breaking the seal of a steam generator plug of the PCRV of the GCFR is as small as that of a double ended rupture of a coolant pipe in the PWR system, that is 10^{-4} per reactor year or less, than we obtain a chance of not meeting the emergency cooling requirements of less than 10^{-8} per reactor year, which is equivalent to that at present estimated for the PWR in Germany.

The first hypothetical accident investigated was that related to the maximum possible reactivity ramp injected in the core. This is given by the ejection of a forgotten shut-down rod from the core operating at full power under the action of the full helium pressure and corresponds to a maximum reactivity ramp of 60 β /sec. The energy release calculated with the Karlsruhe disassembly code KADIS was 22000 MWsec [3]⁺. The program ARES of INTERATOM allowed the calculation of the stresses caused by this energy release on the PCRV. The maximum calculated strain on the prestressed tendons and on the liner was 0.3 %, showing that the PCRV could withstand this release energy very well. This accident is however extremely unlikely. A preliminary reliability analysis lead to the conclusion that the probability of this accident to occur would be much smaller than 10^{-8} per reactor year.

Due to low coolant void coefficient and to the small reactivity worth of the control rods (0.85 β) reactivity accidents and Transient Over Power accidents are very unlikely in a GCFR. Less unlikely are accidents connected with loss of coolant capability, both due to loss of coolant pressure and/or flow coast-down of all main and auxiliary blowers. A typical accident of this type was investigated with our newly developed code PHAETON 2 [4]. The accident is initiated by the loss of the helium pressure with a time constant of 30 seconds (more conservative than the loss of pressure initiating the DBA) and contemporary failure to scram of both independent shut-down systems. Due to the relatively low coolant void coefficient the reactor power increases up to a maximum of only 30 % above nominal after 20 seconds of accident begin. Then it decreases again. The cladding starts to melt at the lower edge of the reactor core after 38 seconds of accident begin. The core probably melts relatively slowly down without large reactivity excursions, and falls on to the base of the reactor cavern in the PCRV. Also here second criticalities are very unlikely. Calculations have shown that a fully compacted cylinder formed by the total amount of UO_2PuO_2 in the core surrounded by a steel reflector becomes subcritical when the diameter of the cylinder is 25 % higher than the diameter of the core (10 % without steel reflector) [5]. The diameter of the reactor cavern is about 8 meters, of which 6 meters are free from the base of the vertical neutron shields, while the diameter of the core is 3 meters only.

PREVIOUSLY INVESTIGATED PAHR SOLUTIONS FOR THE GCFR

The absence of large reactivity excursions and the downward direction of the cooling flow ensures that the core melt is falling on the base of the reactor cavern. The fact of knowing the position of the core melt after the accident makes the containment of the melt less difficult. The problem becomes one of cooling the core melt from below, for instance by means of water flowing in coils placed underneath the PCRV liner. For this reason we investigated the temperature and heat flux distribution in a slab of molten GCFR core and blanket material [6]. The calculations were based on the experiments of Fiedler and Wille [7] and showed that the major part of the heat is radiated in the upward direction in the reactor cavern. Recent experiments of Fieg [8] although predicting rather higher temperatures in the melt confirm this. The heat radiated

⁺ Subsequent calculations, where due account was taken of the effect of gas fission products in the fuel lead to an energy release of less than half of this value. Further calculations are foreseen, which will take into account of the effect of helium in the fuel pins, which has a similar effect.

from the melt has as consequence that in 2 to 3 days all the internals of the reactor cavern up to the directly cooled liner fall or melt down. One has therefore to control not only the mass of about 200 tonnes formed by core and blanket material, but also the 1000 tonnes of the other internals, mainly the neutron shields.

A core-catcher capable to trap the melt and avoid the upward radiation of heat was designed [9]. This consists essentially of many narrow containers cooled by water coils on their vertical walls and on the base, the containers being twice as high as the molten material contained and placed on the base of the reactor cavern. This solution has some drawbacks: it is expensive, requires a large number of liner penetrations for the water cooling coils in the region of the reactor cavern base, and relies on the not yet proven assumption that the major portion of the molten mass enters the containers before a considerable part of the reactor cavern internals is molten.

Other solutions have been therefore investigated. These include various emergency coolants, whose objective is not only to protect the internals of the reactor cavern but also to disperse the core melt and therefore decrease the volumetric heat production due to the fission products in the bath and the heat flux to the liner wall. The last point is the most important, because the liner, with its water cooling, constitutes the barrier to the core melt, and obviously when the heat flux increases above a certain limit the liner fails. These various types of possible core-catcher are discussed in reference [10]. The main solutions are:

- a) The use of salt with a relatively low melting and evaporation temperature, like for instance ZnCl_2 . The heat would be transported to the liner walls of the reactor cavern by evaporation and condensation of ZnCl_2 . ZnCl_2 is compatible with the components of the melt and the other materials contained in the reactor cavern. High pressures and explosive reactions with the hot debris are avoided [11].
- b) A lead slab, two meters deep, placed on the base of the reactor cavern. If we assume that, due to the high turbulence caused by the decay heat and to the small difference in density of the molten lead and of fuel pellets, the fuel is dispersed in the lead bath, then calculations show that the heat at the liner walls is always safely lower than the maximum allowable and the limitation below $\approx 1200^\circ\text{C}$ of maximum temperatures in the lead bath ensures that the bulk of the neutron shields inside the reactor cavern is protected against excessive temperatures [12].
- c) Injection of water, either alone or in connection with lead (solution b)) or with another substance (for instance H_3BO_3 , see reference [10]), appears to be a possibility. Water could be present anyway due to possible failure of the heat exchangers. It has been shown that formation of explosive oxyhydrogen gas, given by reaction of H_2 , coming from water in contact with hot steel and the oxygen of the air in the reactor containment, is very unlikely due to the presence of helium and steam which act as dilution media [11].

With all these coolants there are still open questions and problems. For instance it is difficult to ensure that a sufficient amount of condensed ZnCl_2 returns on the base of the reactor cavern, thus the cooling process by evaporation and condensation may be interrupted for lack of ZnCl_2 in the core-catcher. With the lead bath, it is not sure that the fuel is really dispersed in the lead, furthermore steel containing some or all metallic fission products, due to its lower density, may lay on top of the lead bath radiating its heat to the neutron shields and producing dangerous local heat fluxes at the liner walls in the regions where the steel is still molten. Finally, with water injection, pressure pulses could be produced in the reactor cavern due to steam formation from contact of water with hot debris.

For these reasons we thought to use an alternative material for the core-catcher, which would be able of reliably dispersing the fuel and at the same time allow the steel containing the metallic fission products to fall on the

bottom of the core-catcher in direct contact of the liner, so that it could be safely cooled and maintained in a solidified state. A reliable way of dispersing the fuel pellets would be the use of a material capable of dissolving them at relatively low temperatures and at the same time not so chemically active to attack the other materials present in the reactor cavern. Originally we thought to use B_2O_3 , but experiments in our laboratory showed that the time required to dissolve UO_2 at reasonable temperatures would have been too long, so we opted for a more active material namely $Na_2B_4O_7$ usually called borax.

USE OF BORAX FOR THE INTERNAL CORE-CATCHER

Description of the Core-Catcher

Borax is well known substance in the glass industry. It has convenient melting and boiling points ($741^\circ C$ and $1575^\circ C$ respectively) and can dissolve large quantities of UO_2 and probably PuO_2 and the fission products in oxide form, in a relatively short time (≈ 1 hour) in the temperature range $1100-1400^\circ C$ [10]. On the other hand borax is compatible with the other materials contained in the reactor cavern and it contains a strong neutron-absorbing element like boron, thus making absolutely sure that the core melt remains subcritical. Another advantage of borax is that it is soluble in water, thus making much easier the eventual removal of the dissolved core debris [10].

Figure 1 shows the reactor cavity in the Prestressed Concrete Reactor Vessel of the 1000 MWe GCFR KWU-design [13]. The core is hanging from the top and the helium flow through the core is in the downward direction. The core-catcher is placed underneath the core with nothing in between. In case of a melt-down accident gravity and helium flow direction both force the fuel elements to fall into the core-catcher. In the core-catcher region no neutron shields for the liner are required, the material of the core-catcher acting itself as neutron shield. The core-catcher is formed by seven layers of steel cubes of 30 cm side length. Each cube is filled with borax. Also the three charge pipes are filled with borax boxes during reactor operation. The boxes in the charge pipes have to be removed during the yearly fuel element charge and discharge operations. The borax boxes are separated from the liner by a layer of graphite bricks, 30 cm thick near the vertical walls of the liner and 5 cm thick near the base of the reactor cavity. The liner is made up of a 2 cm thick steel plate. If thermal calculations would show that a liner thermal insulation is required, a sufficiently thick layer of silica (SiO_2) would be placed between the graphite and the borax. During the melt-down accident, SiO_2 would be dissolved as soon as it would come into contact with the molten borax. As usual, the liner is directly cooled with water coils of square cross section. In the core-catcher region, however, the cooling coils are much nearer to each other due to the large heat fluxes present at the liner wall in case of core melt down. In the figure, tentatively, a coil pitch is shown only twice the coil side.

Thermal Calculations

Thermal calculations for the borax core-catcher have been performed [14]. In the present paper we will only present the main assumptions and the results of these calculations.

For the preliminary thermal design of the core-catcher we have made the following simplifying assumptions:

- a) The whole core and the lower axial blanket fall coherently onto the core-catcher and spread uniformly over a circle of 4 meters in diameter (diameter of the core = 3 meters).
- b) After about ten minutes the upper axial blanket and the radial blanket fall onto the core-catcher over a diameter of 4.6 meters (diameter of the core + radial blankets = 4.2 meters).

- c) The decay heat of the fission products contained in the fuel and in the steel is generated in the first row of borax boxes corresponding to a cylinder of 4 meters in diameter, and it is used to heat up and melt the borax. Heat losses by radiation in the reactor cavity and by conduction to the adjacent boxes as well as the heat capacity of the steel walls of the boxes are neglected.
- d) When the temperature of the considered cylinder has reached the melting point of steel ($1700^{\circ}\text{K} = 1427^{\circ}\text{C}$) the steel walls of the boxes fail and the melt comes into contact with the borax of the adjacent boxes both in the sideward and downward direction.

Figure 2 shows the various layers of borax boxes and the time required for each layer to reach the melting point of steel, calculated with the above mentioned assumptions. After ten hours all the borax boxes have reached this melting point and the melt comes into contact with the graphite bricks. The use of the steel boxes to contain the borax has been not only chosen for construction purposes, but mainly to provide high melting point barriers to the downward movement of the fuel. To dissolve UO_2 in a reasonable time, borax requires temperatures above 1000°C [10], while the melting point of borax is 741°C only. Therefore these barriers are required to allow UO_2 and borax to remain in contact for a sufficient time at sufficiently high temperatures. A temperature of 1427°C is not necessary, at 1300°C the process of dissolution of UO_2 in borax is fast and complete [10], so stainless steel could be replaced by a lower melting point material (for instance cast iron: melting point $\approx 1300^{\circ}\text{C}$), especially if at this temperature the evaporation of borax would be excessive (the evaporation point of borax is 1575°C [10]).

For the steady state thermal calculations at the time when the melt reaches the graphite layer experimental information on heat transfer correlations for natural convection with volumetric heat production in a finite cylinder is required, which is presently not available to us. We decided therefore to perform the calculations in two steps. First we calculate the heat transfer to the vertical walls of the graphite layer protecting the liner with the usual correlation valid for natural convection to vertical walls, but without volumetric heat production. Then we calculate the heat flux to the upper and lower surfaces of the bath with the heat transfer correlation valid for natural convection with volumetric heat production in an infinite slab [8]. This procedure requires an initial arbitrary splitting of the heat produced between vertical and horizontal walls. The calculation must be repeated until the bulk temperature of the bath calculated from both sides is the same.

Reference [14] shows these calculations in detail. The main results are:

- a) At the moment when the melt reaches the graphite bricks, that is at its maximum extension, the heat fluxes to the bottom and to the vertical walls of the pool are 190 and 150 KW/m^2 respectively. These are the maximum heat fluxes with which the liner and the liner cooling system have to cope with. In reference [14] it is shown that the liner does not fail in presence of these heat fluxes and that it is possible to design a liner cooling system capable to carry away the resulting heat.
- b) The maximum bulk temperature of the melt at the same time corresponds to the melting point of steel (1427°C), while the temperature at the pool/graphite vertical interface is 1320°C . The surface temperature of the pool is 1050°C , while the temperature of the lower side of the borax pool is 1180°C . The steel lies on the bottom of the pool in a solidified phase. The maximum temperature of the liner is 285°C . After the time of contact between the melt and the graphite brick layer, these temperatures decrease due to the effective coming into operation of the liner cooling system and the decrease with time of the fission product decay heat.

Future Work

The thermal calculations indicate that a core-catcher capable to cope with the complete melt-down of the core and blankets of a 1000 MWe GCFR and based on borax as dissolving material of the fuel and oxide fission products, appears to be feasible. There are however still some open questions and problems before a core-catcher based on borax can be deemed to operate reliably. In particular the following points should be clarified by further work:

- a) How the relevant physical properties of liquid borax, and in particular the viscosity are affected by UO_2 , PuO_2 and the oxides of the fission products dissolved.
- b) If sufficient time and sufficient contact surfaces are available to the oxides in the borax boxes to be dissolved in the liquid borax.
- c) If an unduly high amount of borax evaporates away at $1427^\circ C$. In this case one could use cast iron (melting point $\approx 1300^\circ C$) as material for the box walls.
- d) Better heat transfer correlations should be made available, valid for cylinder geometries with internal heat production.
- e) The cooling of the borax core-catcher during normal operation conditions with helium at core inlet temperature should be designed (In principle this should be the same design as for the presently foreseen lower neutron shields).
- f) It should be clarified if the borax slab can act effectively as neutron shield for the liner in place of the usual graphite-steel neutron shields (In the mean time information from General Atomic is available, which shows that the borax slab is even better a neutron shield than the normal shields foreseen now).
- g) It should be clarified if a thermal insulation formed by a SiO_2 slab on the inner side of the graphite layer is required to reduce the heat losses during normal operation. The SiO_2 would be quite effectively dissolved as soon as it comes in contact with liquid borax.
- h) The compatibility of borax with materials which would possibly be available inside the reactor cavity should be further investigated.

EXTERNAL CORE-CATCHER

The considerations of the previous sections refer to an internal core-catcher, i.e. a core-catcher contained in the Prestressed Concrete Reactor Vessel (PCRV). Besides the internal core-catcher a GCFR with its massive concrete vessel offers the possibility of having a second line of defense. Here one would make use of the 8.7 meter thick concrete slab forming the base of the concrete pressure vessel. Recent experiment performed in Karlsruhe show that the reaction velocity of molten UO_2 and steel with concrete is relatively low (300 mm/hour) and the melt is entrapped by the resolidified concrete which forms a glass cover over it [15]. Parallel to these, investigations are being carried out at KWU as well. Other investigations performed by Jansen and Stepniewski [16] show that the use of sacrificial materials similar to concrete, like basalts, in which the fuel is soluble, reduces the temperatures by diluting fission product decay heat generators and increasing heat transfer surface. A molten pool formed by the reactor fuel debris reaches a manageable limiting size rather than penetrating to an indefinite distance in an uncontrolled manner. Jansen and Stepniewski show that the molten pool formed by the fuel inventory of a 1000 MWe fast reactor and by the sacrificial material reaches a maximum penetration of 8.5 meters after 230 days. Jansen and Stepniewski assume a semispherical geometry for the pool, in reality Jahn and Reineke [17] show that in presence of a spherical geometry the radial heat flux on the upper edges of the semisphere is five times higher than the heat flux in the downwards direction. Accordingly the pool should, during the expansion, assume the form of an ellipsoid of revo-

lution, which has been experimentally observed [18] and the maximum penetration in the downwards direction should be considerably less than 8.5 meter. However, although the penetration of the molten pool is limited to a maximum, the outer layers of concrete would be subjected, as time progresses, to large temperature and temperature gradients, which would crumble the concrete. To use the concrete pressure vessel as an external core-catcher, it would then be necessary to cool the outer surface of the lower part of the concrete pressure vessel, in order to maintain intact a sufficiently thick layer of concrete. This could be done either by filling with water the lower space between concrete pressure vessel and reactor containment, or providing the outer surface of the lower part of the concrete pressure vessel with water cooling coils. In case of accident involving a gross core melt failure, plenty of time, probably many days, would be available to provide ad hoc these cooling systems.

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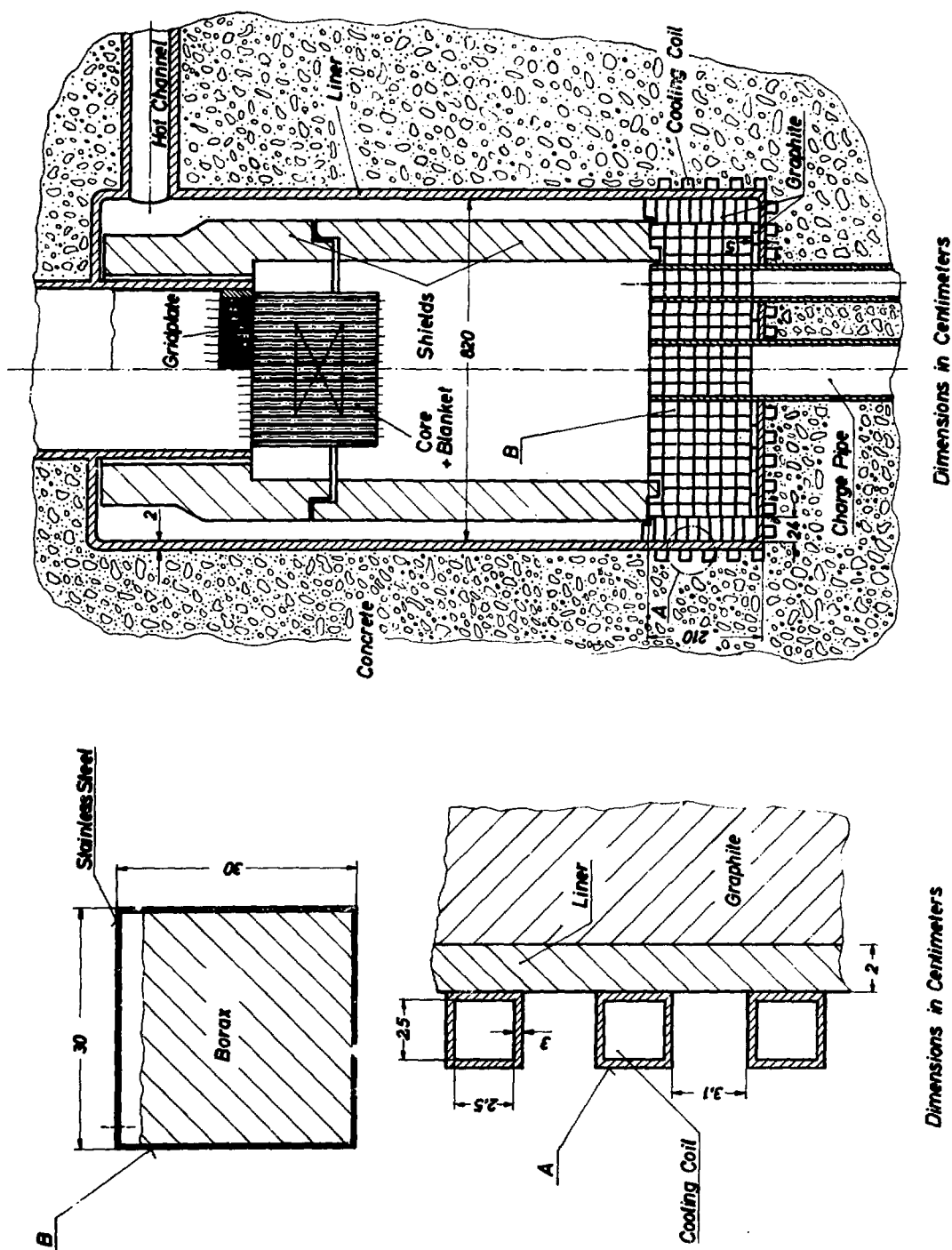


Fig.1: Core-catcher lay-out inside the reactor cavity of the Prestressed Concrete Reactor Vessel.

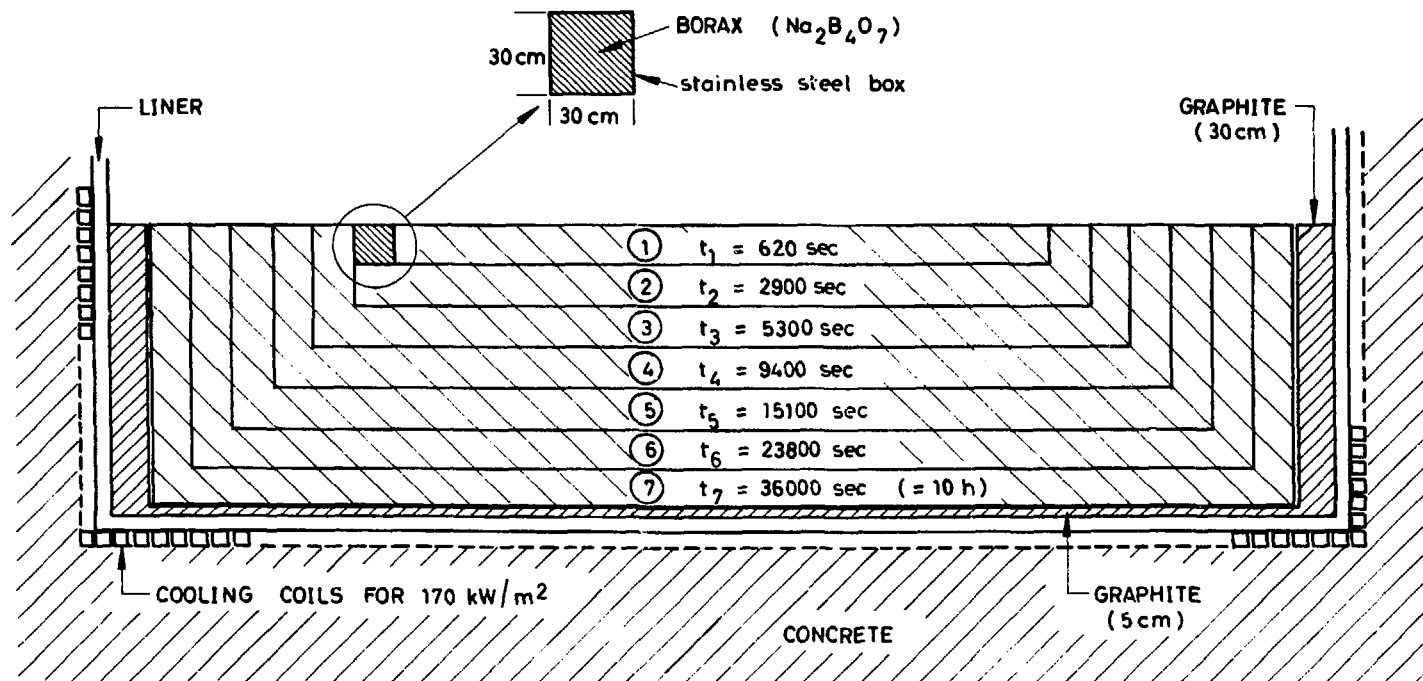


Fig.2: Core-catcher. Melting time of the various rows of borax boxes.

PREDICTION OF DRYOUT HEAT FLUXES IN BEDS OF VOLUMETRICALLY HEATED PARTICLES*

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ABSTRACT

In this paper new data for dryout heat fluxes in volumetrically heated shallow beds are reported. The shallow bed data are correlated with a semi-theoretical correlation based on maximum possible vapor volume flux through the vapor jets overlying the bed. The transition from deep to shallow beds is determined by using the present shallow bed correlation and the deep bed correlation suggested earlier. It is found that the transition height is significantly dependent on the particle size, and the initial bed porosity.

The results of the present study are applied to a hypothetical core disruptive accident in a Liquid Metal Fast Breeder Reactor. The maximum possible coolable depths of the core debris as a function of the particle size, bed porosity and decay heat have been predicted.

INTRODUCTION

After a hypothetical core disruptive accident, molten fuel may come in direct contact with subcooled sodium. A thermal interaction between fuel and sodium may cause the fuel to fragment and disperse in the reactor vessel. In course of time, these fragmented fuel particles may settle on various horizontal surfaces such as core support structure and/or above core thermal baffles. The core debris bed so formed will be cooled by local boiling of sodium, but will dry out when the heat generation rate in the particles is such that the evaporation rate exceeds the natural steady flow rate of coolant through the bed. The dried out bed may eventually remelt and cause the support structure to fail. Thus, an evaluation of the volumetric heat fluxes at which a given bed will dryout is an important safety consideration.

During the past few years limited effort has been expanded to study the dryout heat fluxes in particulate beds. At the last fast reactor safety meeting held in 1974 at Beverly Hills, California, Gabor *et al* [1] presented data of the dryout heat flux experiments conducted at Argonne Laboratory. In this first study on the subject, the volumetric heating was achieved by generating

* This work was supported by the U. S. Nuclear Regulatory Commission, Division of Reactor Safety Research.

heat in the coolant (joule heating) rather than in the particles. This procedure was justified on the presumption that the dryout heat flux was dictated by the gross properties of the bed rather than details of the heat exchange mechanism between particles and the coolant. In these tests, the heat flux was measured by noting the power input to the electrodes. The dryout heat flux data were correlated with the bed loading and no distinction between beds of different particle sizes and porosities was made, while correlating the heat flux data with the bed loading. Subsequently, more experimental work was done by Keowen [2]. In these experiments induction heating was used to simulate volume heating of the particles and the particle mean diameter was much bigger than used in the Argonne Study. Keowen's deep bed data when compared with the Argonne data for the beds of same height and porosity but of smaller particles were found to be much higher. No phenomenological differences are believed to exist between the experimental observations of the dryout heat fluxes in the studies of references 1 and 2. Under such circumstances, the main cause of the higher dryout heat fluxes in Keowen's work would be the bigger particle size. In both of the studies little attention was paid to the bed porosity so the effect of bed porosity on the dryout heat flux also remained uncertain.

To make any meaningful predictions of the dryout heat fluxes, it is essential that the role of the particle size and bed porosity should first be established. After a hypothetical core disruptive accident, the particle size and bed porosity are generally not known and conservative estimates of the maximum heat fluxes can only be made by changing these variables parametrically. The effect on the dryout heat flux of the particle size and bed porosity was recognized in the UCLA Study [3]. In this study, the beds were categorized as deep and shallow beds depending on whether the vapor channels extended only partly into the bed or reached the bottom of the bed and completely fluidized it. Based only on deep bed model predictions of the maximum coolable bed height as a function of particle size, bed porosity and decay heat were made. These results [4] were presented at the 1976 Winter ANS meeting held in San Francisco.

In the present paper, an effort has been made to complete the description of dryout heat fluxes in particulate beds by first developing a semi-theoretical model for volumetrically heated shallow beds. The model is supported by new experimental data. The shallow bed and the deep bed correlation developed earlier [3] are then used to predict the bed height at which the transition from deep to shallow bed takes place. Finally, the semi-theoretical correlations are applied to predict the maximum coolable debris bed height in a prototype fast reactor, as a function of heat generation rate, particle size and bed porosity. Also, the effect of the presence of non-fission heated particles, e.g. steel, is discussed.

THEORETICAL MODELS FOR DRYOUT HEAT FLUXES

The inductively heated particulate beds can be classified as deep, or shallow, according to the vapor channel formation in the bed. Visual observations show that the vapor formed in the bed cuts its way through the bed and moves in paths similar to holes drilled in a solid. The vapor channels in the lower portion of a deep bed are very thin; as the vapor moves upward, the vapor channels grow in size. The vapor channels are well formed in the upper region of a deep bed and the vapor flow induces drag on the particles. The particles are levitated, and conditions similar to that of a fluidized bed exist in this region. However, because of little or no vapor drag, the particles in the lower region of the deep bed remain packed. In shallow beds, the vapor channels are fully developed throughout the depth of the bed, the particles are fluidized and the entire bed is in an expanded state. The formation of vapor flow paths in deep and shallow beds is shown in Figure 1.

Transition from a deep to shallow bed occurs when the channels start to develop throughout the bed. The transition height at which this occurs depends on the vapor volume flux from the bed, and on the mean diameters of the particles.

Before developing the dryout heat flux model for the shallow beds, the deep bed model [3] is reproduced here for the sake of completeness.

Deep Beds

The dryout heat flux in a deep bed is controlled by the maximum coolant flow rate through the lower packed region of the bed. The downward flow rate of the coolant at the surface adjusts itself to balance the outflow of vapor from the bed. The density difference between the liquid and the vapor phases acts as the driving force for the downward flow of the coolant. The liquid encounters minimal resistance in the upper fluidized portion of the bed, however, in the packed bed region the liquid has to flow through the interstitial spaces between the particles. The bed dries out at a particular section in the lower region where gravity can no longer maintain the necessary flow rate to compensate for the evaporation rate. Although, the vapor volume flux is much larger than the liquid volume flux, the resistance to vapor flow does not limit the heat flux, because the vapor moves in well defined paths which offer much less flow resistance. Under the following assumptions: 1) the liquid flow velocity is small (the inertia of the liquid can be neglected), 2) the vapor is inviscid, 3) the relative velocity and cross-sectional area occupied by the vapor in the packed bed region is small and 4) the particles are spherical, the liquid velocity of the coolant in the packed bed region is

$$u = C_1 \frac{\epsilon^3}{(1-\epsilon)^2} \frac{g(\rho_f - \rho_g)(\bar{d})^2}{\mu_f} \quad (1)$$

In equation (1) C_1 is a constant and can be established from experimental data.

For uniform heat generation rate in the particles, the demanded coolant flow rate is maximum at the top of the bed and decreases in proportion to the bed height, becoming zero at the bottom. The average superficial velocity in the bed is half of what it would be, if there was no heat generation in the particles but an equivalent amount of heat was dissipated at the bottom. The superficial velocity, corresponding to the dryout heat flux at a cross-section, can be assumed to be proportional to the maximum, or average superficial velocity in the bed. Therefore, in volumetrically heated deep beds the dryout heat flux is:

$$q_{vd} \equiv Q_v h(1-\epsilon) = \rho_f u h_{fg} = C_2 \frac{\epsilon^3}{(1-\epsilon)^2} \frac{\rho_f (\rho_f - \rho_g) g}{\mu_f} \bar{d}^2 h_{fg} \quad (2)$$

Dividing equation (2) by Zuber's value for the maximum heat flux on a flat plate

$$q_Z = \frac{\pi}{24} \sqrt{\rho_g} h_{fg} [\sigma g (\rho_f - \rho_g)]^{1/4} \quad (3)$$

results in an expression for the dimensionless dryout heat flux as

$$\frac{q_{vd}}{q_Z} = \frac{Q_v h(1-\epsilon)}{q_Z} = C_3 K_1 (\bar{d})^2 \Gamma^{1/2} M \quad (4)$$

In equation (4)

$$K_1 \equiv \frac{\epsilon^3}{(1-\epsilon)^2} \quad (5)$$

is the dimensionless Kozney factor,

$$\Gamma \equiv \rho_f / \rho_g \quad (6)$$

is the dimensionless density,

$$M \equiv \frac{\rho_f^{1/2} \sigma^{3/4}}{\mu_f [(\rho_f - \rho_g)g]^{1/4}} \quad (7)$$

is a dimensionless number, combining the effects of the surface tension, the liquid viscosity and the buoyancy, and

$$\hat{d} = \bar{d} [(\rho_f - \rho_g)g/\sigma]^{1/2} \quad (8)$$

is a non-dimensional mean diameter of the particles.

A variety of data, covering a three-fold variation in $\hat{d}$, seven-fold variation in M and K , and three-fold variation in Γ , were correlated in Reference [3], when the correlation constant, C_3 , was 7.5×10^{-4} .

Shallow Beds

In shallow beds, the particles are in a fluidized state and the coolant has an easy access to the particles or to the bottom of the bed, through the region not occupied by the channels. The flow of vapor in the channels is similar to that in a pipe. A hydrostatic pressure head equal to the bed height drives the vapor through these channels. The flow configuration in the channels within the bed is very stable. However, if the vapor velocity exceeds a certain critical value, the vapor jets in the overlying liquid may become unstable and lead to choking of the flow. The blockage of the vapor flow through the channels would inhibit the flow of the coolant into the bed. A deficiency in the liquid inflow to the bed will lead to the dryout of a local region in the bed which may grow in size with time. The dryout heat flux in shallow beds is, thus, limited by the maximum or critical velocity of the vapor jets. The vapor channels in shallow volume heated beds are conical in shape, because of the addition of vapor along the axis of the channel. The cone angle of the channel can be obtained by assuming that the vapor velocity remains constant along the channel axis. This assumption is consistent with the minimum kinetic energy principle. For a volumetric heat generation rate that is independent of the bed height, the cross-section area of the vapor channels at the top of the expanded bed, and the average cross-section area of the vapor channels can be written as

$$A_{cm} \sim h_e \quad (9)$$

$$\bar{A}_c = \frac{1}{h_e} \int_0^{h_e} A_c dh \sim h_e/2 \quad (10)$$

From equations (9) and (10), the ratio $A_{cm}/\bar{A}_c$ is:

$$\frac{A_{cm}}{\bar{A}_c} = 2 \quad \text{or} \quad \frac{D_{cm}}{\bar{D}_c} = 1.414 \quad (11)$$

Assuming that the friction coefficient in the conical vapor channel is based on the average diameter of the channel, the vapor velocity in the channels is:

$$U^2 = \frac{2\bar{D}_c}{4f} \frac{(\rho_f - \rho_g)g}{\rho_g} \quad (12)$$

The vapor velocity, at which the gas jets in the overlying inviscid liquid become Helmholtz unstable, can be written in terms of the maximum diameter of the channel (diameter at the top of the bed) as:

$$U_c^2 = \frac{2\pi\sigma}{\rho \lambda_H} = \frac{2\pi\sigma}{\rho \pi \bar{D}_{cm}} \quad (13)$$

Solving equations (12) and (13) simultaneously, and using equation (11) to

define D_{cm}/D_c , the expressions for $\bar{D}_c$ and U_c are obtained as

$$\bar{D}_c = 1.68 \sqrt{\frac{\sigma}{(\rho_f - \rho_g)}} f \quad (14)$$

$$U_c = 0.917 \sqrt[4]{\frac{\sigma(\rho_f - \rho_g)}{f \rho_g^2}} \quad (15)$$

A simple energy balance at the top of the bed interims of the number of the vapor channels supported by an area A_v of the volume heated bed results in:

$$q_{vs} = Q_v h(1-\epsilon) = Q'_v h_e(1-\epsilon_e) = \frac{2n \bar{A}_c}{A_v} \rho_g h_f U_c \quad (16)$$

Substituting for U_c in equation (16), and using Zuber's value for the maximum heat flux on a flat plate gives:

$$\frac{q_{vs}}{q_Z} = 1.84 \frac{n \bar{A}_c}{A_v} \cdot \frac{24}{\pi} \cdot \frac{1}{4\sqrt{f}} \quad (17)$$

If h and h_e are the initial and expanded bed heights, respectively, and ϵ and ϵ_e are the initial bed porosity and the expanded bed porosity in the region not occupied by the vapor channels, the total volume of the bed is:

$$A_v h_e = n \bar{A}_c h_e + A_v h(1-\epsilon) + \epsilon_e (A_v - n \bar{A}_c) h_e \quad (18)$$

or

$$\frac{n \bar{A}_c}{A_v} = \left[1 - \frac{h(1-\epsilon)}{h_e(1-\epsilon_e)} \right] \quad (19)$$

The second term in equation (19) represents the ratio of the initial potential energy of the bed to the potential energy of an expanded bed, having a uniform porosity ϵ_e . If we assume that the process would try to optimize itself, then the bed would attain this maximum value after it is fluidized, irrespective of its height. As the bed height is reduced, the number of vapor channels would increase to attain this maximum potential energy. This is consistent with the minimum irreversibility principle. Any other adjustment of the bed (i.e., change in the channel diameter, or in the vapor velocity) will lead to a lower potential energy. Thus, the term $h_e(1-\epsilon_e)$ can be assumed to be a constant, independent of the bed height. However, in equation (19), as $h \rightarrow 0$, $n \bar{A}_c / A_v \rightarrow 1$, which means that the whole cross-section of the bed would be occupied by vapor. This is physically impossible. Observations on a variety of finite heaters (heater width > vapor jet diameter), placed in a pool of liquid [5] show that the maximum vapor area per unit heater area is always less than $\pi/16$, or that the minimum average area occupied by the downcoming liquid is $(1 - \pi/16)$. Since the proposed dryout mechanism of shallow beds is similar to the peak heat flux on finite heaters, equation (10) will be corrected as:

$$\frac{n \bar{A}_c}{A_v} = \frac{\pi}{16} \epsilon_e \left[1 - \frac{h(1-\epsilon)}{h_e(1-\epsilon_e)} \right] \quad (20)$$

Substituting equation (20) into (17) results in:

$$\frac{q_{vs}}{q_Z} = 1.84 \left\{ 1.50 \frac{\epsilon_e}{4\sqrt{f}} \left[1 - \frac{h'(1-\epsilon)}{h'_e(1-\epsilon_e)} \right] \right\} \quad (21)$$

The expanded bed porosity, ϵ_e , and the friction coefficient, f , are inter-related through the vapor velocity and the particle diameter, and should be

considered constant for the velocity at which choking of the vapor flow occurs. Thus, equation (21) for a bed of constant diameter particles becomes

$$\frac{q_{vs}}{q_z} = C_4 \left[1 - \frac{h'(1-\epsilon)}{C_5} \right] \quad (22)$$

Here the constants C_4 and C_5 , will be determined from experimental data.

