

Clinch River Breeder Reactor Plant

Final Technical Report

Volume 1 of 2

March, 1985

Prepared For

**Clinch River Breeder Reactor Plant
Project Office
United States Department of Energy
DE-AC15-76CL50003**

~~APPLIED TECHNOLOGY~~

~~Any further distribution by any holder of this document or of the data therein to third parties representing foreign interests, foreign governments, foreign companies and foreign subsidiaries or foreign divisions of U.S. companies should be coordinated with the Director, Office of Breeder Technology Projects, Department of Energy.~~



**Rockwell
International**
Rocketdyne Division
6633 Canoga Avenue
Canoga Park, California 91304

Distribution of this report is Unlimited David Hamrin OSTI 8/6/2019

GENERAL  ELECTRIC

NUCLEAR SYSTEMS TECHNOLOGY OPERATION
P.O. BOX 3508 • SUNNYVALE, CALIFORNIA 94088



Westinghouse
Advanced Energy Systems Division
Waltz Mill, P.O. Box 158, Madison, PA 15663

MASTER

~~Released for announcement
in ATR. Distribution limited to
participants in the LMFBR
program. Others request from
BAP DOE.~~

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

INFORMATION CONCERNING USE OF THIS DOCUMENT

WARD-D--0340--Vol.1

TI85 028077

~~PRELIMINARY DOCUMENT~~

~~This document contains information of a preliminary nature prepared in the course of work for the U.S. Department of Energy. This information is subject to correction or modification upon the collection and evaluation of additional data.~~

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise, does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

WESTINGHOUSE ELECTRIC CORPORATION
ADVANCED REACTORS DIVISION
BOX 158
MADISON, PENNSYLVANIA 15663

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

WARD-D-0340

CLINCH RIVER BREEDER REACTOR PLANT

FINAL TECHNICAL REPORT

VOLUME 1 OF 2

NOTICE

~~This report contains information of a preliminary nature and was prepared primarily for internal use at the originating installation. It is subject to revision or correction and therefore does not represent a final report. It is passed to the recipient in confidence and should not be abstracted or further disclosed without the approval of the originating installation or USDOE Office of Scientific and Technical Information, Oak Ridge TN 37830.~~

March 1985

APPLIED TECHNOLOGY

~~Any Further Distribution by any Holder of the Document or of the Data Therein to Third Parties Representing Foreign Interests, Foreign Governments, Foreign Companies, and Foreign Subsidiaries or Foreign Divisions of U. S. Companies Should Be Coordinated with the Deputy Assistant Secretary for Breeder Reactor Programs, Department of Energy.~~

Report Integration: F. S. Frantz

Approved by:

Vijay K. Sazawal

V. K. Sazawal, Manager

DOE Advanced Reactor Programs

DOE Contract DE-AC15-76CL50003
Advanced Energy Systems Division
Westinghouse Electric Corporation
Madison, Pennsylvania 15663

MASTER

~~Released for announcement
in ATF. Distribution limited to
participants in the LMFBR
program. Others request from
BPP DOE.~~

JSW

INDEX

	<u>Page</u>
Abstract	iii
Table of Contents	iv
List of Tables	ix
List of Figures	xv
Abbreviations and Acronyms	xxxix

ABSTRACT

This technical report summarizes the work done on the major systems of the Nuclear Steam Supply System (NSSS) of the Clinch River Breeder Reactor Plant (CRBRP). Emphasis is placed on describing the basis for design features and approaches rather than details of design; key problems are identified and the resolutions described and the final status of the systems/components is given together with outstanding items remaining at Project termination. Extensive references to the CRBRP data base are given at the end of each section or major sub-section. These references are available/accessible through the Department of Energy Technical Information Center, unless another source is identified.

TABLE OF CONTENTS

	<u>Page</u>
1.0 CLINCH RIVER BREEDER REACTOR PLANT	1
1.1 General Report Integration	1
1.2 Beyond-the-Design-Basis Analyses for the ACRS	4
1.3 Decay Heat Removal Using Natural Circulation	31
1.4 Resolution of Metal-Joining Problems	50
1.4.1 Fixturing & Fabrication Distortions	50
1.4.2 Bi-Metallic Welds and Weld Problem Resolutions	108
2.0 SODIUM FIRE PROTECTION	122
2.1 Functions and Description	122
2.2 Evaluation of System 26A Design	135
2.3 Key Problems and Resolutions	139
2.4 Final System Status	203
2.5 References	205
3.0 REACTOR	207
3.1 Fuel Rod and Assembly Design	207
3.2 Blanket Assembly Design	258
3.3 Removable Radial Shield Design	295
3.4 Heterogeneous/Homogeneous Core Comparison	312
3.5 Assembly Arrangement in Heterogeneous Core	329
3.6 Flux Tilt Considerations	336
3.7 Control Rod Systems	356
3.7.1 Primary Control Rod System	356
3.7.2 Secondary Control Rod System	388
3.8 Core Transient Design Considerations	400
3.9 Upper Internals Structure Design	426
3.10 Core Restraint Design	499
3.11 Thermal Striping Mitigations	520
3.12 Thermal Striping Design Limits	557
3.13 In-Vessel Flow Management	610
3.14 Reactor Internals Flow Induced Vibration	629
4.0 REACTOR CLOSURE HEAD STRUCTURAL MARGIN BEYOND THE DESIGN BASIS (SMBDB) DESIGN	644
4.1 Summary of Functions and Description	644
4.2 Evolution of Design	662
4.3 Key Problems and Resolutions	686
4.4 Final System Status	708
4.5 Conclusions	711
4.6 References	713

TABLE OF CONTENTS (Continued)

	<u>Page</u>
5.0 REACTOR REFUELING	715
5.1 Overall System Scope	715
5.2 Functions and Description	717
5.3 Evolution of System 41 Design	743
5.4 Key Problems and Resolutions	751
5.5 Final Design Status	770
5.6 References	780
6.0 PRIMARY HEAT TRANSPORT SYSTEM	787
6.1 Piping Arrangement Guidelines	787
6.1.1 Introduction	787
6.1.2 System Description	788
6.1.3 Design Requirements, Guidelines, and Methods	791
6.1.4 Snubber Minimization	800
6.1.5 Seismic Margin Assessments	803
6.1.6 Inelastic Analysis Utilization	806
6.1.7 References	808
6.2 Pipe Clamp Design	810
6.2.1 Introduction	810
6.2.2 Analysis Methods and Modeling Techniques	816
6.2.3 Clamp/Pipe Stiffness and Performance Data	827
6.2.4 Test Programs	831
6.2.5 Primary/Secondary Stress Classification	837
6.2.6 Final Design Status	840
6.2.7 References	840
7.0 INTERMEDIATE HEAT TRANSPORT SYSTEM	843
7.1 Intermediate Heat Transport System and Piping Design	843
7.1.1 Summary of Functions and Design Description	843
7.1.2 Evolution of Design	847
7.1.3 Key Problems and Resolutions	850
7.1.4 Final System Status	863
7.1.5 References	865
7.1.6 Major ECPs Affecting IHTS Design	869
7.2 Leak Jacket Design	871
7.2.1 Introduction	871
7.2.2 Background	872
7.2.3 Design Basis Leak Rationale	874
7.2.4 Leak Jacket Design Requirements and Features	879

TABLE OF CONTENTS (Continued)

	<u>Page</u>
7.2.5 Leak Jacket Design Evaluation	819
7.2.6 Conclusions	895
7.2.7 References	895
7.3 Sodium Pumps	898
7.3.1 Summary of Functions and Description	898
7.3.2 Evolution of Design	898
7.3.3 Key Problems and Resolutions	903
7.3.4 Final Status of the Sodium Pump	907
7.3.5 References	912
7.3.6 List of Major ECPs	913
8.0 STEAM GENERATOR AUXILIARY HEAT REMOVAL SYSTEM	916
8.1 Summary of Functions and Design Description	916
8.2 Design Evolution of the Steam Generator Auxiliary Heat Removal System	920
8.3 Key Problems and Resolution	927
8.4 Final System Status	937
8.5 References	944
8.6 Major Engineering Change Proposals (ECPs) Affecting Design	947
9.0 STEAM GENERATOR SYSTEM	949
9.1 Summary of Functions and Design Description	949
9.2 Steam Generator System Design Evolution	955
9.3 Key Problems and Resolutions	968
9.4 Final System Status	985
9.5 References	989
9.6 Major ECPs Affecting SGS Design	993
10.0 REACTOR HEAT TRANSPORT INSTRUMENTATION AND ELECTRICAL HEATING AND CONTROL	995
10.1 Reactor Heat Transport Instrumentation	995
10.1.1 Summary of Functions and Description	995
10.1.2 Evolution of Design	997
10.1.3 Key Problems and Resolutions	998
10.1.4 Final System Status	1000
10.1.5 References	1002
10.1.6 Major ECPs Affecting System 56 Design	1004
10.2 Piping and Equipment Electrical Heating	1006
10.2.1 Summary of Functions and Description	1006
10.2.2 Evolution of Design	1008

TABLE OF CONTENTS (Continued)

	<u>Page</u>
10.2.3 Key Problems and Resolutions	1009
10.2.4 Final System Status	1010
10.2.5 References	1013
10.2.6 Major ECPs Affecting System 68 Design	1014
 11.0 AUXILIARY LIQUID METAL SYSTEM	 1015
11.1 Overall System Scope	1015
11.2 Functions and Description	1015
11.3 Evolution of System 81 Design	1030
11.4 Key Problems and Resolutions	1045
11.5 Final System Status	1073
11.6 References	1085
 12.0 INERT GAS RECEIVING AND PROCESSING	 1087
12.1 Functions and Description	1087
12.2 Evolution of System 82 design	1100
12.3 Key Problems and Resolutions	1113
12.4 Final System Status	1135
12.5 References	1144
 13.0 IMPURITY MONITORING AND PROCESSING	 1147
13.1 Functions and Description	1147
13.2 Evolution of System 85 Design	1158
13.3 Key Problems and Resolution	1167
13.4 Final System Status	1169
13.5 References	1176
 14.0 PLANT CONTROL SYSTEM DESIGN	 1179
14.1 Summary of Functions and Description	1179
14.2 Evolution of Design	1183
14.3 Key Problems and Resolution	1189
14.4 Final System Status	1196
14.5 References	1197
 15.0 PLANT PROTECTION SYSTEM	 1198
15.1 Summary of Functions and Description	1198
15.2 Evolution of Design	1208
15.3 Key Problems and Resolution	1211
15.4 Final System Status	1226
15.5 References	1227
15.6 Major ECPs Affecting PPS Design	1227

TABLE OF CONTENTS (Continued)

	<u>Page</u>
APPENDIX A DETAILED DESCRIPTION OF THE THERMAL STRIPING METHODOLOGY	1228
APPENDIX B THERMAL STRIPING DAMAGE CHARACTERIZATION	1254
APPENDIX C GENERAL HCDA PROGRESSION PATHS	1258

LIST OF TABLES

<u>Table No.</u>	<u>Title</u>	<u>Page</u>
1-1	Report Organization and Contributors	2
1-2	Maximum Core Outlet Sodium Temperature for Different Pipe Rupture Locations	10
1-3	Reactivity Balance and Estimated Core Temperatures for Hypothetical Core Support Failure Conditions	18
1-4	Operational Assumptions	33
1-5	T/TS Weld Problems and Their Resolution Through Row 5	73
1-6	Defect Correction	74
2-1	Essential Liquid Metal Fire Protection Features Provided by Other Systems	124
2-2	Cell Designation and Sodium Fire Protection Features	127
2-3	Comparison of Test Parameters and SGB Design Basis Leak Parameters	150
2-4	CRBRP Fire Test Results	154
2-5	Sodium Fire Parameter Survey	158
2-6	Large-Scale Fire Suppression Test Conditions	189
2-7	Summary of Fire Suppression Test Results	189
2-8	Oxygen Content of Cores from Frozen Sodium in the Catch Pan Area	193
2-9	Material and Equipment Recommended for Sodium Cleanup	197
3-1	CRBRP Fuel Assembly Functional Requirements	208
3-2	CRBRP Fuel Assembly Comparison with FFTF	210
3-3	CRBRP Fuel Rod Comparison with FFTF	213
3-4	CRBRP Fuel Rod Design Details	214
3-5	CRBRP Fuel Assembly Design Details	217
3-6	Requirement-Damage Severity Limits	223

LIST OF TABLES (Continued)

<u>Table No.</u>	<u>Title</u>	<u>Page</u>
3-7	CRBRP Fuel Rod Duty Cycle Normal Operation, Fuel Damage Severity Limit--No Significant Loss of Effective Lifetime	224
3-8	CRBRP Fuel Rod Duty Cycle Anticipated (Upset) Events	225
3-9	CRBRP Fuel Rod Duty Cycle Unlikely (Emergency) Events	226
3-10	CRBRP Design Basis (Enveloping) Transients	227
3-11	CRBRP Fuel and Blanket Rod Mechanical Design Limits	229
3-12	Design Procedure for Steady State Creep Analysis	232
3-13	Cumulative Damage Function (CDF)	234
3-14	Fuel Assembly Structural Loads	239
3-15	Core Assembly Structural Criteria	240
3-16	Bases for Current Mechanical Design Limits	241
3-17	Guidelines for Structural Analyses	242
3-18	CRBR Core Assembly Structural Inelastic Criteria and Limits	243
3-19	Design Evaluations - Key Fuel Results	245
3-20	Summary of Thermal Transient Data for Fuel Assembly Rods	248
3-21	Core Assembly Structural Analysis	252
3-22	CRBRP Fuel Assembly Structural Analysis Summary	253
3-23	CRBRP Blanket Assembly Functional Requirements	259
3-24	CRBRP Blanket Assembly Design Details	261
3-25	Blanket Assembly Design Description	264
3-26	CRBRP Blanket Rod Design Details	265
3-27	Design Evaluations - Key Blanket Results	269
3-28	Steady State CDF Value for the Hot Rod of Blanket Assembly 201 of the First Core	270

LIST OF TABLES (Continued)

<u>Table No.</u>	<u>Title</u>	<u>Page</u>
3-29	Summary of Thermal Transient Data for Blanket Rod Region	272
3-30	Core Assembly Structural Analysis	276
3-31	CRBRP Blanket Assembly - Structural Analysis Summary	277
3-32	CRBRP Removable Radial Shield Assembly Functional Requirements	296
3-33	CRBRP Removable Radial Shield Assembly Structural Analysis Summary	301
3-34	Reactor System Design Requirements	303
3-35	CRBRP Breeding Ratio - Homogeneous Core	315
3-36	Comparison of the Major Neutronic Characteristics of the CRBRP Homogeneous and Heterogeneous Cores	322
3-37	Comparison of Major CRBRP Nuclear Design Parameters in AFMS and AFMS-M Configurations	334
3-38	Hot Rod Parameters for Uncompensated Single Rod Withdrawal at 15% Overpower with Long Trip Line	345
3-39	Event Classification and Cladding Damage Severity Limits	401
3-40	Summary of Preliminary Design Limits	402
3-41	Assembly Umbrella Design, Duty Cycle Events and Frequency	405
3-42	Names of Umbrella Transients Described in OPDD-10	408
3-43	Specific Design Requirements for the Reactor Shutdown Systems	410
3-44	Core Map, Rev. 4, Primary Control Rod System Scram Insertion Requirements	413
3-45	Core Map, Rev. 4, Secondary Control Rod System Scram Insertion Requirements	414
3-46	CRBR Fuel Assemblies Rod Temperature Nuclear Uncertainty Factors with and Without Control Assembly Influence	418

LIST OF TABLES (Continued)

<u>Table No.</u>	<u>Title</u>	<u>Page</u>
3-47	CRBR Inner/Radial Blanket Assemblies Rod Temperature Nuclear Uncertainty Factors	419
3-48	CRBR Fuel/Blanket Assemblies Rod Temperatures Engineering Uncertainty Factors	420
3-49	Evolution of UIS Design	444
3-50	Major Changes to UIS Design	445
3-51	Minimum Positive Margins for UIS Components	471
3-52	UIS Mixing Chamber Water Table Striping Results	527
3-53	Maximum Striping from Local Interstitial Flow Test	534
3-54	Summary of Interstitial Flow Striping Tests	537
3-55	Summary of Test Results - Y-Junction Striping Tests	540
3-56	Summary of Observed Core Components Thermal Striping Data - PEOV	552
3-57	Thermal Striping-Results	604
3-58	Primary Control Assembly Combination 31/75	605
3-59	Fuel and Blanket Assemblies Orificing Zones Average Flows	621
3-60	Flow Allocation to Non-Fueled Components/Flow Paths	625
3-61	Flow Vibration of CRBRP Internals - General Survey	632
4-1	Partition of Energy for Structural Margin Beyond the Design Base	651
4-2	Phase II Simple Model Experimental Tests (1/20 scale)	667
4-3	Status of Development Program for HCDA Load Simulation	709
5-1	Component Safety and Code Classification--System 41	720
5-2	Reactor Refueling System Component Status	773
5-3	Reactor Refueling System Equipment Ready for Procurement but Delayed by National Policy Debate on Funding Restrictions	778

LIST OF TABLES (Continued)

<u>Table No.</u>	<u>Title</u>	<u>Page</u>
6-1	HTS Piping within the Reactor Containment Building	792
6-2	Design Data for HTS Piping Within The Reactor Containment Building	793
6-3	CRBRP HTS Large Diameter Clamp Drawings	818
6-4	Structural Analyses for CRBRP HTS Clamps	820
6-5	CRBRP Clamp/Pipe Stiffnesses for 24-inch and 36-inch HTS Clamps	829
6-6	Load Ratings for CRBRP In-Containment HTS Pipe Clamps	830
7-1	Plant Pump Steady State Functional Requirements	898
7-2	Pending Changes to Plant Sodium Pump Documentation	909
7-3	Major ECPs for CRBRP Sodium Pumps	914
8-1	AFW Turbine Drive Steam Supply Line Heating; An Evaluation of Alternative Heating Methods	931
11-1	Component Safety and ASME Classification - System 81	1020
11-2	Elements Which Could Cause Valve Failure	1065
11-3	Component Status	1077
11-4	Approved Engineering Change Proposals (ECPs) Not Completely Incorporated at Termination	1079
11-5	Unapproved ECP's Outstanding at Termination	1081
12-1	Safety and Code Classifications of System 82 Components	1090
12-2	Summary Scenario for Post-Accident Cleanup of a Typical Cell Containing High Levels of Radioactivity	1134
13-1	Component Safety and ASME Classification, System 85	1149
15-1	Reactor Shutdown System Protective Subsystems	1200
15-2	Protective Subsystem Summary	1201
15-3	Reactor Shutdown System Design Basis Events	1203

LIST OF TABLES (Continued)

<u>Table No.</u>	<u>Title</u>	<u>Page</u>
15-4	Specific Design Requirements for the Reactor Shutdown Systems	1207
15-5	Event Classification and Cladding Damage Severity Limits	1209
15-6	Instrumentation Common to the Control and Protection Systems	1217
15-7	Protection/Control Interaction	1218
A-1	RRS Evaluation Using Westinghouse Equation	1249
A-2	RRS Evaluation Using EG&G Equation	1250

LIST OF FIGURES

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
1-1	Failure Location for Guillotine Rupture Cases Evaluated	6
1-2	Variation in Power and Flow for Case I	11
1-3	Reactor Response for Case I	12
1-4	Reactor Vessel Na Level Above Outlet Nozzle for Case I	14
1-5	Geometry of CRBRP System Before and After a Hypothetical Core Support Cone Failure	16
1-6	Natural Circulation	21
1-7	CRBRP Loss of Heat Removal	22
1-8	Reactor Scram from Full Power to Natural Sodium Circulation	23
1-9	Bulk Na Temperature, Reactor Scram from Full Power to Natural Sodium Circulation	26
1-10	Cell Temperature as a Function of Time After Loss of Cooling (Station Blackout)	28
1-11	The Elevation Differences in Major Components Provide a Natural Circulation Capability for CRBRP	36
1-12	CRBRP Natural Circulation Verification Program	40
1-13	Cycling Controller - Temperature Response	42
1-14	Cycling Controller - IHTS Flow Response	43
1-15	Cycling Controller - PHTS Flow Response	44
1-16	Controller - Core Temperature Response	46
1-17	Controller - IHTS Flow Response	47
1-18	Controller - Steam Drum Pressure Response	48
1-19	A Cut-Away Isometric View of the Core Support Plate Assembly	52
1-20	A One-Fourth Scale Sketch of the Proposed and the Production Joint Geometries	55
1-21	Core Support Plate Sub-Assemblies	57

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
1-22	Core Support Plate Assembly	58
1-23	Isometric View of Procedure Qualification Test Assembly	59
1-24	Isometric View of Core Support Structure	62
1-25	Topside Welding Setup - Core Support Plate to Reactor Vessel Core Support Cone	63
1-26	Topside Fit-Up and Welding Layout	65
1-27	Backside Welding Setup	66
1-28	Final Machining	68
1-29	Tube/Tubesheet Weld Technique	69
1-30	Tube Installation, Welding and Inspection Process	71
1-31	Typical Riser Assembly Pressure Boundary (Axisymmetric Cross-Section)	109
1-32	Bi-Metallic Weld	111
2-1	CRBRP Inerted Cell Liner System	126
2-2	Typical Catch Pan Fire Suppression Deck Arrangement	129
2-3	Catch Pan System Arrangement, SG Loops 1 and 3	133
2-4	Catch Pan System Arrangement, SG Loop 2	134
2-5	"Q" Deck Arrangement Detail	143
2-6	Vent Pipe	144
2-7	Test Facility Configuration	151
2-8	Test Article	152
2-9	Air Benchmark Test No. 2	155
2-10	Air Benchmark Test No. 2	157
2-11	Modified Piping Insulation Design	166
2-12	LSSS Test Article	167

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
2-13	Large-Scale Sodium Fire Test, Sodium Flow Rates Versus Time	168
2-14	Sodium Fire Test, Cell Gas Temperature	170
2-15	Sodium Fire Test, Cell Gas Average Temperature	171
2-16	Sodium Fire Test, Building Concrete Wall Surface Temperature	172
2-17	Sodium Fire Test, Concrete Ceiling Surface Temperature	173
2-18	Sodium Fire Test, Cell Oxygen Concentration	174
2-19	Sodium Fire Test, Concrete Floor Surface Temperature Under Catch Pan	175
2-20	Sodium Fire Test, Sodium Pool Temperature	176
2-21	Effect of Pool Burning on Sodium Temperature	177
2-22	Structural Temperature Predictions	179
2-23	Sodium Spill Area on Q-Deck Surface	180
2-24	Fire Suppression Deck Temperatures	181
2-25	I-Beam Temperatures	182
2-26	Sodium Fire Test, I-Beam Temperatures	183
2-27	Sodium Fire Suppression Test Article	187
2-28	Schematic of Fire Suppression Test Article	188
2-29	Configuration of Sodium Spill	190
2-30	Initial Condition of Catch Pan Floor	196
2-31	Initial Condition of Catch Pan Floor Near Support Pan	196
2-32	Addition of Dry Ice to Catch Pan Residues	198
2-33	Inerting Drum and Sealing Drum Liner	199
2-34	Opened Q-Deck Section for Cleaning Catch Pan	200
2-35	Q-Deck Drain Pipe Removal	201

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
3-1	CRBRP Fuel Assembly	209
3-2	CRBRP Fuel Rod	212
3-3	Core Assembly Design Process	220
3-4	Cumulative Mechanical Damage Function Due to Steady State Operation, Anticipated Events and Unlikely Event Example	235
3-5	Fuel Assembly Loads	237
3-6	Maximum Fuel Rod Bundle - Duct Diametral Clearance and Interference Versus Time - F/A 45, First Core	249
3-7	Maximum Duct Radial Dilation Across Flats Versus Axial Location at End-of-Life for F/A 45	250
3-8	CRBRP Blanket Assembly	260
3-9	CRBRP Blanket Rod	263
3-10	Maximum CRBR Blanket Assembly Bundle - Duct Interference and Clearance Versus Time	273
3-11	Duct Dilation Versus Axial Height for IB/A 67 and RB/A 201	274
3-12	CRBRP Duty Cycle - Load Following Characteristics for Typical Day of Power Operation	286
3-13	Typical EBR-II Transient Cycles During Run 112 after Startup and Hold Time	288
3-14	Removable Radial Shield Assembly	297
3-15	Removable and Fixed Radial Shielding Configuration; Elevation View	304
3-16	Location of Removable Radial Shield Assemblies	306
3-17	RRSA Material Configuration	307
3-18	Duct Surveillance Tensile Specimen	308
3-19	Nesting of Tensile and Charpy V Notch Specimens in Tube	309

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
3-20	Location of Duct Surveillance Specimens	310
3-21	Clinch River Breeder Reactor Homogeneous Core	313
3-22	Clinch River Breeder Reactor Heterogeneous Core	319
3-23	Evolution of the CRBRP Heterogeneous Core Arrangement	332
3-24	Percent Increase in 36-Inch Integrated Power Shape Due to 15¢ Uncompensated R7C Control Rod Runout (~1.1 Inches from Half Inserted). CRBRP (4), Beginning of Cycle Four.	339
3-25	Percent Increase in 36-Inch Integrated Power Shape Due to 15¢ Uncompensated R7C Control Rod Runout (~2.1 Inches from 8 Inches Inserted). CRBRP (4), End of Cycle Four	340
3-26	Relative Power Variation for Fuel Hot Rod Cases with and without Flux Tilt Effect.	342
3-27	Effect on Fuel Hot Rod Maximum Cladding Temperature of Power Rise Rate with and without Flux Tilt Effect	343
3-28	Effect on Fuel Hot Rod Maximum Fuel Temperature of Power Rise Rate with and without Flux Tilt Effect.	344
3-29	CDF Histogram for a Single Control Assembly Withdrawal Event	347
3-30	CDF Cumulative Distribution for a Single Control Assembly Withdrawal Event	348
3-31	Percent Molten Fuel Versus Power Level to Typify Molten Fuel Increase with Increasing Power	349
3-32	Rod Block System	352
3-33	Reactor Cross Section	358
3-34	Primary Control Rod Drive Mechanism (PCRD)---Upper Assembly	360
3-35	Primary Control Rod Drive Mechanism (PCRD)---Lower Assembly	362
3-36	Primary Control Rod Driveline (PCRD)	364

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
3-37	Primary Control Assembly (PCA)	366
3-38	Shield and Seismic Support	368
3-39	Disconnect Actuating Tool for the Primary Control Rod System	370
3-40	Secondary Control Rod Schematic	390
3-41	Secondary Control Assembly	392
3-42	Comparison of System 31 SCRS Absorber Pin Designs	395
3-43	Comparison of System 31 - SCRS Control Assembly Filler Rod to Absorber Pin Spacing	396
3-44	Longest and Quickest Flow Coastdown Rates for CRBR Primary System	415
3-45	Relative Axial Locations for Hot Rod Temperatures	417
3-46	Graphical Illustrations of Semi-Statistical Method	421
3-47	Upper Internals Structure	427
3-48	Chimney Effluent Temperature Time History	436
3-49	Upper Internals Structure - Elevation	439
3-50	Upper Internals Structure - Plan View	440
3-51	Upper Internals Structure - Outlet Flow Module and Chimney	441
3-52	Elevation Schematic of the Upper Internals Structure	447
3-53	Instrumentation Post Installation	451
3-54	Instrumentation Post (Bench Subassembly)	452
3-55	Method of Positioning Control Rod Shroud Tube	455
3-56	Instrumentation and Conduit Installation	456
3-57	Upper Internals Structure	457
3-58	Photograph of Full Size Upper Internals Structure Mockup	461

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
3-59	Prototypic Fuel and Blanket Exit Temperatures and Reactor Flow Rate Following a Reactor Trip	480
3-60	Schematic of Integral Reactor Flow Model	483
3-61	Evaluation of Thermal Striping Fatigue is a 5-Step Process	521
3-62	Selected CRBRP Temperature Distributions (°F)	524
3-63	Lower Shroud Tube Water Table Model	528
3-64	Typical Temperature Trace from Lower Shroud Tube Water Table Results	530
3-65	Test Set-up on Water Table of Local Interstitial Flow Test Rig	532
3-66	Plan View of Interstitial Flow Test Rig	533
3-67	Plexiglass Model for Interstitial Flow Testing	536
3-68	Y-Junction Model Test Setup	539
3-69	ANL 1/10 Scale Outlet Plenum Thermocouple Map	542
3-70	ANL 1/10 Scale Outlet Plenum Thermocouple Map	543
3-71	ANL 1/10 Scale Outlet Plenum Thermocouple Map	544
3-72	CRBRP Integral Reactor Flow Model - Phase II	546
3-73	Instrumentation Post and Chimney Model	549
3-74	Lower Shroud Tube Model	550
3-75	Structure Internal Striping Temperature Amplitudes	553
3-76	Fuel/Blanket Assembly Outlet Nozzle Design	559
3-77	Thermal Striping Mechanism	560
3-78	Fuel & Blanket Assembly Outlet Nozzle	562
3-79	Section of F/A Outlet Nozzle (Drawing 1182E45)	563
3-80	Schematic of Na Flow Pattern from Reactor Core	565

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
3-81	Core Assembly Striping Potential	572
3-82	Striping Temperature History of Fuel and Radial Blanket Assembly Interface	573
3-83	Seven Assembly Striping Test - FA-08; TC Location FN-7N	575
3-84	Seven Assembly Striping Test - FA-08; TC Location FN-7JJ	576
3-85	7 Assembly Test Results	577
3-86	Saturation of Maximum Striping with Time	578
3-87	Probability of Measuring Larger Striping in Longer Test is Small	579
3-88	Mixing of Hot and Cold Fluid in a Channel Indicates $\geq 50\%$ Decrease of Striping Near Wall	582
3-89	Calculated Surface Striping	584
3-90	Calculated Striping Film Coefficient-H	586
3-91	Qualitative Comparison of Thermal Film Coefficients	587
3-92	Failure of SA 316SS In Air at 538 to 566°C	588
3-93	Fatigue of SA 316SS at 538 to 566°C, In-Sodium Data Versus In-Air Correlation	589
3-94	Fatigue of SA 316SS at 538 to 566°C, In-Sodium Data Versus In-Sodium Correlation with In-Air Correlation Asymptote	591
3-95	Comparison of In-Air Fatigue Limits at 10^8 Cycles	592
3-96	Summary of 316SS TSTF Thermal Striping Results	593
3-97	Construction of Elastic-Plastic Strain Fatigue Design Curve	595
3-98	Striping Fatigue Analysis Method	596
3-99	Record of Minimum and Maximum Temperature Striping	597
3-100	Rain-Flow Cycle Counting Method	599

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
3-101	Use of a Constant Amplitude S-N Curve with the Miner Rule to Predict Fatigue Life for Variable Amplitude Loading	600
3-102	Allowable Gradient (Striping) Correlations	602
3-103	CRBR In-Vessel Flow Paths: Flow Allocation and Its Basis	611
3-104	CRBRP Core Orificing Map	620
3-105	Complete Schematic of CRBRP In-Vessel Flow Paths	626
4-1	Thermal Source for Structural Margin Beyond the Design Base	648
4-2	Pressure Volume Relationship for SMBDB Loading	649
4-3	Slug Force on Under Head Shielding of Reactor Vessel Closure Head	652
4-4	Reactor Closure Head Assembly	654
4-5	Closure Head Assembly Plan View	655
4-6	Reactor Schematic	656
4-7	Reactor Closure Head Elevation	658
4-8	CRBRP Riser Layout	660
4-9	Margin Ring Sections	661
4-10	Schematic of Static Vessel Head Apparatus, Test SM1	668
4-11	1/20 - Scale Models of the CRBR	670
4-12	Schematic of Test Fixture for SM7	673
4-13	Differences in SM7 and SM8 Shield Plate Designs	674
4-14	Shield Plate Designs for SM7 and SM8	675
4-15	Profiles of Head at Six Pressures; Static Test SM1	680
4-16	Profiles of Head at Seven Pressures for SM7	684
4-17	Profiles of Head at Seven Pressures for SM8	688

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
4-18	SM-8 Model Plug Configuration of Failure (2010 psi)	689
4-19	Design Modification Logic CRBRP Closure Head SMBDB Capacity Enhancement	672
4-20	CRBR RV and Rotating Plugs Absolute Displacements Due to HCDA	673
4-21	CRBR RV and Rotating Plugs Absolute Displacements Due to HCDA	675
4-22	Relative Displacements Across Point of Disengagement for Tests SM7 and SM8	676
4-23	Projected Closure Head Deflections and Interferences at 2700 psi	677
4-24	Projected Closure Head Deflections and Interferences at 3000 psi	678
4-25	Machining Modifications Proposed to Eliminate Interferences at the Node 5-6 Interface	679
4-26	Machining Modifications Proposed to Eliminate Interferences at the Node 5-6 Interface	680
4-27	Projected Closure Head Deflections at 2700 psi with Prototype Radial Restraint at the LRP-Reactor Vessel Interface	682
4-28	Projected Closure Head Deflections at 3000 psi with Prototype Radial Restraint at the LRP-Reactor Vessel Interface	683
4-29	Acceptance Criteria	684
4-30	Joint Shear Strength	686
4-31	Flange Stud Capacity	687
4-32	Potential Gap Reduction Factors	688
4-33	Seismic Model: From Calculation LRA-SA-78-77	689
4-34	Acceleration Versus Frequency for Linear and Non-Linear Analyses	690

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
4-35	Head Seismic Response with Reduced Radial Gaps Between Plugs	691
4-36	Plug Strain Summary	693
4-37	Head Energy Input	695
4-38	Propagation of Pressure Waves Caused by Instantaneous Complete Gate Closure	696
4-39	Slug Bulk Compression Energy	697
4-40	Pressure Volume Change for Original and Modified Head Designs	699
4-41	Head System Energy Absorption Capability	701
4-42	SMBDB Analysis Model	702
4-43	Force Time History Head Up/Force CSS Down/Force	703
4-44	Vessel Flange SMBDB Response	704
4-45	Reactor Vessel Support and Ledge Dynamic Force-Deflection Relationship (75 MJ Slug Load)	705
4-46	Closure Head System Energy Absorption Summary	706
4-47	Summary Logic Diagram for CRBR Closure Head Modification SMBDB Enhancement	710
5-1	Reactor Refueling System (Perspective)	718
5-2	In-vessel Transfer Machine (IVTM)	722
5-3	Auxiliary Handling Machine (AHM)	724
5-4	AHM Floor Valve	725
5-5	Ex-vessel transfer Machine (EVTM)	727
5-6	EVTM Gantry-Trolley	729
5-7	Core Component Pot (CCP)	731
5-8	Ex-vessel Storage Tank (EVST)	733
5-9	Fuel Handling Cell (FHC)	735

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
5-10	New Fuel Unloading Station	737
5-11	New Core Assembly Inspection Equipment	739
5-12	Reactor Refueling System (RRS) Control Concept	740
5-13	Refueling Communications Center (RCC) Block Diagram	742
5-14	Reactor Refueling System Drawing Tree	771
6-1	General Arrangement of CRBRP Heat Transport System	789
6-2	Loop 1 of CRBRP Heat Transport System Piping	790
6-3	Initial HTS Flexible Clamp Design	812
6-4	Typical HTS Horizontal Pipe Clamp Assembly (Type B)	814
6-5	Typical Horizontal Restraint Assembly for CRBRP HTS Piping	815
6-6	Typical HTS Vertical Pipe Clamp Assembly	817
6-7	2-D Clamp/Pipe Interaction Model	822
6-8	3-D Clamp/Pipe Interaction Model	826
6-9	Typical Clamp and Pipe Super Elements	828
7-1	CRBRP Intermediate Heat Transport System Schematic	845
7-2	IHTS Leak Jacket Design Concept	882
7-3	IHTS Leak Jacket Details and Interfaces	883
7-4	IHTS Inner Jacket Attachment Detail	884
7-5	IHTS Z-Ring Details at Pipe Clamp	885
7-6	IHTS Insulation Leak Test Specimen Assembly	888
7-7	IHTS Modified Z-Ring Seal Over Marinite Ring Halves	889
7-8	CRBRP Prototype Pump - Cutaway View	901
7-9	CRBRP Prototype Pump	902

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
8-1	Steam Generator Auxiliary Heat Removal System Schematic	917
8-2	Process Streams in Relationship to Major Components Schematic	921
8-3	Steam Supply Line Schematic	929
8-4	Control Logic for Turbine Driven AFWP	938
8-5	Control Logic for Turbine Driven AFWP	939
9-1	CRBRP Steam Generator System Schematic	952
9-2	CRBRP Steam Drum	959
11-1	System 81 Schematic - Final Configuration	1017
11-2	Flow During EVST Cooling Operation	1027
11-3	Flow During EVST-Plus-DHRS	1028
11-4	System 81 Schematic - Initial Conceptual Design	1031
11-5	System 81 Schematic - Final Conceptual Design	1035
11-6	Small Insulated Pipe Clamp Assembly	1050
11-7	EM Pump Header Configurations	1057
11-8	EM Pump Throat Configurations	1058
11-9	EM Pump Assembly	1059
11-10	Flow Mapping at 276 V and 800°F	1060
11-11	Flow Mapping at 368 V and 800°F	1060
11-12	EM Pump Design Limits at 600°F	1061
11-13	Typical Thermal Transient Valve	1062
11-14	Typical Thermal Transient Operator Assembly	1063
11-15	Valve Disc-to-Stem Design	1069
11-16	Bonnet Modifications	1071
11-17	System 81 Documentation Diagram	1075

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
11-18	System 81 Electrical, Instrumentation, and Control (EI&C) Documentation Diagram	1076
12-1	Simplified Flow Schematic RSB Argon Distribution	1092
12-2	Simplified Flow Schematic RCB Argon Distribution	1093
12-3	Simplified Flow Schematic SGB Argon Distribution	1094
12-4	Simplified Flow Schematic RSB/RCB Nitrogen Distribution	1096
12-5	Simplified Flow Schematic	1097
12-6	Radioactive Argon Processing Subsystem	1099
12-7	Cell Atmosphere Processing Subsystem	1101
12-8	Use of CAPS for Post-Accident Cleanup	1123
12-9	Dose in Cell 307 After Postulated Rupture in Cell 379	1124
12-10	Site Boundary Doses After a Postulated Rupture in Cell 379	1125
12-11	Calculated Kr and Xe Inventory as a Function of Time Following a Large Postulated Release in Cell 379 Followed by a Purge to CAPS at 15 cfm	1131
12-12	System 82 Documentation Diagram	1137
12-13	System 82 I&C Documentation Diagram	1138
13-1	Primary Plugging Temperature Indicator Package	1150
13-2	Plugging Temperature Indicator	1151
13-3	Primary Sodium Sampling Package	1153
13-4	Multipurpose Sampler	1154
13-5	Ex-vessel Sodium Sampling Package	1155
13-6	Intermediate Sodium Characterization Package	1157
13-7	System 85 Drawing Tree	1171
13-8	System 85 I&C Document Diagram	1172

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
14-1	CRBRP Plant Control Schematic	1180
14-2	CRBRP Part Load Profile	1181
15-1	Control/Protection Interface	1213
15-2	Control/Protection Interface	1214
15-3	Effect of PM Flowmeter Time Constant on Signal Noise	1223
A-1	Fatigue of SA 316SS In Air at 538 to 566°C	1230
A-2	Fatigue of SA 316SS at 538 to 566°C, In-Sodium Data Versus In-Sodium Correlation with In-Air Correlation Asymptote	1231
A-3	Illustration of Fluid Temperature History as Input to "TAP A" (as Boundary Temperatures)	1232
A-4	Illustration of Instantaneous Structural Temperature Amplitude (ΔT^*) History as Input to "STRIPE"	1233
A-5	Illustration of Total Thermal Striping Amplitude (ΔT^*) History as Input to "RAINFAT"	1234
A-6	Element Representation of Stress-Strain Curves	1236
A-7	Use of Elements and Availabilities to Count Cycles and Simulate Stress-Strain Behavior	1237
A-8	Fatigue Design Curve Sodium, -2σ	1238
A-9	Study of the Effect of Film Coefficient on Damage	1240
A-10	Typical Fuel Assembly Outlet Temperature Striping Potentials	1241
A-11	Typical Fuel Assembly Outlet Temperature Striping Potentials	1242
A-12	Study of How Damage Saturates with Time	1244
A-13	Fluid (Surface) Percent Striping	1245
A-14	How the Maximum Strain Range and Low Cycle Strain Ranges Effect Total Damage	1247

LIST OF FIGURES (Continued)

<u>Figure No.</u>	<u>Title</u>	<u>Page</u>
A-15	Constant Striping Potential Versus Fatigue Damage for Measured Fluid Striping Signals	1251
B-1	Hardness Profiles Through the Thickness Follow Temperature Profile	1255
B-2	TSTF Specimen Hardness Change Versus Depth Below Surface Due to Striping	1256
C-1	CRBRP HCDA Progression Paths	1259

ABBREVIATIONS AND ACRONYMS

Following is the alphabetical list of abbreviations and acronyms as they appear within this document:

ABHX	-	Air Blast Heat Exchanger
ACLP	-	Above Core Load Plane
ACRS	-	Advisory Committee on Reactor Safeguards
A-E	-	Architect-Engineer (Burns and Roe, Inc.)
AEC	-	U.S. Atomic Energy Commission (now split to DOE and NRC)
AFMS	-	Alternate Fuel Management Scheme
AFW	-	Auxiliary Feedwater Subsystem of SGAHRS
AHM	-	Auxiliary Handling Machine
AI	-	Atomics International Division of Rockwell International
AISI	-	American Iron and Steel Institute
ALARA	-	As Low As Reasonably Achievable
ANL	-	Argonne National Laboratory
ANSI	-	American National Standards Institute
APU	-	Atmosphere Purification Unit
ARPI	-	Absolute Rod Position Indicator
ASB	-	Auxiliary Steam Boiler
ASME	-	American Society of Mechanical Engineers
ASTM	-	American Society for Testing Materials
BA	-	Blanket Assembly
BDI	-	Bundle-Duct Interaction
BOC	-	Beginning of Cycle
BOL	-	Beginning of Life
BOP	-	Balance of Plant
BPFM	-	Bypass Flow Module

ABBREVIATIONS AND ACRONYMS (Continued)

B&PV	-	Boiler and Pressure Vessel
BTP	-	Branch Technical Position (NRC)
BWR	-	Boiling Water Reactor
CA	-	Control Assemblies
CAPS	-	Cell Atmosphere Processing System
CB	-	Core Barrel
CBI	-	Chicago Bridge and Iron Company
CCP	-	Core Component Pot
CCTV	-	Closed Circuit Television
CDF	-	Cumulative Damage Function
CFR	-	Code of Federal Regulations
CFS	-	Core Former Structure
CLCV	-	Cold Leg Check Valve
CLEM	-	Closed Loop Ex-vessel Machine (at FFTF)
CRBR	-	Clinch River Breeder Reactor
CRBRP	-	Clinch River Breeder Reactor Plant
CRD	-	Control Rod Driveline
CRDM	-	Control Rod Drive Mechanism
CSP	-	Core Support Plate
CSS	-	Core Support Structure (31)
CSS	-	Conditioning and Service Subsystem (41)
CTM	-	Cable Transfer Machine
CW	-	Cold Worked
DBL	-	Design Basis Leak
DDC	-	Direct Digital Control
DELT	-	Cumulative Damage Function Equivalent Limiting Temperature
D-G	-	Diesel Generator
DEG	-	Double Ended Guillotine
DHRS	-	Direct Heat Removal Service
DLS	-	Ductility Limited Strain
DNB	-	Departure from Nucleate Boiling
DOE	-	U.S. Department of Energy

ABBREVIATIONS AND ACRONYMS (Continued)

DRS	-	Development Requirements Specification
EBR-II	-	Experimental Breeder Reactor II (located in Idaho)
ECP	-	Engineering Change Proposal
EDEG	-	Equivalent Double Ended Guillotine
EFPD	-	Effective Full Power Days
EM	-	Electro-Magnetic
EOC	-	End of Cycle
EOL	-	End of Life
ERDA	-	U.S. Energy Research and Development Administration (now DOE)
ESF	-	Engineered Safety Feature
ESG	-	Energy Systems Group of Rockwell International
ETEC	-	Energy Technology Engineering Center (located in Santa Susanna, CA)
EVHS	-	Ex-Vessel Handling Subsystem
EVS	-	Ex-Vessel Storage
EVSS	-	Ex-Vessel Storage Subsystem
EVST	-	Ex-Vessel Storage Tank
EVTM	-	Ex-Vessel Transfer Machine
FA	-	Fuel Assembly
FAG	-	FAG Bearing Corporation
FCMI	-	Fuel Cladding Mechanical Interaction
FFTF	-	Fast Flux Test Facility (located in Hanford, Washington)
FHC	-	Fuel Handling Cell
FIV	-	Flow Induced Vibration
FMEA	-	Failure Modes and Effects Analysis
FRS	-	Fixed Radial Shield
FTR	-	Fast Test Reactor (FFTF)
FTSA	-	Fuel Transfer and Storage Assembly
FV	-	Floor Valve
FWSDR	-	Final Working System Design Review
GE	-	General Electric Company
HAA	-	Head Access Area

ABBREVIATIONS AND ACRONYMS (Continued)

HB	-	Horizontal Baffle
HBM	-	Horizontal Boring Mill
HBRR	-	Horizontal Baffle Retaining Ring
HBSO	-	Hot Bare Strap-On
HCDA	-	Hypothetical Core Disruptive Accident
HEDL	-	Hanford Engineering Development Laboratory
HTS	-	Heat Transport System
HUD	-	U.S. Department of Housing and Urban Development
H&V	-	Heating and Ventilating
I&C	-	Instrumentation and Control
IBA	-	Inner Blanket Assembly
ICD	-	Interface Control Document/Drawing
IEEE	-	Institute of Electrical and Electronics Engineers, Inc.
IFT	-	Instantaneous Fault Trips
IGRP	-	Inert Gas Receiving and Processing System
IHTS	-	Intermediate Heat Transport System
IHX	-	Intermediate Heat Exchanger
IMAS	-	Impurity Monitoring and Analysis System
IPFM	-	Inlet Plenum Feature Model
IRFM	-	Integral Reactor Flow Model
IRP	-	Intermediate Rotating Plug
ISI	-	In-service Inspection
IVHS	-	In-Vessel Handling Subsystem
IVTM	-	In-Vessel Transfer Machine
KE	-	Kinetic Energy
KSTF	-	Key System Task Force
LCC	-	Local Control Center
LD	-	Leak Detector
LDM	-	Leak Detection Module
LEFM	-	Linear Elastic Fracture Mechanics
LHGR	-	Linear Heat Generation Rate
LIM	-	Lower Inlet Module

ABBREVIATIONS AND ACRONYMS (Continued)

LLM	-	Liquid Level Monitor
LLTR	-	Large Leak Test Rig
LMFBR	-	Liquid Metal Fast Breeder Reactor
LN ₂	-	Liquid Nitrogen
LOD	-	Lines of Defense
LOEP	-	Loss of Electrical Power
LOF	-	Loss-of-Flow
LRM	-	Lead Reactor Manufacturer
LRP	-	Large Rotating Plug
LWR	-	Light Water Reactor
MCC	-	Master Control Center
MCR	-	Main Control Room
MEB	-	Mechanical Engineering Branch of the NRC
MEFS	-	Moderate Energy Fluid System
MI	-	Mineral Insulated
MPS	-	Multipurpose Sampler
NCVP	-	Natural Circulation Verification Program
NDHX	-	Natural Draft Heat Exchanger
NDT	-	Non-Destructive Test
NI	-	Nuclear Island
NLD	-	Non-Linear Disengagement
NRC	-	U.S. Nuclear Regulatory Commission
NSSS	-	Nuclear Steam Supply System. Synonymous with Nuclear Island.
OBE	-	Operating Basis Earthquake
OCC	-	Operator Control Centers
OHX	-	Overflow Heat Exchanger
O&M	-	Operations and Maintenance
OPDD	-	Overall Plant Design Description
ORT	-	Operational Reliability Testing
P/F	-	Power to Flow
PACC	-	Protected Air Cooled Condenser
PAS	-	Plant Annunciator System

ABBREVIATIONS AND ACRONYMS (Continued)

PCA	-	Primary Control Assembly
PCRD	-	Primary Control Rod Driveline
PCRDM	-	Primary Control Rod Drive Mechanism
PCRS	-	Primary Control Rod System
PCS	-	Plant Control System
PDH&DS	-	Plant Data Handling and Display System
PEEHCS	-	Piping and Equipment Electrical Heating and Control System
PEL	-	Proportional Elastic Limit
PEOC	-	Plant Expected Operating Conditioning
PHENIX	-	Prototype Fast Reactor (250 MWe) located at Marcoule, France
PHTS	-	Primary Heat Transport System
P&ID	-	Piping and Instrumentation Drawing
PIT	-	PCA Irradiation Test
PMC	-	Project Management Corporation
PNC	-	Power Reactor and Nuclear Fuel Development Corporation (Japan)
PO	-	Project Office, CRBRP
PPS	-	Plant Protection System
PSAR	-	Preliminary Safety Analysis Report
PSB	-	Plant Service Building
PSP	-	Primary Sodium Pump
PSS	-	Primary Shutdown System
PTI	-	Plugging Temperature Indicator
PWHT	-	Post Weld Heat Treatment
PWR	-	Pressurized Water Reactor
PWST	-	Protected Water Storage Tank
RAPS	-	Radioactive Argon Processing System
RBA	-	Radial Blanket Assembly
RCB	-	Reactor Containment Building
RCC	-	Refueling Communications Center
RDSO	-	Reliability Design Support Document
RDT	-	Division of Reactor Development and Technology, U.S. AEC
RFP	-	Request for Proposal

ABBREVIATIONS AND ACRONYMS (Continued)

RFTP	-	Reactor Fuel Transfer Port
RHTIS	-	Reactor Heat Transport Instrumentation System
RM	-	Reactor Manufacturer
RPST	-	Reaction Products Separator Tank
RRD	-	Division of Reactor Research and Development, ERDA
RRPI	-	Relative Rod Position Indicator
RRS	-	Reactor Refueling System
RRSA	-	Removable Radial Shield Assembly
RSB	-	Reactor Service Building
RSS	-	Reactor Shutdown System
RT	-	Room Temperature
RV	-	Reactor Vessel
SAW	-	Submerged Arc Welding
SCA	-	Secondary Control Assembly
SCC	-	Stress Corrosion Cracking
SCRD	-	Secondary Control Rod Driveline
SCRDM	-	Secondary Control Rod Drive Mechanism
SCTI	-	Sodium Component Test Installation (located at Santa Susanna, CA)
SDD	-	System Design Description
SDOF	-	Single Degree of Freedom, Dynamic Response Analysis
SELT	-	Inelastic Cladding Strain Equivalent Limiting Temperature
SEM	-	Scanning Electron Microscopy
SFSC	-	Spent Fuel Shipping Cask
SFPS	-	Sodium Fire Protection System
SGAHRs	-	Steam Generator Auxiliary Heat Removal System
SGB	-	Steam Generator Building
SGM	-	Steam Generator Module
SGS	-	Steam Generator System
SMBDB	-	Structural Margin Beyond the Design Basis
SPTF	-	Sodium Pump Test Facility (located at ETEC)
SRI	-	Southwest Research Institute

ABBREVIATIONS AND ACRONYMS (Continued)

SRP	-	Standard Review Plan (NRC)
SRP	-	Small Rotating Plug (32)
SS	-	Stainless Steel
SSE	-	Safe Shutdown Earthquake
SSP	-	Sodium Sampling Package
SSS	-	Secondary Shutdown System (99)
SSS	-	Shield and Seismic Support (31)
SSST	-	Site Suitability Source Term
SUPER PHENIX	-	Commercial Fast Reactor (1,000 MWe), located at Cley's-Malville, France
SWR	-	Sodium Water Reaction
SWRPRS	-	Sodium Water Reaction Pressure Relief Subsystem
T/C	-	Thermocouple
TDAFWP	-	Turbine Drive Auxiliary Feedwater Pump
TELT	-	Worst-time-in-life Transient Equivalent Limiting Temperature
T/H	-	Thermal-Hydraulic
THDV	-	Thermal Hydraulic Design Value
TLP	-	Top Load Plane
TMBDB	-	Thermal Margin Beyond the Design Basis
TMI 2	-	Three Mile Island, Unit 2 Nuclear Power Plant (located near Harrisburg, PA)
TOP	-	Transient Overpower
TREAT	-	Transient Reactor Test Facility (located in Idaho)
TSTF	-	Thermal Stripping Test Facility (located at WAESD)
T/TS	-	Tube-to-Tubesheet
TWJ	-	Transition Weld Joint
UCFS	-	Upper Core Former Structure
UIS	-	Upper Internal Structure
UISJM	-	UIS Jacking Mechanism
UPS	-	Uninterruptible Power System
UT	-	Ultrasonic Testing
VBM	-	Vertical Boring Mill

ABBREVIATIONS AND ACRONYMS (Continued)

WAESD	-	Westinghouse Advanced Energy Systems Division
WARD	-	Westinghouse Advanced Reactors Division. Renamed WAESD
WSS	-	Water/Steam Subsystem
ZPPR	-	Zero Power Plutonium Reactor (located in Idaho)

1.0 CLINCH RIVER BREEDER REACTOR PLANT

1.1 General Report Integration

1.1.1 Introduction

This final technical report summarizes the work done on the major systems of the Nuclear Steam Supply System (NSSS) of the Clinch River Breeder Reactor Plant (CRBRP). A similar report by the Architect-Engineer for CRBRP covers the Balance of Plant (BOP) [1]. Not every system* or component is covered; in fact, only those components within a selective list of systems which were deemed either to have a generic utility in future Liquid Metal Reactor designs, or had innovative features to assist future designers, are discussed herein.

1.1.2 Report Organization

This report is organized in Sections, each covering one system (except for Section 10 which covers two of the smaller systems). Table 1-1 identifies the systems/major components/topics covered in this report. Throughout, in discussing systems and components, emphasis is placed on describing the basis for design features and approaches rather than details of design; key problems are identified and the resolutions described and the final status of the systems/ components is given together with outstanding items remaining at Project termination. Extensive references to the CRBRP data base are given at the end of each section or major sub-section and called out from the text by numbers in square brackets. The remainder of Section 1.0 covers subjects which are plant-wide in nature and not limited to a single system (Beyond-the-Design Basis Analyses for the Advisory Committee on Reactor Safeguards

*The Project was divided into many systems for design and procurement purposes. These are defined in Reference 2 which also contains the requirements and criteria used in the design of the plant. Each system has its own System Design Description (SDD).

TABLE 1-1
REPORT ORGANIZATION AND CONTRIBUTORS

<u>Section</u>	<u>CRBRP System No.</u>	<u>System/Major Component/Topic</u>	<u>Contributor*</u>
1.2	N/A	Beyond-the-Design-Basis Analyses for the ACRS	W
1.3	N/A	Decay Heat Removal Using Natural Circulation	W
1.4	N/A	Resolution of Metal Joining Problems	W
2.0	26	Sodium Fire Protection	AI
3.1-3.3	31	Reactor Core Components	W
3.4-3.6	31	Reactor Core Nuclear Design	W
3.7.1	31	Primary Control Rod System	W
3.7.2	31	Secondary Control Rod System	GE
3.8, 3.11-3.14	31	Reactor Core Thermal-Hydraulic Design	W
3.9	31	Reactor Upper Internals Structure	W
3.10	31	Reactor Core Restraint System	W
4.0	32	Reactor Vessel Closure Head SMBDB Design	W
5.0	41	Reactor Refueling System	AI
6.0	51	Primary Heat Transport Piping and Clamps	W
7.1	51	Intermediate Heat Transport System	GE
7.2	51	Leak Jacket Design	W
7.3	51	Main Heat Transport Sodium Pumps	GE
8.0	52	Steam Generator Auxiliary Heat Removal System	GE
9.0	53	Steam Generator System	GE
10.1	56	Reactor Heat Transport Instrumentation	GE
10.2	68	Piping and Equipment Electrical Heating & Control	GE
11.0	81	Auxiliary Liquid Metal System	AI
12.0	82	Inert Gas Receiving and Processing System	AI
13.0	85	Impurity Monitoring and Processing System	AI
14.0	90	Plant Control System	W
15.0	99	Reactor Shutdown System	W
Appendices A & B	31	Thermal Striping Methodology	W
Appendix C	32	Hypothetical Core Disruptive Accident Scenario	W

*W - Westinghouse
GE - General Electric
AI - Atomics International

(ACRS), Decay Heat Removal Using Natural Circulation and Resolution of Metal Joining Problems). Several appendices are included which contain information supporting Sections 3.12 and 4.0.

Because of its size, the report is divided into two volumes with the front end material in both volumes. A list of Acronyms and Abbreviations is provided with the front material, so that it is available in both volumes.

1.1.3 Contributors

This report is the result of many contributors from the three Reactor Manufacturers responsible for the design of the Nuclear Steam Supply System: the Atomics International Division (now a part of the Rocketdyne Division) of Rockwell International; the Nuclear Systems Technology Operation (formerly known as the Advanced Reactor Systems Department.) of General Electric Company and the Advanced Energy Systems Division (.previously known as the Advanced Reactors Division) of Westinghouse Electric Corporation. Table 1-1 identifies which portions of this report are provided by each.

1.1.4 Availability

This document and all of the references contained herein are a part of the CRBRP data base and are available or accessible through the U.S. Department of Energy Office of Scientific and Technical Information (OSTI) RECON data base administered by the OSTI Technical Information Center, unless another source is identified.

1.1.5 References

1. Architect/Engineer Final Technical Report, DOE/CL-98004-4, January, 1985.
2. Clinch River Breeder Reactor Plant Overall Plant Design Description (OPDD-10), Rev. 146, August 1, 1983.

1.2 Beyond-the-Design-Basis Analyses for the ACRS

1.2.1 Introduction

The U.S. Nuclear Regulatory Commission (NRC) evaluated in depth the response of the Clinch River Breeder Reactor Plant (CRBRP) to two aspects of Hypothetical Core Disruptive Accidents (HCDAs), which are beyond-the-design-basis, or Class 9, type of accidents. These two aspects were referred to as the Structural Margin Beyond the Design Basis (SMBDB) and the Thermal Margin Beyond the Design Basis (TMBDB).

The SMBDB considers a hypothetical energetic disassembly of the core with sufficient pressure generated to challenge the structural integrity of the reactor vessel. The TMBDB considers a hypothetical total meltdown of the core as a result of loss of core coolable geometry. Both of these hypothetical conditions were analyzed by the Project in intensive detail, and design features were provided to accommodate severe conditions in both of these areas.

The NRC evaluated the possibility of pipe rupture in the primary heat transport loops and failure of the core support structure. They concluded that these failures were so unlikely, based upon the margins in the design, that the consequences of these extremely improbable events need not be considered. The NRC also did not require an evaluation of the consequence of extended (beyond two hours) plant blackout of all off-site and on-site AC electrical power.

The Advisory Committee on Reactor Safeguards (ACRS), however, did request that an evaluation of the consequences of primary pipe rupture and core support cone failure be evaluated and presented to them. They also requested that an extended AC blackout of the plant be evaluated and presented. The reason for this latter request is that they perceived that a liquid-metal plant can accommodate the loss of all AC power much more easily than a water reactor. They felt that if CRBRP was close to being able to accommodate a plant blackout for an indefinite length of time, but could not quite do it, design changes to enable it to do so would be worthwhile.

The requested analyses were performed and presented to the ACRS. The analyses were never made a formal part of the safety documentation, so the results are summarized here as a means of providing documentation of the analyses.

1.2.2 Hypothetical Guillotine Primary Pipe Rupture

The three Primary Heat Transport System (PHTS) loops of CRBRP consist of a 36-inch diameter hot leg pipe, a hot leg pump, a 24-inch diameter pipe from the pump to the Intermediate Heat Exchanger (IHX), and a 24-inch diameter cold leg pipe containing a check valve. The primary piping is austenitic stainless steel with one-half-inch wall thickness. All primary piping is in an inert atmosphere and carries a non-corrosive coolant, sodium.

The CRBRP operates at low pressure and at a temperature well below the atmospheric boiling point of the coolant.

Numerous analytical and experimental studies have demonstrated that a crack in a pipe in such a system can have only very limited growth. This effect results from the ductile nature of austenitic steel used in thin-walled piping containing a non-flashing coolant. Consequently, the design basis leak for CRBRP is a small leak.

Nevertheless, the consequences of a guillotine rupture were evaluated on a best estimate basis as part of the evaluation for the ACRS of low probability, hypothetical events that are beyond the design basis.

Figure 1-1 shows a schematic of the PHTS components. The reactor vessel, pump and IHX are inside guard vessels. All primary piping not inside guard vessels is elevated. The guard vessels have been sized so that a leak inside them, or a leak in the elevated piping, will stop leaking with a sodium level high enough that sodium can circulate through the remaining two loops and remove decay heat on pony motor flow. (The Plant Protection System includes a trip of the primary pumps which causes the flow to coast down to the pony motor flow whenever the reactor is tripped.)

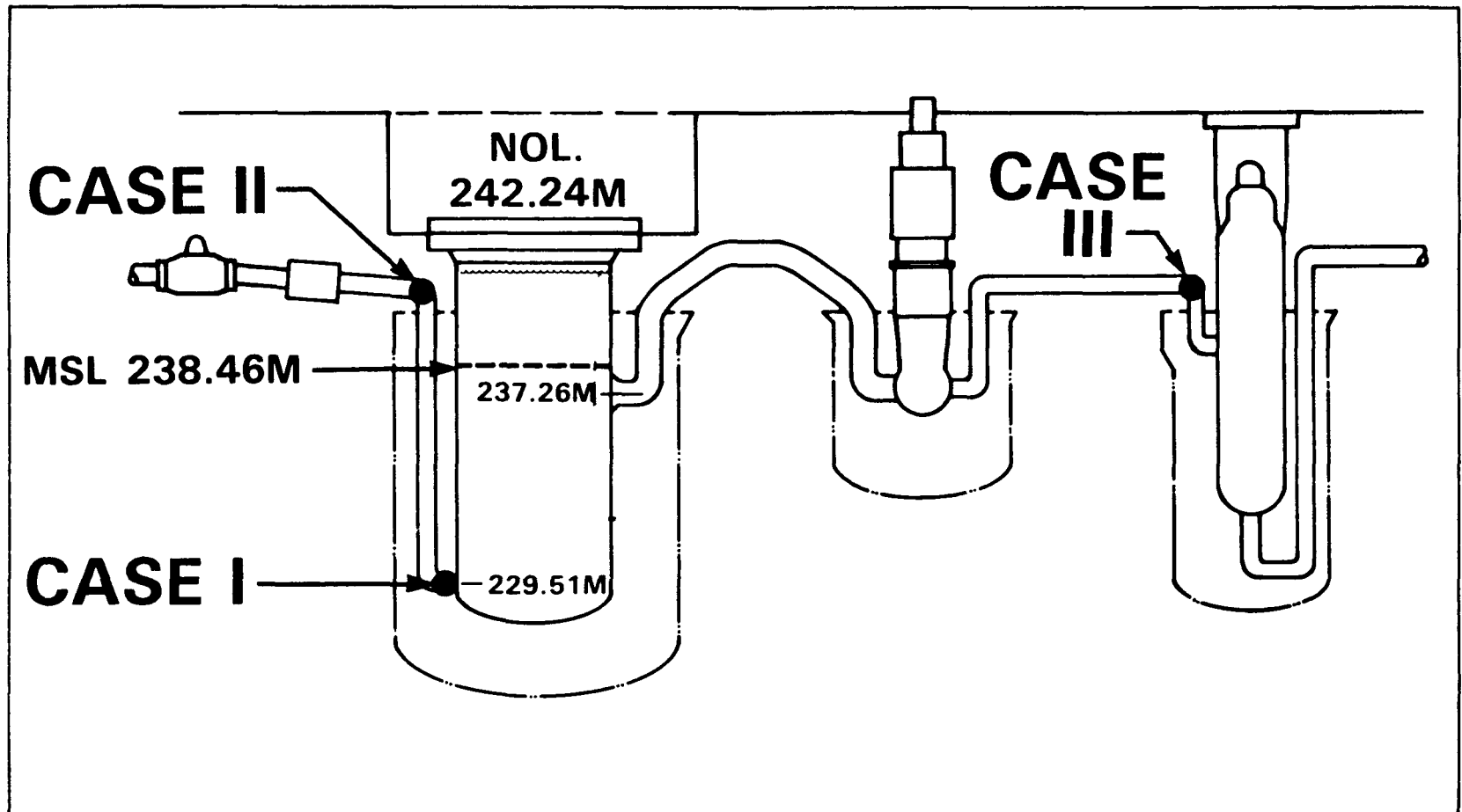


Figure 1-1 Failure Location for Guillotine Rupture Cases Evaluated

The DEMO computer code [1] was used to calculate transient system flows and pressures, and the FORE-2M computer code [2] was used to calculate transient core temperatures for hypothetical guillotine ruptures at the three locations labeled Case I, Case II, and Case III on Figure 1-1. Case I is a break at the reactor vessel inlet. Case II is a break in the cold leg pipe at the top of the reactor vessel guard vessel. Case III is a break in the hot leg at the top of the intermediate heat exchanger guard vessel.

The pipe break analyses were not conducted with the full conservatism applied to design basis safety analyses. For the most part, best estimate values were used; but some assumptions were more conservative than those normally used for beyond the design basis events.

Specifically, for the DEMO analysis, the very conservative assumptions were:

- A. All three breaks were sudden guillotine breaks. That is clearly impossible in Case I, the worst case, where the guard vessel/guard pipe prevents the 24-inch pipe from moving more than five inches away from the reactor vessel plus five inches tangential to the reactor vessel.
- B. No credit was taken for the check valve that should be assumed to operate in a best estimate case even though its operation is not a safety function.
- C. The maximum specification allowance for reactor trip time was assumed.
- D. The minimum insertion rate for the control rods was assumed.
- E. The minimum control rod worths were assumed for the analysis, including the assumption that only the primary rod bank operated and the highest worth rod in that bank failed to insert.

- F. No credit was taken for the leak being slowed by the increasing cell pressure.
- G. The effluent discharge expansion loss coefficient was neglected.

Other assumptions included:

- A. Expected plant operating conditions at beginning of operations were used instead of thermal hydraulic design values (maximum pressure drop - minimum pump head). Degradation of heat transfer surfaces over the 30-year life of the plant could increase the temperature nominally by 14°F.
- B. Pump coast down characteristics from the prototype pump test were used instead of minimum specification allowable pump coast down characteristics.

The FORE-II analyses were a mix of conservative and best estimate values also, instead of the total conservatism used in design basis events. Specific conservatisms were:

- A. The hottest rod at the worst time in life (beginning of Cycle 1) for the fuel was used.
- B. The hottest rod at the worst time in life (end of Cycle 4) for the blanket was used.
- C. The peak temperatures at any time during the incident were compared with the lowest saturation temperature at any time during the incident regardless of the fact that the peak temperatures do not occur at the same time as the minimum saturation temperature.
- D. Decay heat values were taken at the worst time in life for both fuel and blanket, despite the fact that these do not occur simultaneously.

On the other hand:

- A. The original geometrical hot spot was followed in time during the event instead of following the movement, if any, of the hot spot down the rod.
- B. Nominal values for decay heat, film coefficient, power distribution, and so forth, were used instead of the normal design practice of applying direct and statistically combined uncertainties to the parameters to obtain what is referred to as a 3-sigma analysis. For decay heat, these were nominal maximums, of course, as noted in assumption D above.

The failure location resulting in the maximum core temperature was the reactor vessel inlet nozzle (Case I). This can be seen by comparison of the maximum average coolant temperature at the core outlet with rupture location shown in Table 1-2.

Case I is the worst case not only because it has the highest initial pressure but also because the static head of sodium in the cold leg downcomer is lost for resisting reverse flow out of the inlet plenum.

Figure 1-2 shows the reactor core power and flow for Case I while Figure 1-3 shows the temperature/time response of limiting fuel, inner blanket and radial blanket hot rods. On the same figure is plotted the fuel rod saturation temperature as a function of time. The maximum temperature predicted was 1400°F and occurred for a fuel hot rod. The lowest coolant saturation temperature was 1580°F (conservatively based only on cover gas pressure and elevation head). At no time in the transient was there less than 235°F margin to boiling. Although boiling in itself would not necessarily be unacceptable, the lack of boiling in the core, along with the relatively low maximum fuel temperatures, clearly shows that the core would survive the event intact.

TABLE 1-2
MAXIMUM CORE OUTLET SODIUM TEMPERATURE FOR DIFFERENT PIPE RUPTURE LOCATIONS

	<u>Fuel Region</u>	<u>Inner Blanket Region</u>
Case I	1210°F	1265°F
Case II	1185°F	1185°F
Case III	1110°F	1145°F

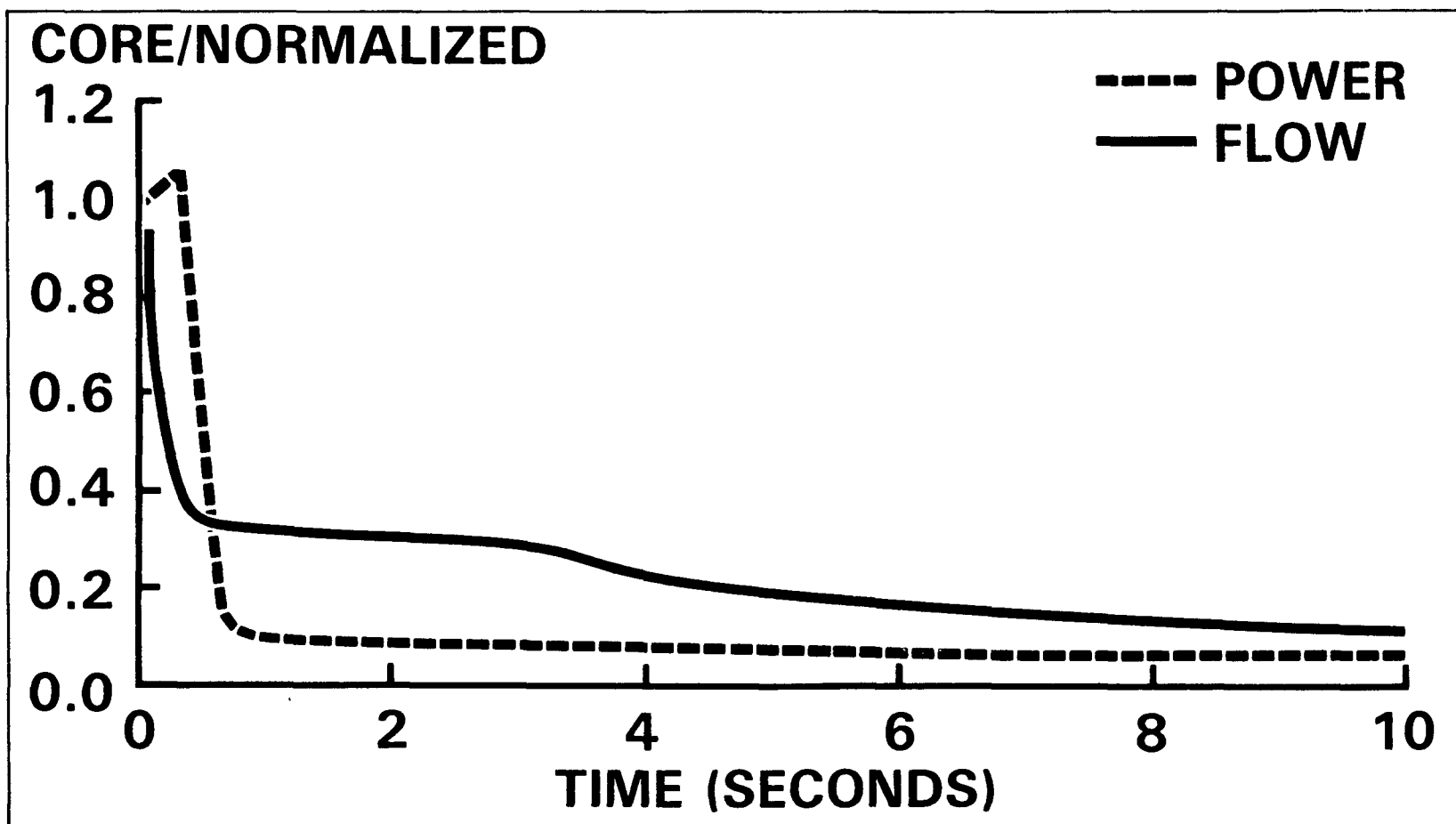


Figure 1-2 Variation in Power and Flow for Case 1

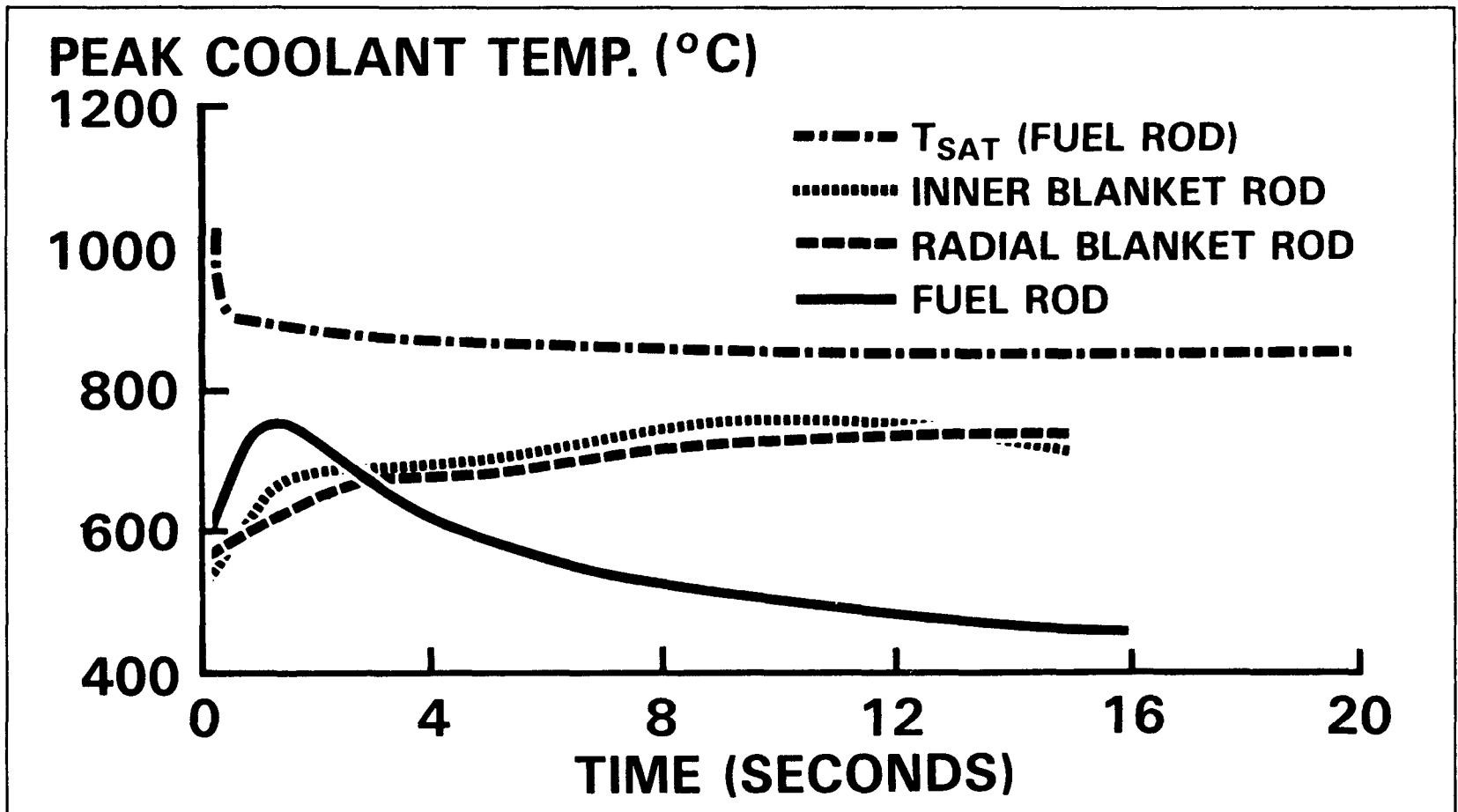


Figure 1-3 Reactor Response for Case 1

The transient terminates with more than adequate sodium inventory above the reactor outlet nozzles, as shown in Figure 1-4, to guarantee decay heat removal capability in the two undamaged loops.

In conclusion, a conservative best estimate analysis of the worst case guillotine pipe break has shown that the CRBRP can safely withstand this hypothetical, beyond-the-design-basis event.