The transition height, where a deep bed changes to a shallow bed, can be obtained by equating the heat fluxes given by equation (4) and equation (22):

$$h'_{tv} = h_{tv} / \sqrt{\frac{\sigma}{g(\rho_f - \rho_g)}} = \frac{C_5}{(1-\epsilon)} \left[1 - \frac{C_3}{C_4} K_1 \Gamma^{1/2} \bar{d}^2 M \right] \quad (23)$$

Equation (23) shows that the transition height greatly depends on the fluid properties, on the particle size, and on the bed porosity.

EXPERIMENTAL APPARATUS AND PROCEDURE

The experimental apparatus used to obtain dryout heat flux data for shallow beds of inductively heated particles is shown in Figure 2. The particulate bed was contained in a 4.7 cm diameter pyrex jar and was inductively heated with a 13.3 cm diameter multi-turn work coil, which was powered by a 10kw, 453 kHz Cycle-Dyne frequency generator. A reflux condenser was used for condensing the large amounts of vapor generated during the dryout runs. The condenser glass tube was open to atmosphere in most of the experiments. In a few low pressure experiments it was closed at the top, and connected to an aspirator, in order to reduce the pressure in the test section. Tap water was used as coolant in the condenser. The temperature was measured at different bed locations by using four copper-constantan thermocouples arranged in the form of a comb. More details of the experimental apparatus are given in Reference [6].

Particles of commercially available cast steel were used in the present study. The particles were graded according to U.S. standard mesh sizes and stored in air tight cans to avoid any oxidation during prolonged storage. A small amount of calcium sulfate was kept in each can to absorb moisture in the air. Prior to each run the particles were thoroughly cleaned with acetone and then were dried on a hot plate. The washed and dried particles were poured into the pyrex jar to a certain height and weighed. The height and weight information was used to determine the porosity of the bed as poured. Coolant was then added to the bed. Thereafter, the thermocouples and the reflux condenser were positioned.

Calibration of the heat generation rate in the particles as a function of generator power setting was made before or after each dryout run. The heat generation rate in different parts of the bed was observed to be within $\pm 15\%$. Complete details of the calibration procedure are again given in Reference 6. Starting with a bed of given height and material content, the power to the work coil was set at a low value and the temperature of the bed raised to the saturation temperature of the coolant. Thereafter, the power was increased in steps. At each power setting, enough time was allowed so that the dryout of the bed would occur if the heat generation rate was high enough. Bed dryout was observed by noting a sharp temperature rise in one or more thermocouples. The maximum probable error in the measurement of the dryout heat flux is less than $\pm 18\%$.

RESULTS AND DISCUSSION

Correlation Constants for Shallow Beds

The dryout heat flux data, as a function of bed height is shown in Figure 3 for steel particles in the range of 295-446 μ m and of 589-789 μ m. The particles were cooled with acetone. The ordinate in Figure 3 denotes the volumetric heat generation rate multiplied by the bed height and the volume fraction of the solids. For a given particle size and bed porosity, the dryout heat flux

is independent of the bed height in a deep bed. For a shallow bed, it increases linearly as the bed height is decreased.

All of the data presented in Figure 3, and some water data from Reference [6] are shown in Figure 4, along with the correlation equation (22). These data are correlated within 20% if we choose $C_4 = 1.84$ and $C_5 = 10.8$ in equation (22).

Prediction of the Maximum Coolable Debris Bed Depth

Using the experimental constants established earlier, and assuming that the heat generation rate in the fuel at full power is 150 w/gm, the maximum coolable debris bed depths in deep and shallow beds for sodium boiling at 1 atmosphere will be:

$$h_d = \frac{0.91 \times 10^{-4}}{p} \frac{\epsilon^3}{(1-\epsilon)^3} \bar{d}^2 \text{ cm} \quad (24)$$

and:

$$h_s = \frac{1}{1-\epsilon} \frac{1}{0.0546p + 0.22} \text{ cm} \quad (25)$$

From equation (23), the transition from deep to shallow bed occurs at a bed height given by

$$h_t = \frac{4.52}{(1-\epsilon)} \left[1 - 4.85 \times 10^{-6} \frac{\epsilon^3}{(\frac{1}{1-\epsilon})^2} \bar{d}^2 \right] \text{ cm} \quad (26)$$

In equations (24), (25) and (26), $\bar{d}$ is measured in microns, and p denotes the decay heat as percentage of the full power.

The maximum depth of the debris, that can be cooled without dryout is shown in Figure 5, as a function of decay heat, bed porosity and the particle size. Equation (24) is used for the calculation of the maximum coolable debris bed depth as long as $h_d \geq h_t$. If $h_d \leq h_t$, the maximum coolable debris bed depth is assumed to be equal to the smaller of h_d or h_s . Figure 5 shows that for a decay heat equal to 4% of the full power, the maximum coolable debris bed depth reaches a minimum value of 3 cm, with a particle diameter of 250 μm and with a bed porosity of 0.6. For other size particles and porosities, debris of substantially greater depths can be cooled.

A comparison between the present study, and the one performed by Argonne National Laboratory, [1] is made in Table 1. In this comparison the bed porosity was 0.5, which is the approximate mean of the bed porosities used in the Argonne Study. Four different bed formations were considered in Table 1. They were: fuel only, a 50% volume mixture of fuel and steel, with the fuel on top of the steel, steel on top of the fuel, and fuel and steel mixed uniformly.

For the case when the steel is at the bottom, the lower portion of the bed is inactive and does not contribute to the dryout heat flux. In this case, the maximum coolable bed depth is increased in proportion to the volume of the steel present. When the steel and the fuel particles are uniformly mixed, the coolant height is calculated by reducing the heat generation rate in the bed to half of that of the fuel. When the steel is on top of UO_2 , the dryout heat flux is calculated by assuming that the bed containing the steel particles acts as if it was bottom heated. The maximum coolable debris bed depths for a bottom heated deep bed are obtained from Reference [3, 6].

The maximum coolable debris bed depths, predicted for all four types of beds considered, are much less than those proposed by ANL. For any application, the values predicted by the present model should be considered liberal, since no allowance has been made for the presence of trapped gases in deep beds. As discussed in Reference [3], the presence of trapped air significantly reduces the dryout heat flux in deep beds. Other variables, such as rapid settling of the bed, segregation of particles of a certain size, and the bed formation can also affect the dryout heat flux.

The difference in the dryout heat fluxes between the present predictions and ANL data can be explained from equation (26) for the transition height.

Equation (26) plotted in Figure 6 predicts a unique transition height only if the size of the particles in the bed does not deviate much from the mean particle diameter. However, when particles with greatly varying sizes are present, only part of the bed may fluidize under given circumstances.

As an example, suppose that a bed contains 18% particles by weight, in the size range 0-100 μm and 82% in the size range 250-350 μm , and has an average porosity of 0.5. From Figure 6, the particles with a mean diameter of 50 μm (0-100 μm) will fluidize when the bed is about 9 cm high, whereas the particles with a mean diameter of 300 μm (250-350 μm) will fluidize at a 7 cm bed height. Thus, for bed heights greater than 7 cm, but less than 9 cm, only part of the bed will fluidize, i.e., the fine particles will levitate and leave the bed while the bigger particles remain packed. Because of the removal of material from the bed, the porosity of the remaining bed of bigger particles will increase to ~ 0.59 . The transition height for such a bed reduces to 5 cm, and causes the bed to behave as a deep bed. The dryout heat flux for this bed ($\epsilon = 0.59$) will be about three times higher than for the original bed, with a mean particle diameter of 255 μm and $\epsilon = 0.5$. Some of the $\text{UO}_2\text{-Na}$ experiments conducted at ANL, on which the maximum coolable bed depths listed in Table 1 are based, contained 18% particles between 0-100 μm , and the average particle diameter was $\sim 250 \mu\text{m}$. Thus, the increased heat fluxes or coolable depths for a given heat flux obtained at ANL probably resulted from partial fluidization of the bed.

CONCLUSIONS

Methods of predicting dryout heat fluxes in different types of beds have been suggested. The dryout heat fluxes are shown to be very dependent on the particle size and on the bed porosity. Best estimates of the dryout heat fluxes can be made only if precise particle size distribution is known. The particle size distribution greatly depends on the nature of the fuel-coolant interaction, subsequent to an HCDA, and is uncertain at this stage. The present predictions when applied to a fast reactor, give a relatively conservative estimate of the maximum coolable debris bed depth since the bed height is chosen to be the lowest value corresponding to a large range of particle sizes and bed porosities.

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TABLE 1. Prediction of Maximum Coolable Debris
Bed Depth (cm) as a Function of Decay heat†

Decay Heat as Percentage of Full Power (150 W/gm)		1	2	4
Fuel Only	Present Study	7.3	6.1	4.6
	ANL [7]			8.8
Fuel on Top of Steel 50% by Volume	Present Study	14.6	12.2	9.2
	ANL [7]			
Fuel and Steel Mixed 50% by Volume	Present Study	8.1	7.3	6.1
	ANL [7]			11.8
Steel on Top of Fuel 50% by Volume	Present Study	12.4	9.6	6.4
	ANL [7]			

† Bed Porosity, ϵ , is taken to be 0.5 and the mean particle diameter is assumed to be anywhere between 0 and 1000 μm .

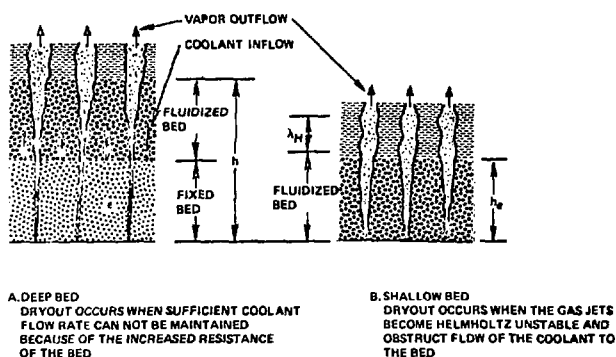


Fig. 1. Deep and Shallow Bed Models
for Dryout Heat Flux.

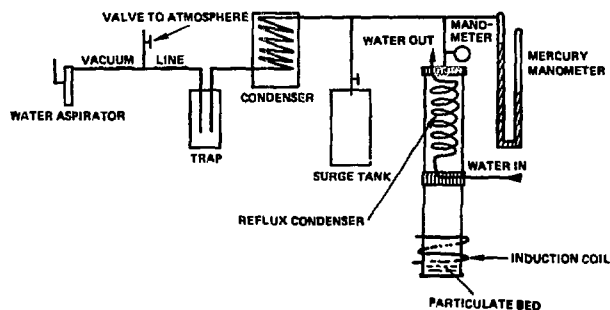


Fig. 2. Schematic Diagram of
Experimental Setup.

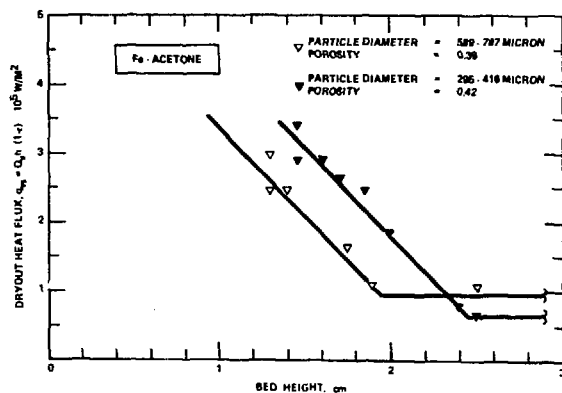


Fig. 3. Dryout Heat Fluxes in Inductively Heated Shallow Beds

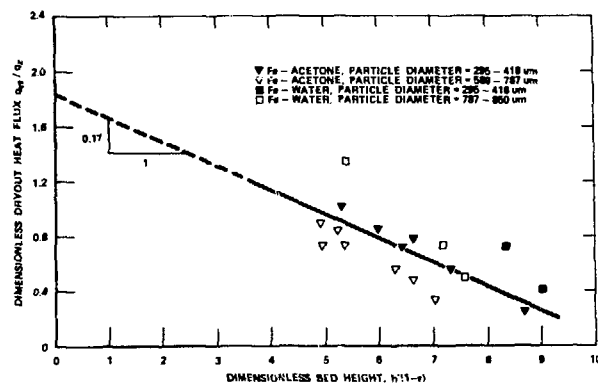


Fig. 4. Dryout Heat Fluxes in Volumetrically Heated Shallow Beds.

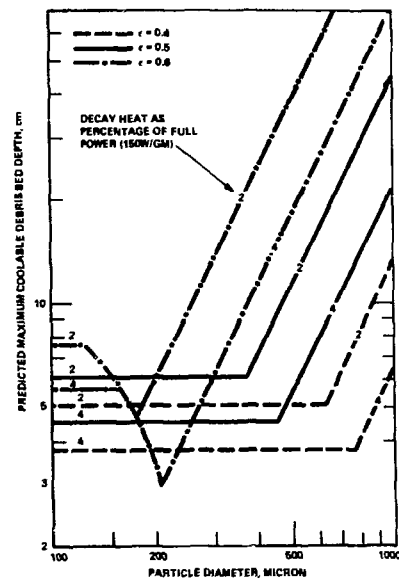


Fig. 5. Effect of Decay Heat, Bed Porosity, and Particle Size on the Maximum Coolable Debris Bed Depth.

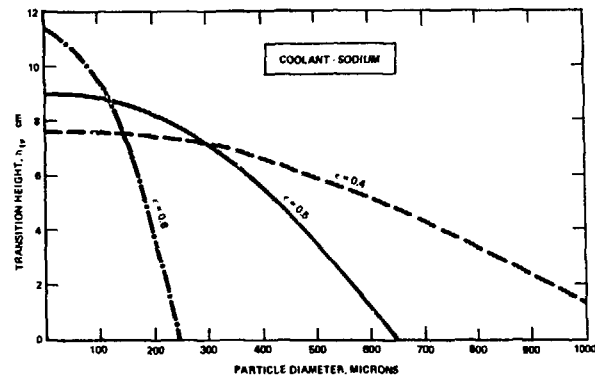


Fig. 6. Dependence of Deep to Shallow Bed Transitional Height on Particle Size and Bed Porosity.

STUDIES OF THE FORMATION AND COOLING OF PARTICULATE FUEL DEBRIS BEDS IN SODIUM

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ABSTRACT

Particulate debris-bed formation and heat transfer experiments have been conducted to provide information needed for postaccident heat removal analysis of sodium-cooled reactors. The particulate debris bed formation experiment, designated M-5, employed molten UO_2 generated by a thermite-type chemical reaction. The molten UO_2 flowed into a dry steel container that simulated the cavity below a reactor. Sodium flow into the cavity followed the fuel flow by about 0.2 s. Examination of the residue indicated that the molten UO_2 was completely fragmented by interaction with sodium to form a particulate bed. Particle distributions down to the submicron size range are reported. Bed heat transfer experiments were performed with metallic particulates in water. Three experimental techniques were used with the same particulate material, including (1) induction heating of the particulate, (2) Joule heating of the aqueous phase (made conducting by the addition of salts), and (3) bottom heating. The results showed that there is a marked effect of the heating method on the experimental dryout heat flux. The dryout heat fluxes decrease in the order of induction heating, Joule heating, and bottom heating.

INTRODUCTION

Molten core debris (fuel, steel, and fission products) generated during hypothetical core-disruptive accidents in sodium-cooled fast reactors has a marked tendency to fragment to form particulate debris upon being quenched in liquid sodium. Such core-material particulate is expected to settle on available horizontal surfaces to form particulate debris beds. The formation of such beds and their coolability through heat transfer to overlying sodium are important to the analysis of postaccident heat removal. New experimental results on the formation of beds and on upward heat transfer from beds are described and evaluated.

The formation of particulate beds within an intact reactor vessel is likely to occur by the flow of molten core debris into a sodium-filled region. Such conditions were simulated in a previous study (Experiments M-1, M-2, and M-3) by discharging molten UO_2 into vessels containing sodium.¹ However, the formation of particulate beds external to a reactor vessel would have to take place by the

flow of molten core debris into a dry cavity which is then followed quickly by the flow of large quantities of sodium into the cavity. An experiment, designated M-5, was performed to simulate such ex-vessel conditions. The principal objective of the experiment was to determine if a fragmented bed is produced.

The results of particle-size distribution analyses of the residues from the earlier tests were reported in Ref. 1. The size distributions were in the same range as those for molten fuel quenching in sodium reported in TREAT experiments and in prior small-scale, out-of-pile experiments. However, all of the previous analyses of particle size distributions were performed using standard sieve analysis. The finest size fraction in each was limited by the availability of commercial sieves and as a result included all material of about 50 μm and smaller. This limitation resulted in rather large uncertainties regarding the size distribution in the range below 10 μm . Accordingly, it was decided to develop methods to analyze the size distributions in test M-5 and in future tests into the submicron range. Such a method was developed and is described in this paper.

The initial series of experiments on heat transfer from particulate beds² was performed with essentially two systems: UO_2 particulate in water and UO_2 /steel particulate in sodium. Tests were conducted in two ways: bottom heating and Joule heating of the liquid phase of the beds. The latter heating method produced heat which was distributed over the volume of the bed. Although the heat was generated within the coolant phase, it was expected that the boiling and dryout characteristics would be determined by the gross properties of the bed, such as particle size and depth, so that the results could be applied accurately to similar beds which had the heat source primarily within the particles. However, more recent results, reported by Keown and Catton,³ had indicated that very high dryout heat fluxes could be obtained for metal particulate in water by induction heating of the particulate. Because of the possibility that higher dryout heat fluxes might be obtained under conditions of particle heating, a series of experiments, similar to those of Ref. 3, were undertaken. In particular, it was desired to determine dryout heat flux data for a batch of particulate by all three methods. The objective of the experiments was to establish whether there is a significant effect of heat source on dryout behavior and, if possible, to ascertain the reasons for such an effect.

PARTICULATE BED FORMATION EXPERIMENT M-5

The experiment was designed to simulate conditions corresponding to the opening of a large hole in the bottom of a reactor and/or guard vessel. This was considered the most likely situation because of the expected uniformity of any thermal attack which would lead to vessel melting. In such a situation, sodium flow would be expected to be nearly simultaneous with core-debris flow. A sodium flow velocity of the order of 10 m/s was chosen as typical of what might be expected from the gravity drainage of the sodium from a breached vessel.

The experimental arrangement is shown in Fig. 1. The thermite mixture (4.2 kg) was loaded into the thermite vessel. The mixture was a stoichiometric combination of uranium metal and molybdenum trioxide powders which on ignition produced molten UO_2 containing about 20 wt% molten molybdenum at a temperature of about 3100°C. Pressure generated within the thermite vessel acted to drive the molten mixture into a vortex tube leading to the reaction vessel after the mixture first melted through a stainless steel diaphragm at the base of the vessel. Tangential injection into the vortex tube resulted in a centrifugal agglomeration of the molten material and an effective separation of the entrained gases. In this way, the molten stream entering the reaction vessel did not have a large gas content and was not driven at an excessive velocity. The separated gases were released through a vent baffle.

Sodium flow into the reaction vessel from the sodium vessel was initiated by a signal generated by the destruction of a burn-wire located at the exit of the vortex tube. The signal from the burn-wire activated a ball valve which allowed the sodium vessel to be pressurized by argon gas from the driver vessel. The total quantity of sodium used was 9 liters and the driving pressure was about 270 kPa.

A cross section of the reaction vessel is shown in Fig. 2. The simulated core debris entered through the upper 50-mm-ID diameter tube while the sodium entered through the lower 25-mm-ID diameter tube. The sequence of events was as follows: As the molten stream emerged from the thermite vessel, it contacted a thermocouple (T-8 on Fig. 1). The initial reading of this thermocouple was taken as zero time for the experiment. At 0.5 s, power was applied to the solenoid by the parting of the burn wire at the mouth of the vortex tube. At the same time, the strain gauge (S-1 on Fig. 1) at the outer base of the reaction vessel began to read, as shown in Fig. 3, indicating that the first of the UO_2 had contacted the inner face of the reaction vessel. At 0.7 s, the pressure in the sodium vessel (P-3) rose to its maximum level when the ball valve opened, admitting the gas from the driver vessel. Simultaneously, the flowmeter E-1 showed the movement of sodium, which began to flow into the reaction vessel. The sodium flow, shown in Fig. 4, appeared to be continuous until 2.0 s, when fluctuations appeared as the flow decayed to zero at 3.0 s. The time difference between the initial readings of the strain gauge and the sodium flowmeter are taken to indicate that the sodium entry began ~ 0.2 s after the entry of core debris.

Simultaneous with the beginning of sodium flow, a pressure pulse was recorded on the reaction vessel pressure transducer (P-2). The pulse, shown in Fig. 5, lasted for only about 11 ms and was about 800 kPa in magnitude. The pulse probably resulted from the initial contact of the molten material with sodium. The pressure pulse may have had the effect of stopping the inflow of the molten UO_2 -Mo mixture. Post-experiment examination revealed that of the 4200 g of charge loaded into the thermite vessel, 2315 had entered the reaction vessel and had contacted 8.3 l of sodium. About 185 g of UO_2 had been ejected into the vent baffle and 408 g was retained as a block in the mouth of the vortex tube. The remaining 1292 g stayed within the thermite vessel.

The strain gauge record and the transient temperature at the outside of the base of the reaction vessel (T-4) are shown in Figs. 3 and 6, respectively. The initial rise to the peak of the strain gauge reading probably resulted from the heating of the reaction-vessel inner face from room temperature on contact with the UO_2 . Subsequent entry of sodium in the time interval from 1 to 3 s caused the temperature at the interior face to drop rapidly. This caused a decrease in thermal stress as the temperature gradient across the lower face of the reaction vessel was decreased. However, as the 425°C sodium filled the container, heating of the container and expansion of the vessel walls caused the stress to increase to a higher level than during the initial UO_2 contact. This value increased the reading to $\sim 0.30\%$ at 8 s, when saturation of the amplifier prevented further recording of the output. However, the temperature trace showed a more gradual temperature decline from 8.0 s on, and it is likely that the stresses were not too much higher than recorded at saturation.

The sharp rise in the outer base temperature, shown in Fig. 6, was analyzed by transient conduction calculations. The results indicated that some areas of the inside surface of the 6.4-mm-thick steel section had been melted. Subsequent examination of the surface showed the presence of many globules of molybdenum which had fused to the steel. Only small quantities of UO_2 particulate adhered to the irregular surface. Scanning electron microprobe analysis of a sample of plate cross section showed the presence of molybdenum inclusions below the surface. It is apparent that the melting of the base had been stopped when the

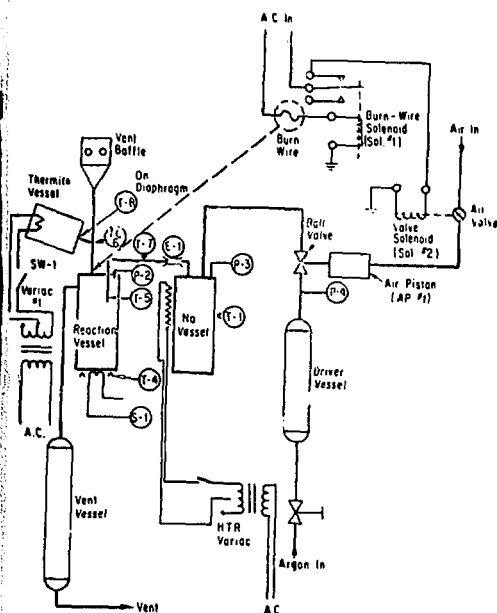


Fig. 1. Experimental Arrangement of M-5 Experiment

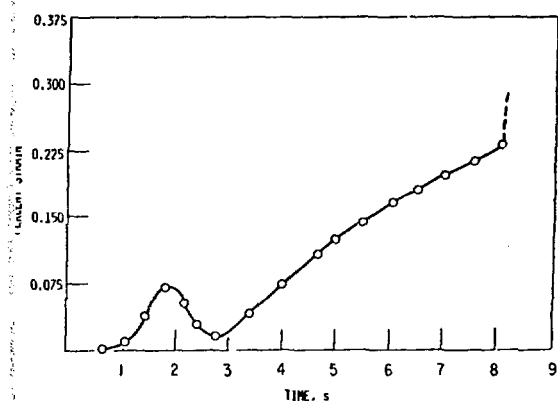


Fig. 3. Output of Strain Gauge S-1 in Experiment M-5

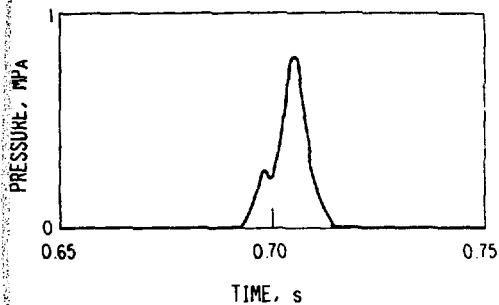


Fig. 5. Reaction Vessel Pressure in Experiment M-5

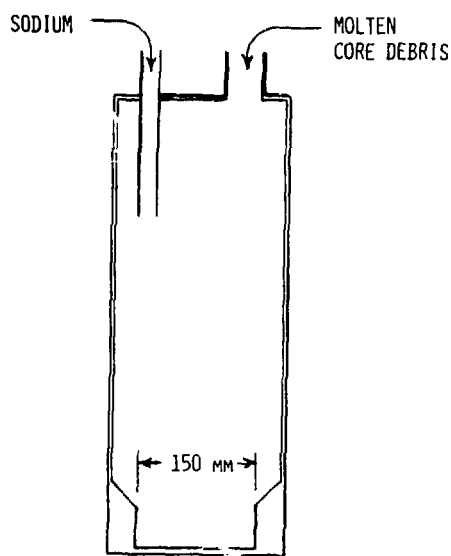


Fig. 2. Cross Section of Reaction Vessel in Experiment M-5

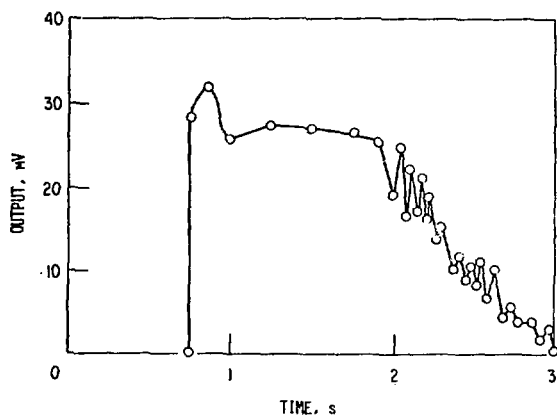


Fig. 4. Output of Electromagnetic Flowmeter E1 in Experiment M-5

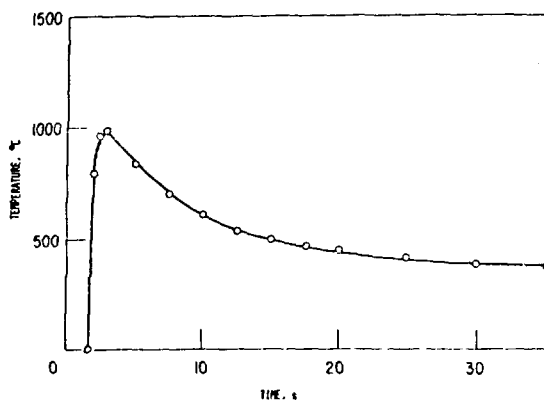


Fig. 6. Temperature at Outside of Base in Experiment M-5

sodium penetrated through the mass of molten UO_2 , reached the surface and caused rapid cooling toward the 425°C bulk sodium temperature.

A radiograph of the reaction vessel showed that the core debris had settled on the bottom of the vessel to form a somewhat irregular bed which was about 80 mm deep. The bed loading in the experiment was 127 kg/m^2 which would result in a uniform bed depth of 25 mm at a porosity of 50%. Subsequent treatment of the bed confirmed that the actual bed was about 80 mm in depth and therefore the average porosity was much greater than 50%.

ANALYSIS OF PARTICULATE FROM EXPERIMENT M-5

Following the experiment, the reaction vessel was removed from the rig and heated to about 300°C for 64 hr to allow for complete settling of any fine particles which might have remained suspended in the sodium. The base of the vessel was sawed off about 80 mm above the inside bottom. The bottom portion of the vessel was placed in an argon-purged glove box for sodium removal by reaction with ethanol. The gas flow out of the box was filtered with $0.2\text{-}\mu\text{m}$ Fluoropore filters (Millipore Corp., Bedford, Maryland). These filters were clean after the sodium-removal operation and gave no radiation counts, indicating that fine UO_2 particles were confined to the slurry and did not escape into the atmosphere during the bubbling caused by the sodium-alcohol reaction.

Absolute ethanol was used for the sodium removal. The unreacted alcohol was subsequently removed by evaporation, and the sodium salts were dissolved in water. Prior tests in which initially measured UO_2 particles were removed from sodium with alcohol indicated that the UO_2 size distribution was not affected by this removal technique. The debris was then wet sieved to ensure effective separation of the very fine particles from the surface of the coarser particles. The fine particles in the size range of $-53 + 0.1 \mu\text{m}$ were measured by sedimentation with a SediGraph 5000 particle size analyzer (Micromeritics Instrument Corporation, Norcross, Georgia).

Table I gives the sieve and fine particle analysis of the material taken in layers at various heights (0-20, 20-40, 40-60, and 60-80 mm) above the base of the reaction vessel. Also given in the table is the total size analysis for all the material. The weight of thermite material recovered from each layer is shown at the bottom of the table. It is estimated that about 0.2 kg of molybdenum metal which was not recovered was welded to the base of the reaction vessel.

Figure 7 compares the cumulative weight distribution for the fragmented material from Test M-5 with the distributions determined by sieving from the previous tests M-2 and M-3. Test M-5 had a higher fraction of fines (27 wt% $< 53 \mu\text{m}$) than M-2 (9 wt% $< 53 \mu\text{m}$) and M-3 (5 wt% $< 53 \mu\text{m}$). However, the median size (size of cumulative fraction equal to 50 wt%) was within a range of 200-240 μm for all three tests.

A chemical analysis was made of the fragmented material taken from 0 to 6 mm above the base of the reaction vessel (see Table II). The molybdenum concentration decreased with particle size; particles in the range of $-4760 + 3360 \mu\text{m}$ contained 97.4 wt% molybdenum whereas particles less than $53 \mu\text{m}$ contained 1.31 wt% molybdenum. This chemical analysis confirmed visual observation that the molybdenum tended to segregate and form larger particles. The behavior of the molybdenum, which was involved in the thermite reaction, is not expected to be typical of stainless steel because of differences in physical and mechanical properties of the two materials.

Table I. Particle-size Analysis for Experiment M-5

Size Range, μm	Weight Fractions of Particles at Indicated Layer Distance, mm, from Bottom of Reaction Vessel				
	0-20	20-40	40-60	60-80	0-80 ^a
+5560	0.014	0.077	0.086	0.018	0.039
-5560 +4760	0.020	0.016	0.020	0.000	0.017
-4760 +3360	0.029	0.029	0.015	0.012	0.026
-3360 +2380	0.036	0.020	0.021	0.015	0.028
-2380 +2000	0.009	0.006	0.005	0.006	0.008
-2000 +1000	0.121	0.089	0.030	0.051	0.098
-1000 +841	0.057	0.054	0.015	0.046	0.051
-841 +595	0.082	0.087	0.019	0.061	0.075
-595 +420	0.069	0.082	0.020	0.068	0.068
-420 +210	0.119	0.153	0.046	0.148	0.124
-210 +105	0.085	0.107	0.070	0.138	0.094
-105 +53	0.102	0.088	0.107	0.143	0.102
-53 +20	0.035	0.030	0.081	0.075	0.041
-20 +10	0.084	0.060	0.153	0.105	0.085
-10 +5	0.058	0.045	0.109	0.047	0.059
-5 +2	0.043	0.035	0.106	0.031	0.046
-2 +1	0.016	0.011	0.035	0.012	0.016
-1 +0.5	0.009	0.004	0.018	0.009	0.008
-0.5 +0.2	0.007	0.003	0.013	0.008	0.007
-0.2 +0.1	0.003	0.003	0.009	0.004	0.003
-0.1	0.003	0.002	0.026	0.005	0.005
Total Weight, kg	0.962	0.520	0.173	0.147	1.802

^aAnalysis of all the material recovered.