1.2.3 Hypothetical Core Support Failure Consequences

The core of CRBRP is supported from the reactor vessel. Extremely large structural margins exist for steady state, transient, and seismic loads. A major failure of the core support structure is, therefore, well beyond the design base. Analyses were performed to evaluate the consequences of core structural failure. These analyses show that, even if the core support were postulated to fail, acceptable safety consequences result with only natural circulation cooling.

CRBRP has two control rod systems which will independently shut down the reactor. Under hypothetical major core support failure conditions, the core is postulated to drop down into the reactor lower plenum and away from the control rods. This event could not occur except in the shutdown mode since the upward hydraulic force is sufficient to support the core for coolant flows much above shutdown flow. Although the primary and secondary control systems are fully inserted during normal shutdown, the primary control rod drivelines are attached to the reactor head so that the primary absorbers would be pulled out of the falling core. The primary control rod drivelines are disconnected during refueling conditions so during refueling the primary rods would fall with the core. The accident is assumed to happen during a shutdown period while not refueling as a worst case. The diverse secondary control system uses hydraulic scram forces without an insertion driveline and would remain inserted in the core throughout the event. The secondary control system is capable of keeping the core subcritical with complete primary control system withdrawal after some of the core excess reactivity has been burned out. However, the secondary system is not designed to maintain subcriticality with the primary system withdrawn under all conditions since it was designed to

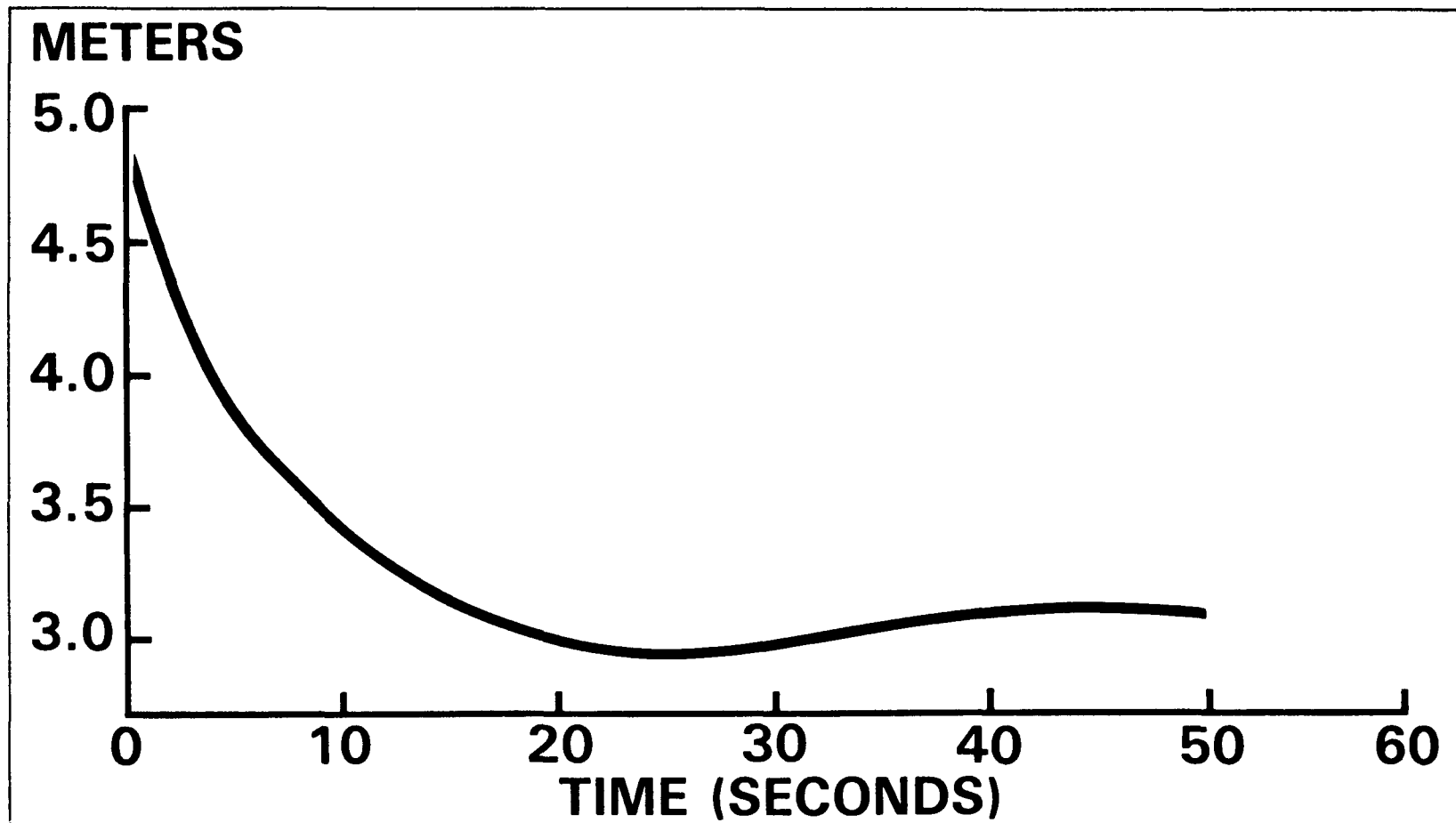


Figure 1-4 Reactor Vessel Na Level Above Outlet Nozzle for Case 1

perform its function with the primary control rods at least partially inserted to shim the excess reactivity in the core. Therefore, early in the burnup cycle, there exists a potential for recriticality if the primary control system is totally removed. The worst case is a failure upon shutdown shortly after refueling, with maximum fuel excess reactivity in the core and minimum core decay heat.

Dynamic analyses were performed to compare the control rod insertion time and scram delay time with the mechanics of the core support failure. The net transient excess reactivity did not exceed the final post-shutdown steady-state values. Therefore, steady-state analysis provides correct results.

The core steady state nuclear and thermal-hydraulic conditions, following the postulated failure of the core support, was determined for the CRBRP core and core support system resting on the vessel bottom in the inlet plenum as shown on the right hand side of Figure 1-5. As can be seen there is adequate access of the inlet plenum sodium to the inlet of the reactor assemblies. Although sodium is being pumped into the inlet plenum and out of the outlet pipe nozzles, the only sodium flow through the heat producing assemblies is by natural circulation.

The natural circulation flow increases with temperature increase, while the excess reactivity or power level decreases with temperature increase. Thus, depending on the excess reactivity in the system at the time of the failure, the reactor will achieve an equilibrium in temperature, flow, and power conditions that may or may not require nuclear power to augment the decay power. The approach to equilibrium is slow and smooth with no overshoot as the primary sodium cools.

Since the core flow rates are small, large inter-and-intra-assembly flow and heat redistribution effects will occur between the core assemblies such that the fuel and blankets will operate at approximately the same sodium temperature. Experiments and analyses performed for Experimental Breeder Reactor (EBR)-II and Fast Flux Test Facility (FFTF) under natural circulation conditions confirm this effect [3].

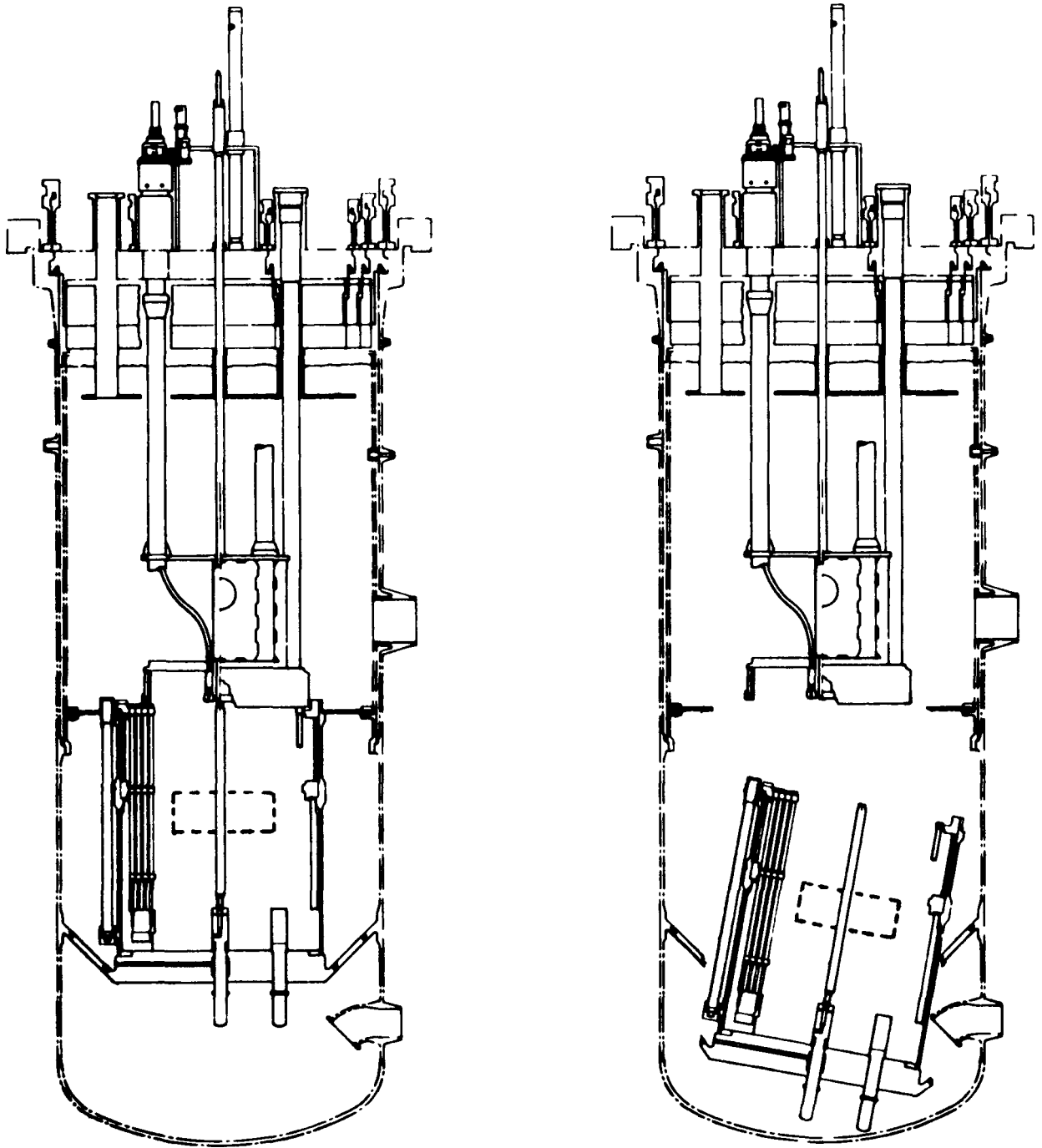


Figure 1-5

Geometry of CRBRP System Following
A Hypothetical Core Support Cone
Failure

The results of the evaluation are presented in Table 1-3 for the most reactive time in life, the beginning of an equilibrium cycle. With the entire primary control system withdrawn and the secondary control rods inserted, there is +56¢ excess reactivity above isothermal shutdown conditions at the full-power inlet temperature of 730°F. Therefore, the reactor power will rise until negative feedbacks achieve zero excess reactivity. As shown in Table 1-3, the reactor nuclear power level required to achieve this is only a few percent, (or less if significant decay heat is present). The core temperatures are relatively low. However, at the 600°F normal core inlet temperature of hot shutdown conditions, which the reactor will automatically seek without operator action, the reactor power level and coolant ΔT must rise to maintain the null-excess-reactivity state. If the operator reduces the inlet temperature to the 400°F refueling temperature, additional reactivity is inserted and this null-excess-reactivity state is attained at a reactor power level of nearly 20% of full power. Under conditions lacking forced coolant circulation, the coolant temperature rise through the core is about 900°F at this latter condition but, significantly, the peak coolant outlet temperature remains below the sodium boiling temperature of 1720°F at the top of the active core. Although boiling itself would not necessarily be unacceptable, the lack of any boiling in the core, along with relatively low maximum fuel temperatures, clearly shows that the core would survive the event with a coolable geometry being maintained.

This analysis was for a worst condition, shortly after the beginning of an equilibrium cycle, or cycles 3 and 4. It is worthy of note that, even for cycles 3 and 4, after less than 40 full-power-days of operations, the reactor would remain subcritical with an inlet plenum temperature of 600°F. For these same cycles, after less than 75 full-power days, the reactor would remain subcritical even if the reactor operator took action to cool the plant to the refueling temperature of 400°F. For cycles 1 and 2, the reactor would remain subcritical even at 400°F. As noted earlier, the reactor would remain subcritical with such a failure any time refueling was being done.

In conclusion, these analyses demonstrate that CRBRP would safely withstand a postulated beyond-the-design basis core support failure event even at the worst possible time in life.

TABLE 1-3
 REACTIVITY BALANCE AND ESTIMATED CORE TEMPERATURES FOR
 HYPOTHETICAL CORE SUPPORT FAILURE CONDITIONS
 (Primary Rods Pulled Out of Core and Secondary Rods Fully Inserted)

Reactor Inlet Temperature, (°F)	Excess Reactivity Potential	Reactivity Feedbacks	Fuel and Blanket Isothermal Temperature, (°F)	Coolant Outlet Temperature (°F)		Estimated Reactor Power (% of Full Power)
				Bulk	Peak ⁺	
730 (Initial full- power inlet temperature)	+56% Above 730°F Isothermal Conditions	Doppler* (-43%); Core Radial Expansion** (-9%); Core Axial Expansion*** (-4%)	950	1100	1190	4
600	+110% Above 600°F Hot Shutdown Isothermal Conditions	Doppler* (-72%); Core Radial Expansion** (-33%); Core Ax- ial Expansion*** (-5%)	970	1200	1350	~9
400	+202% Above 400°F Cold Shutdown Isothermal Conditions	Doppler* (-133%); Core Radial Expansion** (-60%); Core Ax- ial Expansion*** (-9%)	1030	1300	1525	~19

* Doppler Constant $T \frac{dk}{dT}$: - 27.0 x 10⁻⁴ (fuel); - 55.9 x 10⁻⁴ (blanket)

** Core radial expansion reactivity feedback determined by a) thermal expansion to fill Above Core Load Plane gaps and, b) increase in the radial expansion of the outer core perimeter caused by changes in the core former ring temperature.

*** Core axial expansion reactivity coefficient: -0.2%/°F.

+ Coolant peaking factor 1.25.

1.2.4 Response of CRBRP to Loss of All Off-Site and On-Site AC Power (Station Blackout)

CRBRP was designed with four AC transmission lines from three original sources. This is significantly more redundancy than provided for most nuclear plants. Thus, loss of all off-site AC power for a significant time is very unlikely, based upon historical data on power sources of this type. However, three diesel generators were provided in the design to provide on-site AC power for safety functions. The loss of all AC power for any significant length of time is thus extremely unlikely.

The CRBRP was designed with four separate decay heat removal paths. Three paths were through the three normal heat transport loops and one was through a Direct Heat Removal System (DHRS). The Direct Heat Removal System uses only liquid metal and air as heat exchange mediums but requires AC power for operation. Thus, this one heat removal path would be lost in the event of station blackout. The other three loops operate on natural circulation and require no AC power. As described in detail below, feedwater makeup is required even in this natural circulation mode. The requirement is significant for the first one to two hours. After that, only leakage need be made up. One feedwater pump is driven by a steam turbine. The only electrical power requirement is for DC power to the controls. The design incorporated backup battery power for control for at least two hours as required by the NRC. If AC power were not restored after two hours, the controls could be manually operated for the limited makeup water requirements that would be needed after that time. The following describes the sequence of the event in more detail.

This event assumes loss of all AC including diesel AC power. Under this scenario, the principal safety criteria imposed are (1) to preclude the onset of coolant boiling and (2) to limit the bulk sodium coolant temperature to 1150°F (based on acceptability of material properties in the coolant boundary for the longer term transient).

Figure 1-6 is a schematic of the CRBRP natural circulation system. The DHRS is not shown here because under the station blackout assumption, the DHRS would be inoperable and unusable to provide decay heat removal. Decay heat removal is to be provided by the natural circulation capability within the three independent main heat transport loops.

The total loss of off-site and on-site AC power would result in a reactor scram in which the pumps trip, the pony motors would not engage and the pumps would coast down to natural circulation flow (coastdown is on the order of 130 seconds). It is important to note that during these first few minutes nothing is critically happening to the reactor bulk coolant temperatures. There is an inherent heat capacity in the coolant as shown in Figure 1-7. This figure shows that, even with no heat removed whatsoever, the primary coolant heat capacity is sufficient such that the bulk sodium would barely reach the 1150°F conservative limit in four hours. Still assuming no heat removed from the bulk sodium but allowing the intermediate system coolant to take heat from the primary sodium, which it would, would significantly increase this time as shown by the lower curve of Figure 1-7. In any realistic station blackout scenario, steam generator system water is available to remove heat, so the sodium temperature never increases after the first couple of minutes. The data plotted in Figure 1-7 is thus not relevant, but the figure is provided to demonstrate that there is substantial inherent heat capacity in CRBRP.

Figure 1-8 provides a plot of the average reactor (fuel) outlet temperatures (nominal) as a function of time for the reactor scram from full power to the natural circulation condition. This more realistic case shows that average outlet temperatures peak at less than 100°F above normal operating condition and are then well below the limiting boiling criteria (approx. 1700°F). The bulk sodium temperature scarcely increases at all (for details see Reference 4).

All the heat removal functions are automatic; no immediate operator action is required. In fact, failed positions of all valves and louvers are such as to put them in the heat removal mode.

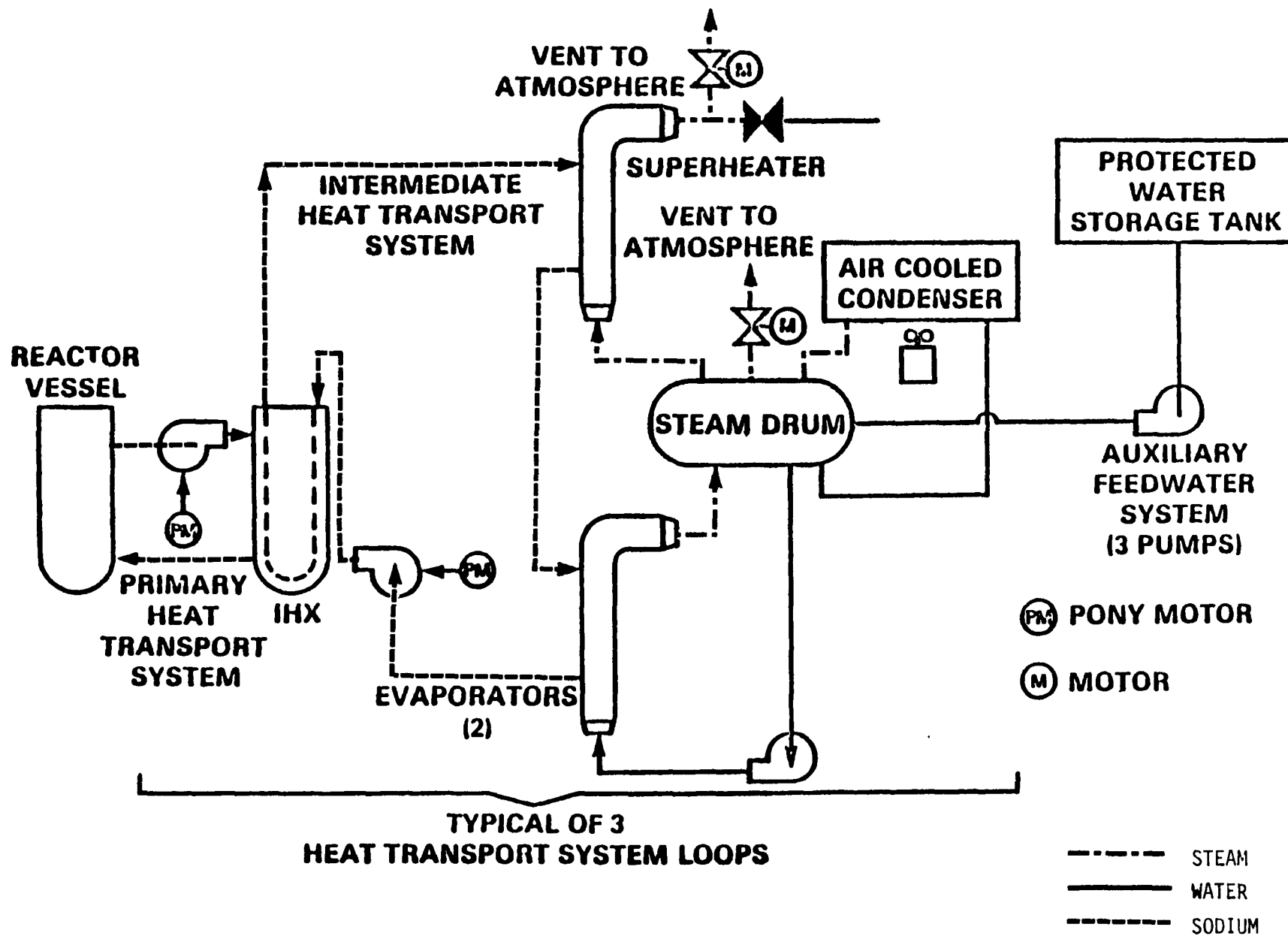


Figure 1-6 Natural Circulation

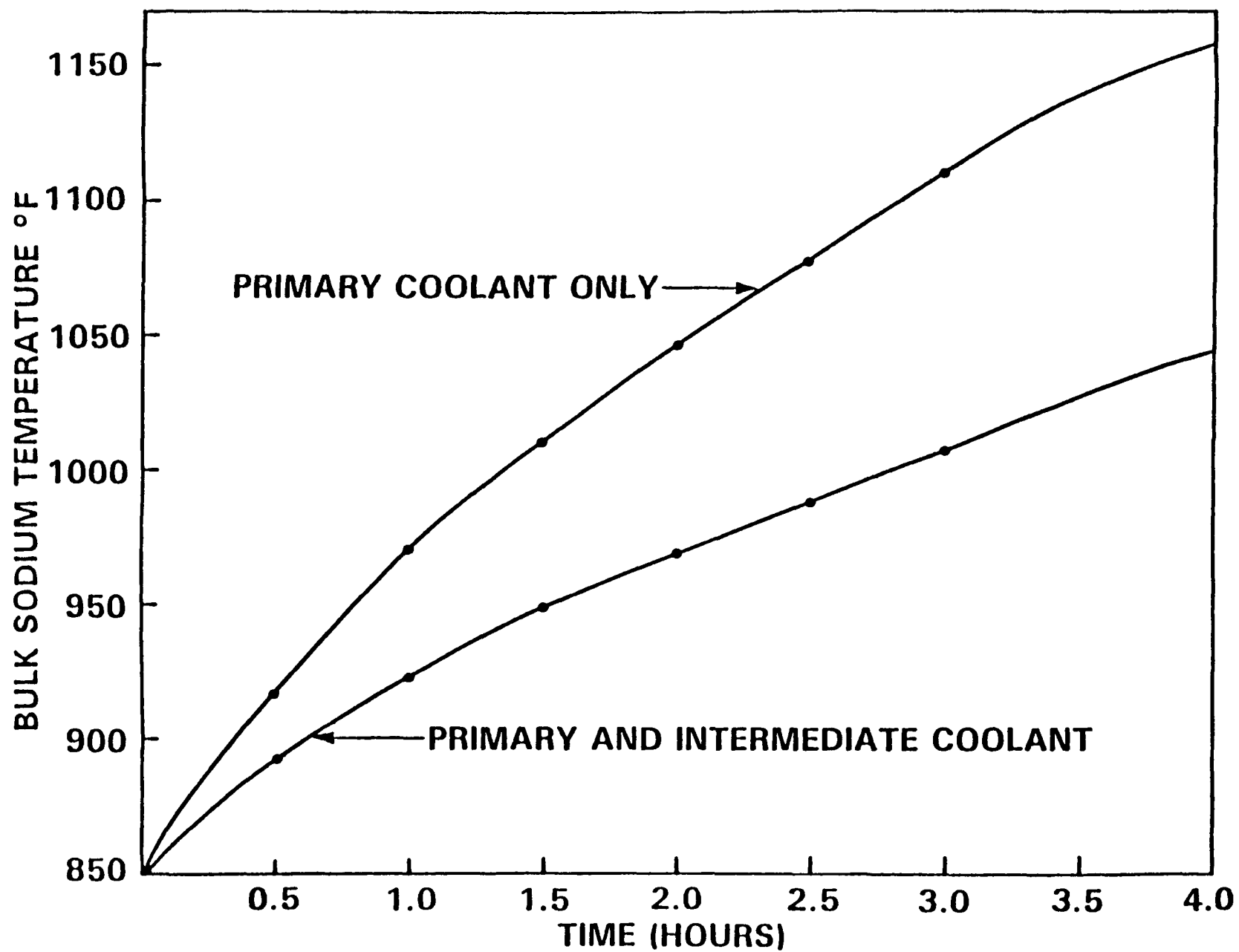


Fig -7 CRBRP Loss of Heat Removal

AVERAGE REACTOR (FUEL)
OUTLET TEMPERATURE (NOMINAL) °F

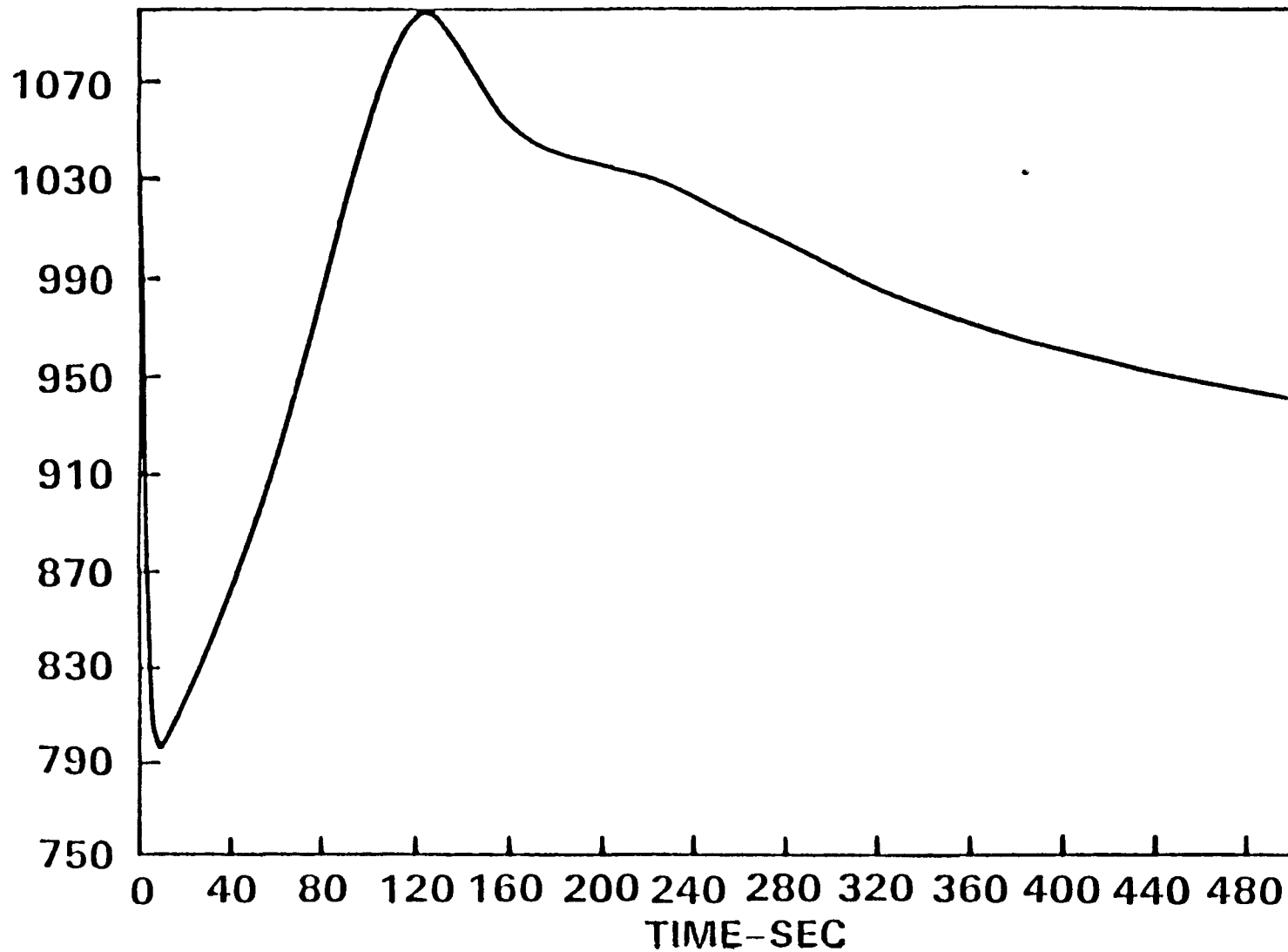


Figure 1-8 Reactor Scram From Full Power
To Natural Sodium Circulation

Following pump coastdown, with no AC power, decay heat removal from the reactor is accomplished by natural circulation flow, through the primary, intermediate and steam generator systems.

The safety grade Protected Air Cooled Condensers (PACCs) are sized to remove 45MW of heat with forced air from blowers and 15MW of heat by natural circulation of water from the steam drum and natural air draft through the PACCs. (Actually, the water side of the PACCs operate as reflux condensers which is more efficient than natural circulation, but that fine point is generally not mentioned).

The PACCs are automatically put into operation upon scram and begin removing heat immediately. However, the maximum decay heat power is above 15MW for about one hour.

If only the PACCs removed decay heat, the sodium temperatures would increase for about one hour to a bulk temperature of about 940°F and then very slowly decrease. However, there are power relief valves from the steam drums and from the outlet of the steam generators as shown in Figure 1-6. These are automatically activated by the steam pressure generated by the increased steam-water system temperature. The automatic (and intermittent) actuation of these valves serves to remove most of the heat for the first hour and begins to reduce the sodium temperatures immediately.

By the end of roughly one hour, the decay heat generation is less than the PACCs capacity on natural circulation. However, the system temperatures are still above standby temperature and the vent valves continue to operate intermittently for about another hour. By the end of two hours the system temperatures are low enough that the PACCs can remove the decay heat and establish the standby temperature with no further heat removal by steam venting from the vent valves.

Obviously significant make-up water is required to be supplied to the steam drums during the venting period. Following a normal scram with AC power available, there are two motor driven pumps and one steam turbine driven pump

available to supply make-up water to the three steam drums. The system is configured so that any pump can provide sufficient feedwater to all three steam drums to make up the water lost through venting.

Without AC power the steam driven turbine is the source of pump power to provide the auxiliary feedwater for make-up. The controls for this operation are automatic. Valves are actuated to pump water to the steam drums upon sensing a low water level and turn off upon sensing a high water level.

The sensors, controls and valves are all battery operated. The design incorporates a safety grade battery power supply adequate to provide DC power for all safety systems, including the decay heat removal system, for at least two hours.

Thus the system operates automatically, without operator action, to safely remove decay heat for at least two hours. At that time the system temperatures and heat loads are such that the decay heat removal will continue with only natural circulation of the primary and intermediate sodium loops, natural circulation of steam drum steam/water through the PACCs and natural draft air circulation through the PACCs. Figure 1-9 shows the bulk sodium temperature during the first two hours. If there were no loss of water, the system would continue operating without operator action in this mode indefinitely. With no control on the PACC air flow control louvers, however, the system temperatures would continue to slowly decrease over months as the decay heat source decreased.

There is loss of water from the system, however, from leakage, primarily through the vent valves. Thus, even after the first two-hour period the steam driven turbine must provide make-up water to the steam drums; rarely but occasionally. In addition to the battery operated water level sensors, the steam drums have sight glasses which can be read manually - the only power requirement being perhaps a flashlight. The valves which must be operated are manually operable. They are located in an accessible area near the turbines, in cells in which the maximum temperatures, based on conservative calculations, will reach 140°F within about 30 hours under blackout conditions

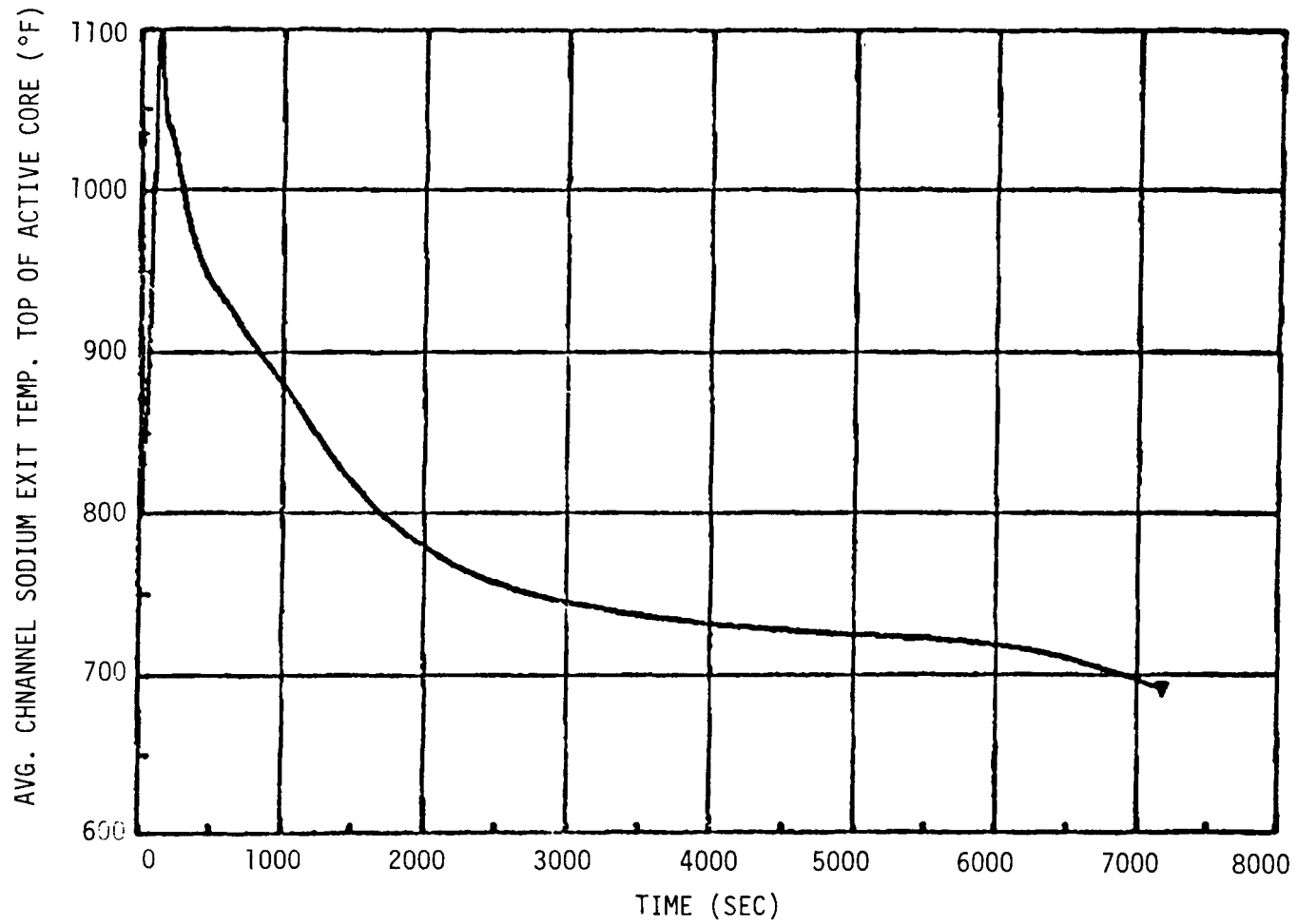


Figure 1-9 Bulk Na Temperature, Reactor Scram From Full Power to Natural Sodium Circulation

(see Figure 1-10). Thus for at least 30 hours two operators could go to their posts periodically, one to the steam drum sight glass, and the other to the valve, and maintain the proper water level. This is the only operator action required to maintain natural circulation cooling. Leaving the steam drum water level at the high level position at the end of 30 hours would obviously provide a capability to continue operation much longer than 30 hours, because the leakage rate is very low at that time, and make-up water would not be required for some time. Thirty hours is sufficient time to restore off-site power, correct the diesel problem, or even bring in a new diesel if necessary. Within 30 hours even backup batteries sufficient to operate the feedwater controls and valves could probably be found. The design was thus based upon no more than a 30-hour manual operation.

The safety-grade Protected Water Storage Tank (PWST) that supplies the needed water was sized to meet a 30-day mission on the PACCs assuming some leakage in that system and assuming the worst case leakage from the valves. In addition, the worst case leakage in the pumps seals was also assumed as part of the criteria to size the 30-day tank volume. There is also an alternate water supply available from the condensate storage tank (250,000 gallon capacity) although it is not safety grade. It has a gravity flow transfer path to either the protected water storage tank or to the suction of the auxiliary feedwater pumps. Thus, sufficient water supply is readily available to assure the long term natural circulation heat removal capability without resorting to the bringing in of water by pumping from the river or by a fire truck, both of which could be used in an emergency situation.

In summary, very conservative calculations show that the CRBRP plant design provides sufficient decay heat removal under station blackout conditions to maintain sodium coolant (and fuel cladding temperatures) well below the established safety criteria and for an extended and adequate period of time.