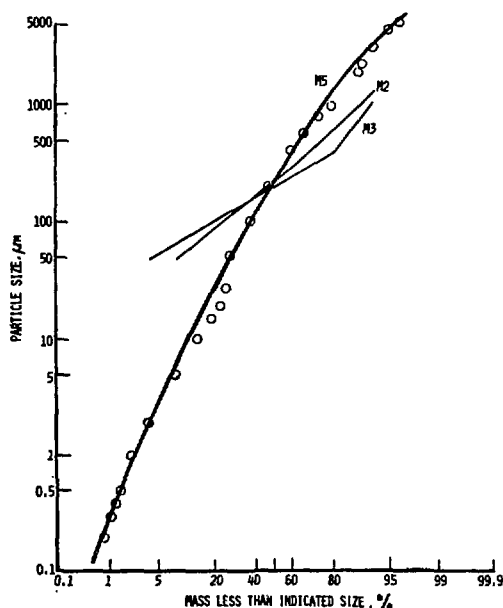


Table II. Sieve and Chemical Analysis of Material 0 to 6 mm above Bottom of M-5 Reaction Vessel

Size Range, μm	Weight Fraction	Wt. Fraction Mo
+ 5560	0.017	-
-5560 + 4760	0.013	-
-4760 + 3360	0.035	0.974
-3360 + 2380	0.042	-
-2380 + 2000	0.010	-
-2000 + 1000	0.162	0.546
-1000 + 841	0.073	-
- 841 + 595	0.102	-
- 595 + 420	0.085	0.161
- 420 + 210	0.139	-
- 210 + 105	0.093	0.0614
- 105 + 53	0.108	-
- 53	0.122	0.0131

Fig. 7. Particle Size Distributions from Experiments M2, M3, and M5

PARTICULATE BED HEAT TRANSFER EXPERIMENTS

Three heating methods were used to obtain comparative data on particulate bed dryout phenomena: bottom heating, liquid-phase volume heating, and solid-phase volume heating. For the bottom-heating tests, a 49.5-mm glass pipe was used with heat applied by a glass blowers torch to a 6.4-mm-thick copper base plate. A chromel-alumel thermowell at the bottom of the bed indicated the bed temperature. The liquid-phase volume-heated experiments were conducted by Joule heating with a Plexiglass column with a 50.8 x 50.8 mm square cross section. Copper electrodes, 3.2 mm thick and the same height as the particle bed, were located on two walls opposite one another. In these experiments, the water was made electrically conducting by the addition of 5 wt% ZnSO_4 . The induction heating tests used a 46.0-mm glass tube in an induction field generated by a power supply operating at 0.35 MHz and a maximum power of 15 kW (THER-MONIC Model 1500 induction furnace manufactured by Induction Heating Corp., New York). Two chromel-alumel thermocouples, one about 5 mm above the base and the other about 25 mm below the top of the particle bed, indicated bed temperatures. A water-cooled glass condenser was mounted on the top of each of the columns to measure the boiling rate. The beds varied in depth from about 70 to 250 mm in all of the experiments.

The thermocouples in the bed indicated dryout for both induction and bottom heating by a sharp rise in temperature above the boiling point of water. However, since the heat was being generated only in the water phase by Joule heating, the particulate phase temperature could not exceed the boiling point of water. Dryout was indicated by the point at which the applied power was a maximum, after gradually increasing the voltage between the copper electrodes. At this point, due to the dryout condition in the lower portion of the bed, the resistance to electric current flow increased because of the reduction in volume of the conducting liquid phase. This was generally accompanied by a reduction in the boiling rate and an observed cessation of liquid percolation to the base of the bed.

An initial series of induction-heated experiments was performed with a narrow size range, 590-840 μm , of stainless steel particulate. The results, shown in Fig. 8, agreed accurately with data reported in Ref. 3 for iron particulate. Another series of experiments was performed by bottom heating. The results, also shown in Fig. 8, indicated the large effect of heating method. Attempts to conduct liquid-phase volume heating with the stainless steel particulate failed because of the generation of short circuits through the metallic particulate.

An additional series of experiments were performed with material which was suitable for all three types of experiments. The particulate was commercially available steel spheres coated with Teflon (-420 + 210 μm size range). The static void fraction for this material was 0.4. The material being steel could be inductively heated, and the Teflon coating provided an insulation between particles which prevented shorting of the electric current through the particulate phase during Joule heating of the liquid phase. Because Teflon is non-wetting, a surfactant (Daxad 23) was added to the distilled water used in all three tests.

The dryout heat fluxes for the Teflon-coated material are shown as a function of bed loading on Fig. 9. The dryout heat flux is the lowest for the bottom-heated tests. This is expected since the vapor formation occurs essentially all at the base of the bottom-heated bed, whereas with volume heating the vapor is generated fairly uniformly throughout the bed with the vapor flow accumulating along the height of the bed to a maximum velocity at the top. Therefore, there is less resistance to an equivalent volume of vapor flow for volume heating compared to bottom heating.

The dryout heat fluxes were generally highest for the induction-heating technique which is most similar to the reactor situation where heat would be generated primarily within the particulate by fission product decay. At the low bed loading of about 300 kg/m^2 (bed height about 85 mm), the dryout heat flux for the induction-heated bed was about twice that of the Joule-heated bed and four times that of the bottom-heated bed. For the deeper beds ($>600 \text{ kg/m}^2$ or 170 mm), the dryout heat flux was about the same for the induction-heated and Joule-heated beds which was about five times as great as that for the bottom-heated beds. The surface temperature of the inductively heated particles most likely exceeded the water boiling point, resulting in superheating of the nucleating vapor bubbles which in turn caused a greater pressure to drive the vapor through the bed compared to the Joule-heated bed. This effect was most apparent at the lower bed loadings.

It can be concluded that previous estimates of dryout heat fluxes for reactor materials based on bottom-heating and Joule-heating experiments can be regarded as conservative. The dryouts by induction heating which is most representative of the type of heat generation from reactor materials were higher than that for the other two techniques for shallow beds and equivalent to that for Joule-heating with deeper beds.

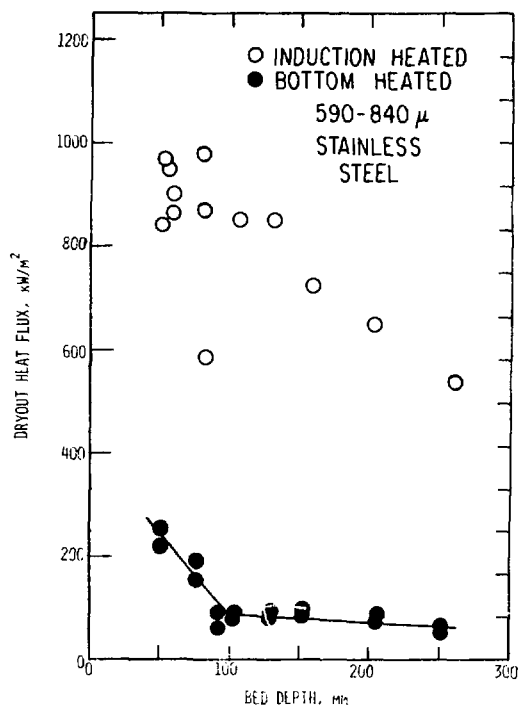


Fig. 8. Dryout Heat Fluxes for Stainless Steel Particulate in Water

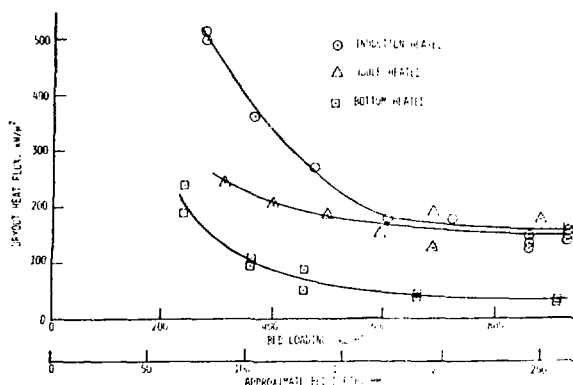


Fig. 9. Dryout Heat Fluxes for Teflon-coated Steel Particulate in Water

CONCLUDING REMARKS

The particulate bed formation test M5 showed that the molten UO_2 content of the $\text{UO}_2 + \text{Mo}$ thermite mixture was completely fragmented by interaction with sodium even though the molten mixture entered a dry cavity ahead of the sodium. The molybdenum content of the mixture was not fragmented and a significant quantity was found adhering to the steel base as frozen globules. A severe temperature transient was recorded at the base of the cavity which indicated partial melting of the interior of the base section. The severe temperature transient

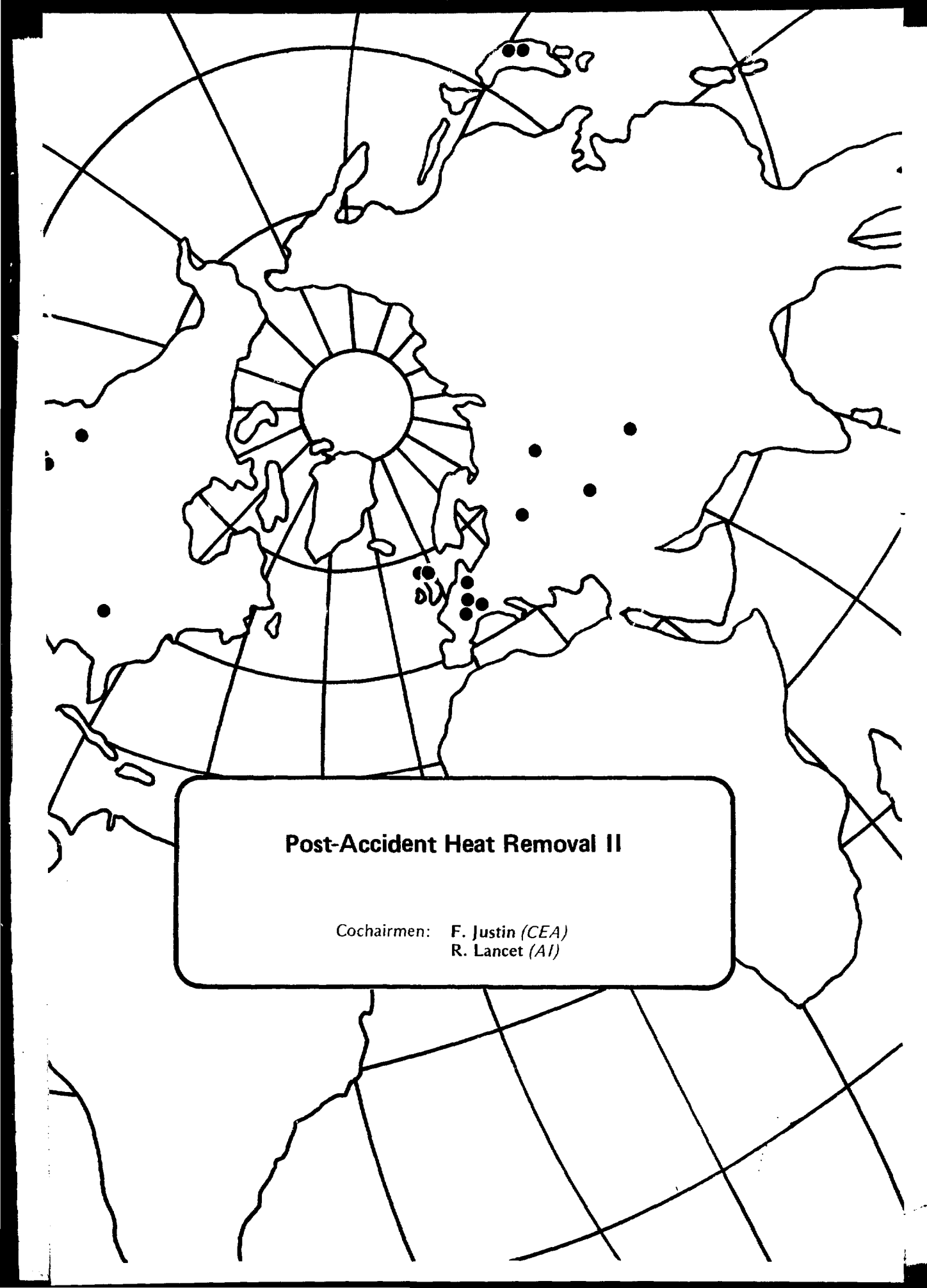
may have been caused by the behavior of the molybdenum. In the reactor accident situation, the metallic constituents of the core debris would be molten stainless steel which would not be expected to settle toward the base because of its lower density and would be expected to fragment more thoroughly because of its lower melting temperature.

The analysis of the particulate from M-5 included the usual sieve analysis and, in addition, included analysis to the submicron size level. The slope of the log-normal distribution was significantly different from those of previous tests. More data are needed to determine whether the slope is characteristic of the contacting mode. The slope was such that there was a higher proportion of fines in the M-5 residue than in previous tests.

The particulate bed heat transfer experiments indicate strongly that dryout heat fluxes are higher when the internal heating is within the particulate phase of the bed instead of within the coolant phase of the bed. This implies that the existing data for dryout heat flux for UO_2 particulate in sodium is conservative. However, in the absence of a quantitative understanding of the effect of heat source location, it is not possible at this time to predict the magnitude of the increase in dryout heat fluxes under reactor accident conditions.

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Post-Accident Heat Removal II

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EXPERIMENTAL INVESTIGATIONS OF HEAT TRANSFER
CHARACTERISTICS IN LIQUID LAYERS WITH INTERNAL
HEAT SOURCES

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ABSTRACT

This work deals with stationary natural convective heat transport from internally heated horizontal fluid layers. As compared to previous measurements the spectrum of boundary conditions has been extended to cover the range from adiabatic lower to adiabatic upper wall, including the special case of equal temperatures at the upper and lower wall. In addition the heat transport from an internally heated fluid layer which is superposed by a second, not internally heated and nonmiscible fluid layer, has been studied. Moreover, heat transfer measurements have been done in internally heated liquid layers through which gas bubbles are rising from the bottom.

INTRODUCTION

After a FBR-HCDA large amounts of molten core material are assumed to exist. Knowledge of the heat transfer characteristics is necessary to predict the behaviour. Because up to now it is not clear if fuel and steel will mix or not, both possibilities have to be envisaged. Besides this uncertainty, one presumably has to deal with a wide variety of boundary conditions. Basic experimental and theoretical work on heat transfer from internally heated fluids has been done by Kulacki and Goldstein [1], Peckover and Hutchinson [2], Jahn and Reineke [3], Fiedler and Wille [4] and Kulacki and Emara [5]. Some of these measurements are restricted to quite low Rayleigh-numbers and therefore extrapolation is indispensable to transfer the results to molten core masses which are characterized by $Ra > 10^9$. In this work the measuring range has been extended up to $Ra \approx 5 \times 10^8$.

The objective of the present investigations was the experimental determination of the natural convective heat transfer characteristics 1.) of a single internally heated horizontal liquid layer and 2.) of two stratified layers, the lower one internally heated. The range of characteristic dimensionless parameters and boundary conditions has been chosen to be relevant for PAHR situations.

The single layer experiments are extensions of similar previous measurements. The range of boundary conditions covers the case of adiabatic lower and adiabatic upper wall, including the special case of equal temperatures at the lower and upper wall. Investigations comparable to the double layer experiments have not been reported hitherto.

APPARATES AND PROCEDURE

The measurements have been performed in a horizontal vessel with the base 15.7 cm x 15.7 cm. The Joule-heated liquid is enclosed between two isothermal horizontal walls. The distance L of the walls varied from 1.6 cm to 6.0 cm. These walls are polished front parts of heat exchangers. The wall temperatures have been controlled with 16 iron-constantan thermocouples distributed over the whole area at a distance of 0.5 mm from the surface. Horizontal heat fluxes through the walls of 10 mm plexiglass were depressed by appropriate insulation of the side walls. This lateral heat loss has been determined with the aid of several heat flow sensors^{x)} and was about 3 % of the totally generated power within the fluid.

Vertical temperature profiles in the fluid have been measured with a 0.25 mm diameter chromel-alumel thermocouple within a thin tip of glass, positioned by a micrometer gauge.

The total heat transport to the upper and lower boundary has been measured by the mass flow and the temperature rise of the cooling water through the heat exchangers. The mass fluxes have been measured with mass flow meters, the temperature rises with thermopiles installed between the heat exchanger in- and outlets. The total electrical power within the fluid, measured with a Wattmeter, agreed within ± 5 % with the measured heat fluxes at the heat exchangers. Fig. 1 shows schematically the experimental arrangement.

RESULTS

1. Single fluid layer experiments

The experiments with a single fluid layer and equal temperatures at the upper and lower boundary have been extended up to internal Rayleigh-numbers

$$Ra_{int} = \frac{g\beta}{\nu\alpha} L^3 \Delta T^* = \frac{g\beta}{\nu\alpha} \left(\frac{L}{2}\right)^3 \left(\frac{QL^2}{8\lambda}\right) \approx 5 \times 10^8 \quad (1)$$

For the explanation of the constants and physical properties see the nomenclature below. Fig. 2 shows the results of these measurements: η , as a function of Ra_{int} , is the fraction of the total generated power which is transported to the upper boundary. Also inserted are the results of Kulacki and Goldstein [1], Peckover and Hutchinson [2] and Jahn and Reineke [3]. The common range of $Ra_{int} < 10^7$ shows a good agreement of the different results.

The convective cooling parameter M , Fig. 3, defined by

$$M = \frac{\frac{QL^2}{8\lambda}}{T_{max} - T_{wall}} \quad (2)$$

also has been determined as a function of the internal Rayleigh-number. $QL^2/8\lambda$ is the maximum temperature difference within the layer at heat conduction, $(T_{max} - T_{wall})$ is the maximum measured temperature difference in the layer with natural convective heat transport. M is a characteristic dimensionless parameter. Therefore the relationship M vs Ra_{int} is applicable to other fluids with $Pr > 1$. In the case of a molten core layer with internal heat generation the flow regime curve (boiling - no boiling) can be estimated when T_{max} is set to the boiling point temperature. T_{wall} is thereby set to the melting point temperature.

x) R&F Corporation, Hudson, New Hampsh.

Fig. 4 shows the result; the material parameters are listed in the table nearby. There are large uncertainties in the accuracy of these data, therefore two sets have been used, named A and B [6]. Curve A results from the present measurements and data set A. The error region is due to the experimental inaccuracy. The two other curves named AKG and BKG result from the Kulacki and Goldstein correlations [1] with data set A and B respectively. By using the data of Jahn and Reineke and data set A the resulting flow regime curve lies between the curves A and AKG. These curves show that discrepancies due to material parameter inaccuracy are at least as large as those due to different experimental heat transfer correlations. Therefore, to get a more exact statement on the flow regime, the material parameters must be known with a better accuracy.

From the flow regime curve one can conclude that the value $Q \cdot L$ cannot exceed $\approx 100 \text{ W/cm}^2$ in the one-phase flow case; so the upward heat flux also cannot exceed this value. If one now assumes a hemispherical emissivity of $\epsilon \approx 0.5$ of UO_2 at the melting point temperature $T_M = 3070 \text{ K}$, the total radiant energy is $E = \epsilon \cdot \sigma \cdot T^4 \approx 200 \text{ W/cm}^2$. Therefore always a solid crust would be formed at the top in the one-phase-flow; the thickness of it is determined by the upward heat flux.

The emissivity of a molten core material surface is not well known. The value $\epsilon = 0.5$ has been taken from [7] for thoriumoxyde at 2270 K . If the hemispherical emissivity should be smaller than assumed above, the temperature at the top could exceed the melting point and a stabilizing inversion layer can be formed within the internal heated fluid, a condition which also has been investigated experimentally.

In addition to the internal Rayleigh number, Ra_{int} , an external Rayleigh number

$$Ra_{ext} = \frac{g \beta}{\nu \alpha} L^3 (T_{low.wall} - T_{upp.wall}) \quad (3)$$

affects the heat transfer characteristics of the fluid layer by different temperatures at the lower and upper wall. Heat fluxes have been determined for constant internal Rayleigh numbers and different external Rayleigh numbers, Fig. 5. Again, η is the fraction of energy transport at the upper boundary to the total generated power in the fluid, $\eta = q_{up}/Q \cdot L$. $Ra_{ext} = 0$ corresponds to the case $T_{lower wall} = T_{upper wall}$ and is described in the section above.

With $q_{up}/QL = 1$ the case of a lower adiabatic wall is reached; the corresponding positive external Rayleigh number can be compared with the results of other authors (Fiedler-Wille [4], Kulacki-Emara [5]), the agreement is satisfactory.

For negative external Rayleigh numbers, that is for stabilizing inversion layers, the experimental result can be approximated by

$$\eta/\eta_0 = \left(1 + \frac{1}{32} \frac{Ra_{ext}}{Ra_{int}}\right)^{2.5} \quad (Ra_{ext} \leq 0) \quad (4)$$

see Fig. 6. Here η_0 represents the corresponding value of η for $Ra_{ext} = 0$, see Fig. 2. For $\eta/\eta_0 = 0$ the upper wall is adiabatic, and the downward heat transport is conductive. The temperature profile is parabolic and the temperature difference between the boundaries is

$$\Delta T_{conduct} = \frac{QL^2}{2\lambda} \quad (5)$$

This leads to the relation

$$Ra_{ext} = -32 \times Ra_{int} \quad \text{for } \eta/\eta_0 = 0. \quad (6)$$

Fig. 7 shows the experimental temperature profiles for some characteristic cases.

A short example shows the consequences of this section: A molten core layer of $L = 10$ cm with internal heat generation of $Q = 7$ W/cm³ (this is still in the one-phase-flow regime) has an internal Rayleigh number $Ra_{int} \approx 10^9$ so $\eta \approx 85$ %. To enlarge now the downward heat flux by a factor of two, i.e. from 10 W/cm² to 20 W/cm² the external Rayleigh number must be $Ra_{ext} \approx -2.5 \times 10^9$ which corresponds to $\Delta T > 500$ K. This shows that at internal Rayleigh numbers characteristic for PAHR situations the external temperature differences must be extremely large to change the heat flux relations essentially. Such high temp. differences can be maintained only in the two-phase-flow regime.

2. Density-Stratified Liquid Layers:

The one-layer experiments just discussed, which are defined by two independent Rayleigh $\sim$ numbers, have been continued under further aspects: Again the heat transfer from an internally heated fluid layer is considered, this layer now superposed by a second one, without internal heat generation and nonmiscible. Fig. 8 shows a typical experimental temperature profile of such a configuration.

Corresponding experiments [8] on heat transfer through stratified liquid layers with surface heating demonstrate the existence of a hydrodynamically rigid interface between the two liquids, i.e. the interface can be interpreted as a very thin layer with high heat conduction.

The present experiment shows that this hypothesis of a rigid interface is applicable also in the case of internally heated liquid layers. The temperature profile in the lower layer is similar to that in Fig. 7, curve C. The interfacial temperature T_{int} again defines an external Rayleigh number in the lower layer with $\Delta T = T_{low.wall} - T_{int}$. The temperature profile in the upper layer corresponds to that of the known Bénard problem with T_{int} as the temperature at the bottom of the fluid.

With the correlation (4) for the lower layer and the known heat transfer correlation of the Bénard problem for the upper one the heat transfer to the boundaries can be calculated. These calculated values agree with the experimental within less than ± 10 %. With regard to the errors (inaccuracy of measurements and material parameters) one can conclude from these experiments that the heat transfer through stratified liquid layers can be reduced to the known heat transfer characteristics of single fluid layers.

3. Gas flowing through internally heated fluid layers

These measurements deal with the heat transfer characteristics of internally heated liquid layers through which gas bubbles are rising from the bottom. Similar problems in PAHR situations can arise when the water encased in concrete below the molten core masses will vaporize and flow upwards. The question arises how the heat transfer to the boundaries and the temperature profile will be changed.

In the experiment the lower heat exchanger wall has been perforated by 0.05 cm diameter holes at 2.0 cm distances; the air flow through the holes has been controlled with a flow meter. The upper heat exchanger consisted of a series of parallel 6 cm diameter pipes with 0.2 cm distances; these 0.2 cm slots are necessary to let the gas bubbles out of the system. With V_{gas} ($\frac{cm^3}{sec}$) as the volume gas flow and the base area $F = 15.7$ cm x 15.7 cm the superficial gas velocity v_s is

$$v_s = \frac{V_{gas}}{F} \left(\frac{cm}{sec} \right) \quad (7)$$

The mean velocity of the single gas bubbles has been measured stroboscopically to be $\bar{v}_b = 23$ cm/sec. This value agrees well with the data from [9].

The fraction α of gas within the fluid layer is

$$\alpha = \frac{v_s}{v_b} \quad (\text{void fraction}) \quad (8)$$

and represents the additional characteristic dimensionless group. The results, Figs. 9 and 10 show, that, with increasing α , the maximum temperature within the fluid is reduced considerably, whereas the fraction η of the upward heat transfer is changed considerably only when a stabilizing inversion layer is present, i.e. at negative external Rayleigh-numbers.

These investigations are continued not only for gas flows through the fluid layers, but also for liquid droplets like Heptane to get a dependency from other parameters like the density difference of the two phases.

NOMENCLATURE

a	thermal diffusivity
g	gravitational acceleration constant
L	height of fluid layer
M	convective cooling parameter
Pr	Prandtl number
q_{up}	upward heat flux
Q	volumetric rate of energy generation in the fluid
Ra_{int}	internal Rayleigh number
Ra_{ext}	external Rayleigh number
T	Temperature
α	Fraction of gas bubbles within the liquid layer
β	Isobaric coefficient of thermal expansion
ϵ	hemispherical emissivity of a surface
η	Fraction of energy transport at the upper boundary
λ	thermal conductivity of the fluid
ν	kinematic viscosity
σ_0	Stefan-Boltzmann constant

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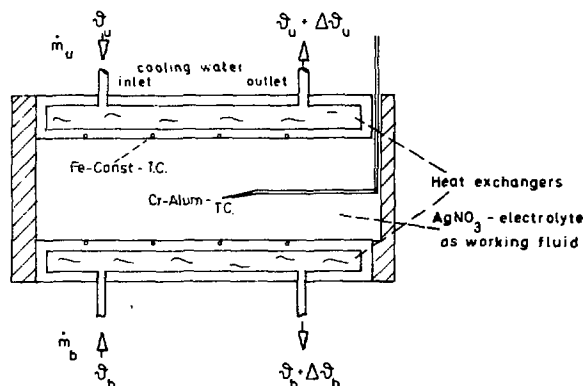


Fig.1 Experimental arrangement of the convection cell.

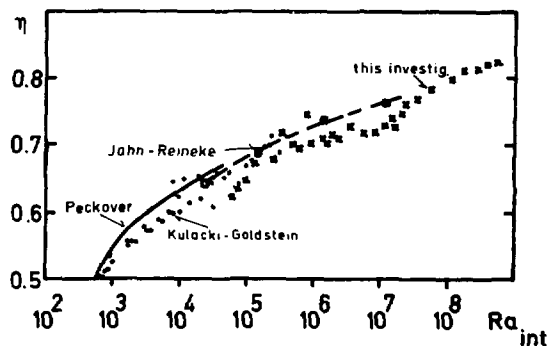


Fig.2 Fraction of Energy Transport at the Upper Boundary vs Internal Rayleigh Number.

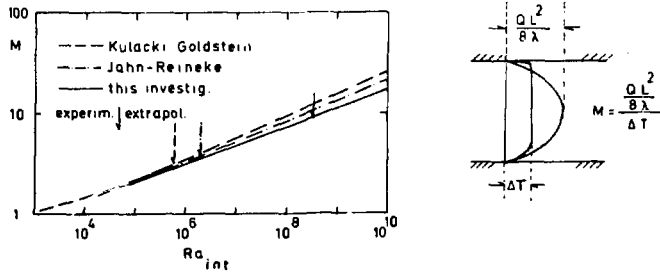


Fig. 3 Convective cooling parameter M vs internal Rayleigh Number.

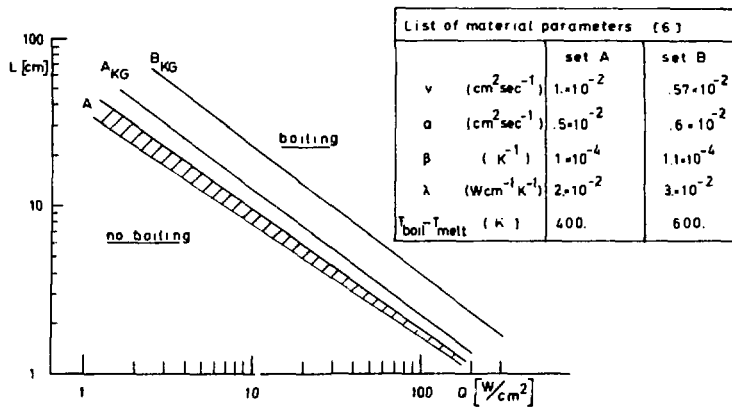


Fig. 4 Flow regimes in UO_2 as a function of power density Q and layer depth L .

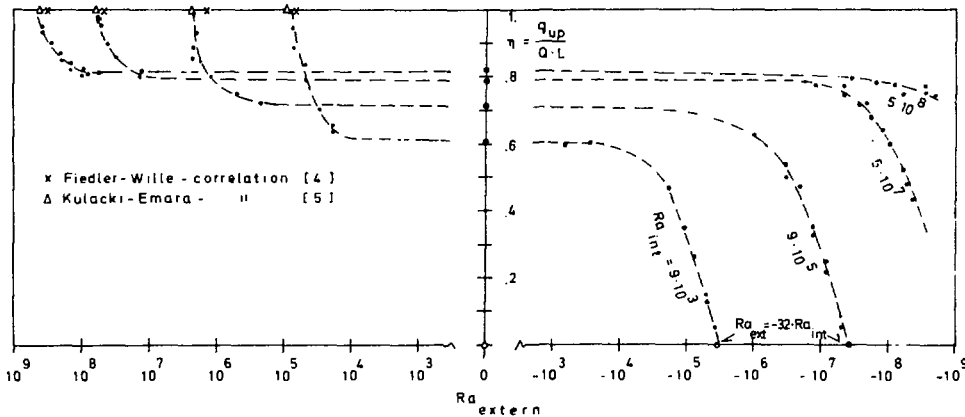


Fig. 5 Fraction η of heat transferred upward vs external Rayleigh Number for different internal Rayleigh Numbers.

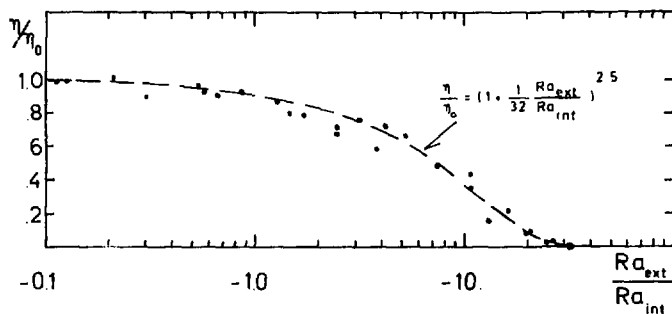


Fig. 6 Fraction η of heat transfer upward vs Ra_{ext}/Ra_{int} , related to η_0 for zero external Rayleigh Numbers.

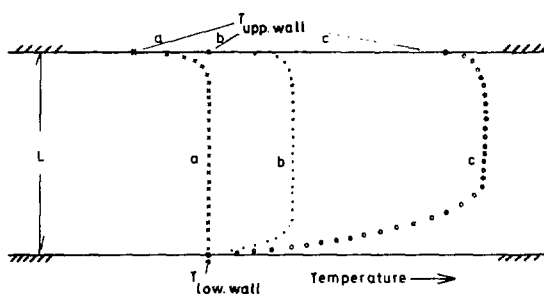


Fig. 7 Measured temperature profiles in an internally heated liquid layer : $Ra_{int} = 5 \times 10^7$;
 $Ra_{ext} = 6.1 \times 10^7$ (a); 0 (b); -2.4×10^8 (c).

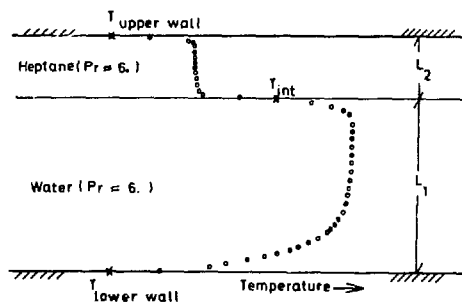


Fig. 8 Measured temperature profile in two stratified liquid layers, the lower one internally heated.

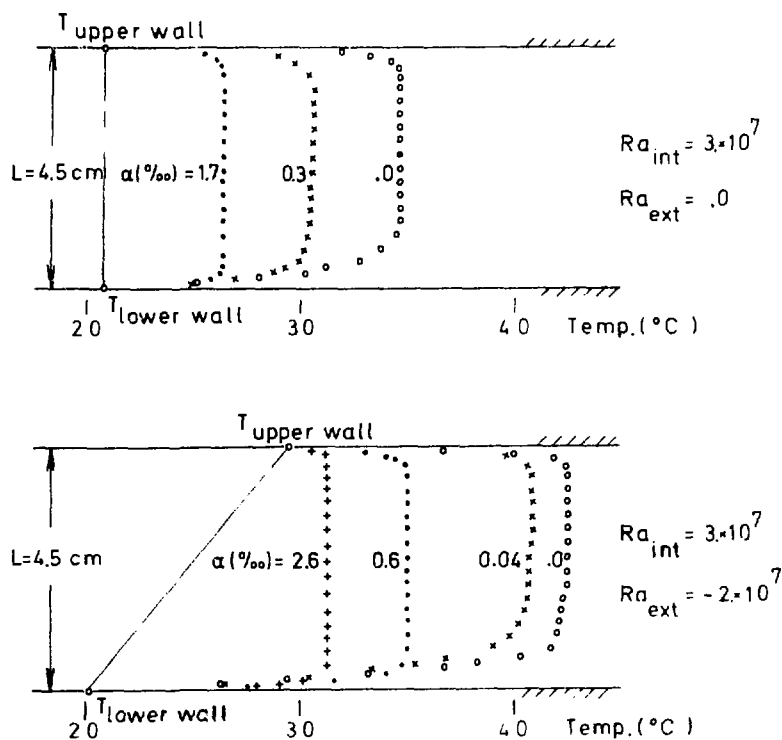


Fig. 9 Measured temperature profiles in the fluid layer at different void fractions α .

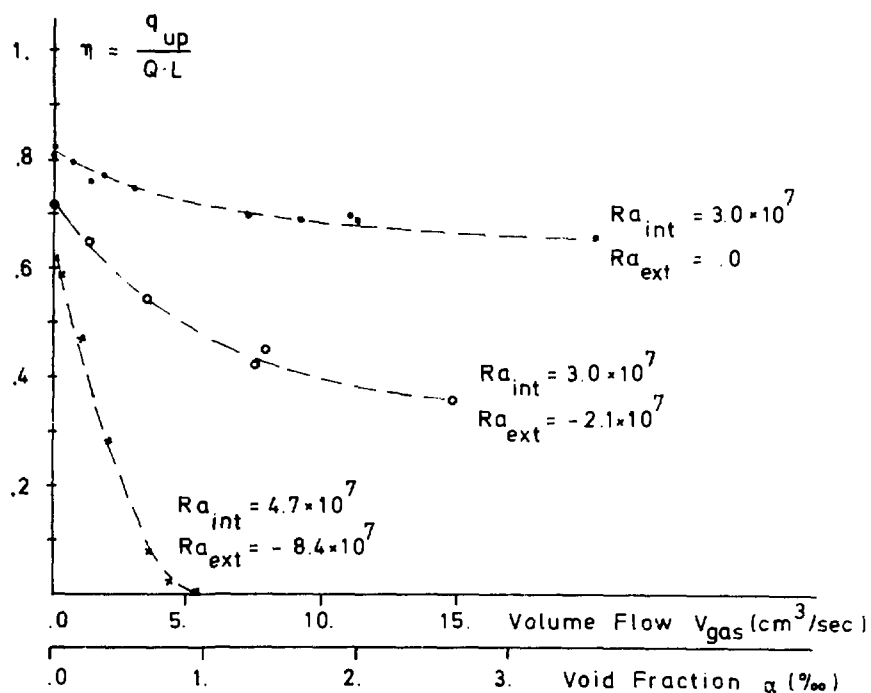


Fig. 10 Fraction η of heat transferred upward vs void fraction α (or volume flow V_{gas}) for different internal and external Rayleigh Numbers.