1.2.5 Outstanding Changes to Auxiliary Feedwater Supply

The baseline design of CRBRP shows that the steam turbine driven pump is turned on at shutdown and runs continuously during the shutdown event. Water is pumped from the PWST and back to the PWST as long as the steam drums don't

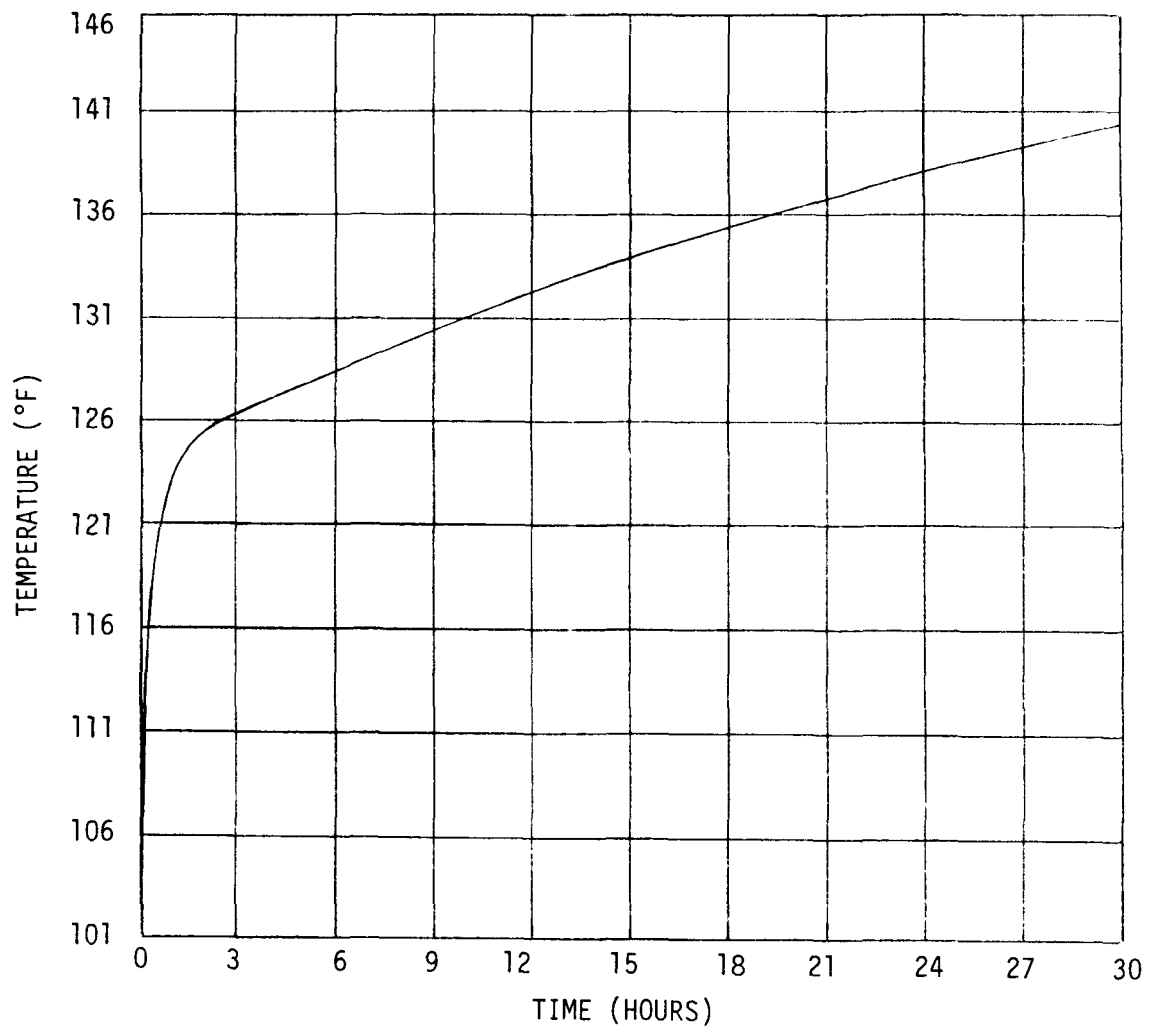


Figure 1-10 Cell Temperature as a Function of Time
After Loss of Cooling (Station Blackout)

need water. When the steam drums need water, valves are thrown to divert water from the circular loop into the steam drums. That mode of operation is fine for a battery control system that can switch on and off. However, the loss of steam in driving the steam turbine results in extra make-up water requirements and thus more frequent manual servicing after battery power is lost.

At the time of CRBRP termination, an Engineering Change Proposal (ECP) was being prepared by General Electric Co. to make the turbine run only when make-up water was required. The possible degradation in reliability of the steam turbine as a result of a greater frequency of cycling had been evaluated and found acceptable. The ECP had in principle been approved by General Electric Co., Westinghouse and the CRBRP Project Office. It involved only a single control change, involving essentially no cost, other than the reliability verification noted above. The ECP would have been put in place in late 1983. The description above is essentially unchanged with either mode of operation with the following exceptions. The 30-day water supply of the PWS1 is for continuous turbine operation: it would obviously be much longer for the intermittent mode of operation. The statement that leaving the steam drums at high water level at the end of 30 hours would provide for much longer operation is relative. With the change implemented, as should be assumed for all analysis, the time before needing further action could be very long. In fact, if the leakage rate from seals was as low as might be expected it is likely the system could continue to operate without operator assistance until the cells cooled down enough to allow routine personnel entry into the cells containing the valves and steam drums.

1.2.6 References

1. W. H. Alliston, et al., "CRBRP; LMFBR Demo Plant Simulation Model (DEMO)", CRBRP-ARD-0005, February, 1978.
2. J. V. Miller and R. D. Coffield, "FORE-2M: A Modified Version of the FORE-II Computer Program for the Analysis of LMFBR Transients," CRBRP-ARD-0142, November, 1976.
3. R. D. Coffield, et al., "Buoyancy Induced Flow and Heat Redistribution During LMFBR Decay Heat Removal," Proceedings of Specialists Meeting on Decay Heat Removal and Natural Convection in FBR's, Brookhaven National Laboratory, NY, February, 1980.
4. W. J. Severson, R. R. Lowrie, R. D. Coffield, R. A. Markley, "Summary Report on the Current Assessment of the Natural Circulation Capability with the Heterogeneous Core" CRBRP-ARD-0308, February 1982.

1.3 Decay Heat Removal Using Natural Circulation

1.3.1 Introduction

The CRBRP has been designed to provide for decay heat removal through the steam generator system (SGS) with forced circulation of sodium in both the primary and intermediate heat transport systems (PHTS, IHTS) in at least one of the three heat transport loops. Due to the redundancy and diversity of power supplies providing power to the pony motors, there is very little likelihood that there would be a need for removing decay heat by natural circulation [1, 2]. The capability to remove decay heat by natural circulation is a desirable feature of LMFBRs in general and the CRBRP specifically. As no electrical power is required to provide adequate cooling flow in the heat transport system loops, the inherent safety of the plant is significantly enhanced. Therefore early in the design, natural circulation was made a design requirement on the CRBRP. This requirement has impacted the design in several ways, including the component arrangement and certain hydraulic requirements. The inherent safety of the plant provided by these design considerations has therefore been a significant positive addition to the CRBRP interaction with the federal regulatory agencies.

Although one could conceivably define other scenarios for the plant to be in the natural circulation mode of decay heat removal, the mechanism requiring the least number of failures would be a loss of AC power to the plant. The assumptions made for component and overall plant responses to a natural circulation event therefore are consistent with this loss of AC power. The only plant power that is assumed to be available is the Class 1E battery power supplying power for the plant controls. The batteries supplying this power, are designed to supply the required power for two hours following event initiation. From that time on, the plant control function could be provided by operators at local control stations in the plant. It is the project position that a two hour natural circulation event is adequate time to reestablish forced circulation in the plant.

The worst case plant conditions, which have traditionally been used for evaluation of the natural circulation decay heat removal capability of an LMFBR, provide only a partial verification of the capability of a plant to remove decay heat by natural circulation. Although the worst case conditions provide the basis for evaluation of the early transient, coastdown and transition to natural circulation cooling which is important for safety evaluations, the nominal and best case analyses provide the information required to assure that adequate controls and design margin exists for the long term natural circulation decay heat removal condition as well as for the most likely plant operating conditions. As all plants operate with "minimum" decay heat after refueling and with nominal decay heat for most of the design life, the capability of the plant to undergo the transition to and stable operation at natural circulation operating conditions can only be verified by analysis at these varied conditions. The assumptions used in the worst case safety/design evaluations are discussed in References 3 and 4. The worst case operational assumptions are summarized in Table 1-4.

The criteria for evaluation of these transients logically follows the definition of the conditions for analysis. The criteria for acceptability of the response of the plant to the transition to and operation in the natural circulation decay heat removal mode falls into two separate categories. The first is defined by the safety considerations and the second is defined by prudent engineering design for plant operation. From a safety standpoint, the acceptability of the response of the plant is judged solely on the resulting temperatures seen in the core during the transient. Experimental evidence has shown that at low powers and flows, stable boiling can exist within the core without adversely impacting natural circulation or the safety of the plant. This evidence has been used as a safety margin and CRBRP has used the criterion to conservatively limit the maximum core hot spot temperatures (in fuel and blanket assemblies) to less than sodium saturation temperature. For the CRBRP, this temperature is approximately 1720°F at the top of the active core under zero flow conditions. The maximum hot spot temperature in the worst assembly was compared against this boiling limit. This evaluation neglected the beneficial effects of core inter- and intra-assembly flow and heat redistribution which provides further margin. This redistribution would tend to equilibrate the temperatures across the core [5] making the hot spot

TABLE 1-4
OPERATIONAL ASSUMPTIONS

<u>Parameter</u>	<u>Assumption</u>
o Plant Initial Conditions.	Thermal Hydraulic Design Values Plus 20°F.
o Decay Power.	End of Equilibrium Cycle Plus Uncertainty.
o Flow Decay During Cooldown.	Most Rapid.
o Pump Locked Rotor Resistance.	Maximum.
o Cold Leg Check Valve Pressure Drop.	Maximum.
o Reactor Pressure Drop.	Maximum.
o Buoyancy Effects.	Not Included.
o Trip Function.	Maximum Delay and Minimum Rod Worths Minus Uncertainties.
o Hot Channel Analysis.	3 σ Hot Channel Factors Assumed.
o Protected Air-Cooled Condenser Operation.	Not Included.

analysis conservative for natural circulation analyses. From the prudent design standpoint, the plant design criteria is less easily defined. Stability is the governing concern for this evaluation. For example, the relative primary, intermediate and steam generator system flows define the thermal centers and therefore contribute to defining the flows during the transition to and operation in the natural circulation mode. During the coastdown, the thermal centers should be driven to the top of the heat exchangers to maximize the natural circulation driving heads. To accomplish this for the intermediate heat exchanger (IHX), the intermediate flow should be higher than the primary flow. In the same light, the steam generator system recirculation flow should be high enough to drive the thermal center to the top of the evaporator units, thus maximizing the intermediate thermal driving head. The steam generator heat sink must be controlled to provide a stable operating condition to assure that temperatures do not oscillate enough to impact natural circulation by introducing the potential for reducing the thermally induced flows or in the limit for cold trapping. Although, these considerations are important in the evaluation of the plant decay heat removal by natural circulation, the governing consideration for acceptability is the safety criteria for maintaining a core coolable geometry, although core lifetime may be adversely impacted at temperatures of approximately 1700°F or higher.

The large temperature difference between the hot and cold legs in both the primary and intermediate systems makes natural circulation in these loops inherent. Therefore, design decisions which enhance this capability have been made which make this inherent capability adequate for plant decay heat removal under off normal conditions. The potential enhancements to natural circulation fall into two distinct areas: (1) thermal driving head versus system pressure drop considerations, and (2) a smooth transition to and during operation in the decay heat removal mode. The actions taken to promote natural circulation decay heat removal for CRBRP in these two areas are discussed below.

The most obvious consideration in promoting natural circulation decay heat removal is having an increasing thermal center distance from the source to the heat sink. The relative elevations of the IHX to the core, the evaporators to

the IHX, and the steam drum to the evaporators for CRBRP are shown in Figure 1-11. Increasing these thermal center differences increases natural circulation flows, however it must be limited by the construction cost increases. The thermal center differences are not static during the transition portion of a natural circulation event. The control of the thermal driving head during the transition to and operation at natural circulation decay heat removal conditions will be discussed later. The thermal driving head must be balanced against the system pressure drops. The most significant of these resistances in the PHTS are the reactor, pump and Cold Leg Check Valve (CLCV). The reactor pressure drops are primarily governed by the core design, however, the low flow resistance was taken into account in the design. It should be noted that the dynamic resistance is important to the evaluation of the plant. Intra- and inter-assembly flow and heat redistribution act to mitigate local temperature effects in the core, however they also modify the system resistance characteristic vs total flow. The dynamic resistance is higher than would be calculated if fixed flow fractions were considered. The development of the dynamic core pressure drop characteristic is discussed in Reference 6. The pump pressure drop is defined by the locked rotor resistance of the pump impeller after the pump stops. Therefore, the time at which this resistance is added to the system (coastdown time) plays a significant part in determining the loop flow rate and therefore in determining the response of the plant to a natural circulation event. The coastdown effect will be discussed later in this report. The locked rotor resistance of the pump must be balanced against the operating consideration of a loss in pump efficiency as the passages in the pump are opened to reduce the locked rotor resistance. The CLCV pressure drop is controlled by the free hang angle of the disc. The CLCV pressure drop, along with the remainder of the pressure drops used for this analysis, are discussed in Reference 4. The IHX and piping in the PHTS provide a smaller percentage of the system pressure loss and therefore design considerations for 100 percent power operation are sufficient for natural circulation pressure drop considerations.

The second area of consideration in promoting natural circulation is to assure that a smooth transition can be made from forced to natural circulation and that no automatic control action is taken which could be detrimental to the system pressure drop or thermal driving head. The first area that design

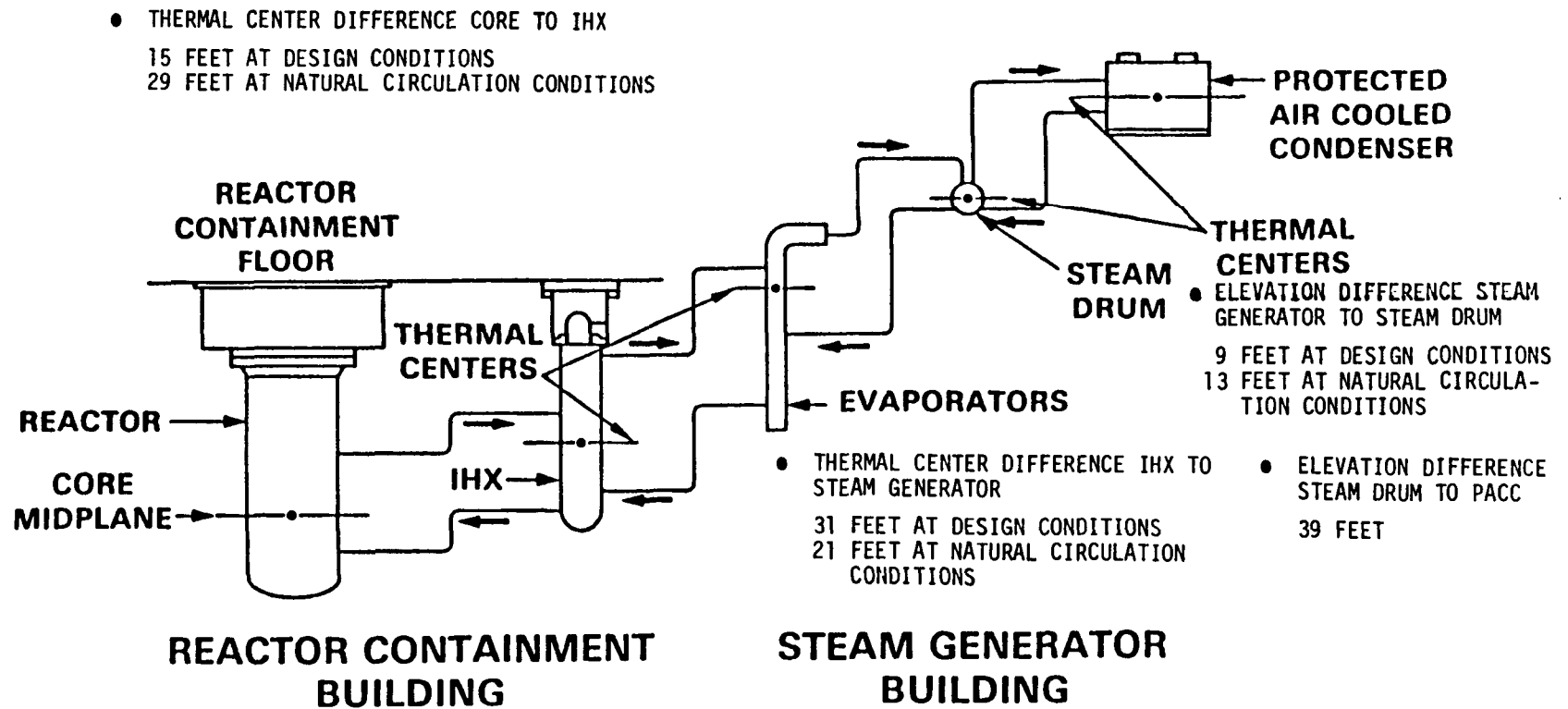


Figure 1-11 The Elevation Differences in Major Components Provide A Natural Circulation Capability for CRBRP

action can be taken is by tailoring the pump coastdown. This tailoring of the coastdown reduces the power to flow ratio during the transient. The reduction in power to flow ratio occurs because of two effects. First by extending the coastdown, it delays the time at which the locked rotor resistance of the pump is added to the system losses. This increases the integral of flow versus time during the transition to natural circulation and therefore increases the sensible heat that is removed from the core/reactor. Secondly, the delay decreases the decay heat load that must be removed from the core by natural circulation flows. This reduction in decay heat load reduces the core temperatures that result as the thermal driving heads are reestablished following the coastdown. The intermediate pump coastdown is also tailored such that the intermediate system flow is higher than the primary flow at the end of the coastdown, therefore driving the thermal center in the IHX to the top of the bundle at the transition from forced to natural circulation flows. The IHTS pressure drops vs thermal driving head must also be specified such that they result in natural circulation flows which are higher than the PHTS assuring that the thermal center remains at the top of the unit. The only control action that takes place during the operation in the natural circulation decay heat removal mode is control of the heat sink (Steam Generator Auxiliary Heat Removal System, SGAHRS). This control is required to assure that the heat sink remains available and that it does not adversely affect the continued removal of decay heat by natural circulation. The design of the SGAHRS and its controls are such that the smooth transition to and operation in the natural circulation decay heat removal mode are maintained. The Auxiliary Feedwater (AFW) is added to the steam drum via the AFW sparger in the steam space. This aids in the system depressurization and heats the AFW from 70°F to saturation temperature mitigating the thermal transient on the system. The impact of the sparger can be seen in comparing References 3 and 4 which show the natural circulation transient before and after the AFW sparger was added to the design. As will be discussed later in this report, the control of the SGAHRS steam vent, Turbine Drive Auxiliary Feedwater Pump (TDAFWP) and Protected Air-Cooled Condenser (PACC) play a significant role in the control of the continued stable decay heat removal operation in the natural circulation mode.

1.3.2 Evaluation Evolution

The importance of natural circulation to the safety evaluation of the CRBRP has been a major point of study since 1976. The importance of natural circulation decay heat removal to CRBRP was documented in Reference 3. Because of the importance of natural circulation to LMFBRs, a Natural Circulation Verification Program (NCVP) was initiated in 1976 [7]. A series of studies to define the importance of some of the parameters to the natural circulation event was completed. These included fluid stratification effects, coastdown effects, loop heat capacity effects, etc., as well as a series of comparisons to operating plants such as EBR-II and FFTF. Based on the maturity of the understanding of the parameters which are important to natural circulation predictions, the NCVP was updated in May 1982. The results of this overall program are discussed in Reference 6. The NCVP was sponsored by both the Base Technology Program and the CRBRP Program. The Reference 3 evaluation of natural circulation in CRBRP provides the basis for comparison of all analysis which has been performed. The Reference 3 conclusion that, from a safety standpoint, no condition analyzed was more severe than the three loop full power operation to three loop natural circulation transient has permitted a limited evaluation of natural circulation in LMFBRs when performing safety evaluations for the three-to-three case. The detailed core and plant analysis, which was performed with DEMO, COBRA-WC, and FORE-2M, also demonstrated the applicability of a general criteria of 1600°F hot spot temperature in the core as predicted by DEMO was applicable in assuring that no local boiling occurred in the core during the event. Although this preliminary analysis was based on a homogeneous core design, the same conclusions were reached in the updated analysis performed on the heterogeneous core design [4]. The conclusion of the analysis is that the CRBRP design provides adequate margin for decay heat removal in the natural circulation mode.

The only tasks remaining after the determination of the acceptable operation of the plant from a safety standpoint using conservative analysis were to determine the operational characteristics that would assure that stable and controlled decay heat removal continued and to complete the verification of the analysis tools used in these predictions. As noted earlier in this

report, the Reference 6 Breeder Reactor Natural Circulation Verification Program for DEMO final report was issued in September 1982. This report covered the base technology verification program for DEMO. However, the NCVP for CRBRP [8] had not been completed at the termination of the program. The overall NCVP is graphically outlined in Figure 1-12. The direct experimental verification of the codes and methodology on FFTF provided the final verification for the Reactor, PHTS and IHTS. The verification of the predictions for the SGS however had not been completed. Due to the nature of the natural circulation in the SGS (two phase flow) the potential for sufficient flow should not be questioned. However, the stability and accuracy of the predictions do require verification. This verification was planned at the Sodium Components Test Installation (SCTI) facility as an integral part of the overall CRBRP steam generator test program. The test data for portions of the SGAHRS was also not completed at the termination of the program (e.g., PACC).

1.3.3 Present Status

The status of the acceptability of natural circulation for the CRBRP at the termination of the program falls into two distinct areas. First the safety considerations were seen as acceptable to NRC and ACRS pending the completion of the NCVP. Second, the overall acceptability of the plant controls during the natural circulation event were under evaluation.

The acceptability of natural circulation of CRBRP for safety considerations falls into two categories (analysis results and verification of that analysis). The worst case analysis discussed in Reference 4 demonstrated that even if no credit is taken for the intra- and inter-assembly flow and heat redistribution within the core, there is a significant margin to boiling (greater than 150°F at the core maximum hot spot). As noted above, the majority of the NCVP had been completed at the time of project termination. The areas of most concern for the plant analysis were possible impacts of flow stratification, flow streaming and system synergistic effects on both natural circulation in the plant as well as the ability of the codes being used to predict the phenomenon. The most often discussed point was the ability of a one dimensional code (DEMO) to accurately predict the natural circulation flows. Through component and whole plant testing and analyses over a wide

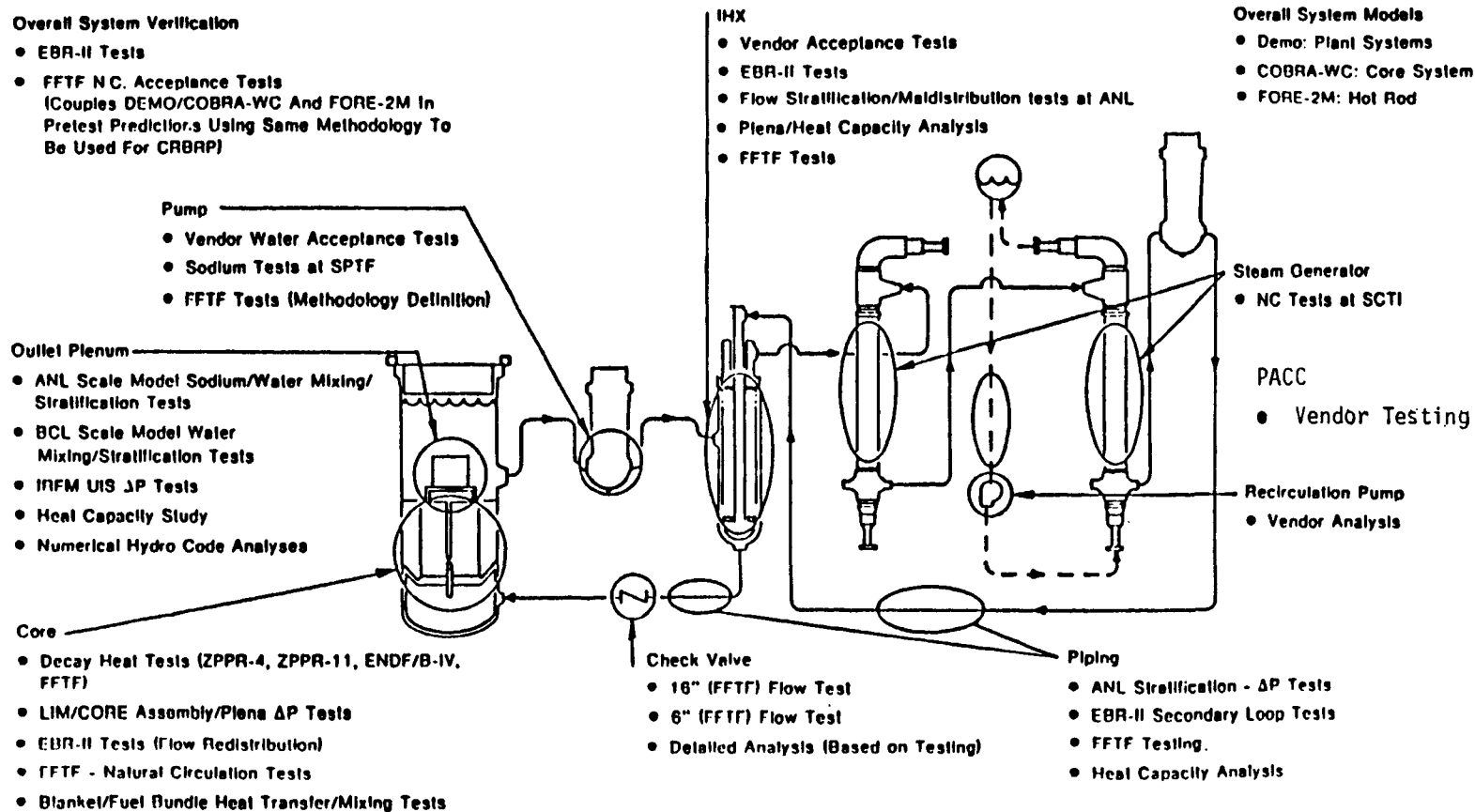


Figure 1-12 CRBRP Natural Circulation Verification Program

range of conditions, each of these areas of concern were shown to be acceptable for the CRBRP. It should be noted that each of the major areas of consideration resulted in a Base Technology evaluation which is generic for LMFBRS but which used CRBRP as a basis. The most significant contribution to the overall verification program was the FFTF natural circulation testing. The pre-test predictions and post-test analyses demonstrated the acceptability of the natural circulation analysis methodology used for CRBRP. The area of most concern for core analysis was the impact of inter- and intra-assembly flow and heat redistribution. This phenomena was shown to provide a considerable effect on peak core temperatures [5] verifying the conservatism used in the evaluation of the natural circulation event.

The overall acceptability of the plant operation in the natural circulation mode was being evaluated at project termination. The most significant finding was the importance of the SGAHRS control system on the stable operation of the plant in this decay heat removal mode either for long term decay heat removal (beyond two hours) or for short term decay heat removal with reduced decay and sensible heat in the plant (nominal or minimum decay heat and/or trip on startup). Worst case analysis would tend to lead the designer toward a simple controller with long time constants for the SGAHRS. However, nominal or minimum analysis shows the need for rapid controlled response to mitigate the transient and reduce or eliminate cycling in the controller. For low decay heat cases, action must be taken relatively quickly to reduce the heat sink. The TDAFWP turbine removes approximately twice as much energy as the PACCs, making the transition to PACCs only decay heat removal an important consideration for design. For minimal decay heat cases, early removal of the TDAFWP turbine from the heat sink is important to assure that the plant is not overcooled. Once deactivated, a delay in restart or an uncontrolled restart can result in system oscillation. This transition from steam venting to PACCs only decay heat removal is critical to assuring stable SGAHRS operation. Figure 1-13 through Figure 1-15 shows the results of a two hour natural circulation transient analysis with a control system as described above which allows cycling. As noted earlier, this is an operational consideration and not a safety consideration as long as the decay heat removal path is maintained. Note that the relatively large swings in temperature at the evaporator exit result in minor IHTS flow oscillations and essentially no

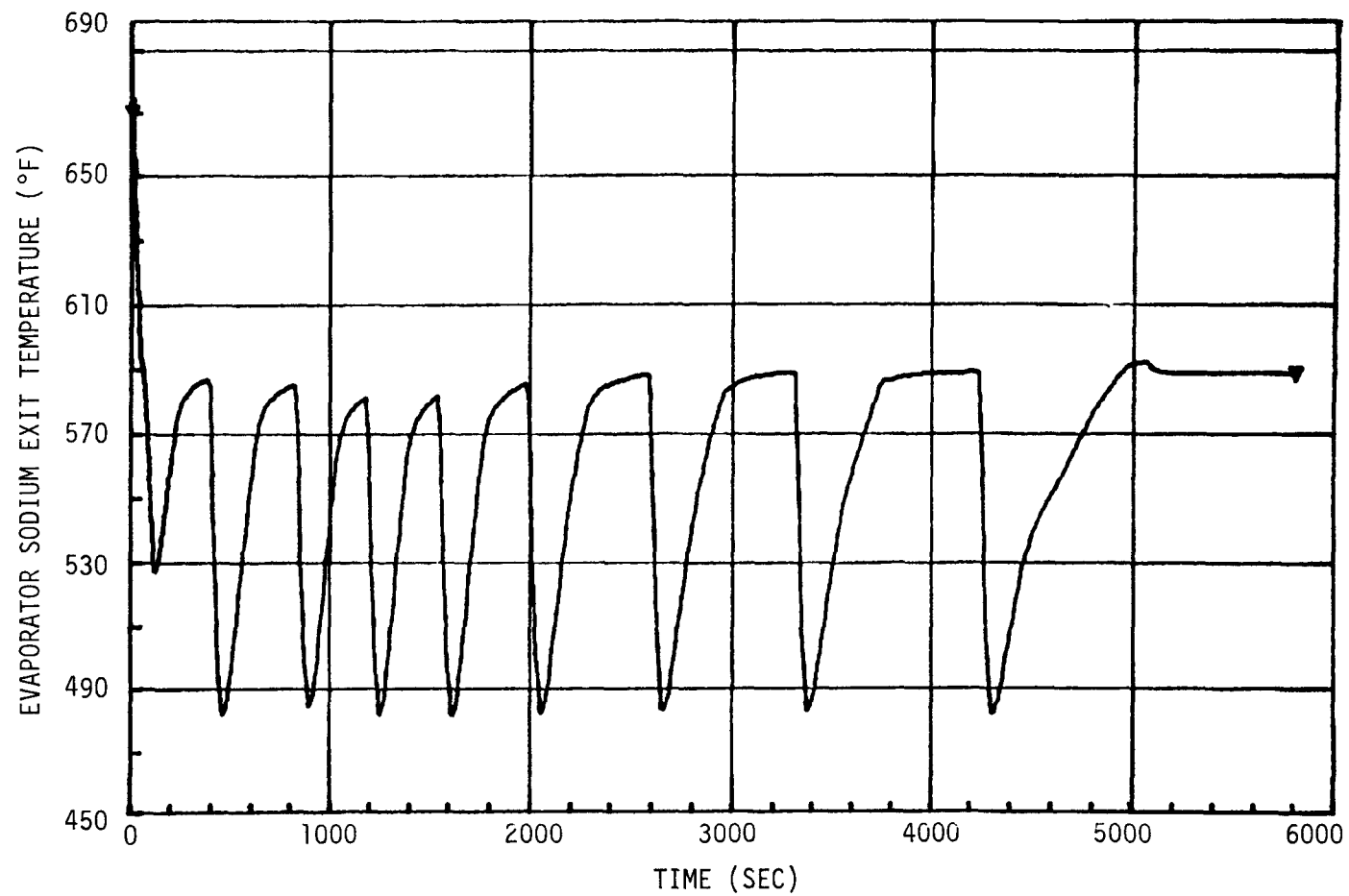


Figure 1-13 Cycling Controller - Temperature Response

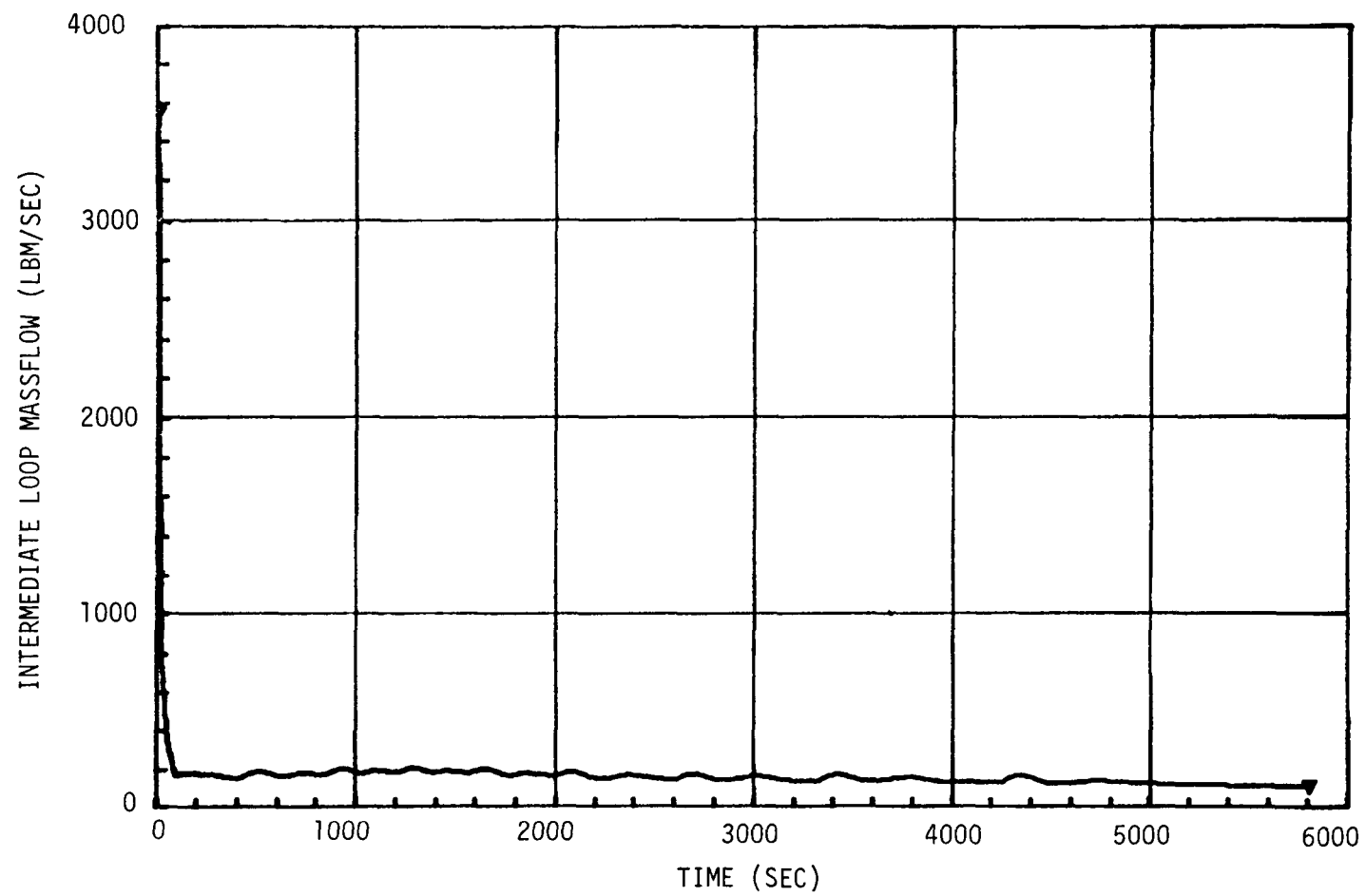


FIGURE 1-14 Cycling Controller - IHTS Flow Response

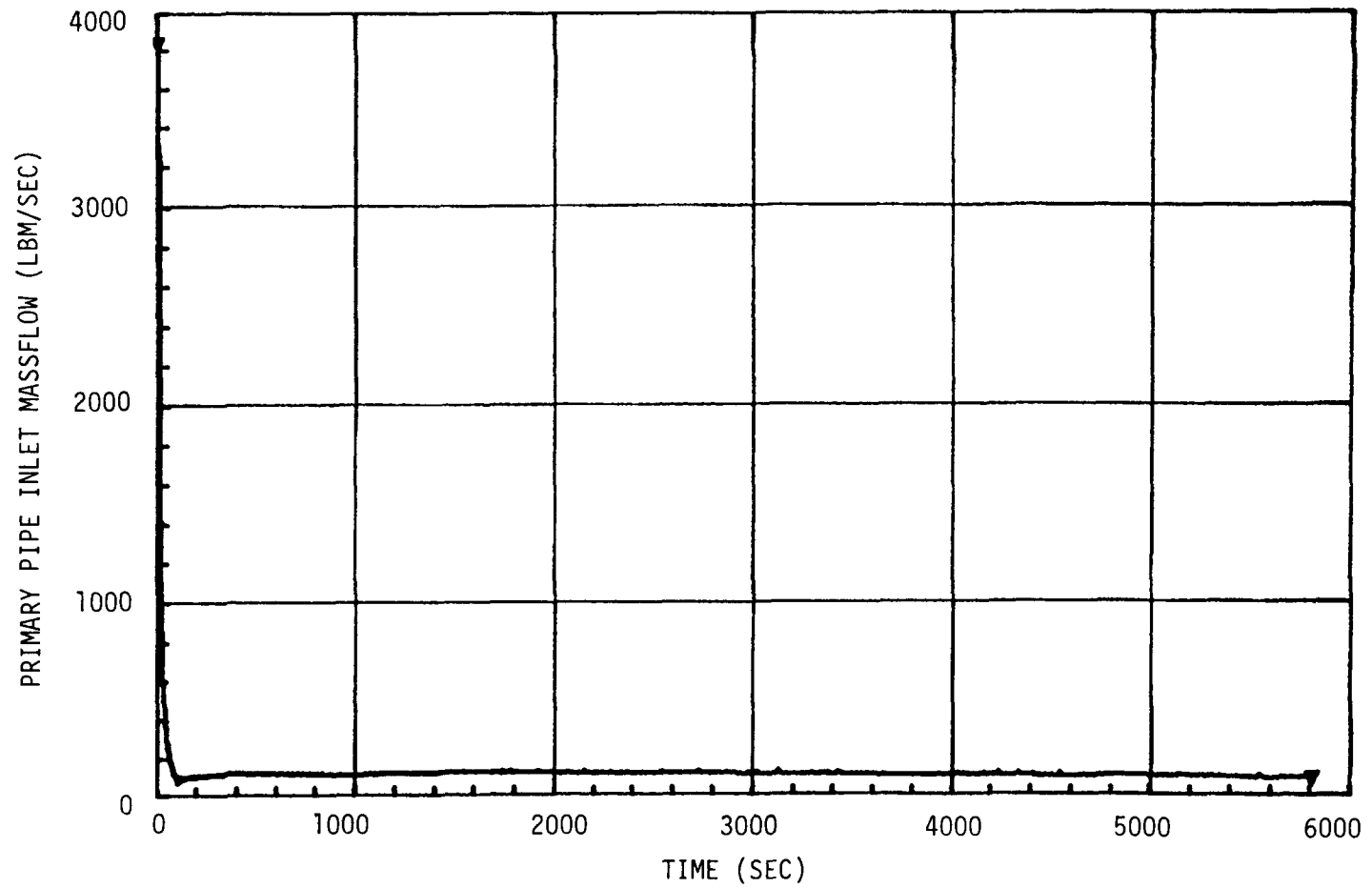


Figure 1-15 Cycling Controller - PHTS Flow Response

impact on the PHTS flow. The transient mitigation afforded by the system heat capacity protects the PHTS from these oscillations. The SGAHRS control system was being redesigned at project termination; however, the last transient presented to NRC, with the SGAHRS control design partially completed, is shown in Figure 1-16 through Figure 1-18. Note that the controller logic had at that time not eliminated the drop in temperature caused by a SGS depressurization. Although this does not affect the safety of the plant, from a design standpoint it was seen as undesirable.

1.3.4 Actions Required to Complete Evaluation

The two areas of effort (other than documenting the entire CRBRP NCVP) that require completion before the natural circulation capability of the CRBRP design could be considered a closed subject are SGS verification and SGAHRS control definition which would allow stable operation under natural circulation operating conditions. Testing such as that proposed for the steam generator at SCTI and for the PACCs would be required to complete this verification. The control system for the SGAHRS (TDAFWP, PACC, AFW control, and vent valves) would have to be completed and analytically tested at a range of possible operating conditions to demonstrated applicability. This would include not only full power nominal, minimum, and maximum transient analysis, but also part power and startup analyses. The particular importance of analysis at the "expected" operating conditions had just become a project concern prior to project termination. It is expected that the modifications to the plant to accommodate the nominal analysis would assure stability for all natural circulation cases which could result in high core temperatures.

1.3.5 Conclusions

In conclusion, the significant effort applied in the Natural Circulation Verification Program demonstrated the acceptability of natural circulation decay heat removal for the most significant areas of concern for CRBRP. The only area that remains to be completed in the overall verification program is the SGS verification, where the two-phase tube side fluid helps assure the adequacy. As the basis for the verification program is testing, this program can offer a solid basis for verifying the inherent LMFBR natural circulation capability to remove decay heat under off-normal conditions.

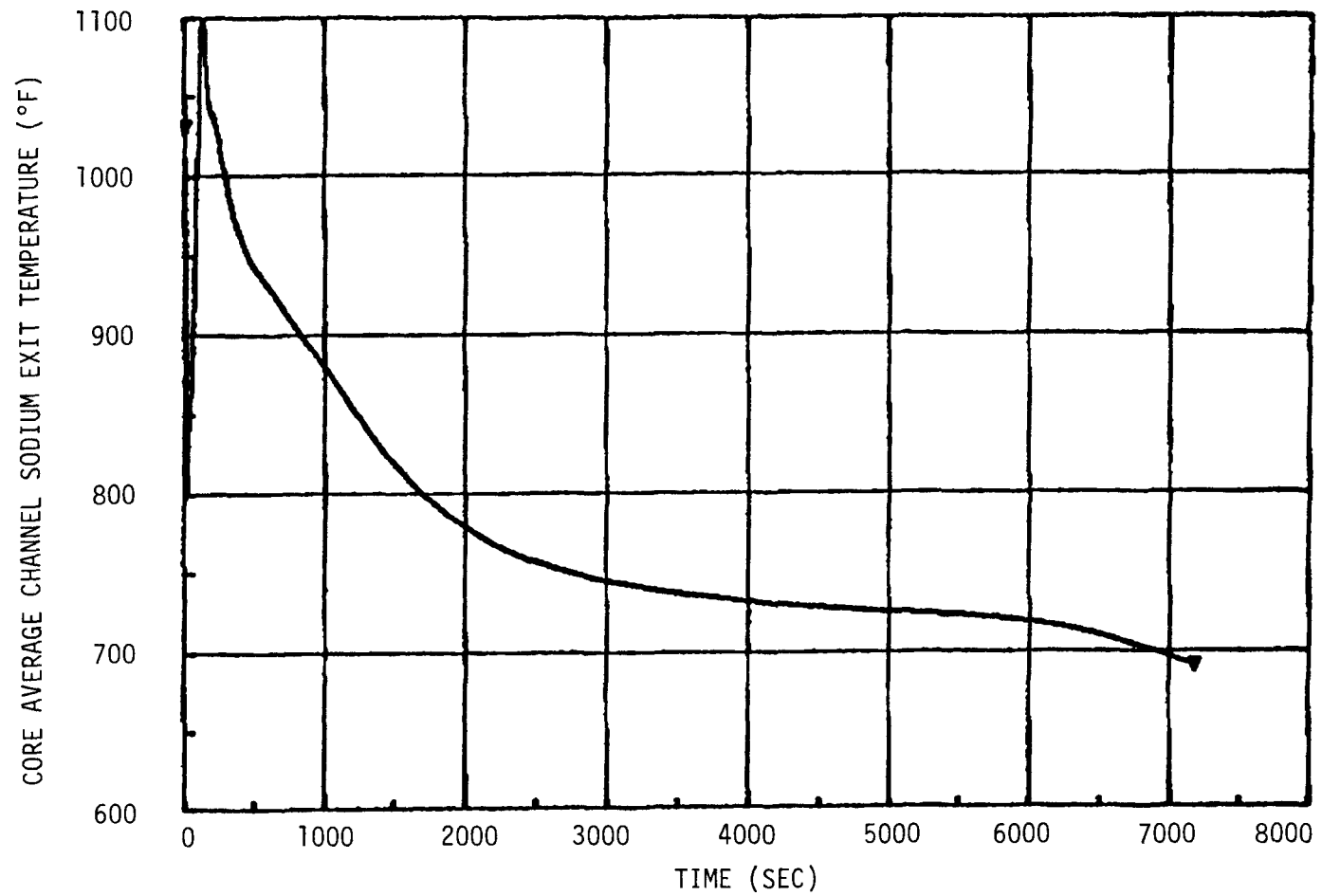


Figure 1-16 Controller - Core Temperature Response

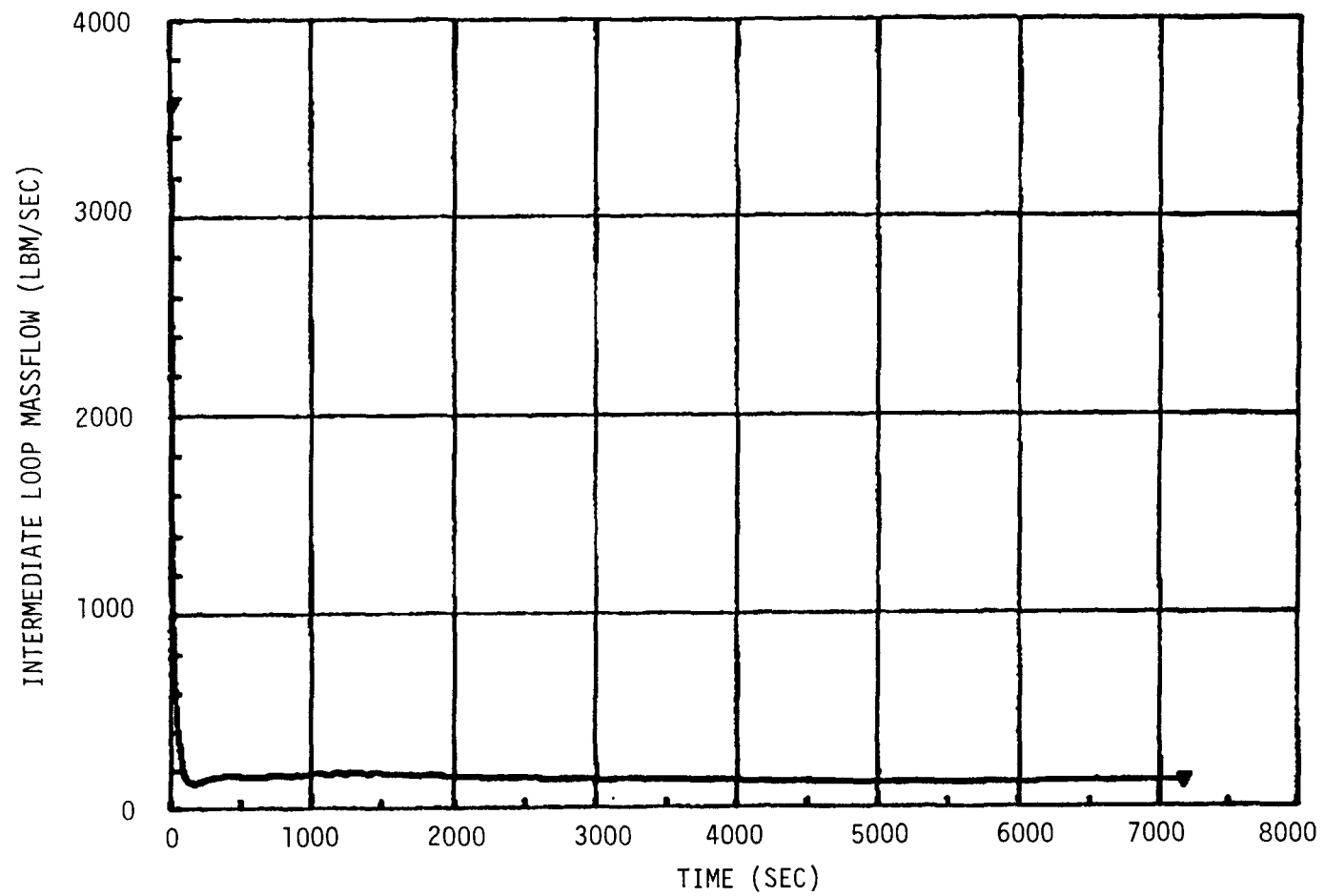


Figure 1-17 Controller - IHTS Flow Response

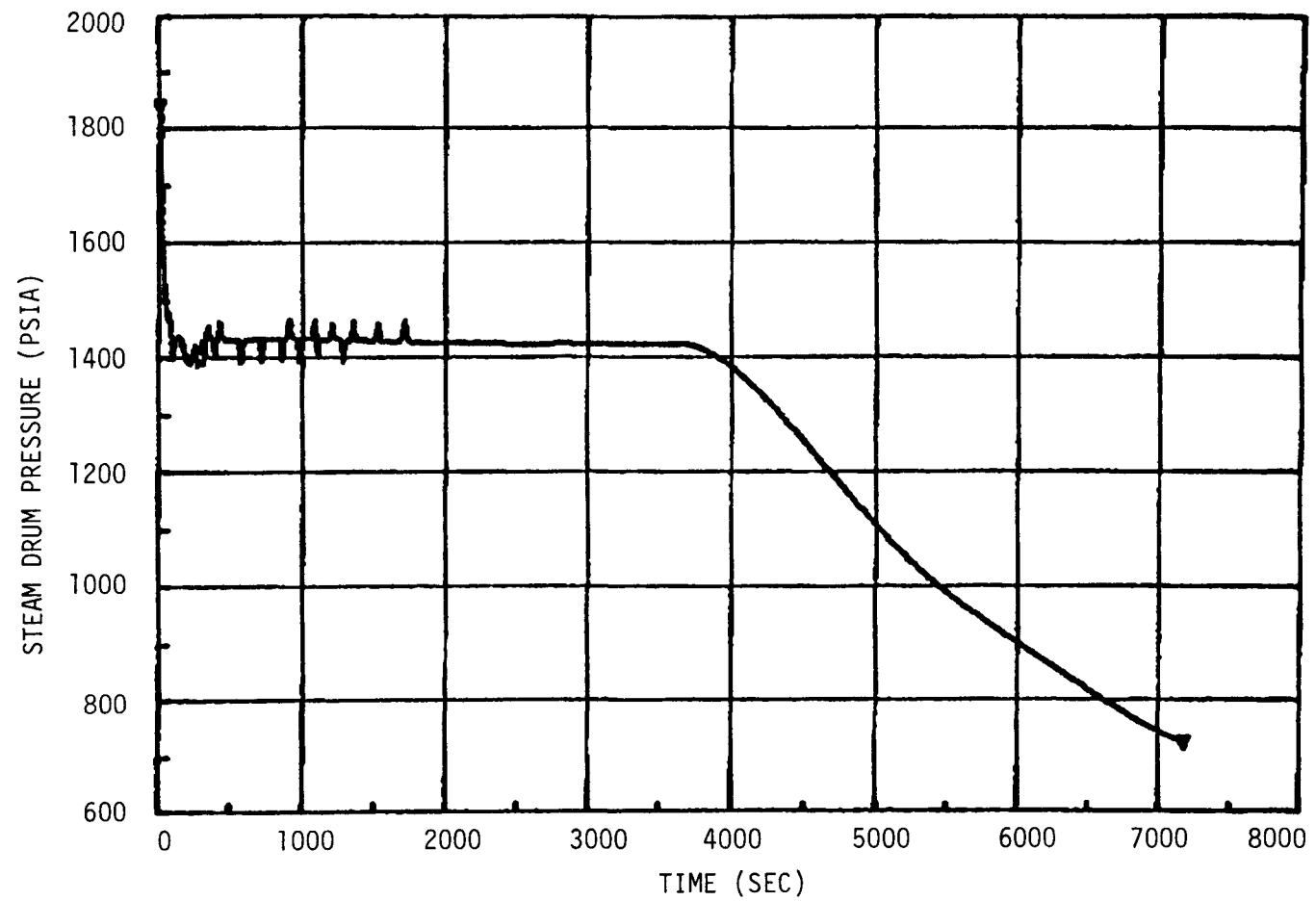


Figure 1-18 Controller - Steam Drum Pressure Response

1.3.6 References

1. CRBRP System Design Description Number 11, "Power Transmission System."
2. CRBRP System Design Description Number 12, "Building Electrical Power System."
3. R. R. Lowrie, W. J. Severson, "A Preliminary Evaluation of the CRBRP Natural Circulation Decay Heat Removal Capability" CRBRP-ARD-0132, November 1977.
4. W. J. Severson, R. R. Lowrie, R. D. Coffield, R. A. Markley, "Summary Report on the Current Assessment of the Natural Circulation Capability with the Heterogeneous Core" CRBRP-ARD-0308, February 1982.
5. R. D. Coffield, et al., "Buoyancy Induced Flow and Heat Redistribution During LMFBR Core Decay Heat Removal," Proceedings of Specialists Meeting on Decay Heat Removal and Natural Convection in FBR's, Brookhaven National Laboratory, NY, February, 1980.
6. A. C. Cheung, "The Breeder Reactor Natural Circulation Verification Program for DEMO Final Report" WARD-NC-94000-7, September 1982.
7. R. D. Coffield, H. P. Planchon, "LMFBR Natural Circulation Verification Program (NCVP) Review of Experimental Facilities and Testing Recommendations" WARD-NC-3045-1, July 1977.
8. J. R. Longenecker to P. S. Check, "Verification of Natural Circulation in the Clinch River Breeder Reactor Plant--An Update", Docket No. 50-537, June 10, 1982.

1.4 Resolution of Metal Joining Problems

From the standpoint of materials and metal working, the various components that will be discussed are quite a step up in technology. Austenitic stainless steel is the baseline material of construction, though use of carbon steels, alloy steels, and several high alloys are also prominent.

Some of these components will be operating in dynamic sodium environments in the high temperature range of 800-1200°F for a design life of 30 years. As a result, these components are designed and fabricated to stringent ASME code and RDT Standards requirements. Fabrication of these components have placed the fabricator in a situation where practical ingenuity and innovative fabrication techniques are necessary.

A number of components have been selected to illustrate innovative fabrication techniques which have been developed to prevent and resolve potential fabrication difficulties. A brief description of the component, weld area, weld process, problem area, and fabrication resolution are discussed in two sub-sections; the first dealing with the resolution of fixturing and fabrication distortions and the second with bi-metallic welds and weld problem resolutions.

1.4.1 Resolution of Fixturing and Fabrication Distortions

This section details fabrication techniques used in:

- o The 20 inch thick weld between the core support plate and core support ring of the Core Support Structure.
- o The attachment weld used to install the Core Support Structure in the Reactor Vessel.
- o Tube-to-tubesheet weldments for the Intermediate Heat Exchangers and Steam Generators.

- o Manufacturing the Horizontal Baffle Assembly and the Lower Inlet Modules.

1.4.1.1 Core Support Plate Fabrication

A. Description of the Problem

The Core Support Structure (CSS) provides support and position of the core and components, transmits loads to the reactor vessel, provides the upper boundary for the inlet plenum, distributes reactor coolant and provides anti-flow blockage features. The CSS consists of the Core Support Plate (a 106,715 lb forging), the Core Barrel (a 94,130 lb forging), and 61 Module Liners for a total dry weight of 198,800 lb.

The Core Support Structure was probably one of the most difficult stainless steel fabrications ever attempted. Because of its external dimensions, very few manufacturers have the machining, handling, and other equipment capabilities necessary to fabricate it. The 24-inch thick 178 inch diameter Type 304 stainless steel Core Support Plate (CSP) was made from two forgings. A 137.5-inch diameter forged disc was placed in a 178-inch diameter forged ring and joined with a circular 21-inch thick full penetration weld (see Figure 1-19). The limitation on disc forging diameter required this method of fabrication.

Fabrication and inspection were performed to the requirements of the ASME Boiler and Pressure Vessel (B&PV) Code, Section III, Subsection NB as supplemented by NC, for Class 1 components, and modified by Reactor Development and Technology (RDT) Standards. The RDT Standards supplement the ASME B&PV Code and are prepared by DOE.

The one exception to this was the non-destructive examination of the Core Support Plate weldment which is too thick to be radiographed conveniently. As an alternative, the liquid penetrant inspection of each 1/2-inch of weld was permitted.

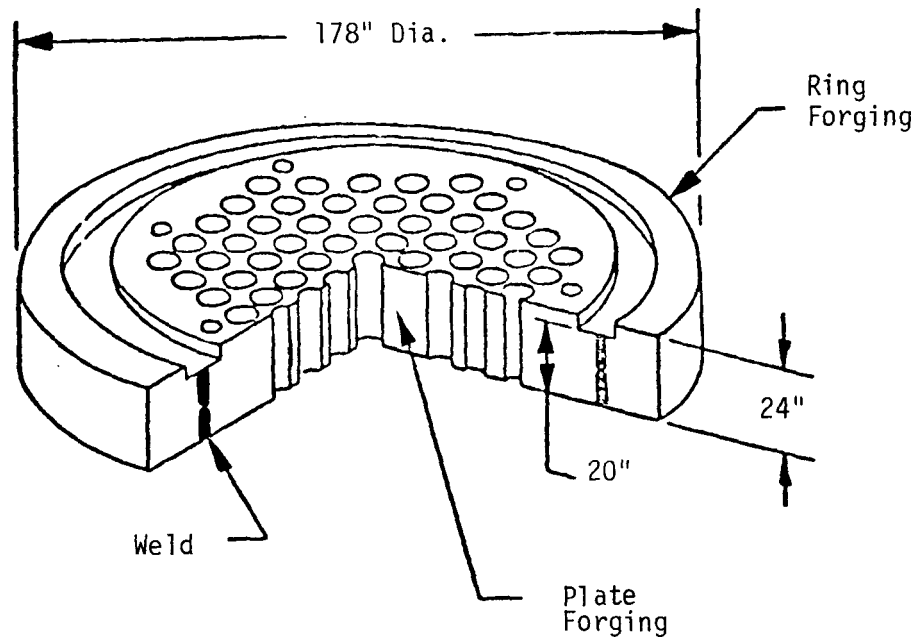


Figure 1-19 A Cut-Away Isometric View Of
The Core Support Plate Assembly

B. Fabrication of the 20-inch Core Support Plate Weld

The process and sequence of operations to complete the joining of the core plate to the core ring was as follows:

The submerged arc welding process (SAW) was selected for the core support plate (CSP) weldment. This was done because of the high deposition rate of SAW, low propensity for defects, and the ability to adapt the equipment for welding in a deep circular groove.

Welding consumables posed something of a problem because of RDT Standard chemistry limitations. The weld deposit was required to have a silicon content of 0.25% to 1.00%, and a ferrite content of 5% to 9% as determined by chemical analysis and use of the Schaeffler Diagram. The compositions of both the bare filler wire and the submerged arc flux were carefully chosen to achieve these weld deposit requirements. Type ER308 wire was used with a modified Arcos S-4 flux. The actual silicon content of the weld deposit produced by this combination was 0.96% and the ferrite level was 9%.

Weld groove geometry selection was considered to be critical because of its heavy impact on fabrication time and costs. The selection criteria included the following:

- (1) The volume of weld metal required to fill the joint should be minimized.
- (2) The ability to make the weld without extensive repairs.
- (3) The ability to backmachine the groove.
- (4) The ability to produce a desirable weld bead contour.
- (5) The ability to remove the slag easily.
- (6) The ability to make meaningful liquid penetrant examinations.

Common practice on conventional weldments of 1 to 4 inch thick has been to use a joint with a 3/8-inch groove radius and a 15° included angle. If this geometry were used for the CSP weldment, it would have taken an estimated 360 hours of arc time to deposit the required 4763 lb of weld metal. Several mockups were welded to determine the narrowest groove opening which could be satisfactorily welded and still meet all other selection criteria. The joint which was selected had a 3/8-inch groove radius and a 2° included angle. This permitted the use of a deposition technique which resulted in a single bead per layer of weld.

With a narrow groove, the top face of each layer of deposited weld metal was concave which reduced the probability of slag entrapment at the side walls in the succeeding pass. The concave bead also aided in the removal of slag. The welding parameters selected were 440 amperes, 32 volts, and 9-inch/minute travel.

The conventional joint design, as well as the one selected for this application, is depicted in Figure 1-20. For the full penetration weld, a double "U" groove was used with the first side to be welded having a depth of 12 inches, and the second side a depth of 8 inches with slightly more width. The second side groove was designed to accommodate tooling requirements for the backmachining operation which was done on a vertical boring mill. The backmachining was accomplished using a specially made reinforced, extended length grooving tool with a high speed steel cutting insert. This joint design resulted in the actual disposition of 1872 lb of weld metal in an arc time of 135 hours. This represents a saving of 62.5% in time and material when compared with conventional practice.

Additional provisions were made to allow the use of the narrow weld joint and to meet all of the operating criteria listed above. A specially made needle scaler, with extra long needles and a guide tube, was used to break the slag into small pieces and allow its removal with a standard vacuum flux recovery system. Both the needles and guide tube were made from stainless steel to avoid

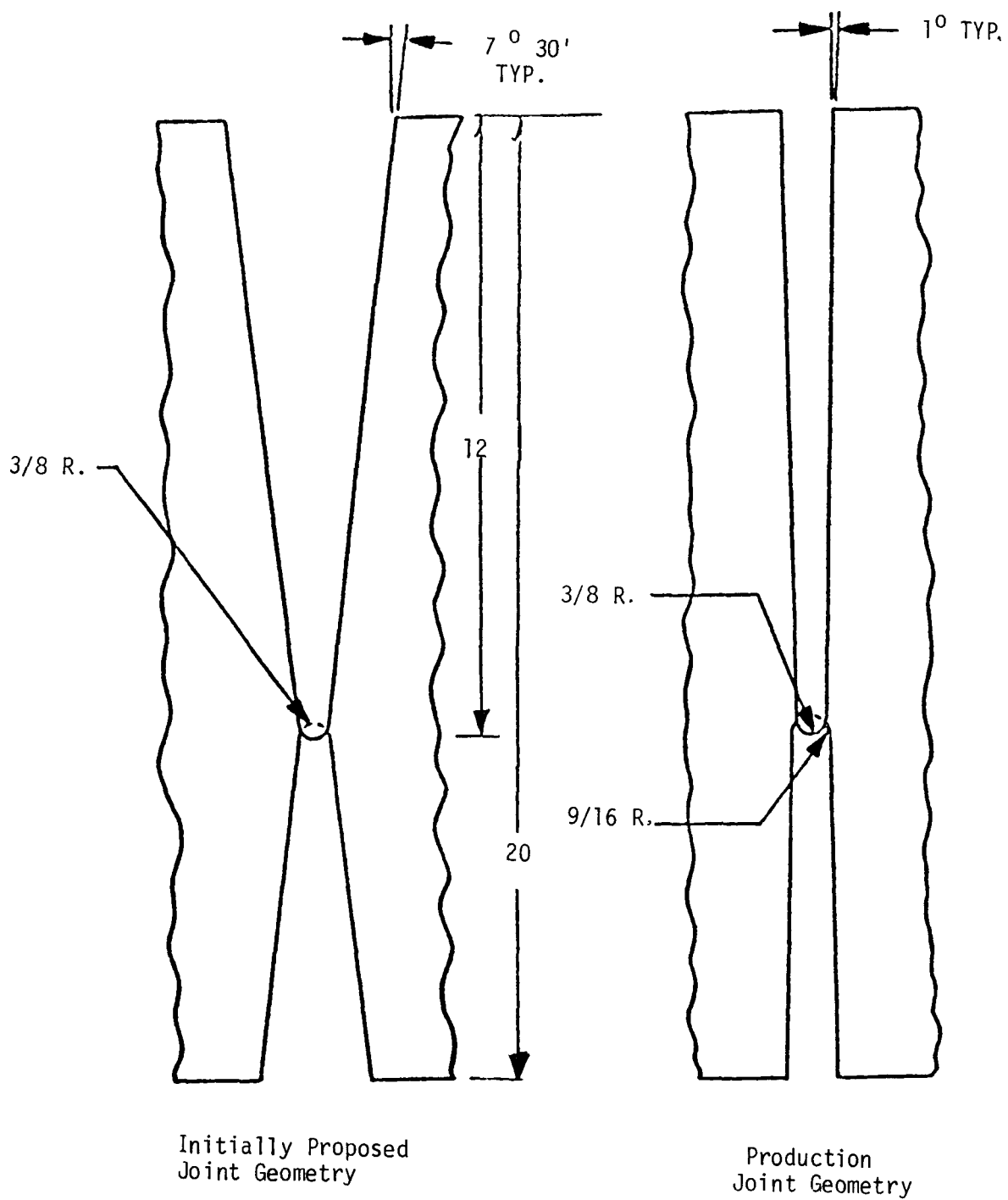


Figure 1-20 A One-Fourth Scale Sketch Of The Proposed And The Production Joint Geometries

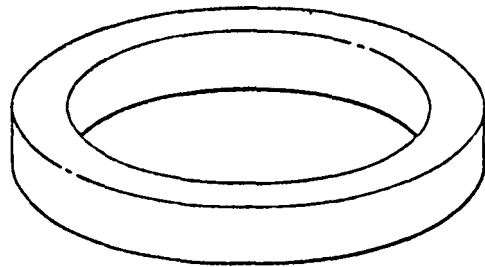
contamination of the workpiece. A long, small diameter brass wire guide tube was made to feed the filler wire from the wire feeder to the welding contact tip at the bottom of the joint. This tube was electrically insulated on its outer surface by wrapping with a low conductivity tape to prevent accidental short circuiting to the groove sidewalls. An air-operated hand grinder with a long narrow guided belt was used to grind starts and stops where necessary.

Flux was dispensed into the groove using a standard flux hopper and flexible tubing which was attached to the wire guide tube. The position of the end of the tubing was adjusted above the bottom of the groove to control the depth of the flux during welding. The assembly sequence is shown in Figures 1-21 and 1-22.

A post-emulsified dye penetrant method was selected for its inherent adaptability to use in areas of limited access. The inspection of a single layer was completed in less than four hours. The dye penetrant examination was accomplished by dispensing the dye from a standard pressurized can with a long, small diameter tube attached to the spray can nozzle. After emulsification, the penetrant was removed using a wet pickup vacuum cleaner. The developer was applied using a standard pressurized can with a spray nozzle modification similar to that for the application of the penetrant.

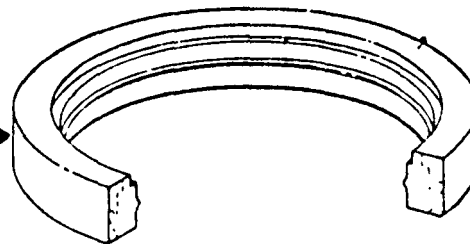
Prior to fabrication, the welding procedure was qualified to the requirements of the ASME B&PV Code, Section IX and RDT F6-5. To do this in an expeditious manner, three 8-inch thick rolled plates were stacked and edge-welded together to provide a 24-inch thick test assembly weighing 1760 lb, which was then prepared with the proposed joint design. To duplicate the inherent restraint to the production assembly, "C"-shaped blocks were welded to each end of the qualification test assembly to restrain the closure of the joint. Welding of the test assembly was completed in eight days. Figure 1-23 shows the qualification test assembly.

CORE RING FORGING

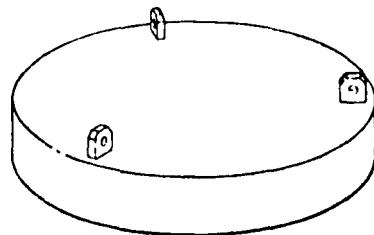


- DWG. 5932D36H03
- WT. 74,300 LBS
- MAX. DIA. 178.00

CORE RING

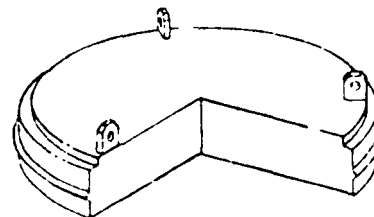


- FINISH I.D. WELD PREP
MATCHED MACHINE TO
CORE PLATE INTERFACE



CORE PLATE FORGING

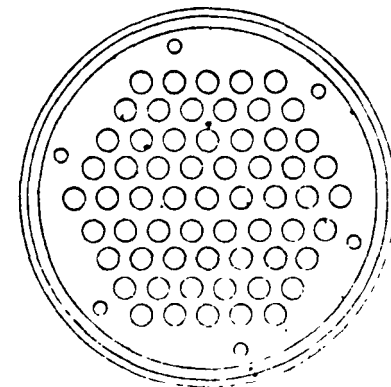
- DWG. 5932D34H02
- WT. 108,900 LBS
- MAX. DIA. 139.00



CORE PLATE

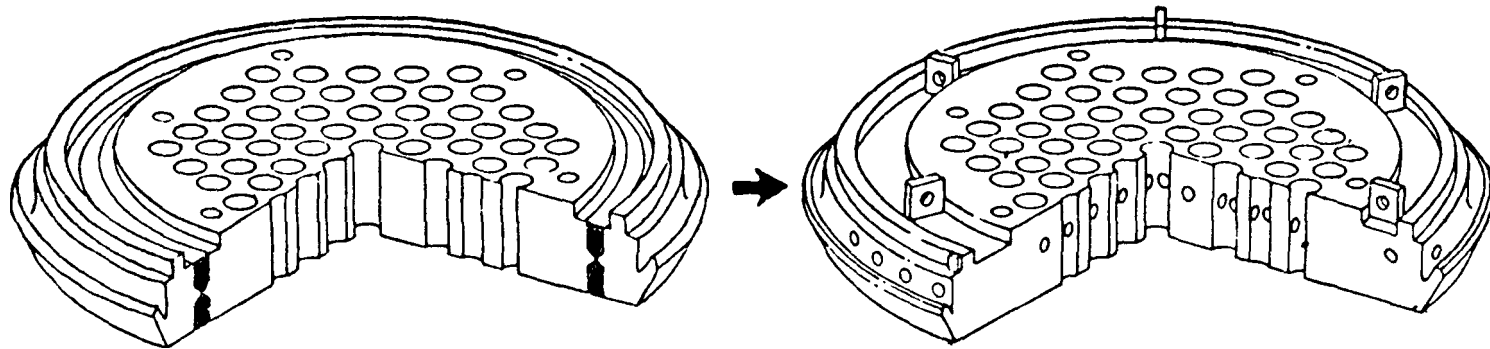
- FINISH O.D. WELD PREP
MATCHED MACHINE TO
CORE RING INTERFACE

CORE PLATE



- TREPAN THRU HOLES
UNDERSIZE
- DRILL REMAINING HOLE
PATTERN UNDERSIZE
- REMOVE LIFTING LUGS

Figure 1-21 Core Support Plate Sub-Assemblies



- Assemble Core Ring/Plate
- Finish Sub Arc Weld
- Finish Turn and Face Lifting Lug Area and Top Face
- Turn Plate Over and Finish Face Bottom Surface
- Layout Position and Weld in Lifting Lugs
- Finish Gun Drill Horizontal Coolant Holes

Figure 1-22 Core Support Plate Assembly

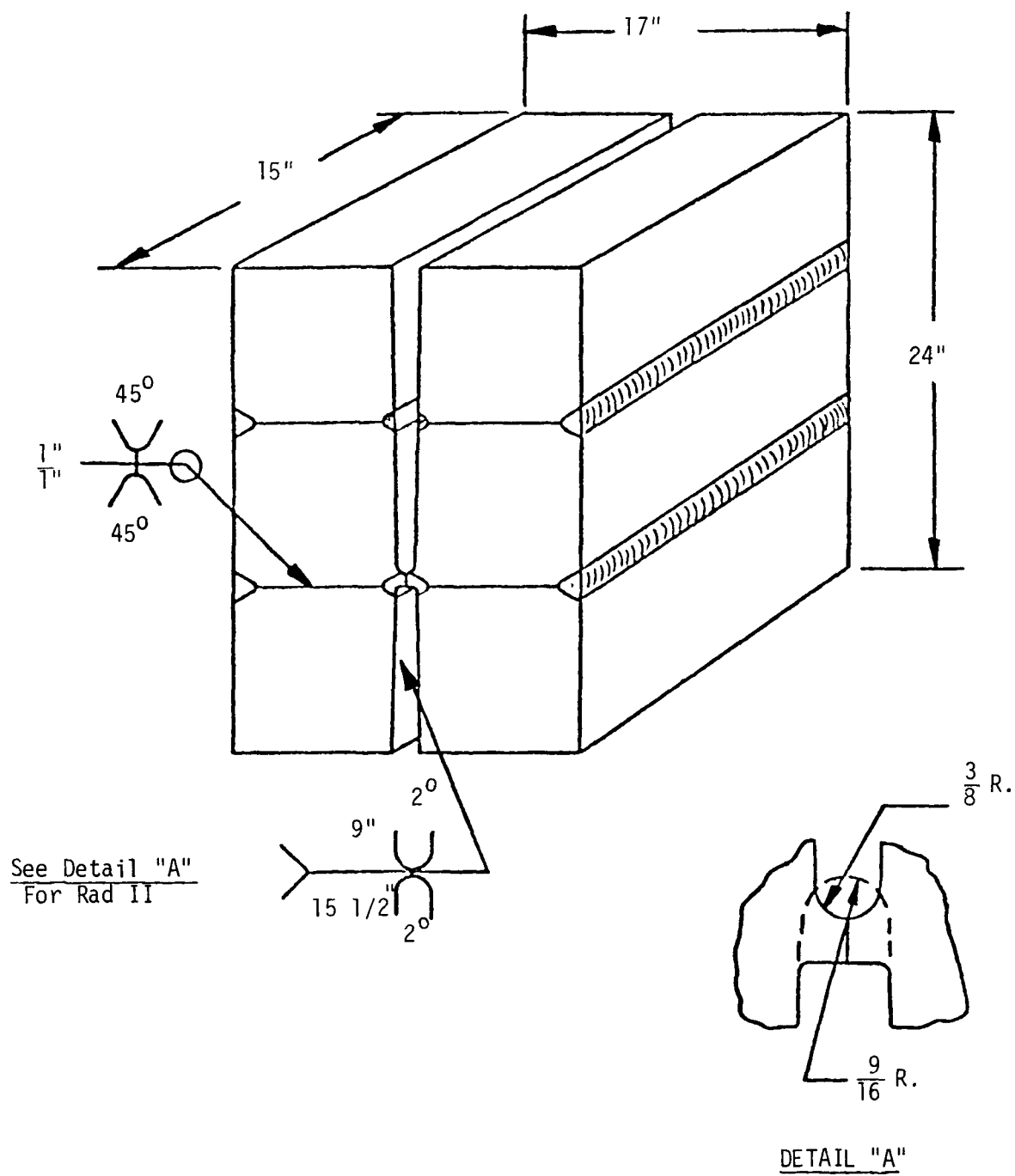


Figure 1-23 Isometric View Of Procedure Qualification Test Assembly

ASME destructive testing requirements called for two transverse tensile tests and four side bend tests on the full weld thickness. The test specimens were split into a number of pieces, as permitted by ASME, to accommodate testing equipment capacity limitations. In this case, a total of 18 transverse specimens and 72 bend specimens were required to accomplish testing of the full thickness. All ASME B&PV Code and RDT Standards requirements were fully met.

During production welding, the entire 12-inch deep groove was welded full prior to inverting the CSP for backmachining. Joint closure was monitored after each dye penetrant examination. Total closure at the top surface was only 1/8-inch when the 12-inch deep groove was full. The CSP was then inverted and backmachined to sound metal prior to welding the 9-inch deep groove. Total closure for this side was only 0.1-inch.

Welding progressed from start to finish with absolutely no difficulties. In the 44 penetrant examinations of completed weld layers, no defects were found. The CSP assembly was dimensionally inspected before and after welding. Bowing in an "oil can" fashion across the face of the plate was only 0.061-inch, well within tolerance. Original schedule planning allowed four months for the welding operation. It actually took only six weeks.

C. Conclusion

The following accomplishments were realized in fabricating the CSP weldment:

- o Tools were obtained which successfully removed slag and ground starts and stops in a deep, narrow weld groove.
- o Dye penetrant examinations were successfully and routinely performed in a deep, narrow weld groove.

- o A single weld bead per layer technique was successfully used with no requirement for interpass grinding.
- o A minimum amount of welding consumables and arc time was used in fabricating the CSP.
- o A 24-inch thick Type 304 stainless steel weld was qualified and a 20-inch thick weldment was successfully made in production using the SAW process in a narrow groove.
- o The CSP welding was completed without the requirements for any rework.
- o The fabrication of the CSP was completed 2-1/2 months ahead of schedule.

1.4.1.2 Installation of the Core Support Structure into the Reactor Vessel

The installation of the Core Support Structure (CSS), see Figure 1-24, into the Reactor Vessel was one of the more exacting fabrication situations due to stringent tolerance requirements.