HEAT REMOVAL FROM MOLTEN FUEL POOLS

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ABSTRACT

Two experiments were conducted to study the distribution of internally generated heat in nonboiling pools of rectangular cross-section. In one experiment, the heated fluid was an aqueous solution, and in the other the fluid was molten uranium dioxide. Heat was generated by the passage of AC current through the pools. Analysis of the heat flux data from the water experiments led to an empirical model of pool heat transfer. The model takes account of the interactions of the vertical heat fluxes with the horizontal heat fluxes. The model is shown to predict heat fluxes which are in reasonable agreement with experimental and analytical results reported by Mayinger *et al.* The empirical model, when applied to the conditions of the molten UO_2 experiments, predicted significantly smaller downward heat fluxes than were observed. The increased downward heat fluxes in the molten UO_2 could possibly be caused by effects of partial transparency within the molten UO_2 .

INTRODUCTION

Core debris generated by an HCDA in a fast reactor could be in the form of one or more pools of molten fuel, heated internally by the radioactive decay of dissolved fission products. Knowledge of the possible magnitudes of the heat fluxes at the boundaries of such pools are necessary to predict the transient growth into adjacent structures and to define the requirements for heat removal to stabilize the pools and prevent any melting attack.

Analysis of postaccident heat removal requires knowledge of heat fluxes from internally heated pools in the downward, upward, and sideward directions under boiling and nonboiling conditions. Initial estimates¹ of the downward heat flux from a liquid pool were based on a stagnant lower liquid layer heat conduction model with the upper boundary of the layer taken as a zero heat flux surface at the bulk temperature of the fluid (conduction model). Analysis of preliminary experiments under boiling conditions² indicated that downward heat fluxes under boiling conditions could be many times larger than those calculated from the conduction model. However, under nonboiling conditions, analysis³ of the data of Kulacki and Goldstein⁴ and Jahn and Reineke⁵ for horizontal fluid

layers indicated that the conduction model did provide a good estimate of the downward heat fluxes when combined with the results of the Kulacki-Emara correlation⁶ for upward heat flux. This analysis³ provided an approximate method to calculate upward and downward heat fluxes from an infinite fluid layer not only for the condition of equal boundary temperatures, but also for the general conditions of unequal boundary temperatures. Based on the simplification afforded by the conduction model, it has been possible to extend the analysis to predict the behavior of certain cases of two liquid layers.⁷

Experimental and theoretical data on the sideward, as well as upward and downward, heat fluxes from internally heated, nonboiling pools in the form of right circular cylinders of various aspect ratios (height/diameter) have recently been reported by Mayinger *et al.*⁸ A series of experiments involving the measurement of heat fluxes in the three directions with nonboiling pools of rectangular cross-section have also been completed at ANL and are reported in detail in this paper. An empirical treatment of the data has been used to develop an approximate predictive model of pool heat transfer which is shown to account for not only the new ANL results, but also the results of Mayinger *et al.* The results of recent ANL measurements of heat fluxes under boiling conditions in rectangular water pools have been reported and analyzed by Gabor *et al.*⁹

Because all of the available experimental data, noted above, are based on experiments with aqueous solutions, high priority in the ANL program has been placed on the development of a pool heat transfer experiment which uses molten UO_2 (MP: 2840°C) as the heated fluid. Some of the preliminary experimental results are presented in this paper and preliminary comparisons with the model are described.

EXPERIMENTAL

The water experiments were conducted with $\text{CuSO}_4\text{-H}_2\text{O}$ or $\text{ZnSO}_4\text{-H}_2\text{O}$ electrolyte contained between two vertical copper electrodes. Heat is generated internally by the passage of AC current. The electrodes are water-cooled and serve as heat transfer surfaces. The sides of the containing vessel are constructed from double plates of Pyrex glass and are insulating surfaces. The bottom is a water-cooled copper base plate in good thermal contact with the solution, but insulated electrically by a 0.076-mm Teflon film. A series of experiments was performed with an open top surface that was cooled only by surface vaporization. Another series was performed with a water-cooled copper top plate in good thermal contact but electrically insulated from the solution. Details of the experimental method are identical with those described for the boiling experiments.⁹ Experimental results for nonboiling conditions are listed in Table I.

The molten UO_2 experiments were performed in an apparatus composed of alternating U-shaped copper and zirconia sections that contain the fuel on two sides and the bottom, as shown schematically in Fig. 1. The UO_2 is contained on the two remaining sides by tungsten electrodes seated in electrically insulating transite mounts. The copper U-shaped sections are water-cooled; the electrodes and top of the sample are cooled by radiation to water-cooled plates. Heat fluxes through the sides, bottom, and top of the cell are obtained by monitoring the inlet and outlet temperatures of the cooling water flowing in these parts of the apparatus, coupled with measurements of the water flow rate. Initial heating of UO_2 is achieved by passing current through a tantalum coil embedded in the powdered fuel, increasing the voltage across the conducting UO_2 , and shifting to a low-voltage/high-current mode to allow larger currents to pass through the cell. Post-test examination of the cell contents in all experiments revealed essentially the same observations: (1) a hollow cavity in the region where the tantalum coil had been located; (2) the cavity extended from electrode to electrode; (3) the sintered upper surface of the cavity was very thin (~ 1 to 2 mm);

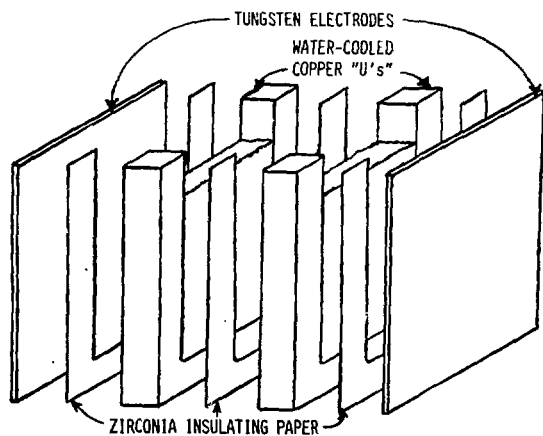
Table I. Experimental Heat Transfer Data: Water Pool

(Pool Cross Section: 190.5 x 152.4 mm)

Pool Depth, mm	Power, kW	Heat Fluxes, kW/m ²			Temperatures, °C				Heat Balance, %
		$\bar{Q}_U$	$\bar{Q}_H$	$\bar{Q}_D$	T_D	T_H	T_U	T_P	
(Experiments with an Open Top)									
114.3	2.15	10.8	45.9	3.67	21.6	38.8	80	80	4.0
114.3	1.93	11.2	41.7	3.67	21.9	38.3	80	80	0.0
114.3	3.08	18.1	64.6	4.96	22.3	40.9	89	89	2.9
114.3	3.08	17.3	61.1	4.87	22.0	40.3	89	89	7.7
114.3	1.50	6.5	32.9	3.30	21.5	35.7	70	70	3.0
114.3	1.48	6.0	32.1	2.97	21.4	36.0	70	70	5.2
114.3	0.89	2.8	22.3	2.63	22.1	30.6	58	58	-5.9
114.3	0.92	2.6	22.3	2.56	22.3	32.2	59	59	-2.0
92.4	1.99	11.4	57.2	5.06	24.1	39.0	90	90	-7.3
92.4	1.48	8.0	39.5	3.69	22.4	34.7	80	80	-0.4
92.4	1.18	4.1	33.5	2.90	21.7	31.6	70	70	1.3
92.4	0.99	3.0	26.7	2.46	20.5	29.0	62	62	6.8
92.4	1.99	11.4	51.6	4.41	23.1	38.2	89	89	1.6
92.4	1.45	5.6	41.3	3.69	21.7	34.1	79	79	-0.9
92.4	1.19	4.1	32.3	2.88	20.7	30.8	69	69	4.8
92.4	0.96	2.8	26.4	2.68	20.8	29.6	61	61	4.7
63.5	1.43	9.1	60.9	4.56	22.7	33.2	91	91	-7.1
63.5	0.99	4.3	40.6	3.63	20.9	28.7	79	79	-4.1
63.5	1.43	9.1	52.3	4.99	23.8	33.7	92	92	-2.1
63.5	1.08	5.2	40.2	3.92	22.0	29.8	80	80	1.6
63.5	0.77	3.7	30.0	3.04	20.7	27.0	69	69	-2.8

(Experiments with Top Cooling)

114.3	1.95	21.5	31.6	3.48	21.1	31.2	39	75	6.5
114.3	3.23	35.5	58.3	6.41	22.5	41.6	51	97	-0.4
114.3	2.35	29.0	38.0	6.65	21.3	35.7	45	83	-0.6
114.3	1.37	15.9	24.2	4.07	19.5	28.4	33	63	-3.9
114.3	4.22	42.9	73.0	7.37	8.1	28.9	40	97	5.2
114.3	2.79	34.0	50.8	4.86	6.3	21.1	32	81	-4.0
114.3	1.79	24.3	29.8	5.04	6.6	14.2	24	60	-5.5

Fig. 1. Schematic of internally heated molten UO_2 poolTable II. Preliminary Experimental Heat Transfer Data: Molten UO_2 Pool

(Pool Cross Section: 92 x 92 mm)

Pool Depth, mm	Power, kW	Heat Fluxes, kW/m ²		
		$\bar{Q}_U$	$\bar{Q}_H$	$\bar{Q}_D$
25	9.7	50	857	168
35	12.1	65	725	242
38	8.7	46	490	171
51	10.1	63	441	152

(4) the lower surface of the cavity was comprised of UO_2 that had been molten;
 (5) below this molten region there was usually a thin layer of sintered material.
 In some experiments, there have been as many as three overlying layers or crusts of solid UO_2 .

CORRELATION OF DATA FROM OPEN-TOP WATER EXPERIMENTS

The upward heat fluxes from the open-top water experiments were relatively small, being caused by surface vaporization to the ambient air above the heated pool. Because of this, it was considered a reasonable approximation to assume that the sideward and downward heat fluxes were not significantly influenced by the upward heat flux in these experiments. The interacting effects of downward and sideward heat fluxes were assessed empirically by dividing the pool conceptually into four zones as shown in Fig. 2. The heat generated within Zone 1 is assumed to transfer upward, that generated in Zone 2 is transferred downward, and that generated within Zone 3 is transferred sideward. The heat generated within Zone 4 is transferred partly downward and partly sideward.

Two heat fluxes, Q_H and Q_D are defined as that which would be obtained if there were no interacting effects between the sideward and downward heat transfer processes. The quantities Q_H/q and Q_D/q , where q is the volumetric heat generation rate, define the dimensions of the zones as indicated in Fig. 2. The experimental downward heat flux, $\bar{Q}_D$, was considered to be a weighted average between Q_D from Zone 2 and some fraction n of Q_D from Zone 4 as follows:

$$\bar{Q}_D W = Q_D (W - 2 Q_H/q) + 2nQ_D (Q_H/q). \quad (1)$$

Also the experimental sideward heat flux, $\bar{Q}_H$, is Q_H from Zone 3 and $(1 - n)Q_H$ from Zone 4, or

$$\bar{Q}_H L = Q_H (L - (Q_D/q)) + (1 - n)Q_H (Q_D/q). \quad (2)$$

Equations 1 and 2 were solved simultaneously for each experiment for Q_D and Q_H using the experimental values of $\bar{Q}_D$ and $\bar{Q}_H$ given in Table I and trial values of n . The results of calculations of Q_D for $n = 0.5$ are indicated in Fig. 3. The ordinate in Fig. 3 is $Q_D / (k \Delta T_D q)^{1/2}$, which is dimensionless and has the value of $2^{1/2}$ or 1.414 for heat transfer by conduction through a stagnant layer with uniform internal heat generation. It was found by varying n that a value of $n = 0.5$ gave the following empirical correlation of the data:

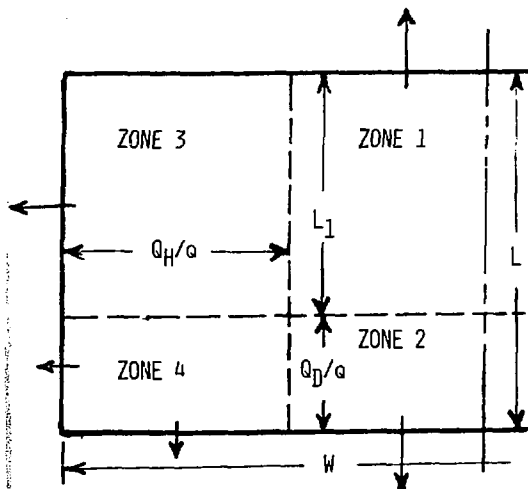


Fig. 2. Arrangement of zones within an internally heated pool with an open top

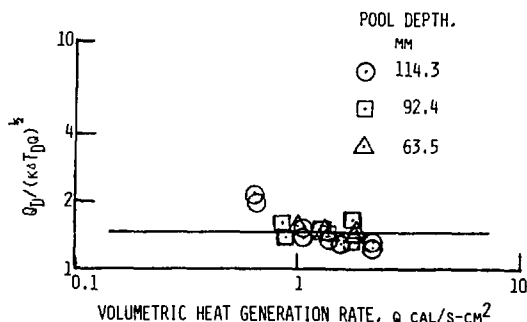


Fig. 3. Correlation of downward heat fluxes for nonboiling pools

$$\frac{Q_D}{(k\Delta T_D q)^{1/2}} = 1.45 \pm 13.3\%, \quad (3)$$

which is closely equivalent to conduction heat transfer.

Various attempts were made to correlate the corresponding values of Q_H obtained by the solution of Eqs. 1 and 2 with $n = 0.5$. The best correlation obtained is shown as the dashed line in Fig. 4. The correlation uses the Kulacki-Goldstein Rayleigh number, which involves the total pool depth L :

$$Nu_H = 0.59 Ra_{KG}^{0.25} \pm 5.9\%. \quad (4)$$

However, the experimental data reported by Mayinger *et al.*⁸ at lower Rayleigh numbers indicate that the sideward heat flux has an exponent of 0.19 instead of the 0.25 indicated by Eq. 4. Also, Mayinger *et al.* report results for the right circular cylinder with unit aspect ratio obtained by the THEKAR Z code which yields an exponent of 0.18 for the sideward heat flux. Because of these data, a tentative correlation using a round figure exponent of 0.2 was chosen for current use. This correlation is shown as the solid line in Fig. 4 and represents the water data almost as well as Eq. 4. The correlation obtained by least squares analysis is

$$Nu_H = 1.80 Ra_{KG}^{0.20} \pm 9.0\%. \quad (5)$$

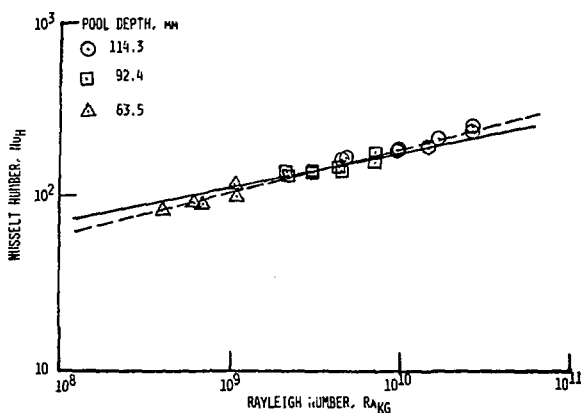


Fig. 4. Correlation of sideward heat fluxes for nonboiling pools

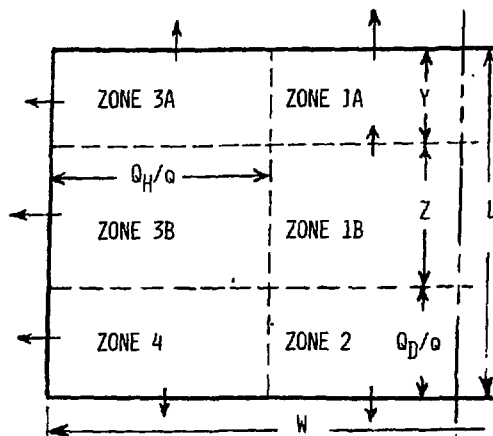


Fig. 5. Arrangement of zones within an internally heated pool with top cooling

DEVELOPMENT OF GENERAL MODEL FOR NONBOILING POOLS

The pool model is based on the correlations obtained from the open-top water experiments combined with general observations of the heat transfer in the series of top-cooled water experiments which were performed at a single aspect ratio of $L/W = 0.60$. Analysis of the heat flux distributions in the experiments indicated that the presence of a large upward heat flux induced by the top cooling had the effect of reducing the sideward heat flux. The data indicated that about $1/4$ of the heat generated in Zone 3 in Fig. 2 was directed upward and about $3/4$ was directed sideward for the particular aspect ratio of the experiments. The experimental bulk pool temperatures were lower than values calculated from the Kulacki-Emara correlation⁶ for the depth L_1 , indicated in Fig. 2. This is consistent with the presence of concerted upward convective flows in the

central portion of the pool which facilitates upward heat transfer. The Kulacki-Emara correlation represents the extreme condition when the upward flow is not augmented by convective downflow at the cooled sidewalls.

The nature of the data with the cooled top suggested an empirical method to take account of the augmented upward heat flux. The method involves the zone arrangement shown in Fig. 5 where Zones 1 and 3 have each been divided into two portions. The heat generated within Zone 3A is directed equally upward and sideward while the heat generated within Zone 3B is directed sideward. The heat generated within Zones 1A and 1B are directed upward. The depth of Zone 1A and 3A, designated Y, is arbitrarily defined by the Kulacki-Emara correlation⁶ expressed as follows:

$$\frac{qY^2}{k\Delta T_U} = 0.403 \left[\frac{g\beta qY^5}{\alpha \nu 2k} \right]^{0.226}, \quad (6)$$

which provides an implicit relation between Y and ΔT_U . The augmentation to the upward heat flux is accounted for, empirically, by requiring the total heat generated within Zone 1B to equal that generated within 3A, i.e., Zones 1B and 3A are of equal volumes. This requirement assures that the model will extrapolate to the infinite layer model of Ref. 3 where there are only two zones: the upper Zone, 1A, is described by the Kulacki-Emara correlation and the lower Zone, 2, by conduction. In the extrapolation to the infinite layer, Zones 3A and, therefore, 1B become negligible in magnitude.

The model was developed for three geometrical cases: (1) the rectangular pool with heat removal at two vertical walls (water experiments), (2) the pool with square cross-section with heat removal on all four vertical walls (UO₂ experiments), and (3) the right circular cylinder with heat removal on the vertical circumference. The models for cases (2) and (3) are identical, i.e., the pool width, W, in case (2) becomes the pool diameter in case (3).

The calculational method is outlined as follows: The inputs are the pool dimensions, L and W (see Fig. 5); the boundary temperatures, T_D , T_H , and T_U ; the volumetric heat generation rate, q; and the fluid properties. The Kulacki-Goldstein Rayleigh number is calculated directly, then a trial value of the corrected sideward heat flux Q_H is used. From this, the dimension Q_H/q is obtained and a tentative value of the pool bulk temperature, T_P is obtained from Eq. 5 rearranged as follows:

$$T_P = T_H + \frac{Q_H L}{k 1.8 Ra_{KG}^{0.2}}. \quad (7)$$

The dimension of the conduction zone is calculated as

$$Q_D/q = \sqrt{2k(T_P - T_D)/q}. \quad (8)$$

The dimension Y is calculated by solving Eq. 6. The dimension Z is calculated from the requirement of equal volumes of Zones 1A and 3B. If the total pool depth, $Y + Z + Q_D/q$, is greater than L, the actual pool depth, then the calculations are repeated with a smaller trial value of Q_H . Iterations are continued until $Y + Z + Q_D/q$ is equal to L within 0.03%.

Results calculated for the conditions of the top-cooled water experiments are compared with experimental results in Table III. The results shown in Table III indicate that the empirical model produces pool temperatures and heat fluxes in reasonable agreement with experimental values.

Table III. Comparison of Calculated and Experimental Data for Cooled-Top Water Pool Experiments

Heat Fluxes, kW/m ²						Pool Bulk Temperature, °C	
Sideward		Upward		Downward		Calc.	(Exp.)
Calc.	(Exp.)	Calc.	(Exp.)	Calc.	(Exp.)		
30.7	(31.6)	26.5	(21.5)	3.84	(3.48)	70.9	(75)
49.6	(58.3)	45.8	(35.5)	6.09	(6.41)	94.2	(97)
36.8	(38.0)	32.0	(29.0)	4.59	(6.65)	79.7	(83)
21.1	(24.2)	18.9	(15.9)	2.88	(4.07)	59.6	(63)
64.3	(73.0)	60.6	(42.9)	7.70	(7.37)	96.5	(97)
43.6	(50.8)	38.3	(34.0)	5.38	(4.86)	74.7	(81)
28.5	(29.8)	23.9	(24.3)	3.63	(5.04)	57.0	(60)

Table IV. Heat Transfer for Internally Heated Right Circular Cylinders with Equal Boundary Temperatures

Rayleigh Number Ra_{KG}	(Ra')	Percent Heat Sideward		Percent Heat Upward		Percent Heat Downward		Overall Dammkohler Number (Da)	
		This Study	(Ref. 8)	This Study	(Ref. 8)	This Study	(Ref. 8)	This Study	(Ref. 8)
		(Aspect Ratio = 1.0)							
1.56×10^3	(10^5)	72.7	(68.5)	13.3	(18.6)	14.0	(12.9)	23.3	(23.0)
1.56×10^4	(10^6)	74.6	(68.7)	14.3	(20.5)	11.1	(10.7)	36.8	(34.6)
1.56×10^5	(10^7)	75.9	(68.6)	15.3	(22.4)	8.8	(8.9)	58.4	(52.5)
1.56×10^6	(10^8)	76.7	(72.6)	16.3	(19.6)	7.0	(7.9)	93.1	(75.1)
1.56×10^7	(10^9)	77.1	(76.4)	17.4	(16.7)	5.6	(6.9)	148.6	(108.0)
1.56×10^8	(10^{10})	77.1	(--)	18.5	(--)	4.4	(--)	238	(--)
1.56×10^9	(10^{11})	76.9	(--)	19.6	(--)	3.5	(--)	381	(--)
(Aspect Ratio = 0.5)									
1.56×10^3	(10^5)	55.8	(--)	23.4	(--)	20.7	(--)	15.1	(--)
1.56×10^4	(10^6)	58.1	(--)	25.4	(--)	16.4	(--)	23.6	(--)
1.56×10^5	(10^7)	59.6	(54.8)	27.4	(31.5)	13.1	(13.7)	37.3	(46.8)
1.56×10^6	(10^8)	60.4	(54.5)	29.2	(34.6)	10.4	(11.0)	59.3	(73.0)
1.56×10^7	(10^9)	60.7	(53.7)	31.1	(37.6)	8.3	(8.7)	94.8	(114.6)
1.56×10^8	(10^{10})	60.5	(52.5)	32.9	(40.6)	6.6	(6.8)	152	(181.0)
1.56×10^9	(10^{11})	60.0	(--)	34.7	(--)	5.2	(--)	245	(--)
(Aspect Ratio = 0.25)									
1.56×10^3	(10^5)	36.5	(--)	34.4	(--)	29.1	(--)	11.4	(--)
1.56×10^4	(10^6)	38.6	(--)	38.1	(--)	23.2	(--)	17.4	(--)
1.56×10^5	(10^7)	40.0	(37.7)	41.5	(43.4)	18.5	(18.9)	27.2	(34.0)
1.56×10^6	(10^8)	40.7	(37.4)	44.6	(47.5)	14.7	(15.1)	43.0	(53.1)
1.56×10^7	(10^9)	40.9	(36.7)	47.4	(51.4)	11.6	(11.9)	68.6	(83.9)
1.56×10^8	(10^{10})	40.7	(35.6)	50.0	(55.1)	9.2	(9.3)	110.3	(133.8)
1.56×10^9	(10^{11})	40.1	(--)	52.5	(--)	7.3	(--)	178	(--)
(Aspect Ratio = 0.10)									
1.56×10^3	(10^5)	17.2	(--)	44.0	(--)	38.8	(--)	9.4	(--)
1.56×10^4	(10^6)	18.4	(--)	50.2	(--)	31.4	(--)	14.1	(--)
1.56×10^5	(10^7)	19.2	(19.5)	55.6	(56.1)	25.2	(24.4)	21.7	(26.3)
1.56×10^6	(10^8)	19.6	(19.3)	60.3	(61.3)	20.1	(19.4)	34.1	(41.2)
1.56×10^7	(10^9)	19.7	(18.8)	64.4	(66.0)	15.9	(15.2)	54.1	(65.4)
1.56×10^8	(10^{10})	19.5	(18.1)	67.8	(70.1)	12.6	(11.8)	87.0	(105.2)
1.56×10^9	(10^{11})	19.2	(--)	70.9	(--)	9.9	(--)	141	(--)

COMPARISON OF MODEL WITH LITERATURE DATA

The Mayinger et al. calculations for pools in the form of right circular cylinders of unit aspect ratio are summarized as follows:⁸

$$\begin{aligned}\overline{Nu}_U &= 0.34 (Ra')^{0.22} & 10^5 < (Ra') < 1.5 \times 10^7 \\ \overline{Nu}_U &= 2.8 (Ra')^{0.09} & 2 \times 10^7 < (Ra') < 10^9 \\ \overline{Nu}_H &= 0.495 (Ra')^{0.18} & 10^5 < (Ra') < 10^9 \\ \overline{Nu}_D &= 0.935 (Ra')^{0.10} & 10^5 < (Ra') < 10^9\end{aligned}\tag{9}$$

The Rayleigh number $Ra' = 64 Ra_{KG}$. The calculations used the THEKAR Z code and apply to the case where all boundary temperatures are the same $T_U = T_H = T_D$. Because of this, a single temperature difference ΔT characterizes each case. The temperature differences in the Mayinger et al. results refer to the difference between the average pool temperature and the boundary temperatures, whereas the zone model implies differences between the mean peak temperature T_p and the boundary temperatures. The difference between the two definitions of ΔT should be very small at the high Rayleigh numbers of practical interest.

The Damkohler number ($Da = qL^2/k\Delta T$) is essentially a dimensionless reciprocal temperature difference for the pool as a whole and was found to be a convenient means to compare the results of the two studies. The overall Damkohler number for a right circular cylinder (or a pool having vertical sidewalls and a square cross section) is given by

$$Da = 4 \frac{L}{W} \overline{Nu}_H + \overline{Nu}_U + \overline{Nu}_D.\tag{10}$$

The fraction of the total heat generated which is transferred sideward, upward, and downward are:

$$F_H = 4(L/W)\overline{Nu}_H/Da \quad F_U = \overline{Nu}_U/Da \quad F_D = \overline{Nu}_D/Da, \text{ respectively.}\tag{11}$$

Results calculated by the empirical model are compared with results calculated from Eqs. 9, 10, and 11 in Table IV, where it is apparent that the empirical model predicts results for heat flux distributions which are in general agreement with the THEKAR code calculations, although the differences in Damkohler number are significant at high Rayleigh numbers. It should be noted that the THEKAR code does not contain a turbulence model and could yield low values of Da at high Rayleigh numbers.

Mayinger et al. report experimental results for pools in the form of right circular cylinders of aspect ratio 0.5 or less as follows:⁶

$$\left. \begin{aligned}\overline{Nu}_U &= 0.345 (Ra')^{0.233} \\ \overline{Nu}_H &= 0.6 (Ra')^{0.19} \\ \overline{Nu}_D &= 1.389 (Ra')^{0.095}\end{aligned}\right\} \quad 10^7 < (Ra') < 3 \times 10^{10}\tag{12}$$

Table IV includes comparisons of results calculated by the empirical model with those calculated from Eq. 12 for aspect ratios of 0.5, 0.25, and 0.1. The comparison indicates reasonable agreement between the heat flux distributions. However, the empirical model predicts Damkohler numbers up to about 20% lower than the experimental results. In general, the empirical model is considered to represent the available experimental data with an accuracy of the order of $\pm 20\%$.

APPLICATION OF MODEL TO MOLTEN UO_2 EXPERIMENTS

The empirical model was applied to the conditions of the molten UO_2 experiments. The sideward and downward boundary temperatures are expected to be equal to the melting temperature of UO_2 , which is 2840°C . The temperature of the upper surface of the pool could be higher because of the insulating effects of the solid fuel crusts which formed above the surface of the molten pool. These crusts acted as radiation shields and insulating layers which decreased the upward heat flux to the water-cooled top plate of the apparatus. This is believed to be the reason for the low values of upward heat flux apparent in Table II. The downward heat fluxes obtained in the experiments were found to be about a factor of two greater than those predicted by the model. A possible explanation for the high downward heat fluxes is that pure molten UO_2 could be partially transparent in the near infrared such that internal radiation could significantly affect heat transfer. A recent study of radiative heat transfer in molten UO_2 was reported by Anderson¹⁰ who studied the case of the horizontal layer with equal boundary temperatures. He concluded that the likely effects of internal radiation heat transfer would be to increase the effective Nusselt numbers for upward and downward heat flux by an additive factor as follows:

$$\text{Nu}_R = \text{Nu} + \frac{4}{3N} \frac{(\theta_m^4 - 1)}{(\theta_m - 1)}, \quad (13)$$

where N = a conduction-radiation coupling parameter which is probably 0.5 or greater, and

θ_m = ratio of absolute peak pool temperature to the absolute boundary temperature.

Studies are underway to determine if the heat transfer results from the molten UO_2 experiments can be accounted for by incorporation of Eq. 13 into the empirical model.

CONCLUDING REMARKS

Results from water experiments on internally heated pools with vertical sidewalls have been used to develop a simple empirical model of pool heat transfer. The model is capable of predicting heat flux distributions and pool peak temperature given the pool dimensions, fluid properties, internal heat generation rate, and boundary temperatures. The model has been shown to generate results in reasonable agreement with those reported by Mayinger *et al.* Because of this, the model should be extremely useful for engineering calculations of heat-generating fluids, including molten pools containing fuel debris from hypothetical core-disruptive accidents. The model offers a simple calculational procedure to take account of the interacting effects of heat transfer in the three directions.

Fuel-containing pools growing into concrete or sacrificial material beds can be expected to develop curved lower surfaces and edges. The present model does not take account of such curvature. However, it is likely that such growing pools can be satisfactorily represented as right circular cylinders for the present time. In the near future, it is expected that the empirical model can be modified to take more explicit account of curved boundaries.

At present, the model underpredicts downward heat flux values obtained in preliminary experiments with molten UO_2 pools. A possible explanation for this is that partial transparency exists within the molten UO_2 which is sufficient to strongly affect the heat transfer. Additional experimentation and modeling effort are required to explore this possibility.

NOMENCLATURE

Da	Damkohler number, $qL^2/k\Delta T$	Greek Symbols:	
F	Fraction of total heat generated	α	Thermal diffusivity
g	Acceleration of gravity	β	Coefficient of thermal expansion
k	Thermal conductivity	ΔT	Difference between average peak pool temperature and boundary temperature
L	Pool depth	ν	Kinematic viscosity
N	Conduction-radiation coupling parameter	θ_m	Ratio of absolute peak pool temperature to absolute boundary temperature
n	Fraction of heat transferred downward	Subscripts:	
<u>Nu</u>	Nusselt number, $QL/k\Delta T$	D	Downward
Nu	Average Nusselt number over a surface, $\bar{QL}/k\Delta T$	H	Horizontal (sideward)
$\bar{Q}$	Corrected heat flux	P	Average peak pool
$\underline{Q}$	Average heat flux over a surface	R	Thermal radiation
q	Volumetric heat generation rate	U	Upward
Ra'	Rayleigh number, $g\beta qL^5/\alpha\nu k$		
Ra _{KG}	Rayleigh number, $g\beta qL^5/64\alpha\nu k$		
T	Temperature		
W	Pool width		
Y,Z	Zone dimensions (Fig. 5)		

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HEAT TRANSFER AND FLUID DYNAMICS IN A VOLUME-HEATED BOILING POOL

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ABSTRACT

An analytical and experimental investigation of the horizontal heat transfer from an internally heated boiling pool to a vertical wall has been conducted. A model is presented for the heat transfer based on combined modes of natural and forced convection flow in the liquid boundary layer at the vertical wall. The experimental results, using Joule heating of a $\text{ZnSO}_4\text{-H}_2\text{O}$ solution, indicate that the pool may develop a low-density "foam-like" regime as well as a dense "turbulent" regime. The analytic model compares favorably to the results in the "dense" regime which show that the highest heat flux will develop near the upper edge of the pool. Application of the model to a boiling pool of UO_2 with a low level of boiling power (decay-heat level) suggests that the sideward heat transfer rates will limit the thickness of any fuel crust that may develop between the pool and the surrounding steel structure to a few millimeters. The stability and strength of such a crust have not been investigated.

INTRODUCTION

Characteristics of internally heated pools is a subject that has received increasing attention in the last few years due primarily to its application in safety studies of liquid metal fast breeder reactors (LMFBR). As a hypothetical loss-of-flow accident with failure to scram has been predicted to lead to melting of a large part of the LMFBR core, it became important to estimate the possible consequences of formation of fuel pools at various locations within the vessel, due to either direct fuel melting or remelting of fuel debris. The decay heat of the radioactive fission products may be sufficient to cause fuel boiling. Indeed, analyses of post-accident heat removal for FFTF have required consideration of a boiling fuel pool in the sub-core dome-like walls of the low pressure plenum. Similarly, should a large amount of core debris be relocated rapidly to the CRBR vessel bottom following a hypothetical core meltdown, the fuel debris may form a boiling pool there, which may then proceed to melt through the vessel steel wall.

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Two aspects of the boiling pool behavior are of major interest:

- (1) The heat loads that will be imposed by such a pool on the surrounding structures, including the heat fluxes at any hot spots that may be present (i.e. locations and magnitude of highest heat flux).
- (2) The voiding pattern within the pool due to the internal vapor generation (i.e. the possible density gradients in the pool).

Item (1) above is of concern in assessment of the potential and rate of melting attack on the structure surrounding the fuel pool. Item (2) above is of concern in assessment of the post-accident recriticality potential due to accumulation of the fuel debris in a pool form.

In this paper, an analytical and experimental study related to Item (1) above will be discussed. In particular the horizontal heat transfer to a vertical wall from the boiling pool is addressed. The experiments involved a boiling pool of $\text{ZnSO}_4\text{-H}_2\text{O}$ solution. The patterns of pool voiding and the detailed results of the vertical profile of the void fraction in the pool are addressed in a separate paper [1]. Applications of the study to a UO_2 boiling pool will also be discussed here.

The only previously reported study on the heat transfer from a boiling pool is that of Argonne National Laboratory [2,3]. In the ANL study, also using salt solutions, the effort has concentrated on the average heat transfer coefficient, for both sideways heat transfer and downward heat transfer. The present study provides a mechanistic formulation for predicting both the average and local heat transfer coefficient.