A. Alignment (See Figure 1-25)

Scaffolding was constructed around the assembly and ladders installed into the annulus between the Core Support Structure and the Reactor Vessel. All Reactor Vessel surfaces were protected against damage and contamination by equipment. Protective covers were also placed on the CSS and remained in place during welding.

The CSS was placed on jacks and lowered into place adjacent to the Core Support Cone of the Reactor Vessel. The circle seam weld prep was aligned with the Core Support Cone prep to a 1/16-inch maximum weld gap and with a maximum mismatch allowance of 1/16-inch. Axis lines on the Core Support Structure and the top side surface of the cone were aligned. The tooling skirt was installed within the vessel

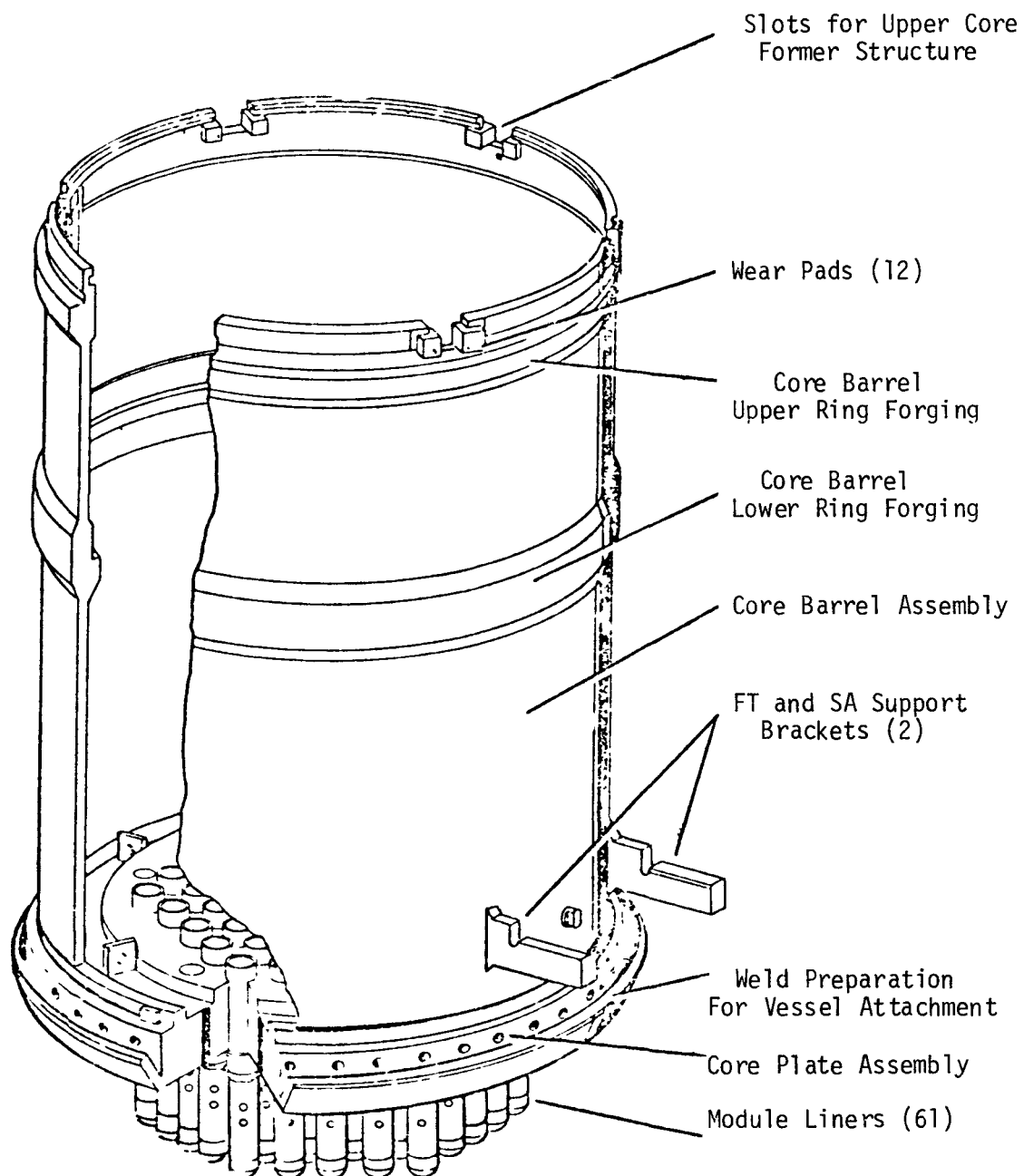


Figure 1-24 Isometric View of Core Support Structure

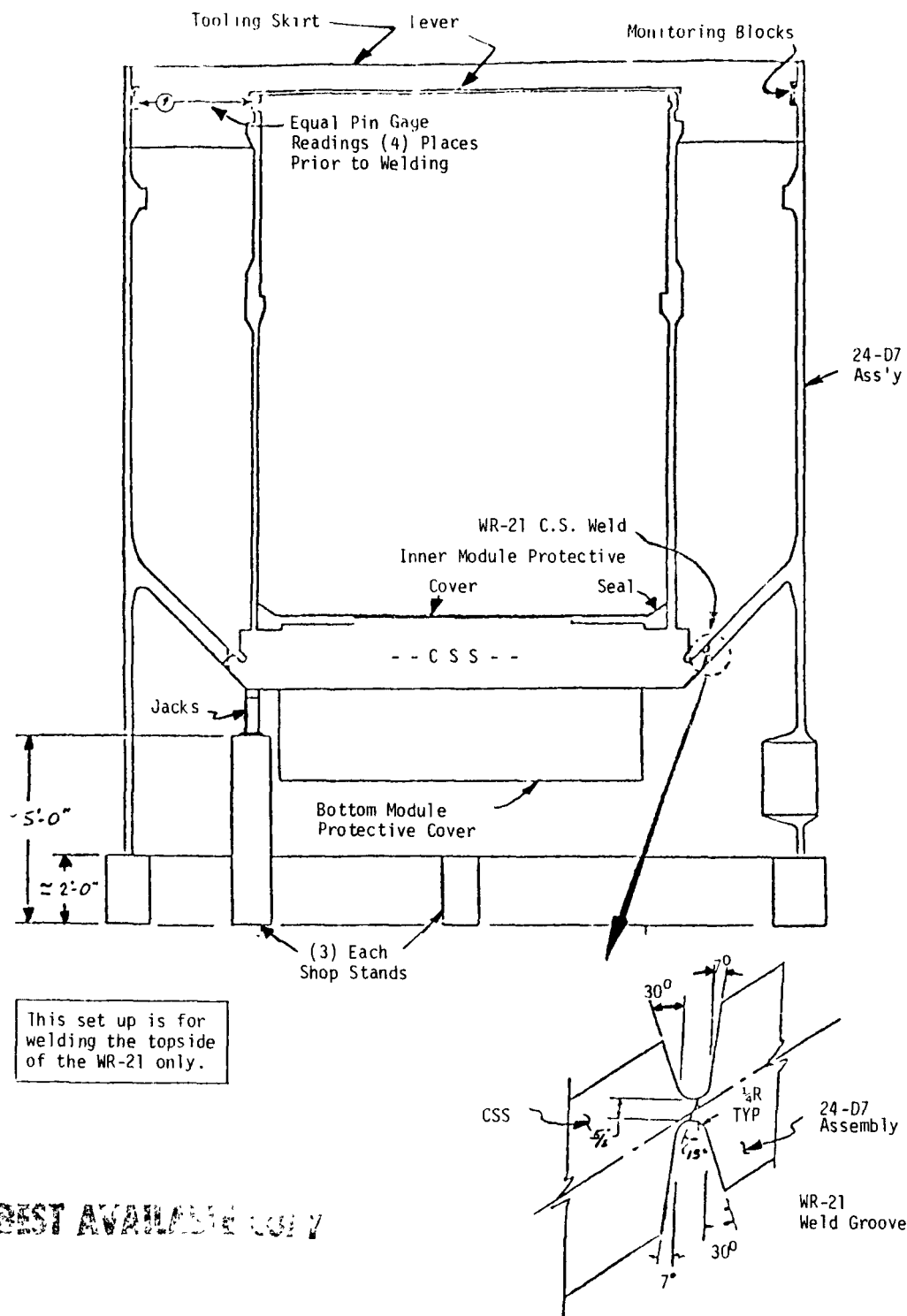


Figure 1-25

Topside Welding Setup
Core Support Plate to
Reactor Vessel Core Support Cone

and the distance between the top OD of the CSS and the 4 machined monitoring blocks on the ID of the tooling skirt were equalized. Exhaust fans and ducts were installed into the annulus between the CSS and the tooling skirt.

B. Fit-Up (See Figure 1-26)

Four wedge blocks were placed into the top side of the weld groove and fillet welded into place. Fit-up and fillet welds were inspected and the jacks and stands removed from under the CSS. The circle seam was mapped out and inspected for block welding. A welder was placed in each quadrant around the weld prep for shielded metal arc welding. The first 1/2-inch of weld was deposited using the block welding technique, using 1/8-inch diameter electrode in areas 1 thru 4. Wedges 1 and 3 were removed and 1/2-inch of weld deposited. The second 1/2-inch of weld was deposited using 5/32-inch diameter electrode. Wedges 2 and 4 were removed and 1-inch of weld deposited. The circle seam top side was completed by 1/2-inch weld deposition in sequential order.

C. Inspection

Measurements between the CSS top OD and the 4 machined monitoring blocks on the tooling skirt were recorded every 15 minutes during deposition of the first 1/2-inch of weld and each 4 hours thereafter.

Upon completion of top side welding, the weldment was liquid penetrant inspected.

D. Backside Welding (See Figure 1-27)

The assembly was upended and positioned with tooling skirt down and the circle seam backgrooved, cleaned, liquid penetrant inspected, and radiographed for information only.

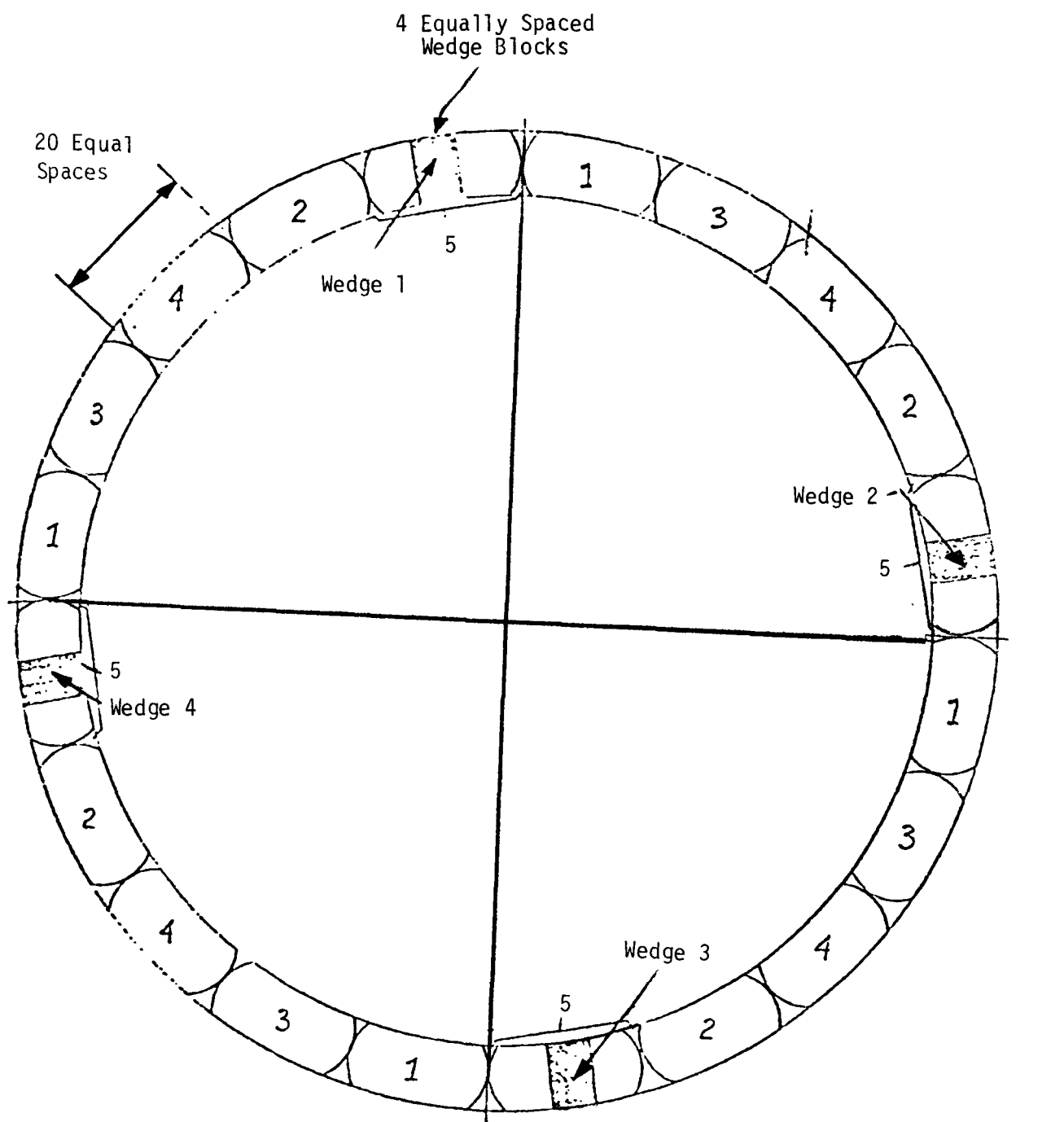
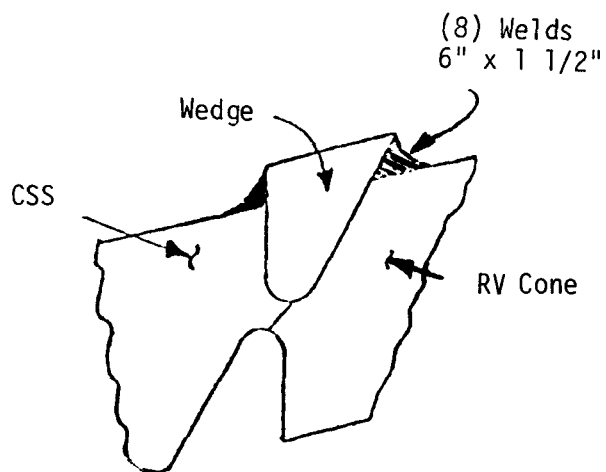


Figure 1-26 Topside Fitup and Welding Layout



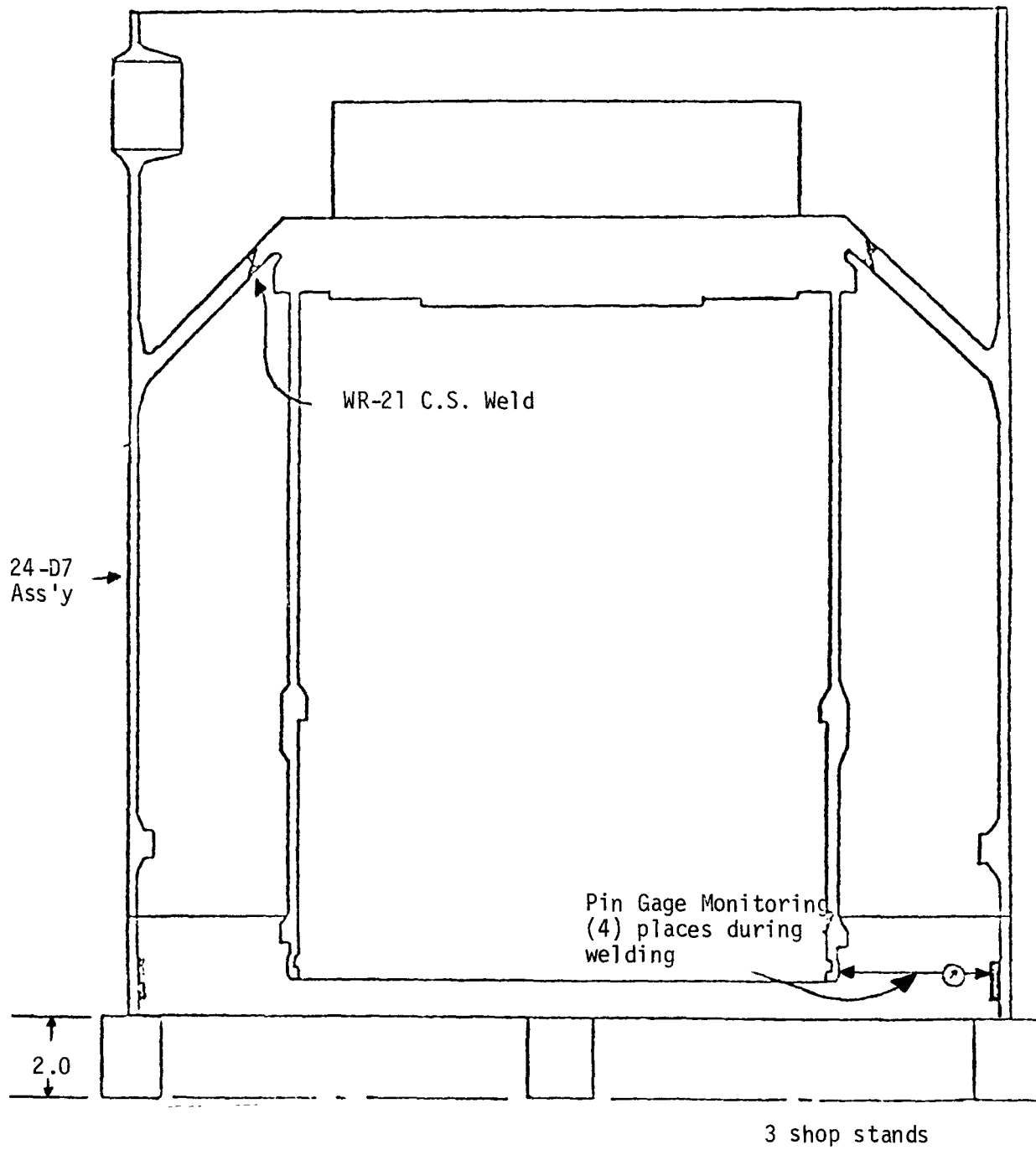


Figure 1-27 Backside Welding Setup

The weld operation for the bottom side of the weld was sequenced as the top side. Measurements between the CSS top OD and the 4 machined monitoring blocks on the tooling skirt ID were recorded at the beginning of each shift during weld deposition.

The completed weld was surface ground, dye penetrant inspected and radiographed. The assembly was re-setup on a Ferrel machine, the tooling skirt removed and the liner support ring and circle seam weld prep machined in relation to the actual CSS centerline as shown in Figure 1-28.

The welding was without incident and tolerances were met with a CSS maximum concentricity of 0.05-inch although a drawing tolerance of 0.13 concentricity was used for acceptance or rejection.

1.4.1.3 Intermediate Heat Exchanger/Steam Generator Tube-to-Tubesheet Welds

Experience with the Prototype Fast Breeder Reactors has shown that tube-to-tubesheet (T/TS) welds can be a particularly troublesome area with respect to leakage. Every attempt, therefore, has been made within CRBRP to ensure integrity of these weldments since they are part of the boundary preventing sodium and steam-water reactions.

A. Description of the Weld Method

In the hockey stick steam generator configuration, the weld joint is the only weld in the Na-steam boundary. The weld is an autogenous GTA in-bore weld between the tube and a 1-3/4-inch boss that has been machined into the side of the tubesheet forging. This length was chosen to minimize thermal stresses developed in the tubesheet forging during welding and post welding heat treatment (PWHT) of the joint (Figure 1-29).

In welding, an arc is set up between the electrode and tube wall. The electrode rotates in the center of the tube hole automatically and begins and ends its rotation at prescribed points. A single

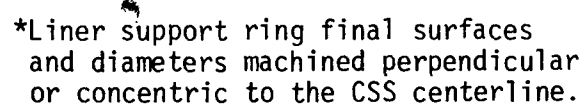
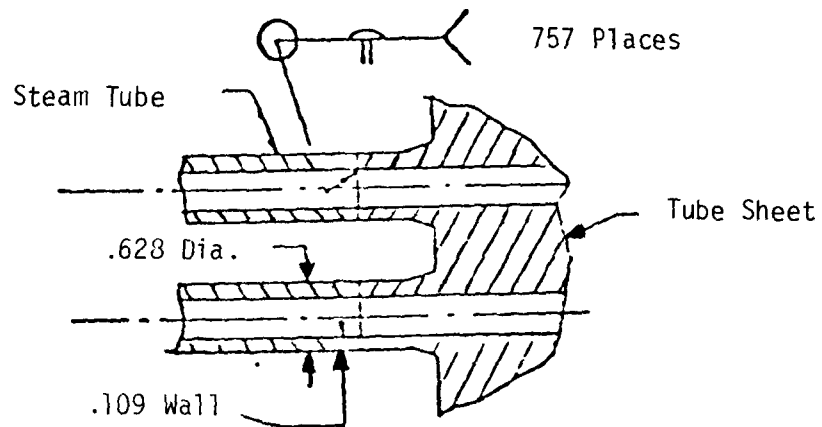


Figure 1-28 Final Machining for Weld Preparation

TUBE/TUBESHEET WELD TECHNIQUE

1. Full penetration fusion weld. No filler material added.
2. Steam tube butted to tubesheet projection (stub).
3. Gas supply - external - clam shell fixture, clamped to O.D. of tube to tubesheet projection in weld area - internal - steam tube plugged down stream of weld joint - removed after weld is completed.
4. Weld fixture - weld electrode inside tube/tubesheet bore.
5. Control - electronic, weld cycle and weld electrode rotation by motor speed control.



Scale: 1/1 Approximately

Figure 1-29 Typical Tube-To-Tubesheet Joint

revolution is made, voltage and current are preprogrammed to be increasing or decreasing, depending upon the heat required at a given moment and the circumferential position of the electrode. These are termed up slope and down slope processes, respectively. Normally, tubes with 2.8mm (0.109 in.) wall thickness would not require a PWHT to prevent cracking after welding. However, because welds in the shell will receive a PWHT, steam tubes near the shell may be subjected to temperatures approaching the shell PWHT temperature. A PWHT for the T/TS joint was required prior to closing the shell, allowing tube joint repair in the event of cracking during the PWHT cycle (Figure 1-30).

PWHT and weld preheat were applied with a barrel-shaped fixture containing resistance heating plugs similar to the bullet storage barrel of a western six-shooter, but sliced into two parts in a clam shell arrangement. The preheat was applied to 401°F and prevented cooling from welding temperatures at rates that might create brittle microstructure as well as high thermal gradients along the tube. The temperature after welding was subsequently raised to 1341 $\pm$ 55°F for PWHT. This was aided by an additional heater in the center of the tube inserted upon withdrawal of the gas-tungsten arc electrode system.

RDT Standard quality level requirements in addition to ASME Section III Code requirements imposed extremely low defect levels. This required the development of high reliability non-destructive testing (NDT) techniques that could be applied in a production environment with the required sensitivity. This was achieved by radiographic inspection using a newly developed rod-anode x-ray machine. Film was wrapped around the outside of the butt joint and the x-ray source inserted within a tubesheet hole and extended into the weld joint region.

It is understood that designers of PHENIX and SUPER-PHENIX have replaced the T/TS or tube plate weldment with a thermal sleeve insert through the tube.

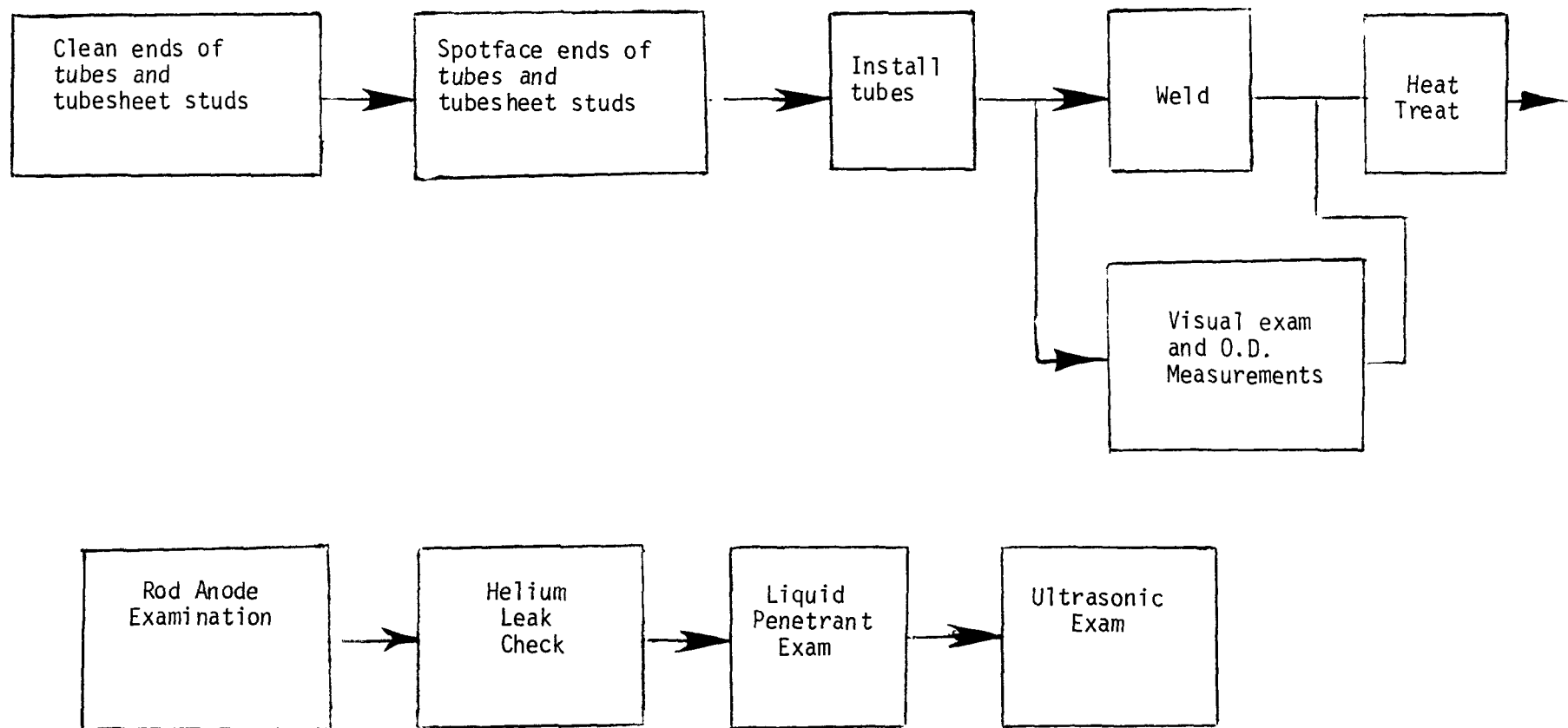


Figure 1-30 Tube Installation, Welding,
And Inspection Process

B. Actual Fabrication Developments

An average production rate of less than 3 tubes welded, inspected, and accepted per day resulted in a review of items or procedures that impact welding and inspection span times. Major causes for low weld production rates were found to be related to shop practices and equipment breakdown.

C. Fabrication Problems and Resolutions

The three most important fabrication problems were lack of weld penetration, weld ID concavity and weld porosity. The resolution of these problems are given below:

- (1) Lack of Weld Penetration. The electrical ground connection was relocated directly to the tube being welded.
- (2) Weld ID Concavity. The use of the lower end of the amperage range that was qualified. Weld concavity varied linearly with the level of preheat. Ten mils of concavity was specified for all levels of preheat above 425°F. The four o'clock start position resulted in acceptable welds, but the 8 o'clock start position produced welds with concavity in excess of 10 mils.
- (3) Weld Porosity. The application of the spray developer may have resulted in contamination of the uncapped tube. Swabbing of the developer for tube-to-tubesheet weld examination and capping adjacent rows was subsequently used (Tables 1-5 to 1-6).

1.4.1.4 Horizontal Baffle Assembly Manufacturing Analysis

In the following manufacturing discussions the Horizontal Baffle Assembly is divided into 5 subsystems: the baseplate, the ID attachment to the core barrel, the OD attachment to the reactor vessel, the 6 access ports, and the 5 fuel transfer and storage assemblies (FTSA).

TABLE 1-5

T/TS WELD PROBLEMS AND THEIR RESOLUTIONTHROUGH ROW 5

<u>Problem</u>	<u>Resolution</u>
1. Fitup problems at upper tubesheet	Beefed up strong back for tube pullers.
2. Insufficient penetration at upper tubesheet.	Increased low pulse width from 25 to 32.
3. Insufficient penetration at lower tubesheet.	Increased current levels.
4. Porosity at upper tubesheet.	Revised cleaning sequence to prevent contamination by rubber stopper and by plastic caps.
5. Wall thinning due to manual repair.	Improved workmanship when mechanically dressing outside of weld. Replace reamer with better tool.
6. Overtemperature during post welding heat treatment.	Qualify probes. "Start of Shift" heat treat. Backup thermocouple readout.
7. Excessive concavity at lower tubesheet.	Reduced pulse width back to 25. Had been increased to 32 which was too high.

TABLE 1-6 DEFECT CORRECTION

<u>Type of Defect</u>	<u>Rework Methods</u>
● Excessive Porosity	Grind out and manually fill. Cut tube out and replace.
● Excissive O.D. concavity	Manually fill, prior to QA inspection remelt (not yet qualified)
● Excessive O.D. Convexity	Mechanical dressing.
● Excessive I.D. Concavity	Grind and fill from outside. Cut tube out and replace.
● Wall thinning below limit	Build up manually, if all other criteria can be met. Cut tube out and replace.
● Incomplete Penetration	Manually fill, prior to QA inspection remelt (not yet qualified)
● Overtemperature During Post Welding Heat Treatment	Cut tube out and replace.

Since all of the metal removal processes that were employed to machine these configurations, as well as most of the setups and operation sequences, are well established procedures, no specific recitations are included. This analysis addresses only special situations, non-standard methods, potentially obscure details, and anything that might create manufacturing problems if allowed to continue unnoticed.

Important trial fit assemblies and appropriate match marking are addressed by Drawing 1620E48.

A. Baseplate

The baseplate (Drawing 1537E30), is made from 6 equal sized plate sections which are roughly shaped by burning. The mating edges are prepared for welding with double bevels. Flatness is important in the finished product, and must be achieved through welding process control and/or subsequent mechanical flattening.

Rough machining of the one piece baseplate must be performed on a Vertical Boring Mill (VBM) in preparation for the ID and OD, double-sided hardfacing. These seal surfaces also have important flatness requirements, and therefore, the hardfacing process must be controlled for minimum distortion.

Final machining must be performed with the baseplate deflected similar to its installed configuration. This requirement is necessary because the exact slope angle cannot be predicted accurately enough to maintain the required sealing surfaces' tolerances. Note that this deflected constraint is imposed for the access ports and FTSA penetrations, the aximuthal orientation key seat, and the ID and OD, top and bottom, hardfaced, seal surfaces. Tooling must be provided for both VBM and Horizontal Boring Mill (HBM) machining.

These final machining techniques are considered non-standard, and the vendor was required to submit detailed procedures for approval prior to implementation, as is defined in paragraph 5.2.4 of the equipment specification, E-953031.

B. ID Attachment

The ID attachment consists of the azimuthal orientation key (1537E34H01), two socket head cap screws (1537E25H07), two plugs (1537E34H04), spacer ring (1537E31), and retaining ring (1537E32). All of these pieces can be machined from appropriately sized stock by conventional methods.

Both the spacer ring and the retaining ring are relatively flexible, necessitating dimensional inspection while still restrained on the VBM table, and requiring careful handling procedures in order to avoid flexure beyond yielding.

Although welding of up to 3 butt joints per each ring is permitted, one piece construction is recommended in order to minimize raw material stock, welding distortion, and machining.

A special problem arises with the hardfacing of the retaining ring. The potential for excessive distortion requires generous oversizing of the raw material, with attendant extra machining to produce the finished ring. Additionally, a technique of intermittent, segmented, hardfacing deposition might have to be developed. Residual stresses, however, might seriously hinder achievement of an acceptable product. Although no easy solution to this problem is apparent at this time, a specialized vendor may have some appropriate, practical experience. Some development effort may be required in any case.

An alternate approach would be to ring roll one piece of Hastelloy C-276, which would bypass all welding and hardfacing operations. Some material property investigations and qualifications would have to be initiated for this material.

C. OD Attachment

The OD attachment consists of 72 each of baseplate supports (1537E33H02), guide pipes (1537E34H04), spacers (1537E33H01), clamps (1537E33H03), locking cups (1537E33H04), and socket head cap screws (1537E33H05). All of these pieces can be machined from stock by conventional methods, with no particular problems anticipated. No machining is to be done on the pins (Amerace Esna part Number 79-077-375-1250).

The baseplate supports and clamps must be rough machined in preparation for hardfacing, and finish machined afterward. The hardfacing process must be controlled for minimum distortion because these are sealing surfaces. The cap screws have supplementary material requirements and receive hard chrome plating.

D. Access Ports

The access ports consist of 6 plug supports (1537E14), 6 plug support nuts (1537E15), 12 pins (235C138H03), 6 top plugs (1537E16), 6 bottom half plugs (1537E17), 18 key latches (9632E56), 6 locking pieces (1537E18), 6 pins (235C138H01), 36 socket head cap screws (1537E19H05), and 36 stops (235C138H02).

Conventional machine methods are adequate for all of these pieces, although the key latches, which are made from Alloy 718, will need extra attention and tooling.

The plug supports and bottom half plugs must be rough machined for the hardfacing and finish machined afterward. The hardfacing process must be controlled for minimum distortion of the sealing surfaces, which, at least for the plug supports, probably means segmented, sequential deposition of the hardfacing material.

The cap screws have supplemental material requirements and receive hard chrome plating.

Both the top half plugs and the locking pieces have some complex geometric shapes which must be thoroughly understood before beginning machining. In particular, the trilobate top aperture of the top half plug should be machined before the 6-inch major bore diameter, which should be machined before the 3 keyway internal flats at the bottom. Also, the turning of the bottom 4 inches of the locking piece should be done before milling the top hexagonal section.

A full size, transparent plastic, working model of the plug assembly was made available to facilitate comprehension.

E. Fuel Transfer and Storage Assemblies

The fuel transfer and storage assemblies consist of the lower support block (1440E27: only final machining remains to be done), 4 pins (9631D54 H01: already finished), 4 stops (9531D54 H02: already finished), 5 bearing pads (1537E36H04), 5 dashpots (1537E36H03), 5 tubes (1537E36G01), 5 piston rings (Dover Corp. Part Number AWE-15350, Rev. 1: no machining to be done), 5 upper nozzle halves (1537D35H01), 5 lower nozzle halves (1537E35H02), and 5 pins (1537E34H03) used at final installation.

Conventional machining methods will be sufficient for all of these noting that the upper and lower nozzle halves, dashpots, and bearing pads are Alloy 718.

Aluminizing the bearing pads and chrome plating the maximum OD of the upper portion of the tubes is required.

The tubes will be made from a straight length with a welded on heavier wall top section. Machining can then be completed, with dimensional allowance for the chrome plating.

Special attention must be accorded the dashpots because, in addition to being made from Alloy 718, they have a 5-inch radius spherical surface at the bottom.

- 1537E31 Spacer Ring
- 1537E32 Retaining Ring
- 1537E33 Base Plate Support, Clamp, and Locking Cup
- 1537E34 Key, Plug, and Pin
- 1537E35 FT and SA Inlet Port Nozzle
- 1537E36 Transfer and Storage Tube, Dashpot and Bearing Pad
- 1620E48 Trial Fit Assembly

1.4.1.5 Lower Inlet Modules Manufacturing Analysis

1.4.1.5.1 Introduction

The Lower Inlet Module (LIM) is a complex design, consisting of an upper body portion with a snowflake shape and a lower cylindrical stem portion. The 61 Lower Inlet Modules fit into module liners in the Core Support Plate of the Core Support Structure (CSS). The module is supported from the surface at the base of the module body by the CSS. Each module supports and positions the lower end of seven core assemblies which include various combinations of Inner Blanket (IB) Assemblies, Fuel (F) Assemblies, Primary Control (PC) Assemblies, Secondary Control (SC) Assemblies, Radial Blanket (RB) Assemblies and Removable Radial Shield (RRS) Assemblies.

This manufacturing study presents in the sub-sections which follow, a description of the LIM, fabrication sequences of the LIM Body Assemblies, receptacles, discriminators, hold down rings, cartridge assemblies, two pressure tests which must be done prior to final assembly, the final assembly sequence and cleaning and shipping considerations.

1.4.1.5.2 Description of the Lower Inlet Module

The basic external configuration of all the modules is the same. The internal construction varies with the location of the module in the array and the types of core assemblies it is designed to accept. There are four basic types of modules designated, according to the assemblies each accepts, as: F, PC & IB; F, IB & RB, F, RB & RRS and F, SC & IB.

In the module body are seven receptacles for core assemblies, orifice plates where needed for specific core assemblies and bleed holes leading to a low pressure region outside the body to provide hydraulic balance to the core assemblies. The main part of the module body is machined from a single Type 304 SS bar. The receptacles are fitted into sockets in the upper end of the body. The remainder of the hardware is machined from Type 304 SS except for the piston rings, which are Alloy 718 with chromium plating for hardness and wear resistance at contacting surfaces. Alignment and Discriminator Pins at the base of the module body assure that each module can be installed only at its correct position in the CSS. The LIM minimum weight is 1054 lb and the maximum weight is 1325 lb depending on type. Inside the lower end of each receptacle is a machined configuration which matches the bottom end of the core assembly inlet nozzle intended for that particular core position. Differences in these configurations provide the discrimination feature which prevents a core assembly from being placed in an incorrect core position.

The LIMs are designed for a 30-year life. However, the modules are not mechanically held down and have the size and features necessary for removal through the head penetrations. This permits the removal of a module from the Reactor Vessel without a major stripping of the reactor assembly.

Drawing 766J895 is the top final assembly drawing on which are tabulated the module bodies, and receptacle part numbers, including receptacle orientation within the module body, for the 61 modules. It also calls out the requirement for final pinning of the receptacles, alignment pin and discriminator pins to the module body. This drawing is very informative.