ANALYSIS

Considering the case of a volume-heated boiling pool, our interest is in the heat transfer process between the pool and its side walls. Several interacting phenomena govern the fluid dynamics of the situation. These may be briefly described as follows:

- (a) Heat generation in the bulk liquid creates a reduced fluid density by both thermal expansion of hot liquid and by the formation of vapor voids. The time-average void distribution results from a dynamic balance between the kinetics of void formation, growth and translation.
- (b) Upward flow of vapor toward the pool surface creates a "vapor lift" which induces a net upward flow of liquid in the region where vapor flux is highest, most likely in the central core region of the pool.
- (c) This up-flowing liquid (by vapor drag) must be returned to the lower part of the pool, with the exception of that fraction entrained into the vapor free space above the pool. It is expected that this downward net convection of liquid occurs in the region of lower vapor flux, near the cold boundary walls.
- (d) The cold side walls create a layer of colder liquid in the boundary layer region next to the walls. A density-gradient driven natural-convection flow occurs in that region.

In the present approach, the heat transfer due to the natural convection component (item d) of flow and the forced convection component (item c) at the vertical wall are combined to give the effective sideward heat transfer. The approach is presented in what follows.

Natural Convection

It is suggested that the classical solution for the single-phase boundary layer problem be generalized to obtain an approximate model for the natural convection process in a boiling pool. Assuming that the wall temperature is sufficiently below fluid saturation temperature, the boundary layer region (A in Fig. 1) is treated as liquid with maximum density ρ_0 evaluated at T_w . The fluid density in bulk region of pool (C in Fig. 1) is approximated by the two-phase density,

$$\bar{\rho}_B = \bar{\alpha}\rho_v + (1 - \bar{\alpha})\rho_\ell \approx (1 - \bar{\alpha})\rho_\ell \quad (1)$$

where ρ_v , ρ_ℓ are vapor and liquid densities at bulk temperature T_B , and $\bar{\alpha}$ is the void fraction in bulk pool. The appropriate local and average Grashof numbers for a boiling pool would then be, respectively:

$$Gr_x = \frac{gx^3}{\nu^2} \left(\frac{\rho_0 - \bar{\rho}_B}{\rho_0} \right) \quad (2.a)$$

$$\text{and} \quad Gr_L = \frac{gL^3}{\nu^2} \left(\frac{\rho_0 - \bar{\rho}_B}{\rho_0} \right) \quad (2.b)$$

Use of these Grashof numbers in the classical heat transfer correlations for boundary layer heat transfer [4] gives a first-order approximation of the natural convection heat transfer to the boundary walls of the boiling pool.

Void Fraction and Pool Density

To obtain a quantitative estimate of pool density (ρ_B), some helpful indications can be obtained from the problem of co-current two-phase flow. For the regime of churn-turbulent flow, Zuber et al [5] suggested the following relation for phase-drift velocities

$$V_D = (1 - \alpha)^{C_2} V_\infty \quad (3)$$

where V_D = drift velocity of vapor phase with respect to liquid phase (equivalent to V_{vj} in Wallis's terminology [6])

V_∞ = terminal rise velocity of a bubble

$$= 1.4 \cdot \left[\frac{\sigma \cdot g \cdot g_c (\rho_\ell - \rho_v)}{\rho_\ell^2} \right]^{1/4} \quad (4)$$

and C_2 = constant with value of 0 for churn-turbulent flow

In the present case of boiling pool, in the core region, there is a net up-flow of liquid caused by vapor lift as mentioned earlier. If V_1 is defined as the time-mean liquid velocity in bulk pool (relative to laboratory coordinate), then the superficial vapor velocity V_v (in laboratory coordinate) may be given by:

$$V_v = \alpha(V_D + V_1)$$

and for churn-turbulent regime,

$$V_v = \alpha(V_\infty + V_1) \quad (5)$$

Since in the pool vapor is generated volumetrically, the quantities V_v and α will vary with depth in pool. If V_1 is assumed to be directly proportional to V_∞ , then equation (5) may be written as:

$$V_v = \alpha B V_\infty, \quad (6)$$

the parameter B being a quantity to be determined empirically. Here B is

assumed to be independent of α or y for a given boiling rate.

From conservation of vapor mass in a saturated pool we obtain:

$$\frac{dV_v}{dy} = \frac{G_v}{\lambda \rho_v} (1 - \alpha) \quad (7)$$

where G_v is the intensity of vaporization source (energy for vaporization per unit time per unit liquid volume). In the cases here G_v is space-independent and time-independent. By manipulating equations (6) and (7) we obtain the local void fractions

$$\alpha = 1 - \exp \left[\frac{-G_v}{\lambda \rho_v B V_\infty} y \right] \quad (8)$$

From equation (8), the average void fraction can be obtained by integrating over y :

$$\bar{\alpha} = 1 - \frac{\lambda \rho_v B V_\infty}{L G_v} \left[1 - \exp \left[- \frac{G_v}{\lambda \rho_v B V_\infty} L \right] \right] \quad (9)$$

Forced Convection

As mentioned earlier, an effectively forced convection flow component may be present due to liquid return-drainage associated with vapor lift. This flow may be characterized by a Reynolds number:

$$Re_x = \frac{V_s x}{\nu}; \text{ local at } x \quad (10)$$

where V_s is the superficial velocity of vapor at the upper pool boundary (i.e. V_v at $y = L$). The choice of the characteristic velocity is somewhat arbitrary and should be judged only in relation to its success in prediction of experimental observations.

Combined Heat Transfer Relation

From other work on combined forced and natural convection in single-phase fluids [7], it is known that the effective Nusselt number approaches asymptotically the natural convection Nusselt number, Nu_n , or the forced convection number, Nu_c , depending on Gr/Re^2 :

$$Nu \rightarrow Nu_n \text{ as } Gr/Re^2 \rightarrow \infty \text{ and } Nu \rightarrow Nu_c \text{ as } Gr/Re^2 \rightarrow 0$$

As a method in combining the modes of convection it is possible to correlate Nu/Nu_n against Nu_c/Nu_n . Following a general correlation procedure [8], it is suggested that the functional form be:

$$Nu/Nu_n = [1 + (Nu_c/Nu_n)^n]^{1/n} \quad (11)$$

The parameter n is a single empirical parameter to be evaluated from data.

The average Nusselt number to the vertical wall of height L would then be given by equation (11) and the following relations:

$$\begin{aligned} Nu_{c,L} &= 0.036 Pr^{1/3} Re_L^{0.8}; Re_L > 3 \times 10^5 \\ &= 0.0664 (Pr Re_L)^{0.5}; Re_L < 3 \times 10^5 \end{aligned} \quad (12)$$

$$\begin{aligned}
Nu_{n,L} &= 0.0246 (Gr_L)^{2/5} Pr^{7/15} [1 + 0.494 Pr^{2/3}]^{-2/5}; \\
&Gr_L Pr > 10^9 \\
&= 0.547 (Gr_L Pr)^{0.25}; \quad Gr_L Pr < 10^9
\end{aligned}
\tag{13}$$

The physical parameters in Gr , Pr and Re_L are taken to be liquid properties at the temperature $(T_w + T_B)/2$.

In a similar manner, the local Nusselt number at the vertical wall would be given by equation (11) and the following relations.

$$\begin{aligned}
Nu_{c,x} &= 0.0288 Pr^{1/3} Re_x^{0.8}; \quad Re_x > 3 \times 10^5 \\
&= 0.332 (Pr Re_x)^{0.5}; \quad Re_x < 3 \times 10^5
\end{aligned}
\tag{14}$$

$$\begin{aligned}
Nu_{n,x} &= 0.0295 (Gr_x)^{2/5} (Pr)^{7/15} [1 + 0.494 (Pr)^{2/3}]^{-2/5}; \\
&Gr_x Pr > 10^9 \\
&= 0.41 (Gr_x Pr)^{0.25}; \quad Gr_x Pr < 10^9
\end{aligned}
\tag{15}$$

It should be mentioned that the boundary layer approach used here implies an infinite heat transfer coefficient may be obtained in the limit $x \rightarrow 0$. In fact, the approach is invalid for small x since the dominant direction of liquid flow changes from parallel to perpendicular to the wall. It may be that a stagnation-point heat transfer model would be more representative in such conditions. More detailed experimental information is needed to arrive at a quantitative model for the corner heat transfer.

Comparison with Previous Data

Recently, Gabor et al [3] presented data for boiling pools of $ZnSO_4$ and $CuSO_4$ aqueous solutions. These data, for cases of side-wall cooling only, are compared to the model predictions in Figure 2. The predictions are based on equations (11)-(13), with the $\bar{\alpha}$ values reported in the experiments and $n = 0.7$. The reasonable agreement obtained tends to confirm the basic hypothesis of the proposed phenomenological model.

EXPERIMENT

Several tests have been performed in which a $ZnSO_4$ - H_2O solution was Joule heated to form the boiling pool. The apparatus used is shown schematically in Figure 3. It consists mainly of (1) the pool container, (2) the power supply including the electrodes, (3) the test plate used in obtaining the heat transfer rates, (4) water make-up system, and (5) the pressure probe and traversing mechanism. The container was an open top Corning Pyrex glass jar with nominal inside dimensions of 17 cm by 23 cm in cross section and 32 cm in height. The copper electrodes, 0.64 cm thick, were placed opposite to the wider sides of the container. The electrodes were cooled by water flowing inside 0.64 cm copper tubes that were silver soldered to the back of the electrodes. The power source used in the experiment had a maximum capacity of 80 kilowatts. Both power and voltage were controlled through a bank of twenty-four powerstats. The power signal was recorded on a continuous single pen recorder with 2% accuracy.

The test plate consisted of a block of aluminum alloy 6061-T651. The plate was 2.5 cm thick, 12.7 cm wide and 33 cm high. The plate was divided along its height into thirteen equal segments. Each segment was isolated thermally by a 2.0 cm deep cut into the plane facing the boiling pool. Each segment had a

central groove from the back that was 1.25 cm deep and 1.25 cm wide to allow individual cooling of the segment by water flow. The flow into each segment was controlled by a fine metering valve. The flow would enter and leave through nozzles fitted on a lexan plate covering the back of the aluminum plate. The coolant temperature rise between the inlet and outlet of each segment was measured. Thermocouples were also placed in the center of each segment close to the plate surface. The test plate was electrically insulated by a 0.075 mm thick layer of teflon film. A silicone based adhesive on the inner side of the teflon provided contact with the aluminum surface.

The water make-up system consisted of a constant liquid level weir connected to the bottom of the boiling pool container by a 1.9 cm I.D. rubber hose. Fresh water was supplied to the weir from a storage tank above the pool at a steady flow rate determined from a rotometer type flow meter. The weir overflow was measured. The difference between the inflow and outflow established a continuous rate of water make-up to the boiling pool.

A differential pressure transducer with range 0-0.5 psig was used to measure the static pressure at any point in the boiling pool. The transducer was located outside the pool and fastened to a screw jack placed on an X-Y traversing table, enabling the device to translate in 3-dimensions. A rigid 2 mm I.D. glass tube probe led from the signal port of the transducer to a point in the pool. The location of the probe tip was known from calibrated scales outside the pool. Air pressure from a compressed air tank was introduced into the glass probe at the transducer. The pressure was controlled to cause a slow stream of air bubbles to exit the probe tip in the pool. Tests in a still pool for widely varied air flow rates and bubble formation frequencies indicated no error in static head measurement from the pressure drop in the glass probe or bubble frequency at the probe tip. The output signal from the transducer was recorded on a two-pen Honeywell 196 chart recorder.

Three thermocouples were located at various positions in the pool to measure the actual boiling temperature. Three thermocouples were placed in holes in the electrodes. All thermocouple readings were recorded on multipoint Honeywell recorders referenced to a physical ice bath. Absolute temperature measurements were within $\pm 0.5^\circ$ F. The relative temperature measurements of the plate coolant temperature had better accuracy because of thermocouple matching.

For each teflon sheet used in the experimental series a calibration was made to determine the thermal resistance of the teflon sheet before and after the experimental life of the teflon. This was accomplished by measuring the temperature profile through a heated aluminum block covering one cooled segment of the test plate. The accuracy of the calibration was estimated to be $\pm 10\%$.

RESULTS AND DISCUSSION

In operation of the experiment, it was observed that two different flow regimes occur - a "dense" regime and a "foaming" regime. The latter was characterized by a cellular two-phase structure whose void fractions tended to be high through the major fraction of the pool.

Results in the "Dense" Regime

As described above, local heat transfer coefficients were obtained over ~ 2.5 cm segments of the vertical test plate. For the "dense" pool (non-foaming) runs, smooth curves were fitted through the measured local coefficients, as a function of distance from pool surface (X). Results for a typical run are shown on Figure 4. Average heat transfer coefficients were then obtained by integration of the smoothed curves. The results for the dense-pool runs are summarized in Table I, along with some pertinent variables.

TABLE I
Dense-Pool Heat Transfer Runs

Run	$G_v \left(\frac{\text{Cal.}}{\text{cm}^2 \cdot \text{Sec}} \right)$	L (cm)	$\bar{\alpha}$	$h_L \left(\frac{\text{Cal.}}{\text{Sec. cm}^2 \cdot ^\circ\text{C.}} \right)^a$
44	1.11	14.1	0.36	0.084
45	1.12	14.1	0.37	0.088
46	0.83	16.6	0.43	0.113
47	0.93	12.8	0.36	0.074
48	1.21	15.4	0.43	0.059
49	0.50	14.1	0.46	0.072
50	1.77	7.43	0.42	0.084
51	0.890	6.16	0.31	0.064
52	0.053	21.9	0.11	(0.087)
53	0.059	23.3	0.12	(0.110)
54	0.130	17.0	0.20	(0.137)
55	0.549	13.8	0.10	0.066
56	0.276	15.2	0.23	0.084
57	0.382	11.9	0.28	0.056

^aThe coefficients for Runs 52-55 are placed in parentheses to indicate that some questions exist regarding their accuracy, as a result of operational difficulties.

These measured average heat transfer coefficients were compared with the phenomenological model proposed above. Using a value of 0.7 for the constant n (see Equation 11), the average Nu_L calculated by the model are plotted against the experimentally determined Nu_L in Figure 5. Also shown is the region obtained for ANL data (from Figure 2). With the exception of the four questionable data points noted above, these results showed reasonable agreement with both the ANL data and the proposed correlation. An even better agreement between experimental and calculated Nu_L is obtained for the reliable data if a value of $n = 1$ is used in the proposed model. This is shown in Figure 6.

In actual application, one may know the boiling source intensity (G_v) but have no information on the average pool density ($\bar{\rho}_B$) or void fraction ($\bar{\alpha}$). From equation (9), the value of $\bar{\alpha}$ may be estimated when B is given. For the cases of a dense pool, B was evaluated [1] by using the vertical profile of the void fraction data and the vertical profile of the vapor velocity as estimated from test conditions. This results in an approximate relation given by:

$$B = 1.55 (v_s/v_\infty)^{0.65} \quad (16)$$

Equations (9) and (16) have been used to obtain an estimated $\bar{\alpha}$, which can then be used to predict Nu_L a priori. The results are shown in Figure 7. Again, the agreement is reasonable.

In comparing the results to the analysis, a note on the transition from laminar to turbulent boundary layer is in order. The values of Gr_L for these runs ranged between 8.6×10^8 and 2.3×10^{10} , thus leading to a $Gr_L Pr$ range between 5.3×10^9 and 1.4×10^{11} . Experiments on boundary layers have shown that the transition from laminar to turbulent regime occurs between $Gr Pr = 10^8$ and 10^{10} . For practical reasons, a value of 10^9 is usually used as a transition point. Since Gr_x is proportional to x^3 , a laminar regime may better charac-

terize the natural convection flow on a major part of the vertical test plate (in the current experiments). Similarly, the values of Re_L in the current experiments range between 1.3×10^4 and 6.9×10^4 . Thus, the forced convective flow regime is expected to be in the laminar regime all the length of the plate. The transition from laminar to turbulent forced convective flow has been observed to occur between $Re = 10^5$ and 10^6 . For practical reasons, an intermediate value (3×10^5) is usually used.

In Figures 8 and 9, the predictions of the correlation of equations 11, 14 and 15 (with the empirical constant $n=1$) are shown using two sets of assumptions:

1. Assuming that both the free convection and forced convection components are in the laminar regime (solid line).
2. Assuming that both the free convective and forced convective components are in the turbulent regime (dotted line).

It is clear from the figures that the laminar-laminar assumption does indeed represent the observed vertical profile of the heat transfer coefficients.

Results in Foam Regime

In Runs 29-43, the boiling pool was observed to be in a "foam" regime with a cellular vapor/liquid structure throughout the greater portion of the pool. The average void fractions measured in the foam boiling pools were significantly higher than those in a non-foaming pool. Using the experimentally determined $\bar{\alpha}$, the phenomenological model was used to calculate Nu_L for these foaming runs. The results shown in Figure 10 indicate that the average Nusselt numbers for boiling pools in the foam regime are slightly higher than those for dense pools at comparable conditions. It is possible that condensation of the foam mixture at the cold wall enhanced the heat transfer process.

Since applications for reactor safety may in fact encounter boiling pools in the foam regime, further investigation of the different phenomenological process should be undertaken, and an adequate model to predict the heat transfer rate in such a regime should be sought.

Application to Fuel Pools and Safety Implications

The relationships developed for the average heat transfer coefficient to a vertical wall, equations (11)-(13), can readily be applied to a UO_2 pool. Consider a numerical example for illustration purposes. If the liquid fuel effective height $L_0 = 10$ cm and $G_v = 2.0$ cal/cm³ sec, the average void fraction $\bar{\alpha}$ is predicted from equations (9) and (16) to be ~ 0.5 . For liquid UO_2 , $Pr = 0.8$, $\nu = 0.047$ cm²/sec and $k = 0.006$ cal/cm sec °C. Thus, based on $L = 20$ cm (actual pool height), the value of $Gr_L Pr = 1.42 \times 10^9$ and $Re_L = 1.7 \times 10^4$.

In this case, the natural convection layer undergoes the transition from laminar to turbulent regime ($Gr_L Pr = 10^9$) at a distance $x = 0.9 L$ down the vertical wall. An exact determination of h requires using a weighted average of both regime-dependent $Nu_{n,L}$ correlations given in equations (12) and (13). For simplification, the laminar regime relation will be used here to illustrate the application combined with the laminar forced convection relation, to obtain

$$h_L = 0.517 \left[1 + 1.178 \frac{(V_s)^{0.5}}{(g \bar{\alpha} L)^{0.25}} Gr_L^{0.25} \right] \frac{k}{L} \quad (17)$$

For the case under consideration, $\bar{\alpha} = 0.5$, $L = 20$ cm and $V_s = 40$ cm/sec, the value of h_L is 0.0518 cal/cm² sec °C. It should be noted that the value of h_L is not sensitive to the boiling specific power (in the decay-heat region). For example, if $G_v = 1$ or 3 , h_L is predicted to be 0.047 or 0.051 respectively.

In a situation in which a boiling pool of fuel is to be retained within a steel structure, a fuel crust of thickness δ_F has to develop between the boiling pool (~ 3300 °C) and the steel melting point ($T_s = 1400$ °C). The steel structure will then experience a total heat load, q_T , equal to the heat flux from the pool,

q_p'' , and the crust, q_c'' ; as the crust will be heat-generating. From heat conduction considerations δ_F may be related to the available heat flux from the boiling pool, q_p by:

$$\delta_F = \frac{q_p^2 + 0.5 k G_v (T_F - T_S)^{0.5} - q_p}{G_v} \quad (18)$$

where T_F and T_S are the melting points of fuel and steel respectively. The value of q_p is:

$$q_p = 0.0518 (3300-2800) = 25.9 \text{ cal/cm}^2 \text{ sec.}$$

Thus, from equation (19), δ_F is determined to be equal to 0.083 cm. Higher heat transfer rates are expected at the upper edge of the pool. This will further reduce the fuel crust thickness that will be able to conduct the heat load to the steel structure at the upper edge of the pool.

The above calculations indicate that only very small thicknesses of fuel crust can conduct the horizontal heat load from a boiling fuel pool. The integrity of such a thin crust under the turbulent conditions in the pool and the possible steel ablation remains to be investigated.

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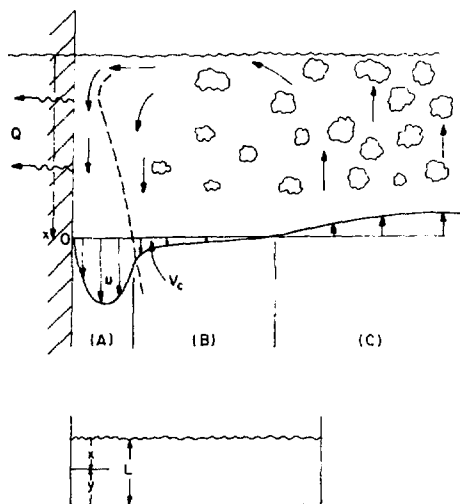


FIG. 1: Liquid Behavior Regions in the Boiling Pool

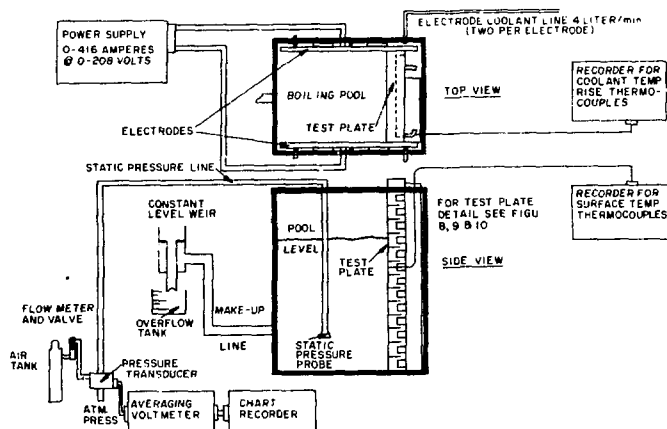


FIG. 2: Schematic Diagram of the Apparatus

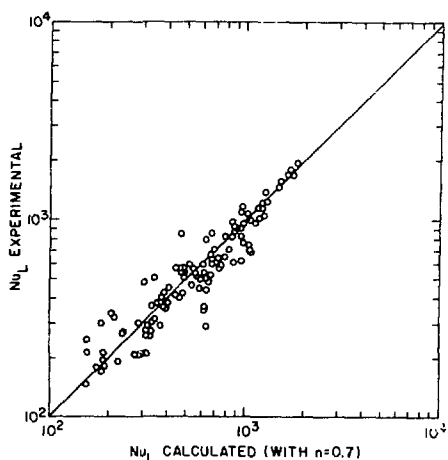


FIG. 3: Comparison of ANL Data with Predictions ($n=0.7$)

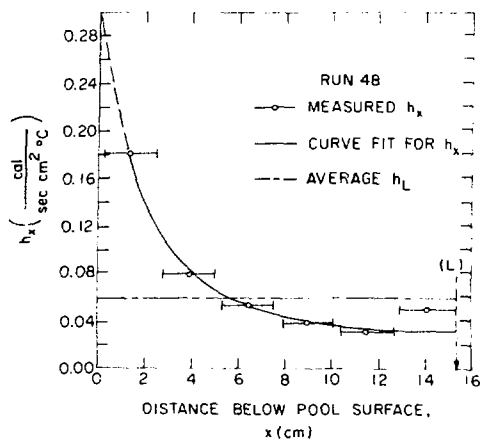


FIG. 4: Illustration of Procedure to Obtain h_L

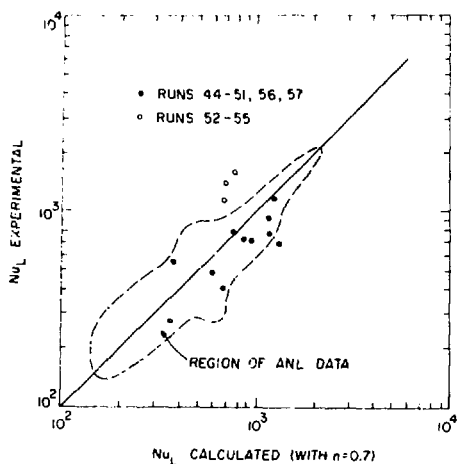


FIG. 5: Comparison of Present Data to Predictions ($n=0.7$, using measured $\bar{\alpha}$)

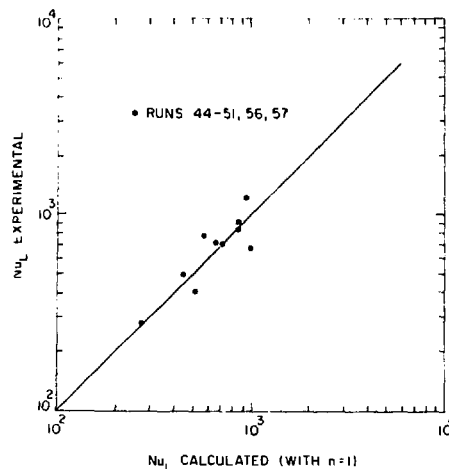


FIG. 6: Comparison of Present Data to Predictions ($n=1.0$, using measured $\bar{\alpha}$)

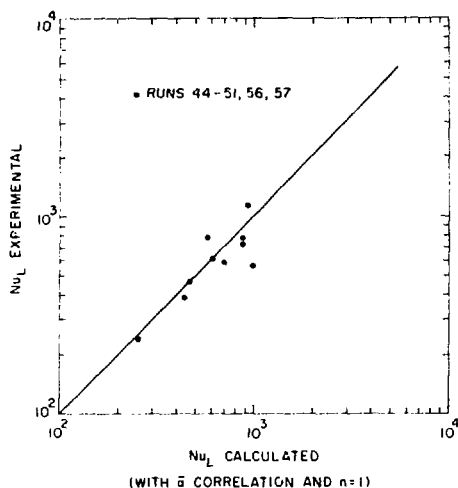


FIG. 7: Comparison of Present Data to Predictions ($n=1.0$, using calculated $\bar{\alpha}$)

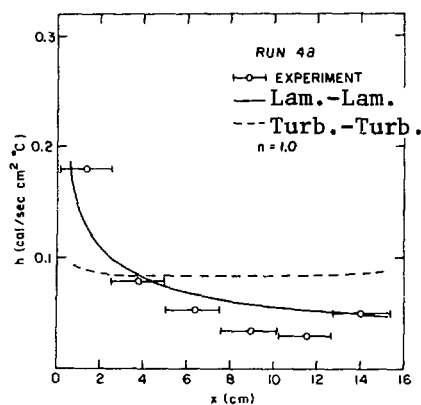


FIG. 8: Comparison of Measured Local Heat Transfer Coefficient to Prediction

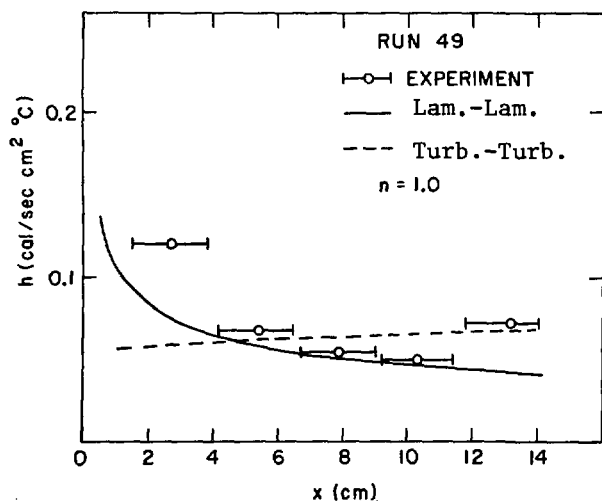


FIG. 9: Comparison of Measured Local Heat Transfer Coefficient to Predictions

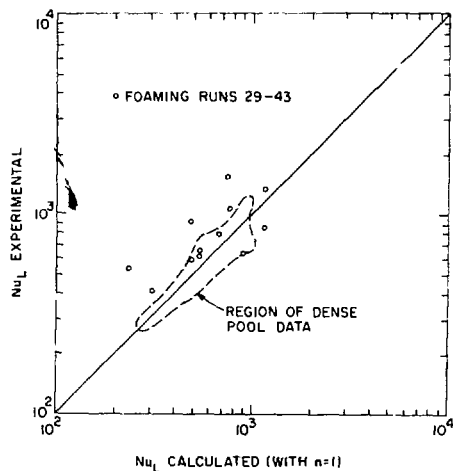


FIG. 10: Foam Regime Experimental Results Compared to Dense Regime Predictions

EXPERIMENTAL INVESTIGATIONS ON THE COMPATIBILITY OF A SNR-TYPE CORIUM WITH GRAPHITE

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ABSTRACT

Tests on the compatibility between UO_2 -pellets, UO_2 /stainless steel mixtures (70 w/o UO_2 , 30 w/o SS) and graphite were carried out at 1600 °C, 2000 °C and 2400 °C. The test duration was 0,5 h. UO_2 -pellets are only completely converted to carbides at 2400 °C, when liquid U-carbide phases occur. In the presence of SS the liquefaction temperature was lowered to 2000 °C. The conversion rates in the case of UO_2 -liquefaction as indicated by the corresponding CO-generation are high. The interactions are completed within a quarter of an hour. Apart from the release of O in the form of CO, the pick-up of C and the evaporation losses at the highest temperatures no significant changes in the chemical composition could be found. Post melting analyses by micro-sections, microprobe and X-ray investigations detected UF_6C_2 -phases and carbides from the M_7C_3 -type together with Austenite. A thermodynamic evaluation revealed, that the C-activity of the melts after UO_2 -liquefaction is nearly 1.

INTRODUCTION

Although the German 300 MW-LMFBR (SNR-300) will be equipped with a number of special safety systems - e.g. 2 independent and diverse shut-down systems and an additional redundant emergency core cooling system - the German licensing authorities require the installation of a special core retention device [1]. To fulfill these requirements a special, supplementary retention device will be installed underneath the reactor tank. Due to this design any core material penetrating the reactor tank bottom in case of an accident will reach a catch tray cooled from below by a special NaK cooling system. The catch tray must be coated by a protective layer. This protective

layer has a double buffer effect: It reduces and smoothes out the heat flux downwards by its big heat capacity and it prevents the propagation of the molten core debris. The protective coating is of the so-called sacrificial layer type. The reference concept at the moment is a liner for the catch tray consisting of depleted dense UO_2 or ThO_2 . As an attractive alternative to this undoubtedly highly expensive arrangement a graphite liner is being considered.

OBJECTIVES

The IMF/GfK⁺ [2] has conducted a series of sodium compatibility experiments with various graphite and hard-coal varieties to determine what graphite qualities are suitable. Graphite of quality LN was found to be compatible with the sodium coolant. Therefore all investigations reported were carried out with the Na-compatible graphite of the quality LN.

A typical core melt composition of the SNR reactor contains, by weight, 70 % UO_2/PuO_2 and 30 % SS 1.4981 (Table 1). The material for the protective layer of the core catcher must meet very stringent requirements as to temperature resistance and compatibility with the core debris, since the decay power may cause the melting core to attain very high temperatures. In order to characterize the compatibility of the molten core debris with the graphite liner of the SNR-300 tank external core retention device, the following questions, had to be answered:

- What are the short-time liquefaction temperatures of steel, UO_2 and a steel- UO_2 mixture in contact with the graphite?
- What amount of carbon monoxide is released in what time during the interaction between the SNR-core-melt and the liner graphite?
- What are the characteristics of the interfacial reactions of the core debris interacting with the graphite?
- What characteristic changes in the chemical composition of the SNR core melt will occur in contact with the graphite liner?
- What influence does the gaseous atmosphere have on the processes to be examined?

EXPERIMENTAL METHODS

To avoid uncontrolled influences on the result of the experiments because of the high operating temperatures, the heating element and all structural materials within the heating zone of the furnace were made of graphite. The actual test crucible was made of the quality LN graphite to be tested. It contained the SNR core melt in its typical composition. Test duration was 30 minutes. The furnace atmosphere used was either argon or nitrogen. The pressure during the melting experiments was 300 mbar, while the test temperatures were 1600, 2000 or 2400 °C. The furnace was equipped with pressure, temperature, and partial pressure monitors.

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The weight loss of the melt samples was always measured. Each sample was also investigated by metallographic examination. The results were supplemented by microprobe investigations, chemical analyses and X-ray diffraction analysis.

EXPERIMENTAL RESULTS

Liquefaction of UO_2 -pellets

The liquefaction temperature of UO_2 -pellets in contact with graphite was found to be between 2400-2500 °C. Below this temperatures only solid state reaction were detected, as described earlier by B. Craven et al. [3] and T.B. Lindemer et al. [4]. The reaction zones are small, the weight loss and the CO-generation is negligible in agreement with the results in the literature. However excessive reactions take place, when the melting point of the rate-controlling U-carbide layers between UO_2 and C is reached. UO_2 -pellets are completely reacted in less than 15 minutes. Weight loss and CO-generation corresponds to 100 % conversion of UO_2 to uranium-carbide.

Liquefaction of UO_2 -pellets within a stainless-steel melt

The liquefaction temperature of the UO_2 in the steel melt is 1950 to 2050 °C, e.g. nearly 400 °C lower than that of the pure UO_2 in contact with the UO_2 .

Fig. 1 shows a typical test result. The left part of the picture consists of survey photographs covering the entire crucible. It can be seen that at 1600 °C the UO_2 is still preserved in its original geometry, while at 2000 °C disintegration is already completed. The right side of the figure shows the microphotographs of the correspondingly marked areas. The lower right detail picture of the 2400 °C test shows the occurrence of characteristic graphite lamellae, presumably formed during the cooling of the melt through carbon segregation due to supersaturation of the melt with C.

Molten steel dissolves at 2000 °C a considerable amount of C. In our experiment sufficient C can be transported via liquid steel phase from the graphite to UO_2 . As reported by Baldock et al. [5] it exists a UFeC_2 -phase with a melting point lower than 2000 °C. The lowering of the liquefaction temperature of UO_2 in liquid SS-phases thus indicates, that the UO_2 -C-reaction is not diffusion controlled in the UC or UC_2 -layers as reported by [4-5] for the solid state reaction between both components. Due to the results of [5] it can be assumed, that the main reaction product besides the gaseous CO is the liquid UFeC_2 -phase.

CO-Generation from UO_2 -SS-Melts in Contact with Graphite

Fig. 2 shows gas release data from typical tests. At 1600 °C no CO generation was observed. This is understandable, since UO_2 does not appreciably react with the graphite at the given geometry. At 2000 °C, where UO_2 disintegration is observed within the core melt, a clearly measurable CO

peak is observed. Owing to the continuous removal of CO by the detection system, it shows a subsequent decrease, indicating that CO release is terminated some 10 to 12 minutes after the start of melting. This was also observed at 2400 °C.

Due to the mass-transport by the CO-release, the melt shows a weight decrease. Fig. 3 summarizes the results of weight loss measurements. The upper curve I shows the pure UO₂ weight losses measured, which are attributable in the final analysis to the transformation of UO₂ into UC and/or UC₂ and the release of CO. The 1.4981 steel weight losses (curve II) can be explained by simple evaporation. The dotted line in the lower part of fig. 3 represents the weight loss curve calculated from I and II. The upper unbroken curve shows the weight losses as measured. The steep increase of the weight loss to its ultimate value at 2000 °C can be explained by the fact that in a steel environment the UO₂ already completely reacts with the carbon at this temperature. The weight loss measurement thus confirms the microscopic findings.