1.4.1.5.3 Sequence of Manufacturing Operations for LIM Body Assemblies

The sequence of manufacturing operations is given for each of the four types of modules identified above.

A. Type F, PC & IB (Drawing 766J896).

There are twenty-five LIM Body Assemblies of this type made up of four major items (body, stem, receptacles and piston rings) along

with miscellaneous items such as holddown rings and various types of pins for constraining items. A review of the manufacturing operations for the body and stem is as follows:

(1) Body Fabrication

The 14-1/2 inch diameter x 30 inch long forging should be setup in a Vertical Boring Mill (VBM) and one end face machined, then the other. It should then go to the layout area for laying out the outside snowflake configurations and the hole pattern top and bottom ends. The forging should then be setup on a Horizontal Boring Mill (HBM) with super spacer and the seven receptacle holes in the top end formed by gun-drilling 1-1/2 inch diameter x 14.8 inch deep blind holes and opening to 3 inch diameter by spade drilling. After turning the forging end for end, gun-drill the bottom pattern center hole 1-1/2 inch diameter by 12 inch deep and open to 2-3/4 inch diameter with a spade drill and finish bore the 7.48 inch diameter center counterbore. With the same setup rotate the dividing head 15 degrees from the horizontal center line, end mill flat bottom at each of the bottom end six cartridge holes for drilling. Drill, finish bore and flat bottom each of the six angled holes. Setup forging to center of rough top and finish bottom center holes, chucking on the top end and cone center the bottom end. Finish turn 14.10 inch outside diameter, OD channel groove, 30° bottom chamfer, and bottom hub including overall hub length and approved weld prep. (Finish face also the "B" bottom surface which must be perpendicular to the body OD surface "A" within 0.001-inch.) This can be attained at this time without problem since it is accomplished with the same setup. Setup the body horizontally on the HBM in a V block fixture with the top surface facing the spindle. Finish face mill body top surface to 27.44 inch upper module overall length. Take a cleanup cut in each of the six receptacle bores, allow 1/32 inch stock for final machining of the critical top bore. Maintain a tight diameter tolerance on the cleanup cut and bottom hole squaring

for an intimate fit with a subsequent drill jig drilling operation. Using the same setup, install and position a drill jig according to the hole pattern in each of the seven top receptacle bores. Finish drill the blind center hole and the 5 to 6 through angle holes in the bottom surface of each of the preliminary bored receptacle holes. Remove the drill jig. Maintain same setup. Install special form shell mill cutter on spindle piloting off the drilled center hole, finish mill the 3.25 OD x 1.56 ID channel groove at the bottom of each receptacle hole. Remove the cutter and finish ream and chamfer the center hole of each of the six bores. Using the same setup, finish machine the concentric center backbore and the six eccentric peripheral backbores. With the same machine setup finish machine the top bore concentric to the bottom center bore in the six receptacle holes. Finish bore the top diameter for threading. Rough fly cut and attempt to finish bore the 4.28 inch x 3.847 inch annular precise seal ring groove to the required tolerance and surface finish.

Continuing with the same setup. Machine tap the receptacle top bore 4.25 inch - 8 thread seven places each module body. Several test pieces should be tapped prior to production bodies, to determine the limit of the number of holes per tap before sharpening or replacing taps to assure quality threads. Re-setup body on HBM so the snowflake sides will be perpendicular to the machine spindle. Use the same fixture as used for the OD finish turning operation. Pilot the dividing head of the top center threaded hole with the body bottom end centered with a cone center. Shell mill, index, mill index and etc. until outer snowflake configuration is completed. Two sides can be milled between each index positioning. With the same setup and a 1/2 inch ball end mill, mill, index, mill etc. the six 1/2 inch radius snowflake corner reliefs. Continuing with the same setup 3/4 inch dia., drill through the snowflake periphery to the bottom center bore of each of the seven receptacle holes. Also, 0.377 inch diameter drill through the

snowflake top periphery for pinning the seven receptacles and 0.502 inch diameter drill through for pinning the seven locking caps. The body is now ready for the final machining operation.

Setup the body, vertical position, receptacle holes down on positioning and constraining, fixture on vertical spindle jig borer. Drill and finish bore the bottom end of the body for one alignment pin hole and two discriminator pin hole and two discriminator pin locations per Table I of the drawing. (This sequence of operations is in reverse of the drawing intent but it is the only way to manufacture the body and meet the drawing requirement.) Position can be met by fixturing. Turn body over. Same setup, drill the hole pattern for under-sodium viewing in the top face surface per detail A-21 and Table I of the drawing for group angular orientation.

(2) Stem Fabrication

Setup the 8-1/2 inch diameter x 55 inch long bar in a lathe. Steady rest the outboard end. Cleanup face end and center drill. Turn bar around, finish face end to overall length. Allow excess stock length for pre-determined stem to body weld joint shrinkage. Setup bar in V block fixture on HBM with top end facing machine spindle. Finish gun drill 1-3/8 inch diameter x 36.81 inch deep hole bottom end on a 2.95 inch radius. The 0 = 0 axis line will be laid out on the bottom end of stem before drilling. Using the same tooling setup, turn bar end for end. Drill and finish bore the 7.48 inch diameter top bore. Gun drill 1-1/2 inch diameter the center of the bar 16 inch deep 0.86 inch off center and open to 5 inch diameter and finish to 5.60 diameter with spade drills or davis blocks, starting at the bottom of the finished 7.48 inch diameter bore. With the same setup, finish flat bottom bore the finish drilled bore. See Section G-15 and Table I of Drawing 766J896 for hole positions. With the same setup, reposition boring bar off center location per Table I and Section G-15 and finish bore one

then the other 2.375 inch radius intersecting bores. Move bar and setup in lathe between centers. (Finish turn outside diameters, weld prep and bottom 35° chamfer except leave 0.060 inch OD stock at each of the piston ring areas for finish machining after weld fabrication to the module body at which time the piston ring grooves will also be machined.) After completion of the turning operation, the stem will be setup on an HBM with the tube centerline horizontal and perpendicular to the machine spindle. The tube will be mounted with the open end in a dividing head and the other end supported and centered with a live center. The 1.38 inch diameter intersecting return flow channel hole will be drilled and finish bored. The stem will then be indexed and the one 3-inch diameter flow hole will be drilled and finish bored, and the 1/2 inch diameter drain hole will be drilled.

Using the same setup, finish drill the 1/4 inch diameter strain hole patterns at each of the three locations. A multi drillhead must be used to the vast number of 1/4 inch holes to be drilled. Each of the three strainer locations per stem of these twenty-five stems require 479 drilled 1/4 inch diameter holes or a total of 35,925 holes. A drilling development program started in the early stages of module manufacturing planning will be well worth the effort in relation to schedular time and machine cost savings. After the drilling is completed, the module stem is ready for final deburring and cleaning. The stem is then finished, except final machining of the piston ring area, and ready for weld fabrication to the body.

(3) Stem to Body Weld

This is a critical connection. The weld joint must be finished in a controlled fashion since the Body is completely finished machined and the stem likewise except for the super critical piston ring areas. These areas and the piston ring grooves will be finish machined in relation to the Body position after

welding. The Stem rolled bar material machined tube fit-up is similar to a situation that proved difficult in the Core Support Structure liner tube to bottom forged cap weld joint. Experience with the CSS liner tubes may help to avoid difficulties with the body to stem attachment. Prior to any welding, special attention must be made to plug, mask, etc., any openings that lead to the inside of the module since many surfaces become inaccessible for cleaning and inspection after assembly. The joint will undoubtedly be welded with either a shiplap joint for alignment and automatic Tungsten Inert Gas (TIG) welding process as in the CSS, or a Type EB insert aligned and tack welded in place on an assembly fixture that would position the parts for the root pass and following TIG welding passes. Alignment is important and steps must be taken to monitor and control the shrinkage and minimize distortion during welding.

After completion of the girth weld, the joint can be radiographed by inserting the film through the three-inch diameter stem midpoint access hole to a position behind the weld. The module is now ready for final machining of the stem piston ring area.

(4) Module Piston Ring Groove Machining

Re-mask flow hole used for radiography. Maintain the remaining openings masked to the module inaccessible area throughout the remaining machining operations to preserve module cleanliness.

Setup module in lathe. Fixture body in chuck and live center stem end. Finish machine piston ring area and groove at the center and bottom end of stem true position to Surfaces B and A, the bottom end surface of the body and body OD to 0.004 inch.

B. Type F, IB and RB (Drawing 766J897)

These six modules are made up of three major items, the body, stem and cartridges along with miscellaneous items such as hold down rings and various types of pins. A review of the manufacturing operations for the major items is as follows, except for the Cartridge Assembly which is reviewed in Section 1.4.1.5.7 as a separate item.

(1) Body Fabrication

This item is identical to the body for the 25 units of the Type F, PC and IB as discussed in paragraph A.(1) above, except that two of the receptacle holes have a shorter main bore and are mated with a bottom stepped bore for orifice plate cartridge assemblies which are pinned in position. The differences in this body compared with the design noted is negligible; therefore, the judgment and conclusion registered in A.(1) applies also to this body's fabrication.

(2) Stem Fabrication

This item is identical to the fabrication of the Stem for the 25 units of the Type F, PC & IB modules given in paragraph A.(2). above. Therefore the judgement and conclusion registered there applies also to the fabrication of this stem.

(3) Stem to Body Weld

This is the same configuration, material, process etc., as the 25 units of the Type F, PC & IB given in paragraph (A).3 above. Therefore, the judgement and conclusion registered there applies also to this weld fabrication assembly with one exception; the body Cartridge Assemblies must be pressed in place and pinned to the body prior to fit-up and welding of the stem.

(4) Module Piston Ring Groove Final Machining

This is also in total the same requirement as that for the 25 units of the Type F, PC & IB modules given in paragraph (A).4; therefore, the judgement and conclusion registered there applies also for this manufacturing operation.

C. Type F, RB & RRS (Drawing 766J898)

These twenty-four units are made up of five major items, the body, stem, cartridges, stem to body welding and module piston ring groove final machining. A review of the manufacturing operations for the major items is as follows except for the Cartridge Assembly which is reviewed in Section 1.4.1.5.7.

(1) Body Fabrication

The overall envelope and configuration is similar to the body item of the other. The machining of the receptacle holes is similar to those of the body for the Type F, IB and RB, but there is additional precision machining for the five bores in the bottom of the body due to the cartridge installation requirement. The one remaining bore is straight forward and does not require cartridges. The additional work at this point adds to the manufacturing problems of the body which was already a difficult component. Therefore, the judgement and conclusion registered in paragraph A.(1) applies also for this item.

(2) Stem Fabrication

The OD size and configuration is the same as the stem reviewed in paragraph A.(2) except six of the required twenty-four groups (GO 2, 6, 10, 14, 18 and 22 shown at F-17 on S-2 of Drawing 766J898) have three areas of two piston rings each instead of two areas of two piston rings each as do the other stems. Also all twenty-four stems have two 1.88 inch diameter outlet holes for the by-pass flow instead of the one outlet hole.

The Stem ID boring is completely different from the twenty-four F, PC and IB and the six F, IB and RB stems. The main bore is made up of two intersecting holes instead of three intersecting holes as in all other stems. This bore is also much longer than the other stem designs since the top end that mates up with the body is bored concentric to the outside diameter 1 1/2 inch deep instead of 14-1/2 inch deep as in the other stems. The main bore is flat bottom as in the other stems but is not a blind bore as in the other cases. These twenty-four stems are through bored at the bottom end for an orifice cartridge assembly. The very bottom is threaded for pressure testing of the module assembly. The overall manufacturing difficulty, considering the design differences, is similar to the previously discussed units. Therefore the judgement and conclusion registered in Paragraph A.(2) also applies for this Stem design.

(3) Stem to Body Welding

This is the same fit-up and weld configuration as the other stems discussed to this point. The same process, tooling and technique will be used for these modules even though the counterbore of the Upper Stem end is shallower than the others. This will not significantly change the procedure for the weld, but cartridges must be installed and pinned to the Body prior to fit up and welding of the Stem.

(4) Module Piston Ring Final Machining

This final operation is identical to all other module liner final machining except for six out of these twenty-four assemblies that require an extra set of piston rings. This extra set of piston ring grooves are machined within eight inches of the body along with the set of grooves machined mid-way and bottom area of the stem. The finished diameter of the piston ring areas is held to within 0.004 inch true position of the perpendicular underside Surface "B" and OD "A" of the

body. This precise position of the Stem Piston Ring area to the body must be machined after welding as previously discussed since that 0.004 inch true position tolerance cannot be expected to be achieved by control of the weld process.

D. Type F, SC and IB (Drawing 766J899)

These six modules are made up of seven major fabrication operations, fabrication of the body, fabrication of the upper stem, upper stem to body weld, final machining lower stem, lower stem to upper stem weld, and module piston ring groove final machining. These are the most difficult modules to machine due to the special flow path required from the SC Assembly receptacle to a port in the module liner.

(1) Body Fabrication

The body envelope is identical to all the other module groups and will be finished machined before weld fabrication except that stock will be left on the bottom boss for final machining after welding. The ID configuration will also be finish machined except the bottom boss which will be final bored with the upper stem after weld fabrication.

Stock will be left in the through intersecting bores for final match machining to the Upper Stem through bore. The rough machined bottom bore of the body will be counterbored for a formed backup ring which will be fit and tack-welded in place and the one 1.75 inch diameter angled through hole will be plug welded to make the body to upper stem weld feasible. The plug will be drilled out and the backup ring will be bored out during the final machining operation.

(2) Upper Stem Fabrication

The upper stem is approximately 17.5 inch long. Stock must be left on the OD for final machining after weld fabrication to the

Body. A four-inch diameter hole will be drilled through the stem, off center to a layout, to give visual access to the inside of the mating of the stem and body to ease fit up. All other machining will be performed after weld fabrication.

(3) Upper Stem to Body Welding

Fit up the Upper Stem to the Body and tack weld in place. Manual TIG weld the joint completely in a carefully planned sequence of weld passes around the periphery in order to control distortion and minimize misalignment of the two components. This is a very difficult weld and full scale mockups will need to be made to develop the best weld process and sequence to meet the requirements. After completion of the welding the subassembly will be radiographed and repaired as needed before final machining.

(4) Upper Stem Final Machining

Upon completion and acceptance of the weld joint the bottom end of the upper stem will be plugged, laid out and scribed for the three bore locations in the stem in proper relation to the finish machined Body. After L/O, the fabrication assembly will be setup in a lathe with Body chucked and Stem end supported in a steady rest. Center drill the plugged end to L/O. Remove steel rest. Install live center and finish machine the stem OD and bottom surfaces of the Body. After finish turning, setup the fabricated assembly horizontally on the HBM with the Body chucked in a dividing head and the stem end facing and supported with a steady rest. Remove plug and finish bore the three intersecting stem contour holes, the stem end concentric bore and weld prep and the Stem OD, L/O with a right angle head on the HBM, index assembly as necessary and end mill the OD stem to Body slot. With the same setup angle the right angle head and finish drill through the weld and insert plug into the Body flow channel hole. The boring and drilling should remove all traces

of backup bar, plugs and the weld root pass, but hand work will be needed to restore a smooth intersection in the 1.75 inch diameter bores. This subassembly is now finished complete and ready for joining to the Lower Stem. The Upper Stem welding and final machining operations are among the most difficult to successfully achieve of the entire job.

(5) Lower Stem Fabrication

This item is similar to all other stems except it is shorter because the module stem over all length includes an Upper Stem section. The ID is the familiar three hole intersecting bores. It has three sets of strainer holes, has two sets of piston ring grooves and is counterbored and weld prepped on the upper end to mate with the finished fabricated and machined Body Upper Stem sub-assembly. No additional problems are expected beyond those presented in subsection E, Conclusion, below.

(6) Lower Stem to Upper Stem Weld

This joining will be similar in setup and process to that which will be used for the Body to Stem weld of all the other Module liners. Both Upper and Lower tubes will be finished machined except the piston ring grooves on the lower tubes that will have stock for final machining after weld fabrication. This final machining operation in all cases will be required after completing the final welds in order to meet the 0.004 inch true position tolerance of the piston ring grooves to the outside diameter of the Body.

(7) Module Piston Ring Groove Machining

This is the final machining operation and the same procedure is used on all the Module Body Assemblies. No anticipated problems are expected for this final precision operation with proper planning.

E. Conclusion

The manufacture of the LIM body assemblies is an extremely difficult job. The combined weight of the Body and Stem raw material for each Module is 2275 lb. The finished machined Module weight ends up at a maximum 1325 lb and a minimum 1054 lb, depending on the various designs. The size, weight, unique configuration and numerous tight tolerance features will contribute to the risk that machining errors will be made. The variety of equipment and numerous setups required to complete the operations will add to the potential for quality problems. Additionally, removal of 950 to 1221 lb of metal for each module provides extended exposure to opportunity for machining errors. It will take a first class shop to satisfactorily produce the Lower Inlet Modules.

1.4.1.5.4 Receptacles

A. Description

There are three basic designs. Two items with integral discriminator and one with an assembled and pinned discriminator that can be modified according to discriminator design. The receptacle starting material is Alloy 718 bar stock weighing approximately 60 lb which is reduced by machining during final machining to about 15 lb. The final configuration is typically a thin wall tube with a flanged top end. Some are more complicated with integral discriminator and blind flat bottomed main bores. The diametrical tolerance and surface finish are extremely tight. The 16 RMS finish requirement on the double angle lead-in bore and inside diameter angle transitions will be impossible to consistently achieve in production of 427 pieces. The 16 RMS on the straight main bore will be difficult to achieve consistently. The 0.001 inch total tolerance with a 63 RMS finish on the outer surface of the annular groove under the top flange is extremely tight. The surface must be generated by back machining since there is no room for any type of grinding operation.

B. Conclusion

The type of material, number of pieces required, extremely tight tolerance and surface finishes applied make this item extremely difficult to manufacture. The receptacles will be a source of difficulty; many deviations will be generated during the manufacturing.

1.4.1.5.5 Discriminator (Drawing 1442E55)

There are three basic designs that supply nine individual items simply by changing lengths and diameters. These are also difficult to manufacture, mainly because of type of material and configuration and not the tight dimensional tolerance and surface finishes as in the case of some other parts. All of the items need to be turned, bored and milled. The deep channel boring of most of the Alloy 718 items is difficult but manufacturable with the present tolerance requirements.

1.4.1.5.6 Hold Down Rings (Drawing 1442E73)

There are six peripheral and one center ring for each module assembly. For the required 61 assemblies there are 427 rings. The rings all have the same 4.250 inch -8N threaded outside diameter. The 61 Center Rings have a fairly complicated machining inside diameter configuration used for pickup assembly and disassembly of the module. The 366 periphery rings have a simple straight bore with a double angle lead-in. Both center and peripheral rings have a requirement to chrome plate 0.4 to 0.6 mil thick the inside diameter only, ignoring the galling potential of the outside diameter thread. The outside diameter threads should be flash chrome plated 0.2 to 0.4 mil thick along with a change of thickness of the plating of the inside diameter to correspond to the thread plating thickness. This will simplify the plating by reduced masking and improve the quality of the rings by reducing the galling potential during final assembly with the Module Liner Body.

1.4.1.5.7 Cartridge Details and Assembly (Drawing 1443E96, Shts. 1 and 2) -
Item 1 through 7 Material Type 304 SS

A. Description

There are a total of 12 assemblies for these six F, IB and RB Module Assemblies and there are 216 assemblies, item 1 through 7 for the 24 F, RB and RRS Module Assemblies. There are also 24 assemblies item 7 and 8 for the Stem in the same 24 F, RB and RRS assemblies. This totals 252 Cartridge Assemblies. The basic design is the same for all cartridges but the size of the parts changes for each assembly to meet the requirements. Each Cartridge Assembly is made up of a housing, end plate, spacer plates, orifice plates and alignment wire. The number of orifice plates and drilling pattern is variable among assemblies.

The Housing is made from a solid bar that will be machined into a shell with an ID lip on one end to support the stacked series of orifice and spacer plates. The wall is drilled for weld fabrication after assembly. The bottom bore is threaded for module assembly. Stock is provided on the outside diameter for final machining after assembly welding. The housing itself is reasonably toleranced for manufacturing.

The Orifice Plate is a 0.100 inch thick plate with a tight + or - 0.001 inch tolerance. The tolerance is extremely tight and will require a specific final grinding operation to attain. To add to this difficulty each plate is drilled through with a series of either 1/4 inch or 3/8 inch diameter holes in a specific pattern and notched for controlled assembly. The tight thickness tolerance applied to the manufacture of the 2300 orifice plates will add substantially to their cost.

The spacers (0.60 inch and 0.250 inch wd.) are a simple ring configuration with the same precise thickness tolerance of + or - 0.001 inch. A similar quantity of these are needed and the thickness tolerance is difficult.

The alignment wire will be procured to size, cut to length with a hook bent on one end for assembly retention. This is a simple straightforward process with no anticipated problems.

The End Plate is the final item of the Cartridge Assembly. The cap is a ring that is assembled last into the Housing on top of the stacked orifice plates and spacer rings. With controlled pressure applied to the total plate stackup, the End Plate is face fillet welded to the housing. The Orifice Plate assembly is plug welded to the housing through staggered 3/4 inch diameter holes located at 90° intervals in the housing wall. After the welding is completed, the fabricated assembly is setup and a cleanup cut is taken on the outside diameter and top face. The interference fit region is machined to a precision diameter.

B. Conclusion

The thousands of pieces required to complete the 252 Cartridges should make it a production job. Unless some of the tolerances can be relaxed, such as plate and spacer thicknesses, the cost benefits of applicable production machinery will not be realized. Also the welding appears to be excessive; possibly it could be eliminated entirely by mechanical fastening instead of welding. Cartridge testing could be beyond the capabilities of the shop with the expertise to manufacture the entire module and may need to be subcontracted.

C. Flow Testing Orifice Plate Cartridge

The orifice plate cartridges will be gas flow tested. The schematic arrangement for this test is shown on Drawing 9630D43. An assembly

drawing of the orifice plate cartridge and test hardware is shown on Drawing 1443E16. The test temperature shall be between 65 and 75°F. The test pressure shall not be applied until the module and the contained gas temperatures are within 5°F. The Flow Test Sequence is as follows:

- (1) Verify that test fixture is clean and free of debris prior to testing in the first cartridge. Without the orifice cartridge in place, allow gas to flow through the system to "blow out" the fixture.
- (2) Install the cartridge in the test fixture per Drawings 1443E16 and 9630D43 making certain that the flow direction arrow on the cartridge body is oriented properly.
- (3) Slowly adjust gas flow to required pressure and flow settings given on Drawing 9630D43. When readings stabilize, record pressure, flow, temperature, differential pressure and cartridge serial number.
- (4) If a differential pressure reading fails to fall within the acceptable range given in Table A of Drawing 9630D43, check test set-up for leakage or other problems. Correct any problems found and retest cartridge. If no test set-up problems are found or if readings are still unacceptable after correcting problems, report unacceptable pressure differential reading to the Designer. Repairs or further retests shall not be performed without the prior approval of the Designer. The Fabricator will probably not accept full responsibility for meeting all the test requirements for a build to drawing part/assembly. This may be a problem.

1.4.1.5.8 Pressure Tests of Lower Inlet Modules

A. General

The Fabricator shall perform two pressure tests on each Lower Inlet Module, as specified in paragraph 6.2.1 of E-953022. The schematic arrangements for these tests, Pressure Tests A and B, are shown on Drawings 9630D41 and 9630D42, respectively. An assembly drawing of the LIM with its pressure test lugs and fixtures for Pressure Test A is shown on Drawing 1442E98. Drawing 1442E97 gives the details of the plugs and fixtures for Pressure Test A. The plugs for Pressure Test B shall be designed and furnished by the Fabricator. All of the other equipment shown on the test schematics, (such as valves, filter, instruments, etc.) not specifically detailed by the Designer, shall be selected by the Fabricator to accommodate the specified pressures, flows, etc. The Fabricator shall also design the detailed arrangement of the test system. Designer approval of the detailed test arrangement is required prior to initiation of pressure testing. Fabricator shall prepare, for Designer approvals detailed test procedures in accordance with the test requirements specified in E-953022.

The pressure tests shall be pneumatic, using nitrogen as the testing medium. Compressed gas is hazardous when used as a testing medium, and it is recommended that special precautions be taken for protection of personnel.

The test temperature shall be between 50 and 100°F. The test pressure shall not be applied until the module and the contained gas temperatures are within 5°F.

Indicating pressure gages shall be used. Preferably all gages shall have dials graduated over a range of about double the intended maximum test pressure, but in no case shall the range be less than 1.5 or more than 3 times that pressure. The gases shall be calibrated against a standard dead-weight tester or a calibrated

master gage before each test or series of tests. A series of tests is that group of tests, using the same gage or gages, which are conducted within a period not exceeding two weeks.

The Code Authorized Inspector shall witness all pressure tests.

B. Pressure Tests A

This is a pneumatic pressure test of the main internal chamber of the module. It shall be performed after all fabrication operations have been completed except for the installation of the receptacles into the module body and the piston rings on the module stem.

- (1) Install the test plugs and fixtures on the module as shown on Drawing 1442E98. Prepare the test set-up as shown schematically on Drawing 9630D41.
- (2) Pressurize the module to 25 psig. Locate and correct any obvious leaks.
- (3) Gradually increase the pressure in steps to 100 psig, 125 psig, 150 psig, 175 psig, and finally to the test pressure of 205 to 215 psig.
- (4) Block the pressure in the module by closing the shut-off valve on the gas inlet line. During the next 10 minutes, the pressure as indicated by the gage shall not drop noticeably. If a drop in pressure occurs, the Fabricator shall determine the location of the leak or leaks. Leaks at temporary seals may be stopped by repair and/or replacement of gaskets, packing, test plugs, etc., or by tightening bolts, and the pressure test repeated. Leaks caused by defects or deficiencies in the module itself shall be reported to the Designer; in such cases, repair or retests shall not be performed without the prior approval of the Designer.

- (5) If the test pressure is successfully maintained, reduce the pressure to 170 psig, and examine the module for signs of leakage or visible permanent deformation. Any of the above rejectable indications shall be reported to the designer and repairs or retests shall not be performed without the prior approval of the Designer.
- (6) Reduce the gas pressure to atmospheric and remove the test plugs and fixtures from the module.
- (7) Record module number, test temperature, maximum test pressure before and after 10 minute waiting period, and any signs of leakage or visible permanent deformation.

C. Pressure Test B

This is a pneumatic pressure test of the hydraulic balance bleed/bypass hole in the module stem. It shall be performed after Pressure Test A and before continuing with any fabrication operations.

- (1) Install test plugs, designed and furnished by the Fabricator, in both ends of the hydraulic balance bleed/bypass hole and prepare test set-up as shown schematically on Drawing 9630D42. Pressurize the inside of the hole to 25 psig.
- (2) Stop any leaks that may occur at the test plugs by repair and/or replacement of the gaskets, packing, test plugs, etc., or by tightening bolts. Close the shut-off valve on the gas inlet line. During the next ten minutes, the pressure as indicated by the gage shall not drop noticeably. Leaks caused by defects or deficiencies in the module itself shall be reported to the Designer.
- (3) Reduce the gas pressure to atmospheric and remove the test plugs from the module.

- (4) Record module number, test temperature, pressure before and after a 10-minute waiting period and any signs of leakage.

1.4.1.5.9 Lower Inlet Module Final Assembly (Drawing 766J895)

A. Method of Installing Receptacles in LIM Body

The following is a suggested method of installing the receptacles in the body of the Lower Inlet Module Body for final assembly.

The receptacles cannot be installed until after the pneumatic pressure test of the Lower Inlet Module has been performed. The suggested steps are as follows:

- (1) Start the installation procedure with the receptacle in the center of the module. Support the module with its axis in the vertical position, top end up.
- (2) Insert a Type 304 SS dummy disk into the receptacle socket at the top of the body. The dimensions of the disk shall be as follows: Diameter -- 4.104 (+0.000, -0.005). Thickness -- 0.70 (+0.01, -0.01). All dimensions are in inches.
- (3) Screw the holddown ring that will be used for that specific socket into the body until it contacts the dummy disk. The ring must be screwed in carefully by hand and it must go in easily with no binding. An approved lubricant may be applied to the threads in the body and on the ring, with all excess wiped off before assembly. If the ring starts to bind at any time, carefully unscrew the ring and inspect the threads on both parts to determine the reason for the binding; correct the cause of the binding before attempting to install the ring again. Do not tighten the ring against the dummy disk.
- (4) Remove the ring and the dummy disk. Make sure that the socket in the body is clean as required by the equipment specification.

- (5) Insert the specific receptacle into the module body holes. There is a very close fit between the receptacle and the body at the 3.847-inch (nominal) diameter, so it may be necessary to heat the module body to facilitate insertion. The predrilled alignment holes in the body and receptacle must be in line to permit insertion of the alignment pin. A tool which extends down into the receptacle and grips the holes or slots in the receptacle wall can be used to help rotate the receptacle to its required orientation in the body. Clamp the receptacle down temporarily to prevent it from lifting or turning in its socket.
- (6) Insert the alignment pin through the hole in the body and into the hole in the flange of the receptacle. Insert a temporary plug in the adjacent receptacle socket to prevent the pin from backing out.
- (7) Apply a small amount of approved lubricant to the threads in the body and on the holddown ring, and wipe off the excess. Apply a small amount of approved lubricant to the top surface of the receptacle and the bottom surface of the holddown ring, and wipe off the excess.
- (8) Screw in the holddown ring by hand, while maintaining the clamp on the receptacle to keep it from shifting or turning. Any tendency of the threads to bind must be investigated and corrected before proceeding further. Run the ring down until it contacts the top of the receptacle.
- (9) Using a double-handled torque wrench, apply 200 ft-lbs of torque to tighten the holddown ring. Maintain the clamp on the receptacle to keep it from turning in its socket; the alignment pin is not intended to prevent rotation of the receptacle. The two handles of the wrench must be pulled equally to achieve a pure couple on the ring without any significant net lateral force. Increase the torque from zero to the maximum value slowly and steadily. Do not use any impact wrenches on this ring. Temporarily clamp the ring to keep it from turning.

- (10) Line-drill the holes for the locking pins in the holddown ring through the predrilled holes in the module body. CUTTING FLUID CANNOT BE USED FOR THIS DRILLING OPERATION. Also, place suitable plugs in the adjacent sockets to prevent chips from falling into the body.
- (11) Insert the locking pins through the holes in the body and into the holes in the holddown ring. Insert temporary plugs in the adjacent receptacle sockets to prevent the pins from backing out.
- (12) Remove the clamps from the receptacle and holddown ring.
- (13) Install the receptacles and holddown rings in the six outer sockets of the module body, using essentially the same procedure as described above in steps (2) through (12). Care must be taken to prevent the alignment pins and locking pins in the center receptacle from backing out; these pins eventually are completely captured when the surrounding receptacles are installed. Each outer receptacle holddown ring has only one locking pin instead of the three that are used for the center position. CUTTING FLUID CANNOT BE USED WHEN DRILLING THE HOLES IN THE HOLDDOWN RINGS FOR THE LOCKING PINS. Prevent chips from entering the module body. For the outer positions, the alignment pins and locking pins are plug welded to the module body. Do not remove the clamps from the receptacles and holddown rings until after these plug welds have been performed, inspected, and accepted.
- (14) Cover the top of the module to prevent foreign matter from entering the module during subsequent fabrication and handling.

B. Acceptable Lubricants for Lower Inlet Module

Lubricants for assembling parts shall be used only when needed to prevent jamming or galling, and then only in the minimum quantities required to achieve the desired results. Excess lubricant shall be carefully wiped from the parts.

The acceptable lubricants, listed in order of preference, are as follows:

<u>Lubricant</u>	<u>Manufacturer</u>
DAG-156	Acheson Colloids Co. 1637 Washington Avenue Port Huron, Michigan
Neolube	Huron Industries Inc. P.O. Box 104 Port Huron, Michigan
Fel-Pro N-5000 (Nuclear Grade)	Fel-Pro Incorporated 7480 N. McCormick Boulevard Skokie, Illinois
*Never Seez Pure Nickel Special (Nuclear Grade)	Never Seez Compound Corp. 2912 S. 8th Avenue Broadview, Illinois

DAG-156 and Neolube are colloidal suspensions of graphite in alcohol. The former is concentrated and must be diluted for use and the latter is diluted ready for use.

Fel-Pro N-5000 (Nuclear Grade) and Never Seez Pure Nickel Special (Nuclear Grade) are paste mixtures of graphite, nickel flakes, and mineral oil.

*Before ordering Never Seez Pure Nickel Special (Nuclear Grade), a chemical analysis of the batch that would be supplied shall be obtained from the manufacturer and submitted to Westinghouse Advanced Energy Systems Division for approval. This approval must be granted before the material can be purchased by the fabricator.

C. Conclusion

One glaring problem of the assembly is related to the holddown rings. There are 427 rings screwed into the top of the inlet module body holes to hold down the receptacles. This is presently a threaded 4.250 inch-8N-2 stainless to stainless fit-up. As a further precaution against possible galling, even with the use of an approved lubricant, it is recommended that the hold down nut external thread be flash chrome plated 0.2 to 0.4 mil. It is further recommended that the present chrome plating thickness of the ID configuration be reduced from 0.4/0.6 to 0.2/0.4 mil thickness to improve the quality and be compatible with the thread plating thickness. All the plating could then be handled at one time.

1.4.1.5.10 Cleaning

Following is a list of recommendations to emphasize the importance of maintaining cleanliness throughout the manufacture of the component.

The lower inlet module will operate completely submerged in liquid sodium inside a nuclear reactor pressure vessel. The degree of cleanliness required by the specification is necessary to prevent the following potentially harmful effects of foreign material in or on the lower inlet modules:

- o Contamination of the liquid sodium flowing around and through the lower inlet modules.
- o Corrosion of the lower inlet modules and other metal parts in the reactor by foreign material or by the products of chemical reactions between the foreign materials and the liquid sodium.
- o Plugging of flow passages in the lower inlet modules and in other equipment in the sodium system.

The lower inlet modules require special attention to cleanliness requirements because many surfaces become inaccessible for cleaning and inspection after

assembly operations that occur relatively early in the fabrication sequence. For example, orifice plates are no longer accessible after they have been installed in the housings of the flow control cartridges. The cartridges and many internal surfaces of the modules are not accessible after the module stem has been welded to the module body. Other internal surfaces of the module become inaccessible after the receptacles have been installed in the module body.

Following is a list of some of the design and fabrication features in the lower inlet modules that must be reflected in the fabricator's detailed assembly and cleanliness procedures:

- A. Machining and welding must be performed on the outside of the flow control cartridges after the orifice plates have been installed inside the housing. During these operations, both ends of the cartridge must be tightly plugged to exclude cutting fluids, chips, and welding fumes.
- B. The flow control cartridges and their sockets in the module body and stem must be clean at the time the cartridges are installed. Foreign matter or excess lubricant will violate cleanliness requirements and may interfere with a satisfactory shrink fit of the cartridge into its socket. No lubricant should be applied to the socket because the insertion of the cartridge into the socket could collect this lubricant ahead of the cartridge and push it onto the bearing surface at the base of the socket.
- C. Cutting fluid shall not be used when drilling holes for the locking pins on assembled parts. Fluid would penetrate to internal crevices and would be impossible to remove.
- D. All locking pins, alignment pins, and their sockets must be clean before inserting the pins.
- E. When making the girth weld joining the module stem to the module body, it is necessary to prevent fumes and spatter from going up into

the body and down into the stem. Since the initial pass of this single-welded joint must be made with inert gas backup, the fabricator should consider placing gas dams as close as possible to the weld, in order to minimize the interior surface areas exposed to fumes and spatter. If these dams are collapsible, they then can be removed through the access opening in the stem after the weld has been completed.

- F. The access hole in each stem can be used for removal of gas backup dams, visual examination inside the module with a bore scope after the stem has been welded to the body, and insertion of sources and/or film cassettes for the required radiographic examination of the weld.
- G. Any machining on the module body or stem after welding the two pieces together must be performed in such a way that cutting fluid and chips cannot enter the interior of the module. It will be necessary to seal all the openings in the body and stem by plugging or covering them.
- H. The receptacles and their sockets in the module body must be clean before installing the receptacles. The threads in the body, the threads on the holddown ring, and all surfaces of the holddown ring must be checked for cleanliness before installation. Excess lubricant must be removed.
- I. Liquid penetrant examination should not be used on plug welds or partial penetration welds in the flow control cartridges, module body, or module stem. If the weld were to have a full thickness defect, the penetrating fluid could reach internal crevices and would be impossible to remove. The specification requires a visual examination of such welds in lieu of liquid penetrant examination.
- J. During handling and temporary storage of parts at the shop, and packaging of the lower inlet modules, it is necessary to take appropriate precautions to prevent foreign matter from entering the

many openings in the flow control cartridges and in the module body and stem. To accomplish this objective, suitable plugs and wrapping must be used at all times when the openings would otherwise be exposed to the shop environment.

1.4.1.5.11 Shipping

A conceptual shipping container design, Drawing 1442E79, provides a required container arrangement for final shipment of the lower modules. The E-Spec, Handling, Packaging and Shipping Requirements, page 7-1 through 7-8, has a detailed and comprehensive outline of the requirements for meeting the shipping criteria. Westinghouse AESD Engineering has approval rights on all supplier designs and procedures for shipping.

1.4.1.5.12 Study Conclusion

This is overall an extremely difficult component to manufacture. It is one of the most unique hardware items designed at Westinghouse AESD. The thousands of pieces with numerous tight tolerance features contribute to the risk that manufacturing errors will be made. Weld mockups should be made of the final stem to body girth weld to establish shrinkage and distortion data. Flash chrome plating should be added to the outside diameter of the holddown ring threads to further insure the elimination of galling with the body. The large number and various types of equipment, processes and setups required to meet a reasonable production schedule adds to the problems. All told the successful vendor for this job will need a full time project group for planning, processing, identifying and tracking the numerous items through the various machine operations and inspection stations. Detail manufacturing status and reporting as required by Westinghouse AESD will be most troublesome without a complete vendor support organization.

The unique design configuration precise tolerance and difficult to machine material combination will lead to an excessive number of SNRs.

1.4.2 Bi-Metallic Welds and Weld Problem Resolutions

This section describes the bi-metallic welds and the weld resolutions used for the Outer Riser Assemblies, Reactor Closure Head Penetrations, welding of the Horizontal Baffle Retaining Ring and the Horizontal Baffle Baseplate and hardfacing of the Horizontal Baffle Baseplate.