Characteristics of the interfacial reactions

Fig. 4 summarizes the test results concerning the question of the interfacial reaction between the liquid core melt and the solid graphite. The total disintegration depths observed were between 0.6 and 1 mm. The influence of temperature on penetration depth is small once the UO₂ disintegration temperature has been reached. Extension of the holding time likewise has only a very minor influence on disintegration depth, which can be regarded as an indication that, once the melt has been saturated with carbon and all reactions initiated by the carbon have completely taken place, the melt will stabilize in the graphite. The figure 4 shows the result of a microprobe investigation at the interface between the core melt and graphite after a holding time of 30 minutes at a temperature of 2400 °C, with the furnace atmosphere being nitrogen. It is evident that the melt penetrates into the porosity of the graphite, with penetration depth not being higher than 50 μm, however. As far as penetration is concerned there is no significant preference for any of the components of the corium, as is shown by the element distribution pictures for uranium, iron, chromium and nickel underneath.

The driving forces for the interfacial reactions after the contact of the molten SS and the UO₂ with the graphite are the gradient of C-activity and capillary forces due to the pore structure of the graphite. The gradient of the C-activity decreases rapidly due to C-pick-up by the melt and the liquefaction reactions of UO₂. When chemical equilibrium is reached it is concluded that the core melt only will penetrate into the graphite only due to the capillary forces.

Changes in chemical composition

The dominant change in chemical composition taking place in the core melt is the escape of oxygen via the CO generation, i.e. the fact that after its formation in contact with the graphite the core melt will contain practically no oxygen. The results of the chemical analysis have been compiled in

Fig. 5. The uranium, iron, nickel, and chromium concentrations hardly change within 0.5 h by increasing the test temperature. With increasing test duration and constant temperature, the iron, nickel, and chromium concentrations decrease due to evaporation processes, thus increasing the relative uranium content.

Influence of gaseous atmosphere

Use of argon or nitrogen in the furnace atmosphere did not affect liquefaction temperatures, gas-generation, interfacial reaction and chemical composition.

Phase analysis

Austenite and its carbide phases as well as the needle-shaped UF_6C_2 phase were detected. The absence of UC and UC_2 in the equilibrium melt indicates that UF_6C_2 is more stable. When the core melt is in equilibrium the excess carbon present must cause the corresponding steel carbides to develop. In point of fact the carbides of the M_7C_3 type found should meet these requirements at least at 2400 °C, when the Boudouard equilibrium is shifted strongly toward the CO . From the results of phase determination described one must therefore conclude that the test results describe the behavior of an SNR core melt in equilibrium with graphite. Fig. 6 shows the appearance of the core melt under the microscope, together with the U, Fe, Cr, Ni and Nb concentrations as measured with the microprobe.

CONCLUSION

- The short-time liquefaction temperature of the SNR corium in contact with the graphite lies around 2000 °C.
- During liquefaction most of the oxygen inventory of the UO_2 is converted to CO within 15 minutes.
- The interfacial reaction between the SNR corium and graphite stops following the UO_2 liquefaction, since the core melt then formed is in equilibrium with the graphite.
- The chemical analyses confirm the results as to gas generation and interfacial reaction.
- The gaseous atmosphere N_2 or Ar has practically no influence.

ACKNOWLEDGMENTS

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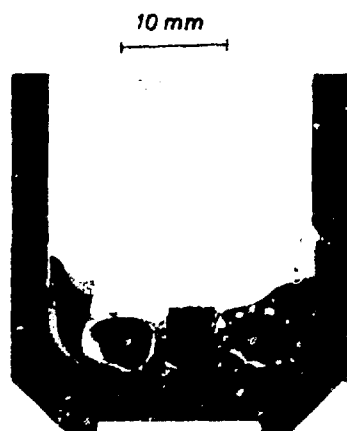
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TABLE I

Composition of SS 1.4981

Fe	67	- 61	w/o
Cr	15,5	- 17,5	w/o
Ni	15,5	- 17,5	w/o
Mo	1,6	- 2,0	w/o
C	0,04	- 0,1	w/o
Si	0,3	- 0,6	w/o
Nb	max.	1,2	w/o



1600 °C



2000 °C



2400 °C



Fig. 1 Interaction of $\text{UO}_2/1.4981$ (7O/3O w/o) with graphite

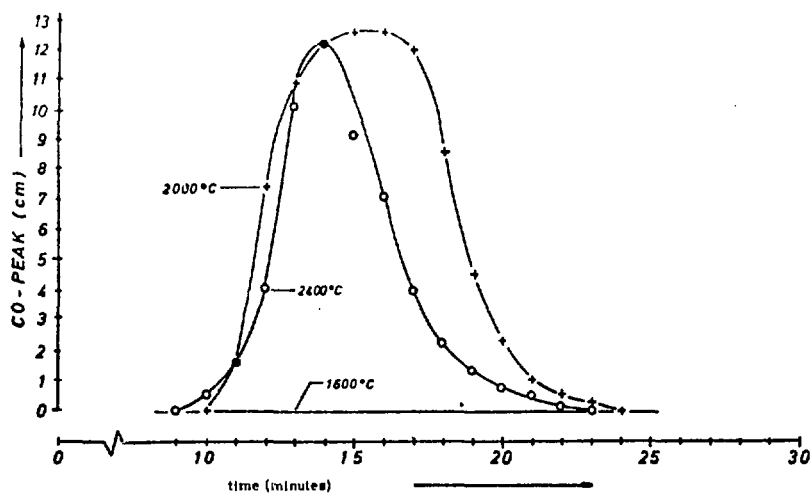


Fig. CO-generation from $\text{UO}_2/1.4981$ (7O/3O w/o) interacting with graphite

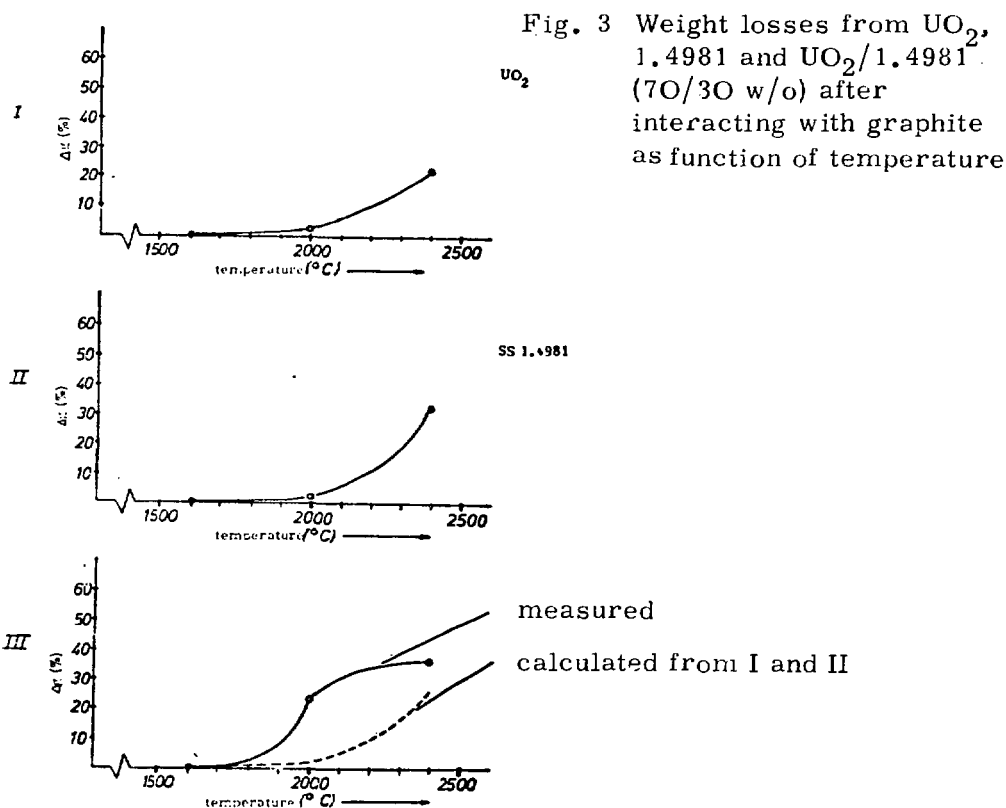
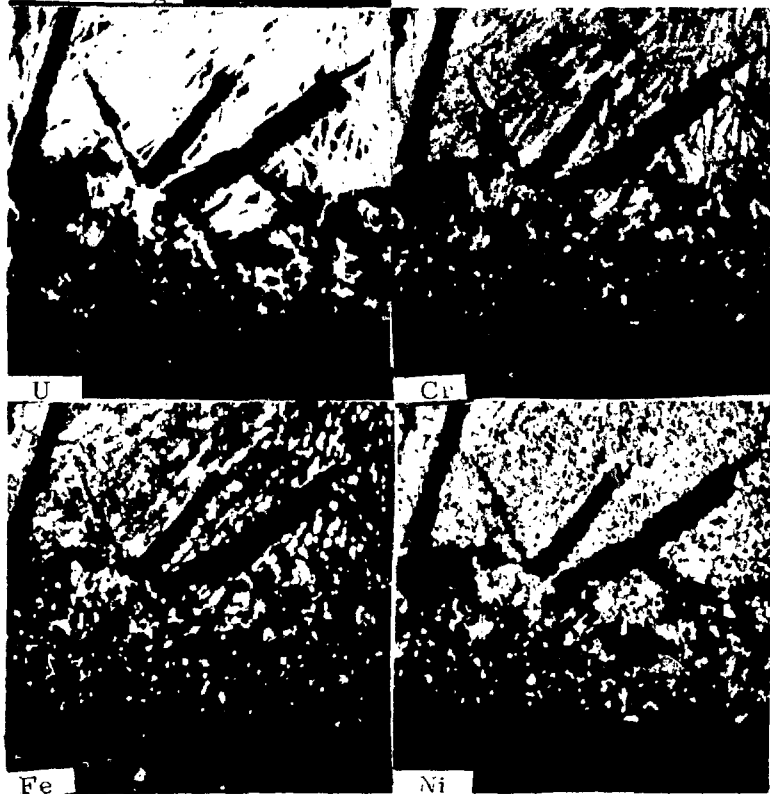




Fig. 4 Microprobe investigation of the interfacial reaction between $\text{UO}_2/1.4981$ (70/30 w/o) and graphite at 2400 °C



0,045mm

2000° C

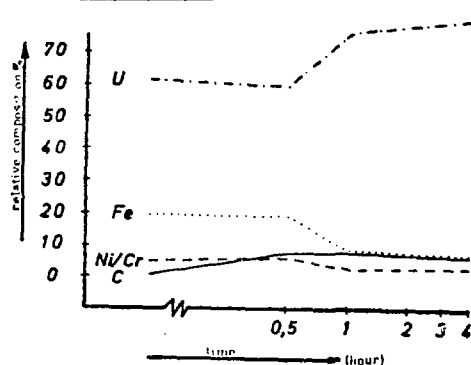
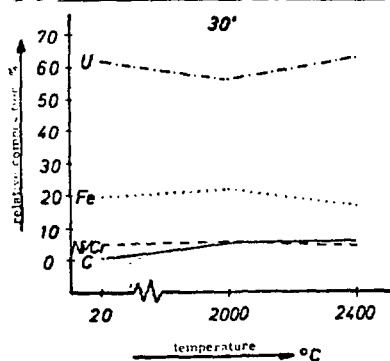
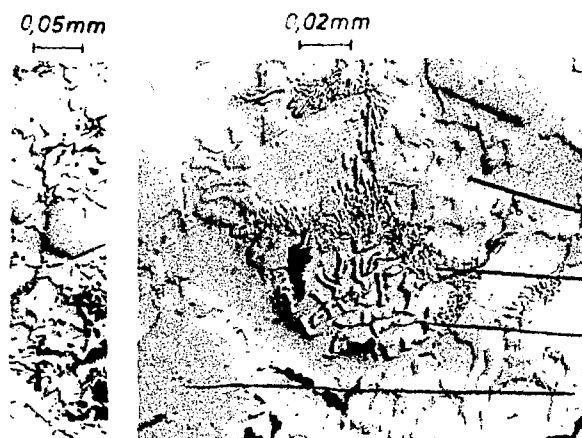
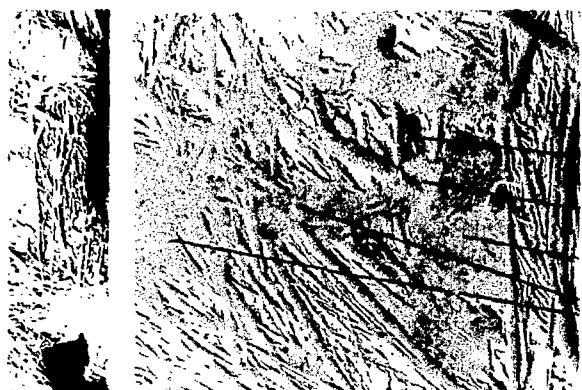


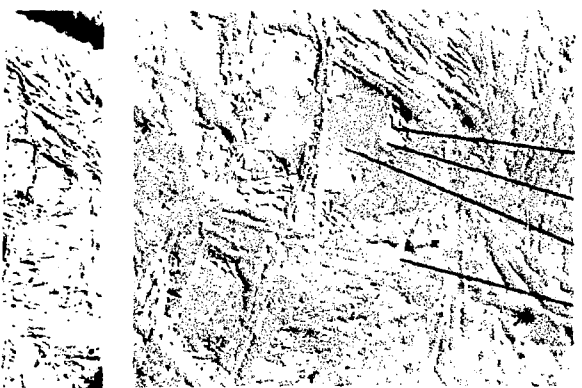
Fig. 5 Influence of temperature and time on the chemical composition of a $\text{UO}_2/1.4981$ -melt after interacting with graphite



1620 °C



2000 °C



2400 °C

	Fe	Cr	W	Mo	Nb
-	65	3	29	-	-
+	+	-	+	+	+
8-17	2-7	4-6	1-3	5-12	37-57
-	44	39	3	-	-
-	63	3	26	-	-
-	43	36	2	-	-
61	21	11	3	-	-
73	18	3	5	-	-
-	61	3	28	-	-
-	43	38	2	-	-
61	22	12	2	-	-
73	18	3	5	-	-

w/o

Fig. 6 Microsection and results of microprobe analysis of typical phases of $\text{UO}_2/1.4981$ -melts after interaction with graphite

THERMOHYDRAULICS OF THE INTERACTIONS BETWEEN MOLTEN CORE MATERIAL AND REACTOR CONCRETE

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ABSTRACT

Investigating the consequences of a core meltdown, a computer code was developed to calculate convective interactions in a system of two fluids with different physical properties such as molten core material and molten concrete after the failure of the reactor pressure vessel.

It is assumed that these two fluids will not be mixed immediately so that the molten concrete will first rise in the core material due to its lower density. The calculations consider different properties and the surface tension between the materials. First calculated results are given for varying the densities, the surface tension and the viscosities. They show, that the lighter material will rise up at fixed points, which are stationary in time. The wavelength is determined mainly by the surface tension. The transport of the lighter material effects a modulation in heat flux density over the surface of the still solid concrete, which will cause locally a different penetration of the melting front into the concrete.

INTRODUCTION

Previous investigations /1/ have shown that the reactor pressure vessel will fail within a short time in a core meltdown accident. Therefore the molten material of the core and the pressure vessel structure will come into contact with the concrete of the reactor containment. Under consideration of the high temperatures of the molten core, it is obvious, that the concrete will be destroyed. The aim of the theoretical investigations presented here, is to answer the questions, how the concrete will fail, i.e. whether local caverns could be formed during the melting process, accelerating the penetration effect. Experimental investigations /2, 3/ show, that the concrete melts at temperatures of about 1300°C,

if it mainly consists of silicates as given in German power stations. In a horizontal layer of molten core material the heat flux at the lower boundary would be constant. The strong buoyancy effects due to the great density differences between the two liquid phases cause a violent convection at the bottom. The consequences of this convection are great differences in the local heat flux density, which can lead the built up of caverns. The computer code BETON was developed calculating in a first version the thermohydraulics between the two phases with the assumption that there is no instant mixing. The aim of these calculations is to show, in which way the molten concrete will behave under the molten core material and how it is transported to the upper surface by buoyancy. Because the physical properties of the materials are not known exactly, parameter studies were performed to demonstrate the possibilities of the behaviour. For code verification some experiments were done with low melting paraffins and water.

MATHEMATICS

The calculations for such a system of different phases have to start from the differential equations ensuing from the conservation laws for mass, momentum and energy. In these differential equations the physical properties are not constant. Additionally the interface forces between the two phases have to be taken into account. Especially the Bussinesque approximation is not valid, because the great density differences effects the inertia forces in an strong manner. The following assumptions were made to establish the code:

1. The system is treated only in two dimensions.
2. Metallurgic and chemical processes in the fluid are neglected.
3. The exact penetration of the melting front is not yet included in the code, but a homogeneous movement of the melting front into the solid concrete is assumed.

The conservations of energy leads to an equation describing the temperature field:

$$\rho c_p \frac{\partial T}{\partial t} + \rho c_p \nabla \cdot (\vec{v} T) = \nabla \cdot (\lambda \nabla T) + q_i \quad (1)$$

The conservation of mass leads to the equation:

$$\frac{\partial \rho}{\partial t} + \nabla \cdot (\vec{v} \rho) = 0 \quad (2)$$

In regions far from the phase border the density can be assumed constant. At this border exists a discontinuity in the density field. Therefore in the area of each phase equation (2) can be simplified to the form

$$\nabla \cdot \vec{v} = 0 \quad (3)$$

This equation leads to a stream function for each phase. The assumption of a constant density in each phase includes that the velocity field has the same value at each side of the phase border.

Therefore the stream function must be constant over the whole considered area.

From the conservation of momentum the Navier-Stokes-equations follow

$$\frac{\partial}{\partial t} \rho \cdot v_x + \rho \cdot \vec{v} \cdot \nabla v_x = \frac{\partial p}{\partial x} + 2 \frac{\partial}{\partial x} \eta \frac{\partial v_x}{\partial x} + \frac{\partial}{\partial y} (\eta (\frac{\partial v_y}{\partial x} + \frac{\partial v_x}{\partial y})) \quad (4)$$

$$\frac{\partial}{\partial t} \rho \cdot v_y + \rho \cdot \vec{v} \cdot \nabla v_y = \frac{\partial p}{\partial y} + 2 \frac{\partial}{\partial y} \eta \frac{\partial v_y}{\partial y} + \frac{\partial}{\partial x} (\eta (\frac{\partial v_y}{\partial x} + \frac{\partial v_x}{\partial y})) - g \cdot \rho \quad (5)$$

If it is assumed, that the density is constant in each phase, the treatment of the pressure can follow a proposition of Chorin /4/, who introduced a new velocity field due to the definition:

$$\vec{v}^* = \rho \vec{v} + \nabla \int_t^{t+\Delta t} p \, dt \quad (6)$$

In this way the Navier-Stokes-equation first can be integrated with respect to time and the pressure than can be diminished by taking the curl of equation (6).

$$\nabla \times (\rho \vec{v}) = \nabla \times \vec{v}^* \quad (7)$$

From this equation the velocity field $\vec{v}$ can be determined from the field $\vec{v}^*$ by the help of the stream function. The surface tension at a bended phase border creates an additional pressure in the fluid due to the Laplace equation. To calculate the interface tension, the rising plumes of concrete are assumed to be rotationaly symmetric. Therefore two curvation radii have to be taken into consideration, one following from the curvation of the phase border in the plane considered, the other following from the assumption of rotationaly symmetry. Consequently one gets two additional terms in the Navier-Stokes-equations, acting only in the region of the phase border:

$$\frac{\partial \vec{v}^*}{\partial t} = -\nabla (\sigma (\frac{1}{R_1} + \frac{1}{R_2})) \quad (8)$$

The variables are transformed in a dimensionless form and doing this, the following dimensionless groupings are derived.

modified Rayleigh-number	Ra	gL^3/a^2
Prandtl-number	Pr	ν/a
dimensionless surface tension	W	$L\sigma/a^2\rho$
Louis-number	Le	D/a

The boundary conditions are set in the following manner:

1. Upper surface = free surface	isothermic
2. Lateral walls = rigid walls	adiabatic
3. Bottom = rigid wall	isothermic

At the bottom it is assumed that the concrete is at its melting temperature. At the beginning of the calculation a linear temperature profile is assumed in the layer of the molten concrete.

NUMERICAL TREATMENTS

The partial differential equations are integrated by the help of the finite difference method. A detailed description of the numerical treatment can be found in /5/. The phase border is represented by foodholds located on the meshlines. It is assumed that the foodholds will follow the fluid motion like massless particles similar to the assumption made in the MAC-method developed by Harlow /6/.

EXPERIMENTS

As mentioned before experiments with water and low melting paraffins were performed to verify the code. In this experiments a layer of solid paraffin was molten by electrically heated water. The test chamber was a small rectangular channel to simulate the region considered in the code. The experiments were done with the following characteristic numbers:

$$\text{Ra}^* = 10^{10} \quad \Delta\varrho = 0.1 \quad \text{Pr} = 7. \quad \text{Le} = 0. \\ W = 10^7 \dots 10^8$$

To change the interface tension, some admixtures to the water were used. Doing this, variation of the tension between 10 and 60 dyn/cm could be obtained. The experiments are not yet finished, but the first results show the principal behaviour of the rising paraffin plumes.

RESULTS

The first calculations were performed to give some information about the principal behaviour of the rising lighter phase. These results can be compared with the experiments done up to now. The calculations as well as the experiments show, that the phase interface is deformed wave-like, at which the wavelength is a function of the interface tension. This is shown in figure 1. If the theoretical wave length does not fit with the width of the test chamber, the interface is deformed in an unregular manner at the rigid walls.

In figure 2 the dependency of the wave length from the interface tension is shown. These results are computed numerically. In a good agreement with the analytical investigation of LAMB /7/, the relation $\lambda^2 - W$ is valid approximately.

If the layer of light material at the bottom is growing by melting up of solid material, the layer becomes instable. The wave-like deformation of the interface leads to the formation of plumes which are rising up to the top of the fluid. Figure 3 shows a computed example of this plume formation. Additionally in this picture it can be recognized, that the rising top of the plume is connected with the material at the bottom by a thin stripe during a relative

long time. This effect is also verified by the experiments.

A serie of pictures taken from the experiments is shown in figure 4. A rather good principal agreement can be observed between experiments and computations. Both, the experiments and the calculations show, that the rising plumes are effecting each other. The convective motion in the molten core material, evented by a rising plume is first delaying the motion of the plumes, formed in the neighbourhood.

Further on the calculations have shown, that the Prandtl-number does not effect the occurrences in a strong manner. Some test calculations, performed to test the influence of the density difference between the two phases show, that the wavelength decreases nearly linear when the density difference increases.

Figure 5 shows an other computed example. The light material, risen up forms a stable layer at the top of the fluid. Besides, in this picture it can be recognized, that the plumes are formed always nearly exactly at the same locations.

A typical example for the computed temperature fields is shown in figure 6. At the top of the rectangular area a layer of the lighter molten concrete is already formed. In this layer there are no internal heat sources, but it is heated from below by the hot core material. Therefore in this layer Benard convection occurs. This can be clearly obtained from the isotherms in figure 6. At the phase border a high temperature gradient can be seen, obviously the material rises faster than the heat is conducted across the phase border.

To calculate the penetration of the melting front into the solid concrete, the temperature gradients at the bottom of the liquid layer have to be known. As it can be recognized from the isotherms in figure 6, this temperature gradient at the bottom is high between and low at the locations where the light molten concrete is rising up.

Figure 6 also shows local temperature gradients after several time steps. In the local temperature gradients there are pronounced maxima which stay at the same location for the considered time. Therefore differences in the local penetration of the melting front can be expected. To investigate whether this local differences in the penetration velocity will lead to a faster destruction of the foundation concrete is one of the main topics of the further research work.

ACKNOWLEDGMENT

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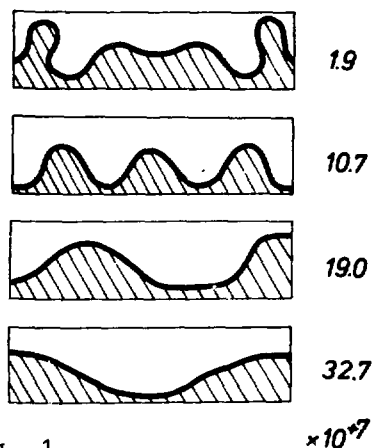


Fig. 1
Surface waves at different surface tensions

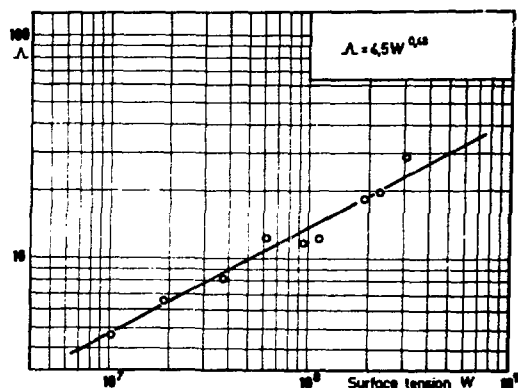


Fig. 2
Wavelength of rising locations at the phase border in relation to the surface tension

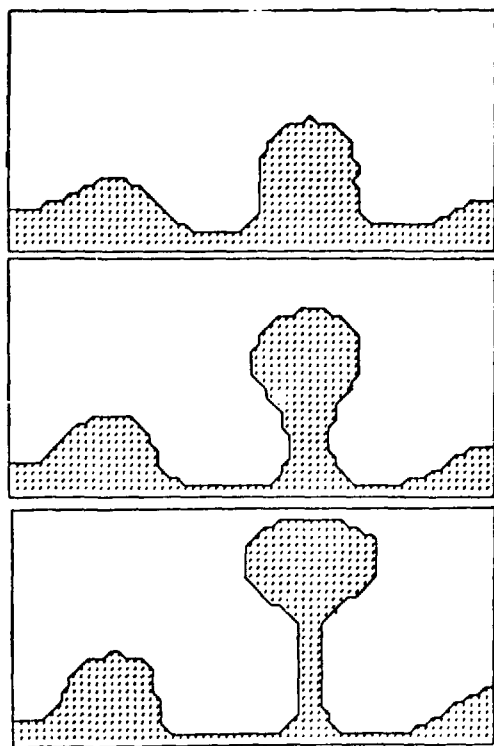


Fig. 3
Rising and loosening of material
 $Ra = 1.4 \cdot 10^{10}$ $W = 1.2 \cdot 10^8$ $\Delta \rho = 0.12$

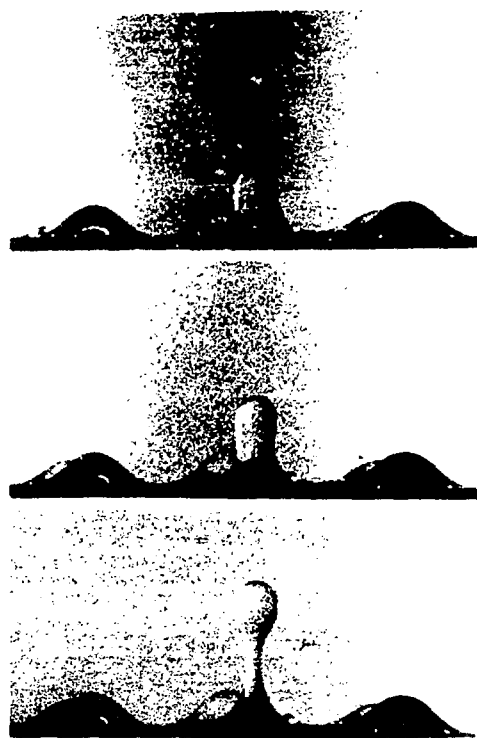


Fig. 4
Loosening of molten paraffin from the ground
 $\Delta \rho = 0.11$ $\sigma = 15 \text{ dyn/cm}$

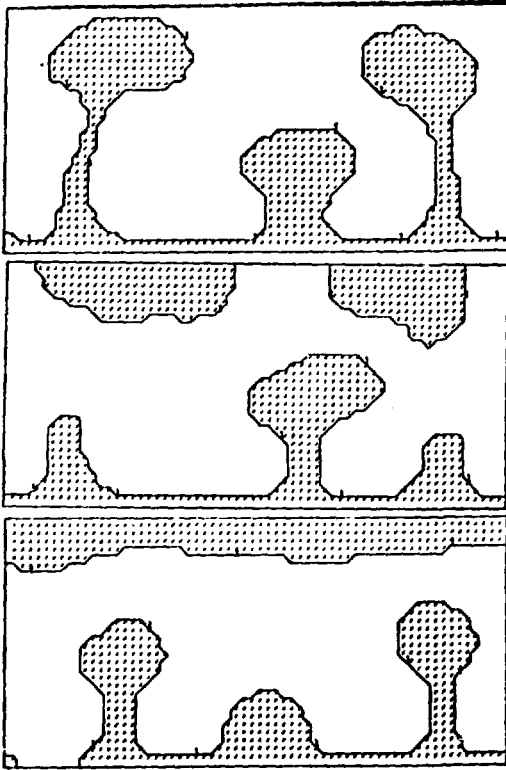


Fig. 5

Rising plumes at different
times of calculation

$$Ra = 1.4 \cdot 10^{10} \quad W = 10^8 \quad \Delta\beta = 0.12$$

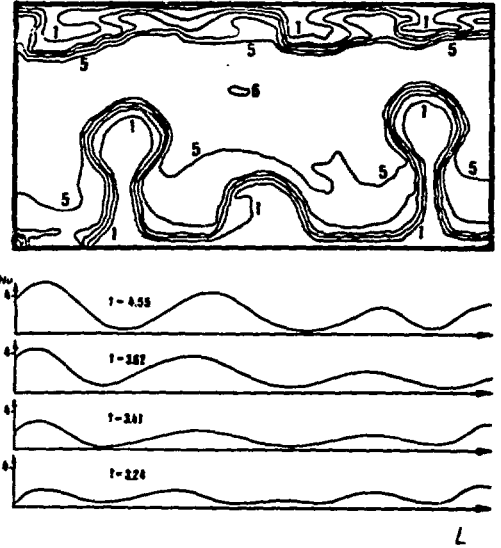


Fig. 6

Temperature field and local
Nusselt-numbers at the bottom
at different times

STUDIES ON MOLTEN FUEL-CONCRETE INTERACTIONS

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ABSTRACT

An ongoing experimental investigation of the phenomena associated with contact between molten, reactor core materials and structural concrete is described. The program uses prototypic light water reactor materials to qualitatively define aspects of the melt-concrete system which are pertinent to questions of nuclear reactor safety. Major elements of the program include exposure of calcareous and basaltic concrete to high heat fluxes on one surface and the deposition of prototypic melts into crucibles of these same concrete materials. Early results of the program are presented which demonstrate the rate and mechanism of concrete erosion under thermal attack and qualitatively describe gas evolution and concrete behavior when in contact with high-temperature melts.

INTRODUCTION

Nuclear reactors are designed and operated to minimize the probability of accidents involving core meltdown. Nevertheless, to completely assess risks associated with the use of light water reactor (LWR) power plants, it is necessary to analyze realistically phenomena which would be associated with such hypothetical meltdown accidents. Interactions between molten core materials and the concrete support structure of LWR reactors have been identified as especially pertinent to such an analysis.¹ Core melt-concrete interactions could affect the potential failure modes of the reactor containment as well as the composition and temperature of the containment atmosphere.

Under the sponsorship of the Reactor Safety Research Division of the U.S. Nuclear Regulatory Commission, Sandia Laboratories has undertaken an experimental, scoping study of core melt-concrete interactions.²⁻⁵ The study is intended to qualitatively define the major phenomena associated with melt-concrete interactions which are pertinent to questions of reactor safety, and attempt to identify physical and chemical processes influential in the development of these phenomena. Prototypic materials have been used to the extent possible throughout the program.

Two important elements of the experimental program are:

1. Investigation of the response of concrete bodies exposed to heat fluxes of magnitudes typical of those expected in a hypothetical meltdown accident.
2. Exposure of concrete to molten core materials.

This paper presents abbreviated descriptions of these two studies and early

results obtained therefrom. Although these studies are related, they address different aspects of the molten core-concrete interaction problem and hence are treated separately. Additional details of both investigations can be found in the program quarterly reports³⁻⁵ and in forthcoming topical reports.

RESPONSE OF CONCRETE TO HIGH HEAT FLUXES

The objective of this phase of the study is to investigate the response of concrete exposed to a high heat flux on one surface in the absence of chemical interactions with molten core materials. Primary emphasis is on determining the dominant surface erosion mechanism--violent spallation versus quiescent melting--including the importance of thermal shock; the surface erosion rates; and the effects on erosion of aggregate material and size, surface heat flux, material removal mechanism, and conduction of heat into the concrete through exposed reinforcing rods. Such information is important for defining the response of concrete to severe thermal attack, for uncoupling the purely thermal phenomena from the overall results of the molten core-concrete interaction experiments, and for developing improved phenomenological models suitable for inclusion in a coupled model for predicting molten core-concrete interactions.

Estimates in the literature of the heat flux from molten core materials to the concrete floor of the containment building during a reactor meltdown range over two orders of magnitude, between approximately 3 and 300 W/cm².⁶⁻⁹ The present study is concerned only with the higher order of magnitude range, from 30 to 300 W/cm². A search of the literature revealed a dearth of information on the response of concrete to heat fluxes of this magnitude. In the absence of definitive experimental data, the only available information on concrete erosion or penetration rates is based on theoretical models and estimates. Values were found which also span two orders of magnitude: from roughly 8 to 44 cm/hr for decay-heat controlled erosion to from 4.6 to 9.1 m/hr for spallation.⁹

Experimental Program

In order to cover the desired range of heat fluxes and to assess the effects of different material removal mechanisms, experiments were conducted in two Sandia facilities: the 2-Megawatt Plasmajet and the Radiant Heat Facility. Parameters varied during the experiments included: aggregate material (limestone and basalt), aggregate size (0.95 and 2.54 cm maximum), and surface heat flux (~ 28, 57, 122 and 280 W/cm²) and pressure (~ 0.18, 0.63 and 0.83 atm).

The concrete test samples consisted of a flat-faced cylinder, approximately 14½ cm in diameter by 8 cm deep, encased in a 15.24 cm O.D. by 3.4 mm thick steel liner. Chromel-alumel thermocouples were imbedded in the concrete parallel to the sample centerline (one on the centerline and from 4 to 6 equally spaced around a 7.62 cm diameter circle) at depths from the exposed surface ranging from 0.635 to 5.08 cm. The influence of reinforcing rods was studied by imbedding in some of the samples a single 1.27 cm diameter rebar along the sample centerline with one end flush with the heated surface.

Measurements made during the tests included: surface erosion characteristics, as recorded by regular and high speed motion pictures; in-depth temperatures, using the imbedded thermocouples; surface temperature, but means of an optical pyrometer; initial surface pressure and cold wall heat flux distributions, using a specially designed calibration model; and surface erosion histories, by an acoustic imaging technique.¹⁰ The radiant heat tests were restricted to the first two types of measurements, while all the above measurements were made in the plasmajet tests. In addition, time integrated spectroscopic measurements were made during the plasmajet experiments in an attempt to identify gaseous concrete constituents evolved during the tests.¹¹

The radiant heat facility provided the heating environment for the low end of the heat flux spectrum, from 28 to 122 W/cm². The two vertically mounted

heater arrays used to cover this range consisted of three graphite elements for the 122 W/cm² condition and a large number of cylindrical quartz lamps for the 28 and 57 W/cm² tests. The resulting heat sources had surface areas of approximately 29 x 30 cm and 46 x 41 cm (height x width), respectively, and provided relatively uniform distributions of incident radiation over the front face of the samples. This is illustrated in Figure 1 which shows the heat flux distribution estimated from calibration data for the 122 W/cm² condition (the surface pressure was constant at the local ambient value).

The well insulated test samples were positioned with their exposed surface parallel to, and from 10 to 16 cm away from, the vertical heater array. A step change in heat flux at the beginning of each test was achieved using an insulated steel plate shutter, positioned between the heaters and sample, which was raised pneumatically to expose the sample face in about 0.20 to 0.25 sec.

A total of 12 tests were performed in this facility, the bulk of which were conducted at the higher heat flux condition (122 W/cm², 0.83 atm) for a nominal test time of approximately 10 min. One test was conducted at each of the two lower heat fluxes, 57 and 28 W/cm², for test times of approximately $\frac{1}{2}$ and 1 hour, respectively.

The 2-Megawatt Plasmajet Facility is equipped with a Linde N-1000, high pressure arc heater. When used with a 2.54 cm exit diameter conical nozzle (1.59 cm throat diameter), it was possible to obtain maximum surface heat fluxes (at the jet stagnation point) ranging from approximately 120 to 300 W/cm² at a nominal surface pressure of 0.18 atm. The convective heat transfer and pressure distributions produced by the underexpanded free jet impinging on the sample face at a location 7.62 cm downstream of the nozzle exit are illustrated in Figure 1 for the two nominal heat flux conditions, 122 and 280 W/cm². Because of the nonuniformities in these distributions, the nominal values represent average quantities over only the central portion of the sample face; an area roughly 4 to 5 cm in diameter.

The test samples and the heat flux/pressure calibration model were mounted on a rotating support and injection mechanism which automatically positioned them coaxially with the centerline of the jet at the desired downstream location. Rotation of the sample from just outside the flow to the jet centerline at the initiation of a test occurred in about 0.1 sec., providing the desired step change in heat flux.

A total of 19 tests were performed in the plasmajet about equally split between the 122 and 280 W/cm², 0.18 atm, nominal test conditions. The nominal test times were $3\frac{1}{2}$ and $1\frac{3}{4}$ min. at the lower and higher heat fluxes, respectively. To minimize the effect of atom recombination at the concrete surface (catalyticity unknown) on the heat transfer to the samples, all but one of the tests were performed using nitrogen rather than air as the test gas. To explore the effects of this phenomena and of higher surface pressure, the final two tests were run at approximately 0.63 atm pressure using nitrogen and at 0.18 atm using air, both at the highest heat flux.

Observations and Results

Visual observations of the tests in both facilities and analyses of the motion pictures provide the following observations concerning the erosion process. A sudden exposure of basaltic or calcareous aggregate concrete to a high heat flux (~ 122 W/cm²) is followed by an initial period of from approximately 5 to 10 seconds during which the concrete heats up but no erosion takes place. This is followed by a transition period of perhaps 30 to 60 seconds during which the cementitious material in the matrix begins to melt, a definite erosion pattern develops (depending on the melt removal mechanism), and the erosion rate increases continuously. At the end of this period, a steady erosion sequence is established, with a concomitant steady erosion rate, that remains essentially unchanged for the remainder of the heating period.

These observations are borne out by the erosion history data from the

acoustic measurements made during the plasmajet tests, typical results of which are shown in Figure 2. These data demonstrate further that the periods of zero and transitional erosion are shortened considerably, and the condition of steady state erosion reached in roughly 30 seconds, when the heat flux is increased to approximately 280 W/cm^2 . The steady state erosion rate also experiences a proportional increase. Since the acoustic transducers were focused on the centerline of the sample, the results reflect the erosion history of this region of the exposed surface, i.e., in the stagnation region of the jet.

In both facilities, and for both types of concrete, the dominant erosion mechanism appeared to be melting of the cementitious material in the matrix accompanied by dehydration and decomposition of the concrete beneath the melt and periodic removal of the larger aggregate. In the radiant heat environment, the steady state erosion process was characterized by a layer of melt over the entire exposed surface that had the appearance of a vigorously boiling liquid of shallow depth, a condition that is believed to result from the bubbling through the melt of water vapor and gaseous decomposition products released from the underlying concrete. The melt flowed down the surface and off the sample under the action of gravity. The only exceptions to this pattern were the few isolated instances of spallation or exfoliation observed during some of the radiant heat tests. In these instances, small platelets (generally less than 3 cm diameter by 3 mm thick) of concrete either fell or were ejected from the surface. Only a very few of these were observed to leave the heated surface with any appreciable normal velocity. In several of the calcareous concrete tests some larger pieces of aggregate were observed to slide down the face of the sample in the melt layer.

The melt on the basaltic concrete appeared much thicker than that on the calcareous concrete and the number of bubbling sites and the bubbling frequency were both much lower for the basaltic concrete while the bubbles were much larger. These differences suggest 1) that the gas evolution rate was lower for the basaltic than for the calcareous concrete, or 2) that the melt of the basaltic concrete was more viscous than the calcareous melt.

In the plasmajet, on the other hand, the steady state erosion pattern consisted of the melt being driven out from around the aggregate by the action of the jet. The melt then flowed radially outward to solidify in a ridge around the periphery of the sample. The aggregate exposed by this process appeared to become much hotter than the molten material (although this could also be an emittance effect) and, in the case of the limestone, remained intact and in place until the surrounding matrix was almost completely removed, at which point aggregate pieces were blown off the face by the jet. This was also true of the basaltic aggregate, except that they appeared to decrease in size with time as if they were melting.

The basaltic aggregate samples appeared to have a larger quantity of melt flowing out over their faces during the tests than did the limestone samples. There was also a greater accumulation of solidified melt around the inside of the test chamber following the basaltic concrete tests than after the limestone runs. Finally, although particles of solid and molten concrete were observed shooting off the face of the samples almost continually during these tests, no spallation of the type seen in the radiant heat experiments was detected in any of the plasmajet tests.

Micrometer measurements were made of all samples to determine post test surface profiles and overall erosion. Representative results from each test facility are presented in Figures 3 and 4. Not surprisingly, the eroded profiles reflect the heat flux levels and distributions characteristic of the two facilities. Samples tested in the radiant heat facility eroded quite uniformly over the exposed surface, while samples tested in the plasmajet experienced nonuniform erosion yielding surface profiles similar to the inverse of the heat flux distributions (Figure 1).

Unfortunately, these measurements were complicated by hydration of the

decomposed concrete in the surface layer of the limestone samples over a period of several weeks following the tests. This caused the eroded surfaces, which were hard immediately following the tests, to turn to a soft powder to a depth varying from about 0.3 to 2 cm below the original post test profiles. In addition, the frozen melt layer covering the surfaces of the basalt samples tested in the radiant heat facility loosened and in most cases fell away during the post test period. As a result, surface profile measurements were made, where possible, both before and after these occurrences.

For comparison purposes, average overall erosion depths were determined by passing a straight line, perpendicular to the sample axis, through the full profile of the radiant heat samples, but only through the central region (the top of the hump), from 2.5 to 5 cm in diameter, of the plasmajet sample profiles. The reason for the latter is that the continually changing shape of the exposed surface in the plasmajet tests undoubtedly produced changes in the heat flux distribution. The stagnation region in the center of the surface, however, should have experienced the most nearly constant heat flux throughout each test.

The results of the post test surface measurements are summarized in Figure 5. Overall erosion rates were computed using the average overall erosion depths from the no- and before-hydration measurements (including estimates of the latter where necessary) and the total test times. Where comparisons were possible, these values ranged from approximately 3 to 18 percent below the corresponding steady state rates from the acoustic history data (for test times of 1-3/4 to 3 1/2 min). In view of the uncertainties involved, however, these results must be considered more qualitative than quantitative.

Examining the results in Figure 5 provides the following observations. Overall erosion rates vary by an order of magnitude over the range of test conditions, increasing with increasing heat flux, as would be expected. The dependence on heat flux is not linear, however, and is a function of the test facility, i.e., the heating environment and material removal mechanism. This is illustrated by the factor of 5 variation in the normalized erosion rates over the heat flux range and the factor of almost 3 difference in the slopes of the curves for the two facilities (Figure 5). The data also indicate that significantly higher erosion rates were obtained in the radiant heat facility than in the plasmajet, at approximately the same heat flux ($\sim 100 \text{ W/cm}^2$). This is somewhat surprising in view of the nature of the material removal mechanisms characteristic of the two facilities.

The effects of aggregate material on erosion rate appear to be reversed between the two facilities with the calcareous concretes eroding generally faster than the basaltic concretes in the radiant heat tests, but slower in the plasmajet experiments. The data suggest a similar reversal in the effect of aggregate size on erosion, primarily for the basaltic concretes, with the coarser aggregates eroding faster in the plasmajet and slower in the radiant heat facility.

The in-depth and surface temperature data obtained during the experiments have not been analyzed to date. Maximum temperatures indicated by the imbedded thermocouples just prior to failure are approximately 1600 K. Surface temperatures measured with an optical pyrometer during the plasmajet tests range from approximately 1350 to 1700 K, based on an assumed surface emittance of 1.0.

Conclusions

Based on the results of the concrete heat transfer experiments described above, it may be concluded that:

1. The dominant erosion mechanism for both calcareous and basaltic concrete appears to be melting of the cementitious material in the matrix accompanied by dehydration and decomposition of the underlying concrete.

2. Erosion is a quiescent process with negligible spallation. Violent erosion, characterized by the removal of concrete in large chunks, and controlled by thermal shock, does not appear to have occurred.
3. Erosion becomes a steady process, with an essentially constant erosion rate, 30 to 60 sec. after initiation of heating.
4. Erosion rates range from approximately 6.4 cm/hr at a heat flux of 59 W/cm² to between 40 and 70 cm/hr at heat fluxes ranging from 240 to 300 W/cm².
5. The erosion rate increases with increasing heat flux.
6. The effects of heat flux, aggregate material, and aggregate size on erosion rate are dependent upon the heating environment and the material removal mechanism.
7. The effect of exposed reinforcing rods on concrete erosion is negligible.

Attention is directed to the fact that the above observations and conclusions are applicable only to the conditions and environments peculiar to the present experiments. Extrapolation of these conclusions to other situations, outside the regimes of the experiments, should be undertaken with considerable caution.

MELT-CONCRETE EXPERIMENTS

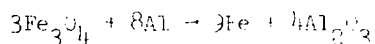
The focus of this scoping, experimental study is on tests involving the interaction between melts of prototypic materials and concrete representative of that found in existing light-water power reactors. The two experimental efforts being pursued to this end are:

- large-scale experiments involving the deposition of 200 kg mild steel melts at 1973 K into concrete crucibles, and
- small-scale experiments involving 12 kg steel or corium melts generated metallothermally in concrete crucibles.

The experiments are designed to obtain at least qualitative data on the rate of melt penetration into concrete and the nature of noncondensable gases generated during the melt-concrete interaction. The responses of concrete with calcareous aggregate and concrete with basaltic aggregate have been compared in the tests. Since steel would constitute a major fraction of the molten core debris in a hypothetical meltdown accident, the early phases of this study have concentrated on molten steel-concrete interactions.

The large-scale melts are produced in an 165 Kw induction furnace from open hearth, hot-rolled, commercial grade, mild steel (0.2-0.3 w/o carbon). Once the melt has reached a temperature of 1973 K, it is teemed into a concrete crucible and no further external heating is supplied. The concrete receptacles are 1.07 x 1.07 x 0.67 meter blocks with hemispherical cavities 0.31 meters in diameter and have internal volumes of 59.4 liters. The maximum depth produced by the melt in these crucible cavities is about 0.22 meters. The crucibles are instrumented with imbedded thermocouples to sense both the thermal response of the concrete and the rate of penetration of the melt into the concrete. Displacement gauges attached to the crucibles monitor the mechanical response of the concrete to the melt. A probe at the crucible cavity orifice allows gas samples to be collected during the tests.

Small-scale steel melts are prepared by the metallothermic reaction



$$\Delta H = -265 \text{ Kcal/mole Fe}_3\text{O}_4$$

The maximum adiabatic temperature of this reaction is ~ 3300 K. The progress of the reaction in the experimental situation is sufficiently rapid that adiabatic conditions may be nearly reached. Experiments with the above reaction in inert crucibles have shown that little gas evolution or violence is associated with the reaction itself. Fission product mocks such as MoO_3 , La_2O_3 , ZrO_2 , UO_2 , SrO and CeO_2 are sometimes included in the reaction charge to the extent of ~ 2 w/o. Typical charges weigh 12 kg.

The metallothermic reactions are run within concrete crucibles having cavities 45.4 cm long by 12.4 cm diameter. Again, the crucibles are instrumented with imbedded thermocouples to monitor the rate of melt penetration and the thermal response of the concrete. The crucible is also fitted with a steel instrumentation tower which is bonded to the concrete with a graphite compression seal. The tower permits sampling of gases generated by the melt-concrete interaction and monitoring their rate of evolution. Aerosol sampling devices within the test cell permit identification of the nature and particle size distributions of solids discharged from the reaction zone.

Conclusions, which are admittedly preliminary, that have been drawn from the early results of both the large- and small-scale tests are as follows:

1. Concrete made with basaltic aggregate and concrete made with calcareous aggregate behave in qualitatively similar ways when exposed to the high temperature melts.
2. Contact between the melt and concrete is marked by the vigorous evolution of gas. This evolution appears to be unimpeded by interfacial layers and is sufficient to levitate and disrupt the molten pool. Gas evolution renders natural convection an unimportant aspect of pool behavior. The melts--at least early in the melt-concrete interaction--may be considered as approximately isothermal, well-stirred pools.
3. Gas evolution during melt interaction with calcareous concrete is noticeably greater than during interaction with basaltic concrete.
4. Evolved gases burn brilliantly in air and are self-ignited. Chemical analysis shows the compositions of the gases to be predominately mixtures of CO , CO_2 , H_2 and H_2O . Carbon dioxide and water are formed by the thermal decomposition of concrete. Hydrogen and carbon monoxide are formed as these volatile decomposition products percolate through and are reduced by the metallic melt.

Gas flames formed in the small-scale tests are sufficiently energetic to melt elements of the instrumentation tower used in these tests.

5. Some spallation of surface layers of concrete is observed in the tests. However, the principle mechanism of erosion of the concrete is by melting. Experiments in this laboratory have shown that the onsets of melting in both concrete with calcareous aggregate and concrete with basaltic aggregate occur at nearly the same temperatures - 1379 and 1363 K, respectively. The liquidus of both mixtures is approximately 1670 K. The onset of melting is associated with cementitious material in the concrete. The melt readily dissolves refractory constituents of the concrete such as CaO .

6. The rates of melt penetration observed in the large-scale tests were 24 cm/hr for both types of concrete. Results of the small-scale, higher temperature tests indicate penetration rates as high as 100 cm/hr.
7. Attack on the concrete by the melt is far from uniform. Little aggressive activity could be assigned to the oxide phase of the small scale melts nor to the slag phase formed in the large-scale tests. Most of the penetration of the concrete was associated with metallic portions of the melt. Since the solid decomposition products of concrete are largely immiscible with steel, this penetration must be the result of the heat flux imposed on the concrete by the molten metal. Non-uniformities in the attack by the metal phase appear to be the result of forced convection effects brought about by evolved gases.
8. Density driven stratification of the melt into slag and metal phases occurs quickly and is not greatly disrupted by the gas evolution processes.
9. Interfacial layers between the melt and the concrete depend on the type of aggregate used in the concrete. With both concrete types, an incipient melt region 0.2-0.5 cm thick forms adjacent to the melt. The layer is thinnest when calcareous aggregate is present. Certainly at temperatures of 1973 K or higher, this layer is not structurally sound and is readily breached by thermally liberated gases. When calcareous aggregate is present, a powdery, solid layer, rich in CaO, is formed adjacent to the incipient melt region. Below this CaO layer the concrete is obviously dehydrated and devoid of structural integrity, but otherwise is not greatly altered. When basaltic aggregate is used, the dehydrated, heat effected zone lies adjacent to the incipient melt region. For both types of concrete the heat effected zone is one to three centimeters thick.

Since the interfacial zones are both dehydrated and decarboxylated, interaction of these materials with the steel melts may differ from the interaction of virgin concretes with the melts. To investigate this possibility, the large-scale tests have been repeated with used concrete crucibles having intact interfacial zones. Differences in the interactions are modest and appear to be in the direction of slightly increased penetration rates and depressed gas evolution rates.

10. Cracks develop in the concrete soon after the melt contacts the crucible. These cracks have appearances consistent with thermally induced tensile failure of the material. The cracks are sufficiently narrow (1-3 mm) that no penetration of melt material into the fissures has been noticed in the tests to date.
11. At least some of the water in the concrete vessels migrates along the crack pathways away from the hot zone of the crucible. Noticeable amounts of liquid water issue from fissures in the crucible walls 15 to 20 minutes after the onset of melt-concrete interaction. During this period, the external walls remain cool. The temperature of these external walls increases significantly 1-3 hours after the onset of melt-concrete interaction.

It must be emphasized that the above observations and conclusions are applicable only to the described experiments. Great caution should be used, along with a clear recognition of the limits of these tests, before any extrapolation of these conclusions to other situations is attempted.

ACKNOWLEDGMENTS

This work is supported by the United States Nuclear Regulatory Commission.

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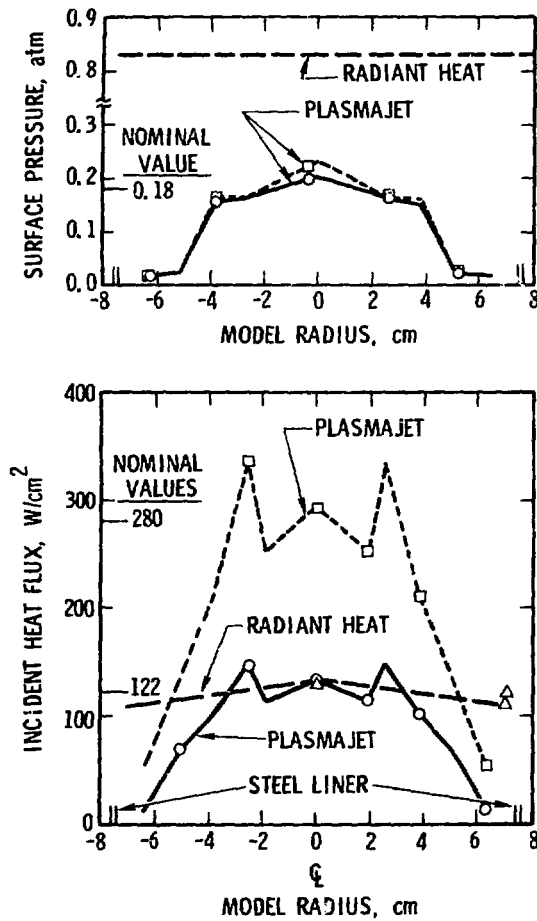


Figure 1. Surface Heat Flux and Pressure Distributions

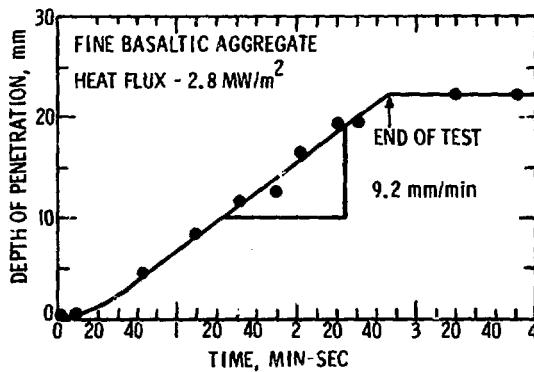


Figure 2. Acoustic Erosion History Data

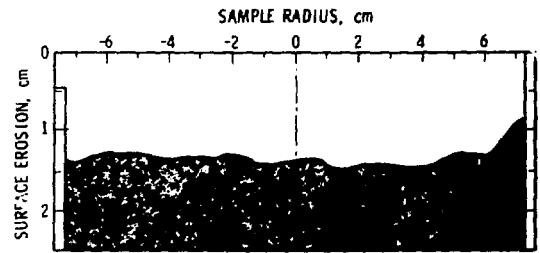


Figure 3. Radiant Heat Test, 1 cm Calcareous Aggregate, 122 W/cm², 3 min. 52 sec.

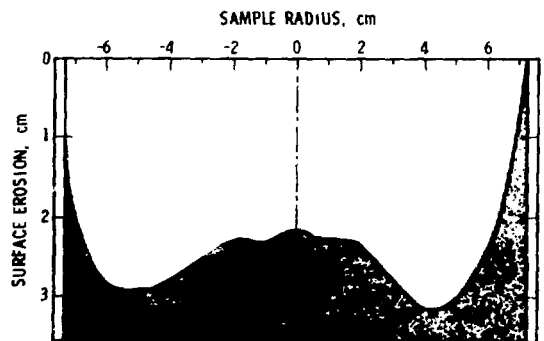


Figure 4. Plasmajet Test 1 cm Basaltic Aggregate, 280 W/cm², 2 min. 45 sec.

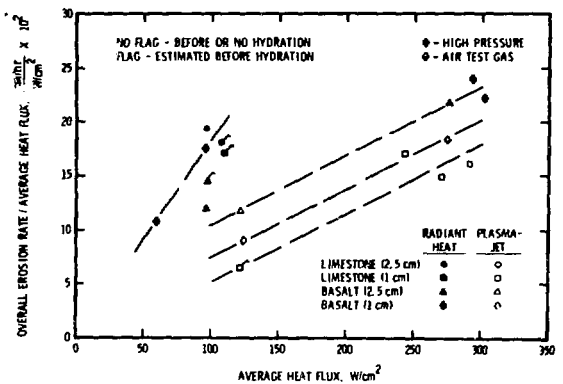


Figure 5. Normalized Overall Erosion Rates

INTERACTIONS OF LMFBR CORE DEBRIS WITH CONCRETE

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ABSTRACT

Experiments were conducted to study each of the four processes of importance in the analyses of the interactions of LMFBR core debris with concrete, including: (1) concrete dehydration, (2) interaction with molten stainless steel, (3) interaction with molten UO_2 , and (4) interaction with liquid sodium. The dehydration experiments, performed with one type of limestone concrete, indicated that as a result of dehydration and moisture migration within the concrete, the effective thermal diffusivity at higher temperatures is several times that at room temperature. Based on the observed rate of thermal diffusion in concrete, a preliminary model of heat transfer in concrete has been developed. Penetration of concrete by molten stainless steel was studied by an induction heating technique. It was found that the CO_2 and H_2O which are continuously released from the concrete, are largely converted to CO and H_2 and the steel is oxidized to a molten mixture of oxides of the Fe_3O_4 type, which is then miscible with the concrete constituents. Penetration of concrete by molten UO_2 was studied by an electrical heating method. The UO_2 was mutually soluble with the molten oxides from the concrete and a glassy melt was generated. Interaction with sodium was studied by an immersion technique. Results showed that large quantities of carbon were generated, indicating that some or all of the CO_2 was reduced to elemental carbon.

INTRODUCTION

Analyses of the behavior of core debris following an HCDA require a thorough understanding of the interactions of core debris with concrete. The nature of the attack of core debris on the concrete below the reactor cavity is important since it involves the final line of defense against violation of the containment. Evaluation of such an attack may involve interactions of molten fuel, molten steel, and molten sodium with concrete. The transient heating of concrete is a key part of each of the three interaction processes. In addition, the transient dehydration of concrete is an important consideration in the design of steel liners to prevent contact between hot sodium and concrete.

The general features of the behavior of concrete at temperatures up to the order of $1000^\circ C$ have been established by experiments concerned with the fire

resistance of structures. Detailed specification of the behavior of concrete under transient thermal conditions is complicated by large number of types and compositions of concrete. While the general features of dehydration and decarboxylation of concretes are known,¹ means to predict detailed behavior for specific compositions are not available. Moreover, there is almost no information on the behavior of concrete at temperatures where fusion and dissolution of concrete constituents into core debris melts would be expected.

Accordingly, experiments have been conducted to study each of the four processes of interest, including: the transient heating of concrete, interaction with molten stainless steel, interaction with molten UO_2 , and interaction with liquid sodium. Also, an analytical method has been developed to account for the internal processes within the concrete of heat diffusion and moisture migration. The experiments have been preliminary in nature and were designed primarily to establish the important features of the interaction and heat transport processes. Therefore, an important result of the study is the definition of specific experimental needs to evaluate particular postaccident situations.

The experiments were performed with a single concrete composition which corresponded approximately to one which was under consideration for the Clinch River Breeder Reactor Plant.* Future experiments will include concretes with different compositions.

CONCRETE

The concrete samples were prepared following the procedures of ASTM-C192-69. The composition included 858 lb/cu yd (510 kg/m^3) of Type I Portland Cement; 2094 lb/cu yd (1242 kg/m^3) of coarse aggregate, 513 lb/cu yd (305 kg/m^3) of fine aggregate, and 338 lb/cu yd (200 kg/m^3) of water. The coarse aggregate was an Illinois limestone which was predominantly dolomite, $\text{CaMg}(\text{CO}_3)_2$. The fine aggregate was Ottawa sand (SiO_2). The mixture produced a concrete with a water/cement ratio of 0.39 and an overall water content of 8.9%. The maximum size of the dolomite aggregate was about 10 mm. The mixes were tamped and some were vibrated for one minute using a Syntron vibrator operating at about 12 Hz. The samples used for all of the tests except for some of the UO_2 interactions were cured for 30-60 days while samples for UO_2 interactions were cured for at least 7 days.

The total concrete composition was 3800 lb/cu yd, corresponding to a density of 2260 kg/m^3 . The specific heat of the concrete at 25°C was estimated from the composition by the method outlined by Harmathy¹ and was calculated to be 1025 J/kg-K . Using the Harmathy model for cement paste, it was calculated that of the total water in the concrete, 55% was evaporable and 45% was combined as water of hydration. Thus it was expected that ~55% of the water could be removed by heating to slightly above 100°C . The CO_2 content of the concrete was calculated to be about 26 wt%. Thermodynamic calculations for the specific composition, using best available data, indicated that: removal of evaporable water at $\sim 100^\circ\text{C}$ would require $\sim 112 \text{ kJ/kg-concrete}$, removal of bound water over the range from 100 to 800°C would require $\sim 168 \text{ kJ/kg-concrete}$, and removal of the CO_2 at about 700°C would require $878 \text{ kJ/kg-concrete}$. The overall composition of the H_2O - and CO_2 -free mixture was calculated to be about $(\text{CaO})(\text{MgO})_{0.51}(\text{SiO}_2)_{0.58}$ and the heat of fusion was estimated to be $562 \text{ kJ/kg-concrete}$.

DEHYDRATION EXPERIMENTS

The experiments were designed to study the dehydration of concrete which was heated on one plane surface. The objective was to determine the rate of

*The project has since changed the composition to one which includes flyash and has corresponding changes in water and aggregate content.

penetration of heat into the concrete and of water out of the concrete. The results of one experiment and a preliminary model of the dehydration process have been described in detail elsewhere.² The apparatus is shown schematically in Fig. 1. The concrete billets were about 150-mm diameter and 200-mm length and were cast into pipes made of either Transite or clay tile. The lower surface of the sample faced a thick stainless steel plate which was rapidly heated by induction to the order of 900°C and maintained there. Although the plate temperature reached 900°C, the concrete surface was judged to be about 800°C because of the temperature drop across the gas gap. Five thermocouples were imbedded into the concrete at various depths from the exposed circular cross section. A gas collection system was used to condense the released water vapor and to collect samples of the released gases.

In the experiment with the transite pipe enclosure, the pipe fragmented badly near the heated surface. However, no spalling or disruption of the concrete was apparent, although cracks parallel to the heated surface were observed after cooling as shown in Fig. 2. The measured transient temperature distributions from the experiment are shown in Fig. 3. The distributions show that for a continuous heating of over 4000 s, only about 15 mm of material adjacent to the heated surface reached temperatures above 500°C. This thickness is about the same as that of the dried surface layer apparent in Fig. 2. The sample mass decreased by an amount approximately equal to the mass of evaporable water (~300 g) although it was apparent from visual observations of the course of the experiment that large quantities of water were coming off from the cool end as well as from the heated end of the sample. The water leaving the cool end was liquid while that leaving the heated end was steam. No gas samples were obtained from the experiment.

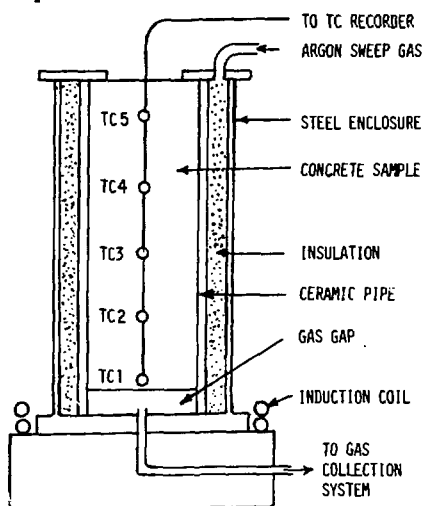


Fig. 1. Diagram of Apparatus for Concrete Dehydration Experiments

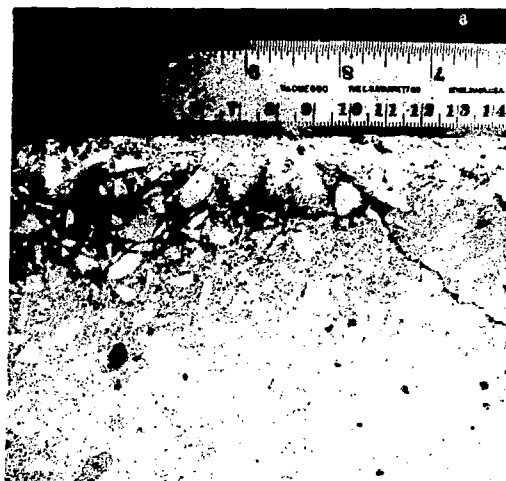


Fig. 2. Cross Section from Concrete Dehydration Experiment (Ruler along Heated Surface)

In the experiment with the clay pipe enclosure, the pipe cracked into several pieces near the heated end and some concrete was broken away around the periphery of the heated end. It was uncertain whether this occurred during the experiment or upon cooling. However, because of this uncertainty and because the heated plate did not reach 900°C rapidly enough to simulate an initially hot surface, no transient temperature data are reported. This experiment demonstrated clearly that liquid water was being rejected at the cooled end of sample. Gas samples were obtained from the heated end. Compositions were as follows:

Heating Time, s	Composition, %			
	Ar	H ₂	CO	CO ₂
1300	13.4	7.6	3.0	76.0
1600	2.2	2.6	3.6	91.6
2250	2.1	5.2	4.0	88.7
4100	2.4	5.6	1.8	90.2

During the experiment, the flow of argon sweep gas was approximately constant. The high argon content at short times indicated that the total gas flow rate was lower. Later, the flow rate was approximately constant. The results indicated that significant quantities of CO₂ were being generated and from 2 to 4% of the CO₂ was reduced to CO. Some reduction of the steam occurred as evidenced by the hydrogen content. It is not clear what reducing agent was responsible for the generation of CO and H₂. Metallic constituents of the apparatus or organic materials in the concrete are possibilities.

DEHYDRATION MODEL

Detailed development of a preliminary model of transient heat transfer in concrete was reported in Ref. 2. The model employs a moment method similar to the technique detailed in Ref. 3 to seek an approximate analytical solution to the partial differential equation for heat flow in a one-dimensional infinite body with heat sinks. Two heat sinks were considered in the original formulation: one at 100°C corresponding to the heat effect of vaporizing some or all of the evaporable water and one at 500°C to take account of the heat effects of vaporizing bound water and/or decomposing the limestone to release CO₂. The model was modified in the present study to account for the variation of thermal diffusivity with temperature. Effect of internal migration of water on heat diffusion was also included through the use of an effective thermal diffusivity. Three heat sinks were considered in the present formulation with decomposition of the limestone being considered to take place at 700°C. The governing equations are

$$\frac{\partial T}{\partial t} = \frac{\partial}{\partial x} \alpha \frac{\partial T}{\partial x} - \sum_{i=1}^3 \Delta H_i r_i / C_p, \quad T(0, x) = T(t, \infty) = T_o, \quad T(t, 0) = T_s \quad (1)$$

where T_o and T_s are the initial and surface temperatures of concrete, α is the effective thermal diffusivity, C_p is the average heat capacity, and Σ is the summation of the various heat sinks under consideration (ΔH_i and r_i are the heat and the rate of reaction of the i th species, respectively). Using an analytical technique similar to that reported previously, a similarity solution was obtained for the concrete temperature transients:

$$T = T_o + (T_s - T_o)[\text{erfc}\eta + g(\text{ierfc}\eta - 2.2568 i^2 \text{erfc}\eta)], \quad (2)$$

where

$$\begin{cases} \eta = x/2 \sqrt{\alpha t} \\ g = -2 \left[\frac{1 - \sqrt{\alpha_s/\alpha_{av}}}{0.134} + \frac{\sum a_i \Delta H_i \beta_i \sqrt{\alpha_i/\alpha_{av}}}{0.0756 C_p (T_s - T_o)} \right] \left[1 + 3.616 \sqrt{\alpha_s/\alpha_{av}} \right]^{-1} \end{cases} \quad (3)$$

In Eq. (3), a_i is the mole fraction of the i th species per unit mass of concrete and α_{av} is an average thermal diffusivity.

The propagation of the dehydration and decomposition isotherms X_i ($i = 1, 2, 3$) were found to be of the square wave type

$$X_i = 2\beta_i \sqrt{\alpha_i t}, \quad (4)$$

where β_i is the propagation constant and α_i is the thermal diffusivity of the corresponding isotherm. In the solution of Eq. (1), the effective thermal diffusivity was taken to be of the following semi-empirical form:

$$\frac{\alpha}{\alpha_0} = 1 - \frac{1 - \alpha_s/\alpha_0}{2} \left\{ 1 + \operatorname{erf} \left[4 \left(\frac{T - T_0}{T_s - T_0} \right)^n - 2 \right] \right\}, \quad (5)$$

where α_0 and α_s are the thermal diffusivities of concrete at temperatures T_0 and T_s , respectively. The empirical constants were determined experimentally as $\alpha_0 = 1.60 \text{ mm}^2/\text{s}$, $\alpha_s = 0.20 \text{ mm}^2/\text{s}$, and $n = 0.3$. The calculated temperature transient is shown in Fig. 4 along with the experimental data. The calculated values corresponding to the case of constant thermal diffusivity (Ref. 2) are also plotted in the figure for comparison.

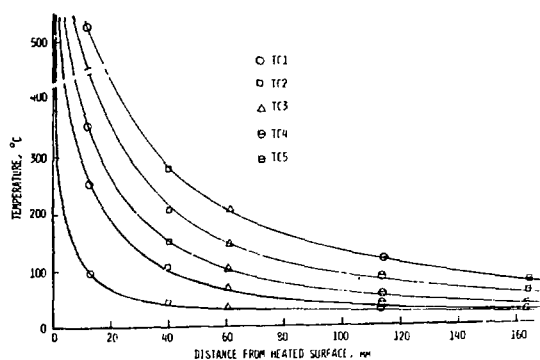


Fig. 3. Transient Temperature Distributions in Heated Concrete. Lines are at 100, 500, 1000, 2000, and 4000 s, respectively.

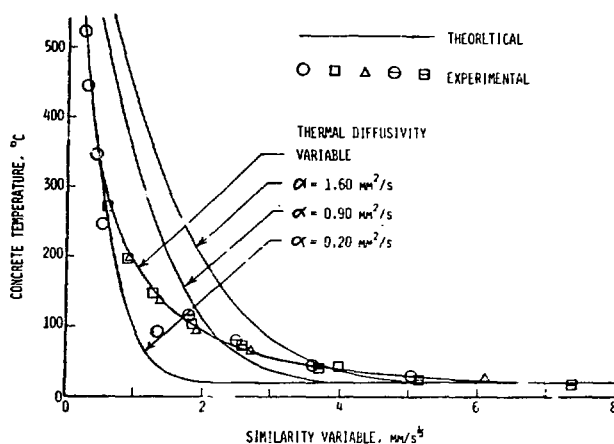


Fig. 4. Diffusion of Heat in Concrete at Elevated Temperatures.

MOLTEN STEEL EXPERIMENTS

The interaction experiments of molten steel on concrete were performed using 68- and 86-mm-dia concrete samples cast into zirconium silicate crucibles. A disc of stainless steel, 20-mm thickness, was placed on top of the concrete and heated rapidly to melting by induction. The internal heat generation rate of the molten steel was calibrated and had a typical value of 10 MW/m^3 . The crucibles were covered by a loose-fitting inverted-crucible with openings to allow escape of released gases. The initial experiments were performed with the crucible openings in contact with room air. In these experiments, the released gases maintained a continuous flame in the air caused by the combustion of CO and H_2 . In later experiments, the crucible was enclosed in a fused silica tube with water-cooled stainless steel end plates. In these experiments a sweep gas of argon flowed through the enclosure and released gases were collected and analyzed in the same way as in the dehydration experiments.

Violent spalling of the concrete was observed in some of the experiments. In one experiment, the rapid heating of the steel disc caused a spall of about 10 mm of the surface layer of the concrete. After removing the loose debris,

reheating caused another spall. However, continued heating without removal of the fragments resulted in no further disruptive events. In another experiment there was no spalling. The cross section of the crucible from that experiment was typical of all of the steel experiments and is shown in Fig. 5. Two other experiments produced delayed spalling events. In both cases, the spalls occurred about 20 min after heating began and the concrete column separated about 25 mm below the concrete-molten layer interface. The mechanical effects from the spall in one of those experiments was sufficient to break the fused silica container and terminate the experiment. In the other one, the experiment could be continued. In that case, the spall had the effect of introducing a gas gap within the concrete column which clearly affected the subsequent heat transfer. Downward penetration of heat below the gap was decreased, which decreased the rate of gas release in the latter stages of the experiment. It is felt that the occurrence of spalling depends strongly on (1) the microscopic structure of the concrete (which can be quite different for concretes with same macroscopic compositions), (2) the initial moisture distribution in the surface layer of the concrete, (3) the initial thermal condition of the overlying molten steel layer, and (4) the uniformity of the downward heat flux distribution along the steel-concrete interface. Further experiments with different types of concrete, different depths, and internal heating rates of the molten steel layer are needed to explore the mechanism of spalling of concrete upon rapid surface heating.

During the experiments, the molten stainless steel (MP $\sim 1410^{\circ}\text{C}$) was continuously oxidized to a molten oxide phase. The molten mixture was in a continuous state of agitation caused by bubbling gases. Previous studies of the stainless steel-steam reaction⁴ have shown that the reaction begins at about 1400°C and that a molten oxide of the $\gamma - \text{Fe}_3\text{O}_4$ type is produced. Fe_3O_4 is a spinel-type addition product between FeO and Fe_2O_3 . In the case of stainless steel, the nickel substitutes for FeO as NiO and the chromium substitutes for Fe_2O_3 as Cr_2O_3 . The minimum melting point of the iron-oxygen system is about 1370°C , which is consistent with the direct formation of a liquid oxide just at the onset of melting of the steel. Gross examination of the solidified melts indicated only two liquid phases: the molten steel and the molten oxide. However, microscopic examination indicated that the oxide phase is an intimate mixture of two phases. It is considered likely that only one molten oxide phase was present because there was no evidence of macroscopic layering among oxide phases.

The chemical reaction between the oxidizing gases (H_2O and CO_2) and the molten steel was quite efficient as judged by the composition of the evolving gases and the decrease in quantity of the metallic phase in the melt. The following gas composition is typical of the experiments:

Heating Time, s	Composition, %			
	Ar	H_2	CO	CO_2
1000	4.4	34.7	31.7	29.2
1500	3.2	46.2	40.4	10.2
2100	1.8	20.9	62.6	14.7

Early in the experiment, the quantities of CO and CO_2 are about equal, but later about 80% of the CO_2 - CO total is CO , suggesting a very efficient reduction of CO_2 and corresponding oxidation of the steel. After about 1 hour, the quantity of steel is so reduced that coupling to the induction coil is decreased and the experiment terminates itself.

The dried surface of the concrete adjacent to the molten layer appeared refractory, i.e., there was no indication of an intermediate layer of melted concrete. This is consistent with the expected composition of the concrete

residue which should have a melting temperature $>2000^{\circ}\text{C}$ while the pool temperature was probably about 1500°C . Accordingly, the penetration of the concrete is considered to be a dissolution process whereby the molten mixture of steel oxides dissolves the concrete oxides forming a melt with increasing quantities of CaO , MgO , and SiO_2 . It should be noted, however, that it is necessary for the melt to supply an effective heat of fusion (modified by a heat of solution) so that the process still depends on heat transfer just as it would in the case of a direct melting attack. The overall average penetration rate under the conditions of the experiments was ~ 0.02 mm/s.

MOLTEN UO_2 EXPERIMENTS

The experiments were designed to study the penetration of concrete by internally heated UO_2 . Concrete samples were fabricated in the form of bricks $100 \times 100 \times 200$ mm. A trough, 100 mm long, 40 mm deep, and 25 mm wide, was located in the center of each brick and tungsten electrodes were positioned at opposite ends of the trough. The electrodes were cast into the bricks so that they extended 25 mm below the bottom of the trough so that electrical continuity could be maintained as the UO_2 penetrated into the bricks. Some of the bricks were prepared by the specifications of the ASTM Standard, noted previously, while some of them received additional heavy tamping. Some of the bricks were prepared with chromel-alumel thermocouples embedded at intervals of 6.4 mm from the vertical surfaces and from the bottom surface of the trough.

Before an experiment, the trough was loaded with powdered UO_2 . A layer of a thermite mixture of uranium metal and molybdenum trioxide powders was placed on top of the UO_2 powder to form a bridge between electrodes. Figure 6 shows a cross-sectional view of the experimental arrangement. To begin an experiment, an alternating-current voltage was applied across the electrodes. Subsequently, the thermite mixture was ignited. The thermite reaction heated the UO_2 to temperatures where it became conductive and current flow between the electrodes was initiated. The power input was adjusted to the desired level and maintained manually. Power input was continued until loss of current indicated that penetration had proceeded beyond the electrodes.

Typical response of the embedded thermocouples during an experiment is shown in Fig. 7. Each of the three thermocouples quickly reached the $\sim 100^{\circ}\text{C}$ isotherm for removal of evaporable water. Then each in turn exhibited a rapid increase leading to thermocouple burnout. From time differences corresponding to burnout for each thermocouple, it was possible to calculate average rates of penetration in two sideward directions and in the downward direction for each experiment. Typical results are shown in Fig. 8. Significant differences such as those apparent in Fig. 8 were observed in most of the experiments between the two sideward penetration rates. This is judged to be caused by complex factors, including: (1) vigorous bubbling caused by the continuous release of gases from the concrete, and (2) the fact that the volume of the initial charge of molten UO_2 did not fill the trough because of the large difference between the bulk density of the original powder charge and that of the molten material. The molten mass tended to spread over the available surface of the growing trough instead of forming a well-defined pool with a level surface. Because of this, the total heat generation per unit area of the original trough was considered to be of significance. Accordingly, the heating rates per unit area are listed for all of the experiments in Table I. Also listed are the volumetric heating rates, the time of the experiment, the range of measured penetration rates, and the volume increase of the trough measured at the end of each experiment.

The evolution of gas during the experiment resulted in ejection of freezing droplets of a glassy material composed of solutions of UO_2 and the oxides of concrete. The solidified pools appeared to be an opaque glass. One sample was

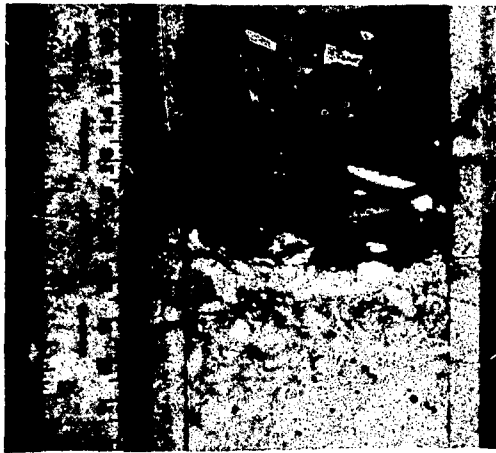


Fig. 5. Cross Section from Molten Steel/Concrete Interaction Experiment

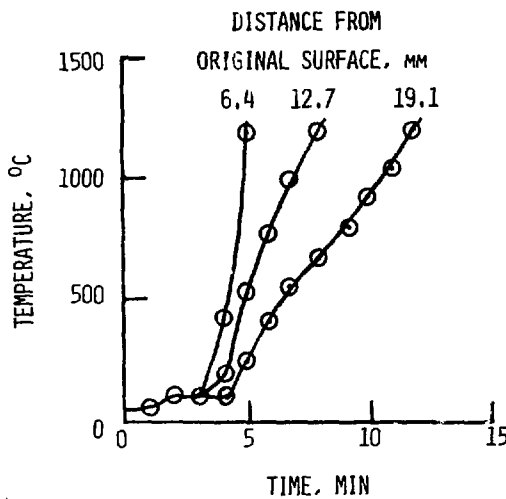


Fig. 7. Response of Thermocouples Embedded in the Concrete during a Molten UO₂ Interaction Experiment

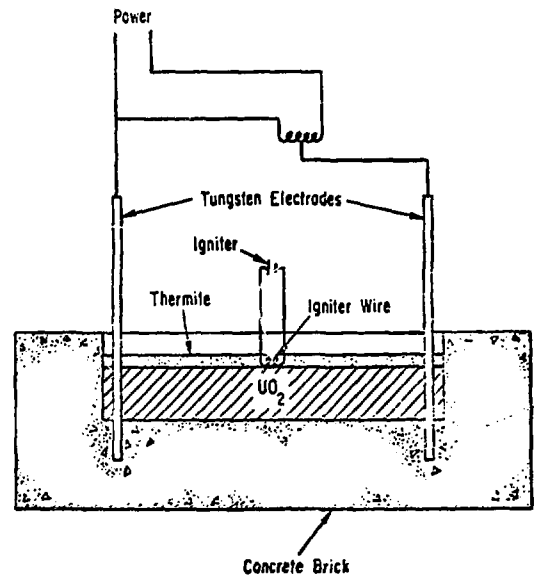


Fig. 6. Diagram of Apparatus for Molten UO₂-Concrete Interaction Experiments

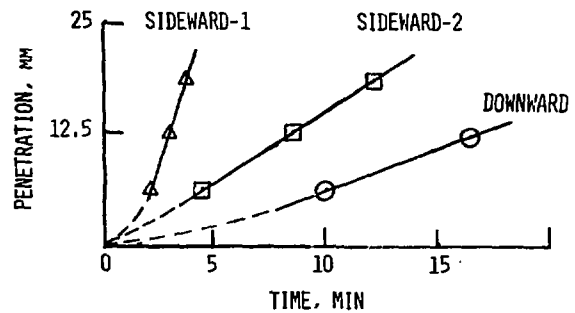


Fig. 8. Penetration of Concrete by Molten UO₂ as Indicated by the Successive Burnout of Embedded Thermocouples

Table I

EXPERIMENTAL RESULTS ON THE PENETRATION OF CONCRETE BY MOLTEN UO₂

SURFACE HEAT FLUX, kW/M ²	VOLUMETRIC HEAT GENERATION RATE, kW/KG	TIME, MIN	PENETRATION RATES, mm/s			PERCENT VOLUME INCREASE
			SIDEWARD		DOWNWARD	
SPECIFICATION CONCRETE						
32	1.6	111	-	-	-	0
96	3.2	26	-	-	-	150
134	4.4	39	0.0073	0.0069	0.0058	330
161	5.3	21	-	-	0.032	145
406	13.6	21	-	-	0.038	360
HEAVILY TAPPED CONCRETE						
75	2.9	91	0.0027	-	0.0038	150
141	5.6	54	0.0076	0.0074	0.0068	270
246	9.7	21	0.017	0.022	0.017	150
348	13.7	15	0.013	0.028	0.016	160

analyzed to be about 45% UC_2 and the remainder was CaO , SiO_2 , and MgO . As the concrete oxides dissolve in the melt, the melt density decreases. The initial molten UO_2 density was about 8400 kg/m^3 , while measured melt densities ranged from $3000\text{--}4000 \text{ kg/m}^3$.

The results in Table I indicate generally that the sideward penetration rates are greater than downward. Also, it is apparent that the heavily tamped samples had a generally lower penetration rate. The internal heat generation rates ranged from 1.6 to 13.7 kW/kg UO_2 , which is equivalent to $1.1\text{--}9.8\%$ decay heat based on the average FFTF operating power density. Although these rates of internal heat generation cover the range of interest, the quantities of fuel were quite small, so that there were relatively large heat losses from the system. Thus, at the lowest power level, there was no apparent penetration because the fuel was solidified owing to heat losses. At the higher power levels, the penetration rates were roughly consistent with the likely heat fluxes. For example, at a surface heating rate of 400 kW/m^2 , it is likely that about one-half or 200 kW/m^2 is effective in transferring heat to concrete. Assuming that 1500°C is a reasonable effective interface temperature between the melt and the concrete, the penetration rate can be calculated by dividing 200 kW/m^2 by the product of the total heat of fusion, including all of the heats of dissociation of the concrete from room temperature, $\sim 3300 \text{ kJ/kg}$ and the density, 2260 kg/m^3 , which yields 0.027 mm/s , which is of the same order as the observed values for molten UO_2 penetration into concrete as well as for molten steel penetration into concrete.

SODIUM EXPERIMENTS

The concrete samples in the form of cylinders, 27 mm in diameter and $\sim 130 \text{ mm}$ long, were loaded into stainless steel mesh baskets. The baskets were placed in an inert atmosphere above the surface of liquid sodium at 500°C . The samples were then partly immersed in the sodium, held there for either 2 min or 10 min , and quickly withdrawn. One specimen which was tamped during preparation but not vibratorily compacted was severely fractured during a 2-min exposure. A specimen which was tamped and vibratorily compacted during preparation was in better condition although broken. A third sample was preheated for 260 min at 145°C , which resulted in a weight loss of 4.3% , i.e., most of the evaporable water. This sample survived a 10-min immersion intact. A fourth sample was preheated for 170 min at 500°C and lost 7.1% of its weight, i.e., most of the water content. This sample also retained its integrity through a 10-min immersion.

Following each experiment, the sodium clinging to the sample was removed by reaction with butyl alcohol. The immersed surfaces of the samples were covered with a black deposit which was identified as carbon. X-ray diffraction analyses of material from the immersed surfaces indicated the presence of a hydrated sodium silicate ($\text{Na}_6\text{Si}_2\text{O}_7 \cdot 11\text{H}_2\text{O}$), MgO , CaO , as well as carbon. An interaction layer of about 1.1 mm was apparent on one of the samples.

The presence of significant quantities of free carbon suggested that the reaction



is occurring. Thermodynamic data have indicated that this reaction should be important at 800K . At 1100K , another reaction



should be important. Reaction 1 is significant in that possibly large quantities of the CO_2 released from the concrete might be converted to carbon and therefore not released as gas.

CONCLUDING REMARKS

The experimental results have demonstrated the key features of the interactions of core debris with one type of limestone concrete. The analysis of the dehydration experiments has shown that the concrete at room temperature exhibits a very high thermal conductivity, 3.7 W/m-K, which then decreases to the order of 0.46 W/m-K at $\sim 800^{\circ}\text{C}$. The value at room temperature might be an effective value which is augmented by the flow of liquid water away from the 100°C isotherm. This is speculative and requires additional analysis and experimentation. Thick sections of concrete might not be capable of significant backflow of liquid water and could lead to different behavior. The extremely low conductivity at 800°C is probably caused by the porosity generated by the loss of more than 25 wt% of the concrete in the form of CO_2 . Such a low value might not result from carbonate-free concretes. The heat transfer model enables tentative extrapolation to much longer times and to other concrete compositions. Confident extrapolation to different compositions and conditions requires additional experimental verification.

The molten steel experiments showed the tendency to produce spalling of the concrete. The spalling was relatively small-scale, i.e., the concrete involved in the spalling process was always within about 25 mm of the heated interface. Larger scale tests are needed to establish if this is a general situation. The experiments indicated that all of the concrete penetrated was dissolved in the growing quantity of molten steel oxides. Thus even concrete fragments produced by spalling are likely to be effective in absorbing decay heat and diluting the heat source in postaccident situations.

The molten UO_2 experiments and the molten steel experiments indicated that concrete oxides are mutually soluble with UO_2 and with the oxides derived from stainless steel. Experiments are needed which include sodium oxides. Solubilities and phase compositions of the concrete oxides in mixtures of UO_2 , steel oxides, and sodium oxides at temperatures in the $1000\text{--}2000^{\circ}\text{C}$ range are likely to be important in analyses of accident situations.

Analytical work is needed to combine the results of pool heat transfer models such as that of Ref. 5 with the interaction data generated in this study in order to predict the behavior under large-scale conditions. The sodium experiments have offered the possibility that large quantities of CO_2 in the concrete might be reduced to carbon and not contribute to the accumulation of gas in the containment, provided that the sodium would be cooled at temperatures much below its normal boiling point.

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ACOUSTIC DIAGNOSTIC TECHNIQUES FOR POST-ACCIDENT
HEAT REMOVAL EXPERIMENTS*

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ABSTRACT

Two acoustic techniques which have applications to reactor safety studies are described here. Both techniques are adaptations of the pulse-echo ultrasonic method. The first technique uses acoustic waves to measure temperature. It will be used to monitor the temperature of UO_2 from 2000 K to above the UO_2 melting point (3130 K). The second technique uses acoustic waves to measure erosion rates and its use is illustrated by monitoring the erosion of a concrete interface by a plasma jet.

INTRODUCTION

As reactor safety experiments become more sophisticated, advanced diagnostic techniques must be developed to provide the required data measurement capabilities. A particularly challenging problem is presented by the molten-fuel-pool interaction studies -- simulations of a hypothetical accident in which the entire core fails and falls onto the main vessel and eventually onto the reactor's substructure. Although the probability of this type of accident occurring is extremely small, experimental information about these interactions is required in order to assess their consequences.

The simulation experiments involve very high temperatures (3130 K or higher), intense radiation fields ($\sim 2 \times 10^{-7} \text{ n/m}^2\text{s}$), and aggressive material interactions (UO_2 , molten steel, container, substructure, and diagnostic materials at extreme temperatures). These extreme environmental conditions, combined with limited visual and physical access to the experiment (personnel safety considerations and physical layout of the experiments), preclude the use of most standard measurements for monitoring the important parameters that govern the interaction phenomena.

In this paper, we examine two applications of acoustic technology, both based on the ultrasonic pulse echo technique,¹ to fast reactor safety studies. In the first part, an ultrasonic thermometer is described, and its use is illustrated by monitoring temperatures as high as 3150 K. In the second part, an erosion-rate ultrasonic technique is described and its use is illustrated

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by monitoring the erosion of concrete by a plasma jet.

ULTRASONIC THERMOMETRY

Among the LMFB safety studies being conducted at Sandia Laboratories for the NRC are a series of in-core "molten pool" experiments in which granular UO_2 in a steel container is fission heated to melt, to simulate fission product decay heating of a debris bed following dryout. The experiments are designed to answer questions about the heat transfer characteristics of and the rate of steel erosion by internally heated granular and molten UO_2 fuel. In order to monitor the progress of the experiments and to determine the heat transfer characteristics of the fuel, it is necessary to measure the temperature distribution within the UO_2 . However, the very high temperatures to be measured (2000 K to 3130 K or higher), coupled with the hostile environment (radiation fields of $\sim 2 \times 10^{17}$ n/m²s; limited physical access; aggressive high-temperature materials interactions), preclude standard thermometry techniques.

Measurement of temperatures in UO_2 with sheathed thermocouples is generally limited to 2200 K and below, due to insulator shunting effects at higher temperatures.² Optical pyrometry is not useful for measurements inside the opaque fuel material. The only technique which has been used for temperature measurements above 2400 K in UO_2 is ultrasonic thermometry. A relatively new technique, it has previously been employed to measure UO_2 fuel pin centerline temperatures as high as 3000 K.^{3,4}

Ultrasonic thermometry depends on the temperature dependence of acoustic velocity which all materials exhibit. For example, for extensional waves, the wave velocity is given by

$$v_e = (E/\rho)^{1/2} = f(T), \quad (1)$$

where both E (Young's modulus) and ρ (the material density) are temperature dependent. Thus, if the acoustic wave velocity of a material can be measured in a temperature zone of interest, the temperature in that zone can be determined from prior calibration data.

Instrumentation

In typical ultrasonic thermometry applications, a short length acoustic pulse is produced in a magnetostrictive material by a pulsed magnetic field generated by an exciting coil (see Fig. 1). A DC magnetic field is also used to obtain the maximum magnetostrictive effect. After transmission through a wire line, the pulse reflects from impedance discontinuities in a sensor wire in the temperature zone to be measured. The acoustic pulse reflections are determined. A calibration curve of the time interval versus temperature is then used to determine the temperature of the measured zone.

Besides a very high temperature capability (tungsten alloy sensor wires could theoretically be used nearly to the melting point, 3680 K), ultrasonic thermometry offers the possibility of making multiple temperature measurements on a single sensor line⁵ by using multiple acoustic discontinuities. This feature is an important advantage for the present application. Disadvantages of the technique as previously employed were limited spatial resolution (typically 30-50 mm), signal attenuation at high temperatures, and interfering sensor reflections at high temperatures due to contact welding of the sensor to the sheath or other surrounding materials.^{3,4} These disadvantages have been addressed and largely solved for the molten pool application.

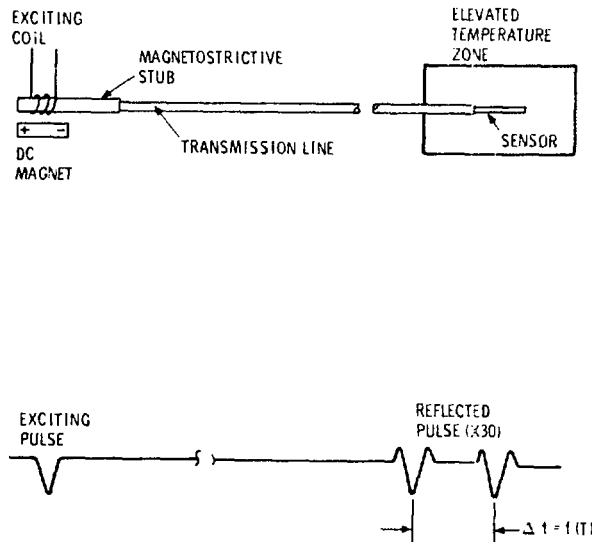


Fig. 1. Schematic representation of ultrasonic thermometry apparatus and signal.

The 2 percent thoriated tungsten material used for sensor wires in fuel pin centerline temperature measurements has proven very satisfactory for the present application. In particular, the signal amplitude remains greater than 50 percent of the room temperature value at temperatures up to 3150 K, for 2 μ s acoustic pulses. However, the 30-50 mm spatial resolution attained previously was not acceptable. For the molten pool studies, a spatial resolution of 10 mm or better was required.

To attain this resolution of 10 mm or better, advances suggested by delay line technology were employed.⁶ To generate the required very short acoustical pulse in the magnetostrictive stub, a short (2 mm) pulse coil was used, wound with 150 turns of #44 copper wire. The generated pulsed magnetic field was further confined by 1.5 mm thick ferrite discs at the ends of the coil. A 1-2 μ s current pulse was used to excite the coil, which was placed very near the end of the magnetostrictive stub. Maximum signal amplitudes were obtained using Remendur 48, a cobalt-iron-vanadium alloy, as the magnetostrictive material. The Remendur was heated with a torch to a dull red heat before use, to maximize the magnetostrictive effect. To minimize signal dispersion with the short pulses, a 0.7 mm diameter Remendur stub was used, coupled to a 0.5 mm diameter (impedance matching) tungsten transmission line. Larger diameter stubs and transmission lines caused unacceptable broadening and distortion of the reflected signals. Discontinuities in the sensor section were provided by cutting notches in the tungsten wire at desired intervals, or by reducing the diameter of alternate wire sections by chemical etching.

Sample Experiments

The sensors have been tested to 3150 K in an inert gas furnace. Fig. 2 shows the signal reflections recorded at 3150 K from a sensor with three diameter discontinuities spaced approximately 15 mm apart. The signal reflections

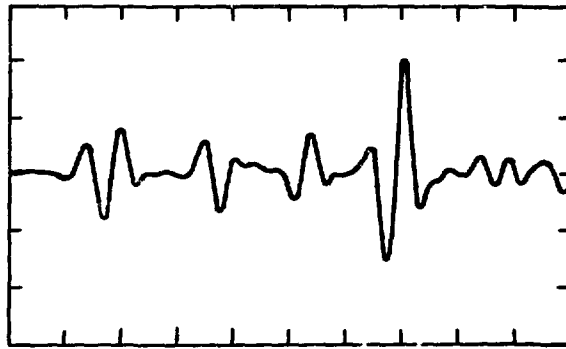


Fig. 2. Ultrasonic thermometry signal with three 15 mm long sensor elements at 3150 K. Vertical scale 50 mV/div, horizontal scale 5 μ s/div.

from successive discontinuities are clearly resolved, and could easily have been used for temperature measurements (accomplished, in this case, by optical pyrometry). The sensor described was not within a sheath as will be required for use in UO_2 medium. However, another sensor in this experiment which had a surrounding sheath operated at 3150 K without developing interfering reflections. The sensor and sheath were separated by a loose helical wrap of 70 μ m tungsten wire, which effectively prevented the contact and welding of sensor to sheath.

Future Applications

In the first in-core experiments next Spring, one ultrasonic thermometer with multiple sensor elements will be employed. In a later experiment series, it is anticipated that five ultrasonic thermometers will be used, each of which will have five 10 mm long sensor elements positioned to give a two-dimensional array of 25 temperature measurements in the fuel at 10 mm intervals. Sophisticated signal-conditioning electronics will be required to acquire the reflected signals, accurately measure the time delay between reflections, and convert the time delay data to temperatures. Much of this electronics has been fabricated. A current driver is now being used in place of a voltage pulse generator to energize the pulse coil. Because of the driver's high impedance, shorter current pulses are possible (L/R controlled decay), and higher amplitude signals are obtained from the reflected acoustic pulses. These signals are fed to a high gain amplifier and limiter, which detects the zero-crossing points of each reflected signal with less than 10 ns jitter. An automatic tracking system allows the signals to be followed when they move in time relative to the input pulse, as will happen with changes in temperature of the sensor. The signals from the zero-crossing detector will be fed to a 10 ns resolution time interval counter, and the time interval data to a time-sharing computer for storage and analysis. Theoretical temperature resolution of the system, assuming a 10 mm sensor length and 10 ns timing accuracy, is 10-15 K between 2000 K and 3000 K, based on calibration curves of ΔT versus Δt . For the molten pool measurements, 30 K resolution may be adequate due to the very large temperature gradients expected in the UO_2 fuel (~ 20 K/mm).

EROSION RATE MEASUREMENTS

The measurement of the erosion rate of the debris (pool) into the liner and/or substructure is also limited by the hostile environment and the materials

being investigated. With simulated pools (paraffin on ice), photographic techniques have proven to be successful,⁷ because the ice is translucent to visible light and pool temperatures are relatively low. However, when substructure materials, as concrete, MgO, and other liner materials are considered, this technique must be discarded because the materials are opaque to visible light. Other imaging techniques, such as x-rays and radiographics, cannot be used in large experiments because of the extremely large power requirements that are required to obtain flash pictures of the position of the pool-substructure interface. Thus, in early investigations erosion rates in opaque materials were calculated from post-test depth of penetration measurements* and time variations could not be obtained. Here, we demonstrate how an acoustic technique may be used to monitor the position of a moving interface, and thereby determine the time dependence of erosion rates during an experiment.

Instrumentation

A schematic diagram of the erosion measurement system is shown in Fig. 3. This ultrasonic pulse-echo apparatus is similar to that previously reported.¹ The output of a stable variable-frequency oscillator is shaped and then used to provide a synchronized trigger for the system and a time-sweep calibration for the oscilloscope. The trigger is obtained by dividing the shaped signal, using a binary divider, to a submultiple that produces a repetition rate sufficiently small to allow all internal reflections in the specimen to die out between pulses. The trigger fires an ultrasonic pulser which excites the transmitting transducer with a square wave of up to 350 V that can be centered about frequencies of 1 kHz to 10 MHz. The emitted acoustic pulse traverses the thickness of the sample, reflects from the interface, and is monitored by the receiving transducer after retraversing the specimen. The received signal is displayed on the oscilloscope.

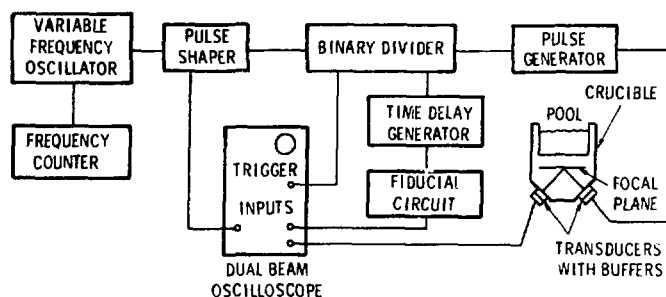


Fig. 3. Schematic diagram of the pulse echo ultrasonic apparatus.

To provide a constant time reference between the received signal and the system trigger, a time delay generator is used in coordination with a fiducial circuit (i.e., a double differentiator). The resulting time "spike" can then be added to the received signal and displayed with it or displayed on a separate trace. The time delay generator is used to position the spike before the experiment is started and the delay is not changed throughout the experiment, thus providing a constant time reference for the system.

A dual beam oscilloscope allowed both the sweep calibration, the fiducial, and the received signal trace to be displayed together and recorded with a

*Steady state conditions were assumed throughout the experiment.

camera. To obtain the exact time of each frame, a digital clock was incorporated into the system. The clock, with a LED display, has both a reset and a hold capability. Its digital display was placed in front of and at the bottom of the oscilloscope screen. With proper focusing the resulting frame includes the oscilloscope traces and the digital display from the clock.

Position Measurement

From each frame and a knowledge of the acoustic wave velocity, we are able to determine the position of the interface relative to some arbitrary, but fixed, reference point. This measurement is accomplished by measuring the time from the fiducial to the arrival of the reflected wave, via the sweep calibration trace. When this time t is multiplied by the acoustic velocity divided by two (double transit time), the interface is located with respect to a hypothetical fiducial surface.* Thus each frame will locate the interface as a function of time (via the clock display) with respect to the arbitrary reference, and differences between frames yield the absolute change in position. By taking the first frame before the test has started, each additional frame can be referenced to the first frame and the absolute position of the interface with respect to its initial position can then be determined. From the position-time plot obtained in this manner, the average erosion rate between frames can be computed by dividing the distance change by the time between frames.

Concrete-Plasma Jet Experiments

To test the system, a series of erosion rate experiments were conducted on reactor-grade concretes. In these tests, 80 mm high by 152 mm diameter specimens of concrete, moulded into a steel support ring, were subjected to a high heat flux from a plasma jet on the front surface. A pair of 0.5 MHz center-frequency transducers were mounted on the rear of each specimen and were focused 10 mm below the initial position of the front surface (see Fig. 3).

The results from one of the eight tests in this series of experiments is shown in Fig. 4. In this particular experiment, a "fine basaltic aggregate" concrete^{8,9} was subjected to a 2.8 MW/m^2 heat flux from the plasma jet. As one may note in this figure, after injection into the flame (zero time in the figure),

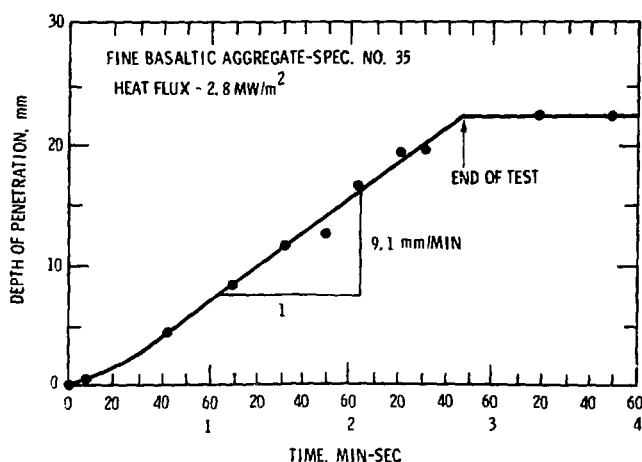


Fig. 4. Results of concrete-plasma test, specimen No. 35.

*The value of the acoustic wave velocity is assumed to be known.

the erosion of the concrete began slowly and then built to the steady state value of 9.1 mm/min. After the plasma jet was turned off, the total depth of penetration was measured ultrasonically to be 22.1 mm. This value compares with a post-test micrometer measurement of 22.0 mm. The average erosion rate (depth of penetration divided by the total time of the test) is computed to be 8.0 mm/min. These results are typical of the results of all eight tests. Namely, the depths of penetration as measured acoustically and by post-test micrometer measurements were within 1 mm of each other except for two cases in which large pieces of aggregate were protruding from the front surface after the test. One case produced an absolute error of 1.2 mm and the other 3.5 mm. In these tests, the steady state erosion rate that was measured acoustically was of the order of 30 percent higher than the average erosion rate measured with a micrometer for a heat flux of 2.8 MW/m^2 and 15 percent higher for a heat flux of 1.1 MW/m^2 .

Remarks

We have demonstrated here an acoustic technique, based on a pulse-echo ultrasonic method, which allows one to monitor an interface between two media that have different acoustic impedances. The technique has been shown to work in controlled experiments with a heterogeneous material in an adverse environment. Although only one experimental configuration has been examined, the technique is not limited to this application. With proper consideration for the subtleties of the technique (e.g., center frequency of the transducers, cross-talk between transducers, temperature dependence of the acoustic wave velocity, acoustic noise from the erosion process), the technique can be modified easily to locate interfaces in a wide variety of other applications.

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