1.4.2.1 Outer Riser Assemblies

The riser assemblies form the connection between the various rotating plugs of the closure head and the reactor vessel flange and form the upper portion of the reactor closure head pressure boundary as shown in Figure 1-31. They are designed, fabricated, and inspected to ASME B&PV Code Section III, Class 1 and RDT E15-2NB requirements.

Bi-metallic weldments join the top and bottom A508 ferritic forged steel rings with an intermediate web section made from Alloy 600 rolled steel. The bi-metallic weldments were made using ENi Cr Fe-3 and ERNi Cr-3 filler material. The weld required radiographic inspection in accordance with Class 1 requirements. The riser assemblies were fabricated by two different fabricators and both fabricators had the same radiography problems.

A. Radiographic Inspection

Prior to final machining, the finished weld was surface finished for radiography inspection. An Ir-192 source was used in accordance with ASME Section V requirements. Radiographs revealed a significant number of linear indications. These indications were indicative of incomplete fusion and/or lack of penetration.

As a precautionary measure, to assure that no other detectable indications existed in the weld at this thickness level, additional radiographs were made using a 300 KV x-ray machine in place of Ir-192. This supplementary inspection revealed all the indications previously disclosed by Ir-192, plus additional linear indications not previously discovered. The additional linear indications ranged

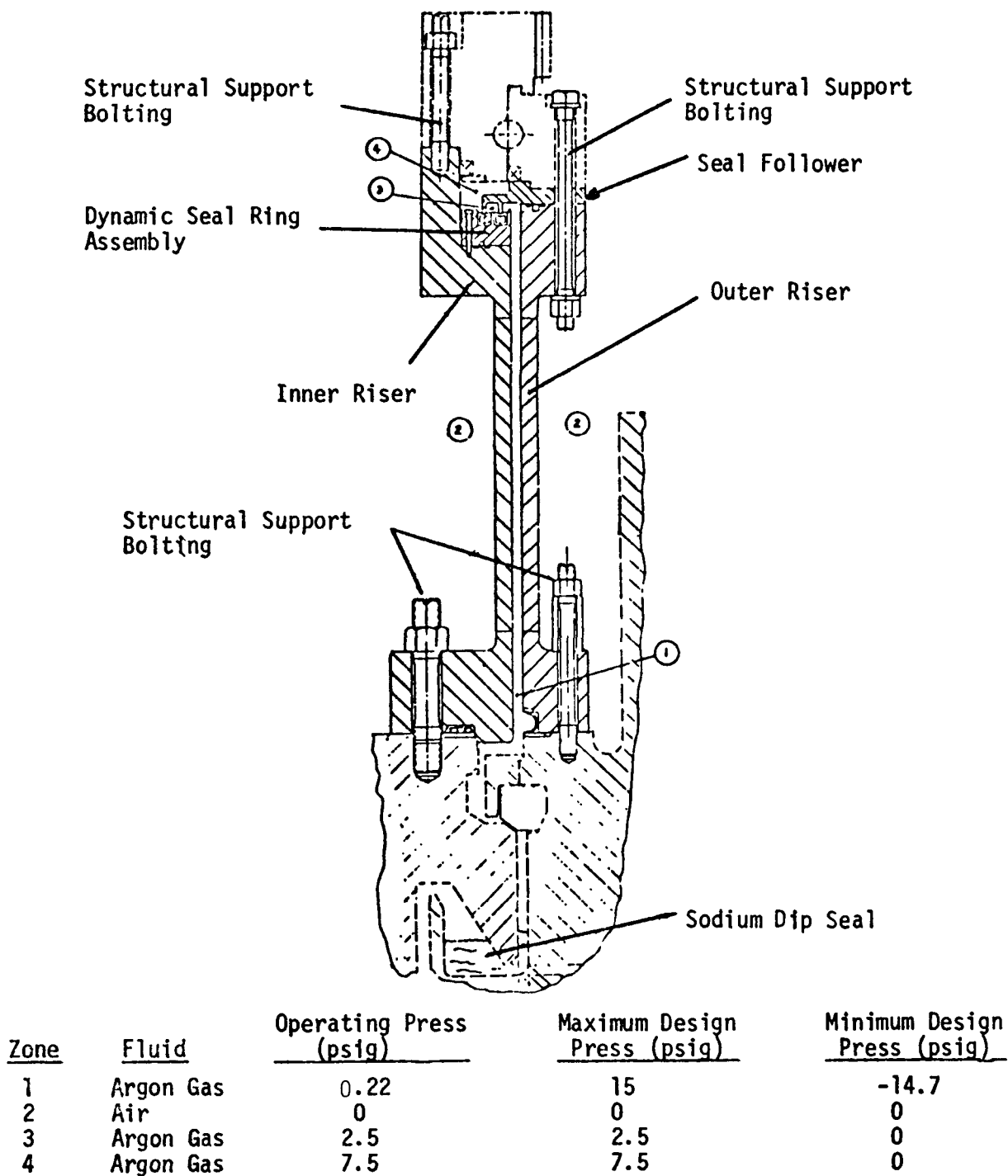


Figure 1-31 Typical Riser Assembly Pressure Boundary (Axisymmetric Cross-Section)

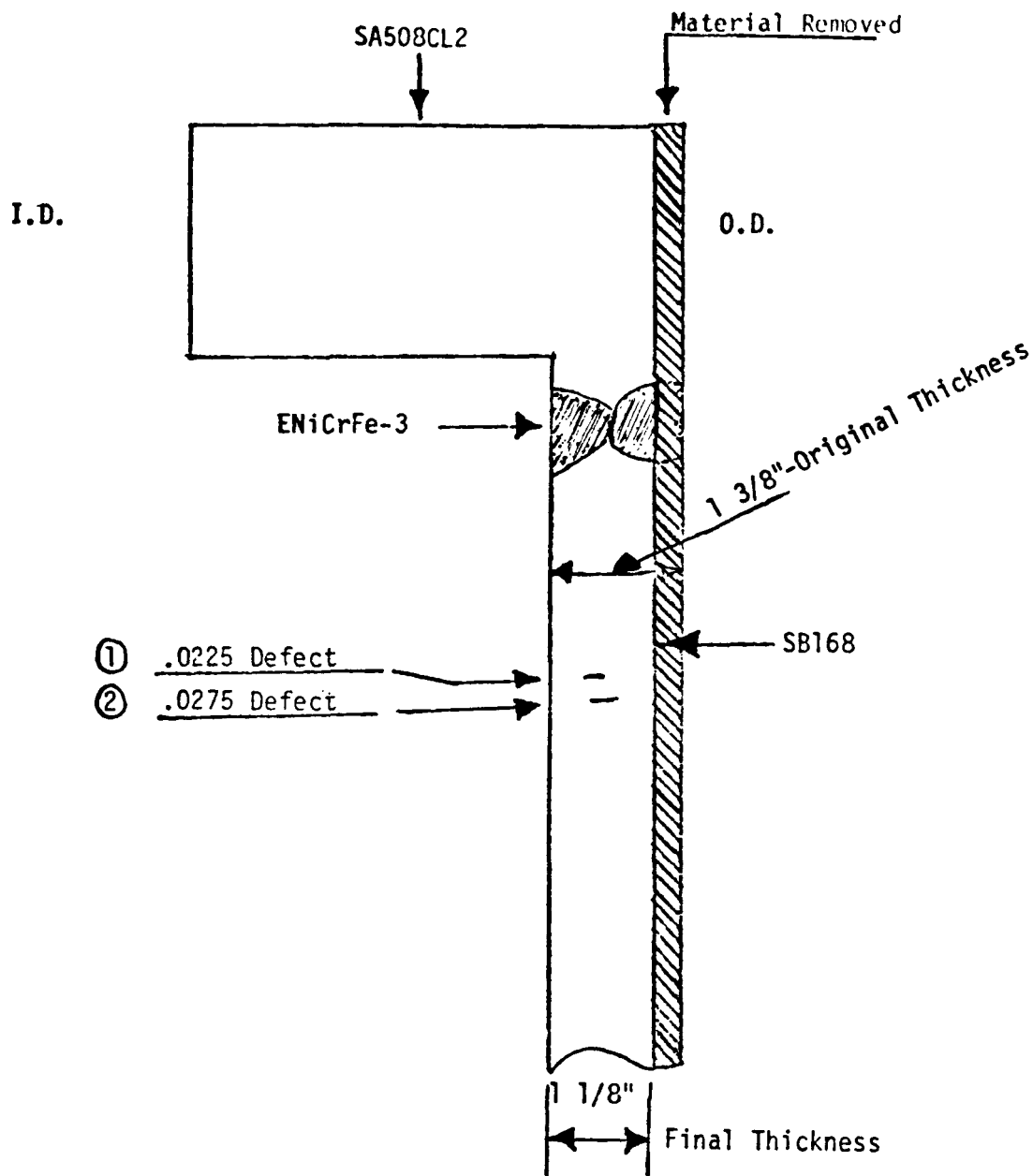
in size up to a 12-inch maximum which were confirmed by subsequent grinding operations. An x-ray can reveal indications not detectable by Ir-192 because the characteristic wave lengths of radiation is from zero to peak KV. Ir-192 emits wave lengths in the 137 to 651 KV range (about 12 useful photos) for a radiation spectrum more homogeneous than mechanically produced radiation. The Ir-192 radiographs are of lower contrast than those produced with an x-ray machine and with the lowered contrast, the ability to distinguish adjacent areas of different densities is reduced.

After repair, stress relief, and 100% re-radiography of the weldments, one quarter of an inch (1/4 inch) of material was removed resulting in a final weld thickness of 1-1/8 inch. This being the final surface, an additional radiographic inspection was performed.

The intermediate riser weld (WR-7) was radiographically acceptable in the 1-3/8 inch thickness range using both isotope and x-ray. However, after final machining (1/4 inch material removal) and stress relief, final radiography revealed seven rejectable indications ranging from 1/4 inch to 5-3/8 inch. These indications were not detected in the 1-3/8 inch thickness range, see Figure 1-32.

B. Defect Detectability Versus Material Thickness

The detection of these rejectable indications is directly related to the increased defect sensitivity achieved by reducing the surrounding material thickness. As an example, based on 2% radiographic sensitivity, the minimum indication size which could be detected in a 1-3/8 inch thick weldment would be 0.0275 inch even if it were properly oriented. The minimum indication size which could be detected in a 1-1/4 inch thick weldment would be 0.0225 inch if properly oriented. Thus, an indication of 0.0225 inch could exist if in a 1-3/8 inch weldment and not be detected even though it were properly oriented.



Defect ① Detectable at 1 1/8" Level Only

Defect ② Detectable at 1 3/8" Level

Figure 1-32 Bi-Metallic Weld

One thought to keep in mind is not to be surprised if indications are revealed when the source type is improved or when material thickness is reduced. An acceptable radiograph means that no rejectable indications are visible at that particular quality level of radiography and it does not mean that the entire volume is free of indications.

The fact that similar radiographic examination problems occurred in two separate plants led to a concern that it may be a generic problem with this type of weld. This resulted in a recommendation that project participants be alerted to the potential problems associated with radiographic inspection of this type of bi-metallic weld.

1.4.2.2 Reactor Closure Head Penetrations

Six stainless steel nozzles of three different configurations were hardfaced on the inside diameter with cobalt chrome hardfacing material. Full-scale mockups, representative of the three production nozzle configurations except for length, were first hardfaced to qualify the welding procedure in accordance with ASME B&PV Code, RDT Standards, and Westinghouse specifications before production welding started. The plasma transferred arc process with powdered metal addition was used.

The stainless steel nozzles ranged from 66 inch to 70 inch long, approximately 18 inch ID with a 2 to 3 inch wall thickness. A Linde PSM-2 plasma hard surfacing machine with high frequency pilot and transferred arc start using a heavy duty PT-9 torch with standard equipment gas cup was used with cobalt chrome powder. The oscillation of each pass was 2.25 inch maximum with a 0.250 inch bead overlap.

Production problems were encountered in hardfacing the nozzles and four out of the six nozzles required either repairs or complete rewelding to achieve acceptable hardfacing deposits.

A major problem was experienced during the ID hardfacing of the first nozzle when it cracked in two places immediately after completing the last pass of

hardfacing. The cracks were approximately 4-6 inches long and went completely through the hardfacing. Similar cracking was also experienced on the mockups. The initial preheating technique on the reproduction nozzles was to only preheat the hardfacing area and not the entire nozzle length. This proved to be unsatisfactory because it produced a thermal gradient too close to the hardfacing and resulted in cracking. The preheat burners on the production nozzles were increased to full length of the nozzles which produced a more uniform temperature over the entire nozzle. No other cracks were experienced on any nozzles hardfaced after increasing the preheat burner length. The preheat temperature was also raised from 400°F to 800°F. However, it was felt that the increase in preheat temperature was not the important variable in successfully hardfacing the nozzles.

Other hardfacing defects were relatively minor compared to the cracking. These defects varied from underfill on one pad area to porosity and undercut on the ID of the nozzles. In fact, the high preheat was viewed as being the cause of the minor defects as well as producing severe maintenance problems for the equipment and also made it difficult for the operator to work around the nozzles. Porosity was caused by burned gas hoses on the torch and undercut resulted from the operator not being able to get close enough to the work to check setup or oscillator malfunction due to the high temperature inside the nozzles. Some parts in the torch head had to be routinely replaced after welding each nozzle, due to deterioration caused by the high temperature. Underfill was encountered on one nozzle pad (approximately 3.5 inch x 1 inch) due to the drawing detail for the pre-machined condition which left a straight wall next to the pad farthest back in the nozzle. This made the pad unweldable until the ledge was ground back to allow proper access with the plasma arc torch.

After welding, a post-welding heat of 800°F was maintained for approximately 15 minutes. The nozzle was wrapped with Kaowood prior to placing in the holding furnace at 800°F.

A mockup hardfacing repair was attempted on a previously ground out cavity. The gas tungsten arc process was used with 1/8 inch Co Cr-A filler metal and 400°F preheat. The cavity could not be successfully repaired due to repeated

cracking of the hardfacing. A subsequent hardfacing repair was successfully qualified. The first layer was applied with the automatic plasma arc process and a second pass applied with the gas tungsten arc process. The welding parameters were the same as on the qualification nozzle. The repair deposit was 1/16 inch thick after surface grinding.

Both the mockup weld qualification and the production hardfacing were inspected to RDT Standard acceptance levels. The following examinations were performed:

- o Visual - prior to welding, after welding, and after finish machining
- o Dimensional
- o Liquid Penetrant - prior to welding, after welding
- o Ultrasonic - after welding
- o Hardness Tests - 38-47 RC
- o Chemical Tests - Weld mockup for procedure qualification, 6% Fe
- o Cross Section Examination - depth of weld layer 0.25 inch after machining

The maximum iron dilution was later relaxed from 6% to 12% maximum at either 0.125 or 0.187 inch from the stainless steel base metal as demonstrated on the mockup.

From the experience of hardfacing the mockup and production nozzles, the following conclusions and recommendations can be made for depositing cobalt chrome hardfacing on future work.

- o Uniform and carefully controlled preheat must be applied to the work being hardfaced in order to prevent cracking.

- o Consideration should be given to lowering the preheat temperature from 800°F to 400°F. This would produce better welding conditions for hardfacing and help eliminate many of the defects caused by the high preheat.
- o Local repairs and pickup can be successfully made to previously deposited hardfacing using the plasma transferred arc process.
- o The RDT Standard requirement for a maximum iron content to 6% can be met in two layers at a minimum thickness of 0.125 inch. The recommendation was made that the minimum hardfacing thickness should be reduced from 0.25 to 0.125 inch. A weld thickness of 0.125 inch would reduce shrinkage effects and cracking hazards.

1.4.2.3 Welding of the Horizontal Baffle Retaining Ring and the Horizontal Baffle Baseplate

The welding sequences of the two components will be addressed separately due to the size and section differences as well as the design tolerance considerations.

1.4.2.3.1 Horizontal Baffle Retaining Ring

The welding of the Horizontal Baffle Retaining Ring (HBRR) poses a unique combination of joining problems and to fabricate it according to the drawing requirements would prove difficult for most fabricators.

The actual fabrication of Type 316 SS for the 13 ft HBRR would not be difficult. However, applying the hardfacing to the ring and subsequent machining would involve many problems. The hardfacing application would result in a total loss of 1-inch on the OD due to hardfacing weldment shrinkage and a bell or dish effect on the OD opposite to the hardfacing.

The change in dimension due to weld metal shrinkage continues even after solidification of the cooling weld metal. The thermal contraction of solid metal during cooling generates stresses, the maximum stress is limited to the

yield strength of the weld metal at the particular temperature. Distortion deviation from the desired form occurs because of localized thermal expansion and contraction, distribution of stresses in the weldment, and the strength of members upon which these stresses act. To anticipate the final movement of the 13 ft OD HBRR after final machining to the approximate 3 inch x 2 inch shape will be difficult. A combination of weld fixturing with a high reliability of the welding equipment used for the process is required in conjunction with a thorough preanalysis of the part to insure proper and effective control of the component distortion.

However, the method that would assure meeting the design tolerances and eliminate the fabrication distortion problems discussed above would be to make the HBRR completely out of the wrought form of Alloy C-276. Haynes 273 or Hastelloy C are filler materials, whereas Hastelloy Alloy C-276 is a wrought version of Hastelloy Alloy C.

Discussions^{1,2} with the Wear Technology Division of Cabot Corporation and Cleveland Hardfacing Co. determined that Hastelloy Alloy C-276 could be ring rolled to form the HBRR and would be the most cost effective method of meeting the design tolerances. However, it was noted that the wrought product will have a lower carbon content than a hardfaced surface. The design engineering group will want to evaluate the adequacy of the wrought product form C-276 for the HBRR.

Ladish and Wyman-Gordon Company are two companies that would be able to fabricate a wrought forged C-276 product the size of the HBRR.

1.4.2.3.2 Horizontal Baffle Baseplate

A. Prior to Welding the Horizontal Baffle Baseplate Segments

-
1. Telephone conversation between S. J. Walmsley (WAESD) and R. Kirchner (National Sales Manager of the Wear Technology Division, Cabot Corp) February 25, 1981.
 2. Telephone conversation between S. J. Walmsley (WAESD) and John Ceretto (President of Cleveland Hardfacing) February 25, 1981.

- o The method used to prepare the baseplate segments shall leave the weld preparation with reasonably smooth surfaces. When thermal cutting methods are used to remove metal, additional material shall be removed by mechanical means to bright sound metal.
- o The weld edge preparation surfaces in material 1 inch or more in thickness shall be examined by liquid penetrant and evaluated in accordance with the acceptance standards of NG-5130.
- o The weld joint configuration of the six baseplate segments shall be a double-groove weld joint which will balance the distortion that will occur during the welding process.
- o The weld joint and adjacent base metal for at least 2 inch on each side of the weld preparation shall be free of grease, rust, scale, dirt, or other foreign matter. Cleaning can be accomplished by wire brushing, grinding, or by using an approved solvent cleaner. Solvent cleaners shall comply with the E-Spec requirements. Grinding shall be done with rubber or resin bonded alumina or silicon carbide wheels. Only stainless steel brushes and grinding wheels that have not been used on materials other than stainless steel shall be used for hand and power cleaning grinding.
- o Fit-up shall be accomplished using clamps, alignment lugs, tack welds, or other appropriate means to properly align the joint for welding. If alignment lugs are used, the lug material must be of identifiable stainless steel material of the same composition. When tack welds are to become part of the finished weld, they shall be visually examined and defective welds removed. The immediate area around temporary attachments is to be marked in a suitable manner so that after removal, the surface can be dye penetrant examined in accordance with NG-4231.2(g). Both the welding procedure and welder are to be qualified in accordance with requirements of Section IX.

B. Welding of the Baseplate Segments

- o Prior to production welding, the requirements of RDT F6-5 shall be met for all new procedures and their qualification, all new performance qualifications and all re-qualifications (ref: E-953031, Rev. 9, para. 5.4.1).
- o The double groove weld joint baseplate segments are to be welded in the flat position, 1G. This joint design and welding position will optimize control of shrinkage and distortion. The double groove weld joint will allow the weld bead sequence to be balanced around the neutral axis and thus, will control weld shrinkage.
- o The amount of base metal distortion can be controlled by the welding process. A weld process that employs a high speed welding technique reduces the amount of base material affected by the heat of the arc and progressively decreases distortion. Semi-automatic and fully automatic submerged arc welding equipment or vapor-shielded automatic electrodes can be used for faster welding speeds. Another process that would be conducive to higher welding speeds with fewer weld passes is the submerged arc bulk weld process.
- o Control of transverse shrinkage is an important factor in being able to meet the design tolerances for the baseplate. A weld made with more passes will have greater shrinkage than one made with fewer passes. Therefore, the processes mentioned above would also optimize transverse shrinkage control. The total amount of transverse shrinkage of the six baseplate weldment can be closely estimated from computed cross-sectional area of the weld. Calculations show that transverse shrinkage is about 10% of the average width of the cross-section of the weld area. Allowance must provide for approximately 1/2 inch total transverse shrinkage during the welding of the baseplate.

- o Before applying weld metal to the second side of the double groove weld joint, the root of full penetration on the double welded joint is to be prepared by suitable methods such as chipping, grinding or thermal gouging to assure sound metal.
 - o The surface of the finished weld may be flush with the base material or may have a 1/8 inch maximum uniform reinforcement.
 - o Non-destructive examination shall be in accordance with NB5000 and E-Spec requirements.
- C. Assembly Techniques that will help Control Distortion of the Baseplate
- o Clamp the member in position and hold during welding.
 - o Preset the joint to offset expected contraction.
 - o Pre-bend the member to offset expected distortion.
 - o Before welding, clamp two similar members back to back with some pre-bending.
 - o Use strong-backs, jigs, and fixtures to maintain proper fit-up and alignment during welding.
 - o Make allowances for contraction when a joint is assembled.
 - o Arrange the erection, fitting, and welding sequences so that parts will have freedom to move in one or more directions as long as possible.
 - o Use sub-assemblies and complete the welding in each before final assembly and welding together.
 - o If possible, break the member into proper sections, so that the welding of each section is balanced about its own axis.

- o Weld the more flexible sections together first so that they can be easily straightened before final assembly.

D. Hardfacing of the Horizontal Baffle Baseplate

- o After the segments of the horizontal baffle baseplate are welded together and before machining of the plate, the hardfacing is to be applied.
- o The main criterion for selection of a hardfacing process is the ability of the process to limit dilution while achieving a high deposition rate, and thus avoid the necessity for a deposit thickness greater than that required for service. The plasma transferred arc welding process would be the optimal mode of application. This process would use a powder form of the hardfacing alloy with a weld metal dilution rate of 5-20%, a deposition rate of 4 to 16 lb per hour for a minimum deposit thickness of 1/16 inch. Single pass deposits between 0.010 and 1/4 inch thickness and up to 1-1/4 inch in width can be closely controlled. Speeds of more than 20 inch per minute can be obtained with 95% deposition efficiency.
- o While the Haynes 273 alloy is not prone to surface cracks during the hardfacing process, cracking can be aggravated by thermal gradients developed during welding and by differences in the thermal expansion-contraction characteristics of the base metal and overlay. Cabot Stellite, the supplier of Haynes 273, recommends a preheat of around 350-450°F prior to welding as well as a slow cool in order to reduce the residual stresses. The slow cooling can be accomplished by covering the hardfacing with insulating blankets after welding until cool.
- o To minimize distortion during hardfacing, consideration should be given to deposit the coating by "quartering" that is, with each bead covering 90° of the ring circumference. After the first bead

has been deposited, the ring is turned 180°, and then alternately through 90° and 180° until the overlay is completed.

- o Post-weld heat treatment is not required.
- o Hard surfacing shall be examined by liquid penetrant and ultrasonic method in accordance with NG5000 and E-Spec requirements.
- o After the hard surfacing process, the baseplate can then be machined to the design requirements of the drawings.

2.0 SODIUM FIRE PROTECTION SYSTEM - SYSTEM 26A

2.1 Functions and Description

2.1.1 Functions

The primary functions of the Sodium Fire Protection System (SFPS), also designated as System 26A in the Clinch River Breeder Reactor Plant (CRBRP), are:

- A. Suppression of sodium and NaK fires
- B. Extinguishment of sodium and NaK fires
- C. Detection of fires or incipient fires (precursor smoke)
- D. Indication of fire location
- E. Actuation of alarms
- F. Protection of personnel from sodium and NaK fires
- G. Smoke abatement
- H. Prevention of chemical reaction between sodium or NaK and concrete.

2.1.2 Description

2.1.2.1 System Features

- A. Plant liquid metal fire protection services shall be provided by the SFPS and by features included in other systems, which serve as a fire protection function. The liquid metal fire protection features

provided by other systems, listed in Table 2-1, are essential to the overall plant fire protection design. Details of the inerted cell liner system are given in Figure 2-1.

- B. In cells normally having air atmospheres, where sodium or NaK spills are postulated to occur, the following fire protection methods shall be provided by the SFPS. (Table 2-2 lists the areas served and the specific methods employed.)

- (1) Catch pans shall be provided to prevent chemical reactions between spilled sodium or NaK and concrete.

(a) The depth of the catch pan shall be sufficient to contain the maximum postulated spill volume.

(b) The sides of the catch pan shall be extended above the maximum postulated spilled liquid level, to prevent washing of spilled liquid over and above the edge of the pan, and to allow space for installation of a fire suppression deck, as required.

(c) A continuous lip plate shall be provided above the edge of the catch pan to restrict Na/NaK entry into the space between the catch pan and the wall.

- (2) A fire suppression deck shall be installed above the catch pan bottom to allow drainage into the catch pan and to act as part of an oxygen depletion zone.

(a) The number and size of downcomers in the deck shall be such that the drainage rate of spilled liquid into the catch pan is sufficiently rapid to prevent appreciable holdup of liquid above the deck.

TABLE 2-1
 ESSENTIAL LIQUID METAL FIRE PROTECTION
 FEATURES PROVIDED BY OTHER SYSTEMS
 (Sheet 1 of 2)

Building	Cell No.	Cell Description	Floor Elevation (ft)	Atmosphere ^{1,2} (Normal)	Cell ^{3,4}	
					Liner	Fluid
RSB	331	System 81 - EVST Third Loop IHX Cell	798-6 in.	N ₂	Yes	Na/NaK
RSB	331A	System 81 - EVST Third Loop Pipeway	798-6 in.	N ₂	Yes	Na
RSB	351A	System 81 - Main EVST Cooling System Pipeway	755	N ₂	Yes	Na
RSB	351B	System 81 - Main EVST Cooling System Pipeway	755	N ₂	Yes	Na
RSB	351C	System 81 - Pipeway	770	N ₂	Yes	Na
RSB	386	System 85 - EVS SSP Cell	779	N ₂	Yes	Na
RSB	387	System 85 - PTI Cell	765	N ₂	Yes	Na
RSB	337	System 41 - EVST Cell	798-4 in.	N ₂	Yes	Na
RSB	351D	System 81 - Pipeway	770	N ₂	Yes	Na
RSB	357A	System 81 - Cooling Loop B Pipeway	798-6 in.	N ₂	Yes	Na
RSB	357B	System 81 - Cooling Loop B Pipeway	798-6 in.	N ₂	Yes	Na
RSB	360A	System 81 - EVS Cooling Loop A Pipeway	798-6 in.	N ₂	Yes	Na
RSB	341	System 41 - Fuel Handling Cell	781	Ar	Yes	Na
RSB	361	System 81 - EVS Sodium Cold Trap Cell	779	N ₂	Yes	Na
RCB	101A	Reactor Cavity	733	N ₂	Yes	Na
RCB	101C	Flowmeter Cell (PHTS Loop 1)	786	N ₂	Yes	Na
RCB	101D	Flowmeter Cell (PHTS Loop 2)	786	N ₂	Yes	Na
RCB	101E	Flowmeter Cell (PHTS Loop 3)	786	N ₂	Yes	Na
RCB	102A	Overflow and Primary Sodium Storage Tank Cell	733	N ₂	Yes	Na
RCB	102B Area	System - Reactor Cavity Piping Penetration	782	N ₂	Yes	Na
RSB	357	SDD 81 - EVS Cooling Loop B Cell	779	N ₂	Yes	Na/Nak
RSB	360	SDD 81 - EVS Cooling Loop A Cell	779	N ₂	Yes	Na/Nak

TABLE 2-1
ESSENTIAL LIQUID METAL FIRE PROTECTION
FEATURES PROVIDED BY OTHER SYSTEMS
(Sheet 2 of 2)

Building	Cell No.	Cell Description	Floor Elevation (ft)	Atmosphere ^{1,2} (Normal)	Cell ^{3,4} Liner	Fluid
RCB	103	Primary Sodium Makeup Pump Cell	733	N ₂	Yes	Na
RCB	104	Primary Sodium Makeup and Future Pump Cell	733	N ₂	Yes	Na
RCB	107A	Primary Sodium Makeup Pump Valve Gallery (B)	733	N ₂	Yes	Na
RCB	107B	System 81 - Pipeway and Valve Gallery	733	N ₂	Yes	Na/NaK
RCB	107C	Primary Sodium Makeup Pump Valve Gallery (A)	733	N ₂	Yes	Na
RCB	121	PHTS Loop 1 Cell	752-8 in.	N ₂	Yes	Na
RCB	122	PHTS Loop 2 Cell	752-8 in.	N ₂	Yes	Na
RCB	123	PHTS Loop 3 Cell	752-8 in.	N ₂	Yes	Na
RCB	131	NaK Cooling Equipment Cell	769	N ₂	Yes	Na/NaK
RCB	132	SSP Cell	769	N ₂	Yes	Na
RCB	141	PTI Cell	783-9 in.	N ₂	Yes	Na
RCB	143	PTI Cell (Spare)	783-9 in.	N ₂	Yes	Na
RCB	157A	Primary Sodium Cold Trap Cell (A)	792-9 in.	N ₂	Yes	Na/NaK
RCB	157B	Primary Sodium Cold Trap Cell (B)	792-9 in.	N ₂	Yes	Na/NaK
RCB	157D	Cold Valve Gallery (A)	792-9 in.	N ₂	Yes	Na/NaK
RCB	157E	Cold Valve Gallery (B)	792-9 in.	N ₂	Yes	Na/NaK

(1) N₂ = nitrogen, Ar = argon, oxygen content 2% during normal operation.

(2) Inerting capability provided by System 82, System 25, and System 28.

(3) Cell liners provided by Building System (System 21 and System 27).

(4) The combination of steel liners and inert atmospheres provide the capability to contain spilled Na/NaK and to prevent sustained burning. Spilled Na/NaK will cool to a temperature at which reignition is unlikely even if exposed to an air atmosphere.

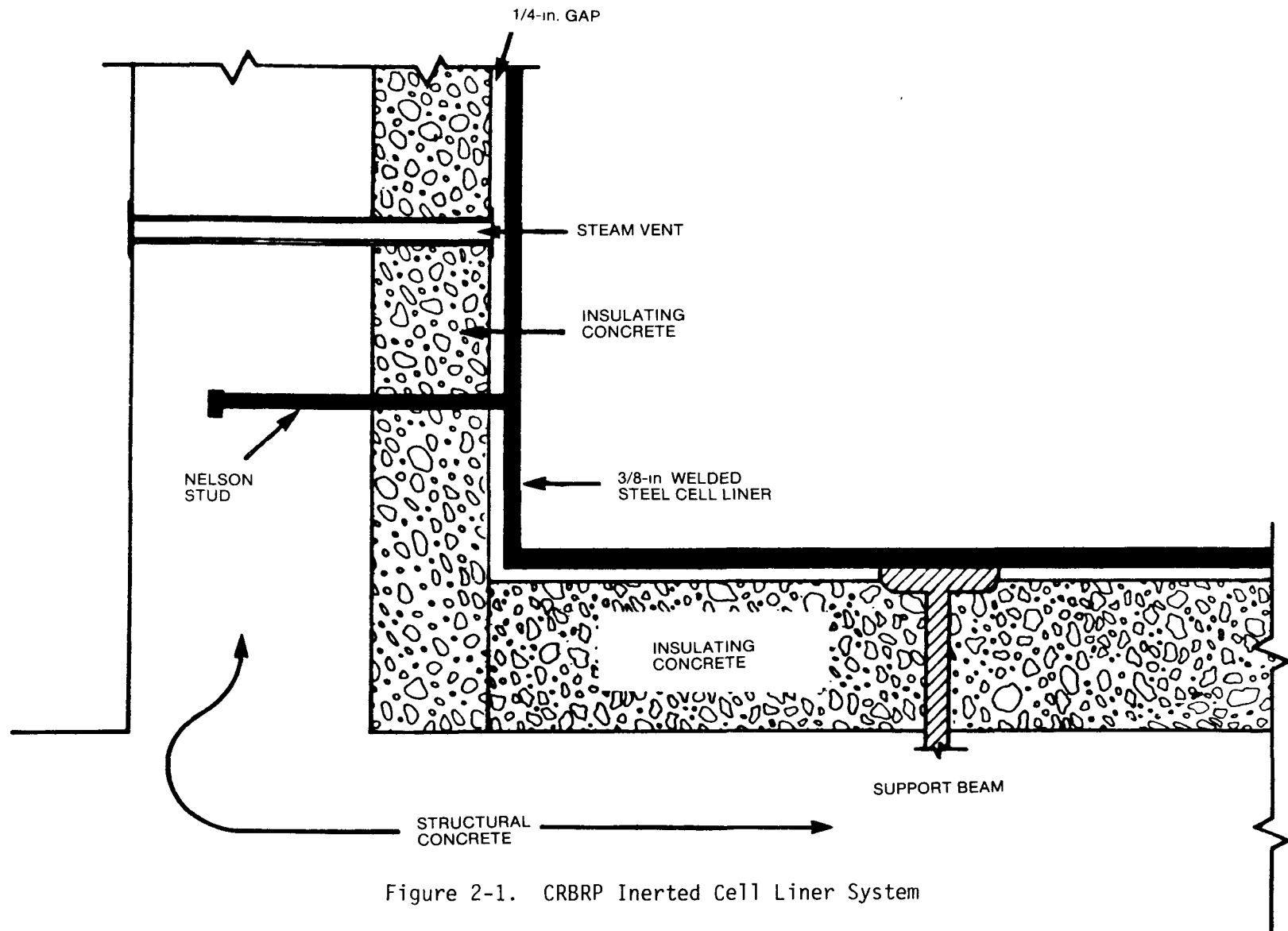


Figure 2-1. CRBRP Inerted Cell Liner System

TABLE 2-2
CELL DESIGNATION AND SODIUM FIRE PROTECTION FEATURES

Building	Cell No.	Cell Description	Floor Elevation (ft)	Atmosphere (Normal)	Catch Pan	Fluid	Fire Suppression Deck	Drains	Smoke Detection
RSB	352A	SDD 81 - EVST Air Blast Heat Exchanger Loop A Cell	765	Air	Yes	NaK	No	No	Yes
RSB	353A	SDD 81 - EVST Air Blast Heat Exchanger Loop B Cell	765	Air	Yes	NaK	No	No	Yes
SGB	207	Steam Generator Cell - Loop 1	733	Air	Yes	Na	Yes	No	Yes
SGB	208	Steam Generator Cell - Loop 2	733	Air	Yes	Na	Yes	No	Yes
SGB	209	Steam Generator Cell - Loop 3	733	Air	Yes	Na	Yes	No	Yes
SGB	211	PSST Cell	733	Air	Yes	Na	Yes	No	Yes
SGB	224	Steam Generator Cell - Loop 1	765	Air	Yes	Na	No	Yes	Yes
SGB	225	Steam Generator Cell - Loop 2	765	Air	Yes	Na	No	Yes	Yes
SGB	226	Steam Generator Cell - Loop 2	765	Air	Yes	Na	No	Yes	Yes
SGB	227	IHTS Pipe Cell - Intermediate Bay Loop 1	765	Air	Yes	Na	Yes	No	Yes
SGB	228	IHTS Pipe Cell - Intermediate Bay Loop 2	765	Air	Yes	Na	No	No	Yes
SGB	230	IHTS Pipe Cell - Intermediate Bay Loop 3	765	Air	Yes	Na	Yes	No	Yes
SGB	231	IHTS Pipe Cell - Intermediate Bay Loop 1	765	Air	Yes	Na	Yes	No	Yes
SGB	232	IHTS Pipe Cell - Intermediate Bay Loop 3	765	Air	Yes	Na	Yes	No	Yes
SGB	248	IHTS Pipe Chase - Loop 2	794	Air	Yes	Na	No	Yes	Yes
SGB	251	IHTS Pipe Chase - Loop 1	794	Air	Yes	Na	No	Yes	Yes
SGB	252	IHTS Pipe Chase - Loop 3	794	Air	Yes	Na	No	Yes	Yes
SGB	211A	Valve Gallery	752	Air	Yes	Na	No	Yes	Yes
RSB	332	SDD 81 - NDHX Third Loop Cell	840	Air	Yes	NaK	No	No	Yes
RSB	350	SDD 81 - NaK Storage Vessel	733	Air	Yes	NaK	Yes	No	Yes
RSB	354	SDD 81 - Pipe Chase	765	Air	Yes	NaK	Yes	No	Yes
RSB	355	SDD 81 - Pipe Chase	765	Air	Yes	NaK	Yes	No	Yes
SGB	244	Steam Generator Cell Loop 1	806	Air	Yes	Na	No	No	Yes
SGB	245	Steam Generator Cell Loop 2	806	Air	Yes	Na	No	No	Yes
SGB	246	Steam Generator Cell Loop 3	806	Air	Yes	Na	No	No	Yes

- (3) Insulation between the catch pan and the building floor and between the catch pan and building walls shall be provided as required to limit the thermal impact of liquid metal spills on structural concrete.
- (4) The catch pans may be interconnected vertically or horizontally between cells to reduce the spill depth in a given cell to the level allowed by consideration of cell penetrations and building structural loadings.

2.1.2.2 Catch Pans, Insulation, Fire Suppression Decks, and Drains

Liquid metal (sodium) fire suppression system features are provided in all air-filled cells of the Steam Generator Building (SGB) and the Reactor Service Building (RSB) containing liquid metal systems and piping, as indicated in Table 2-2.

These cells contain the nonradioactive sodium piping and components of the Intermediate Heat Transport System (IHTS) (System 51B), the Auxiliary Liquid Metal System (System 81), and Impurity Monitoring and Analysis System (System 85), and portions of the NaK cooling piping and components of System 81.

The sodium fire suppression system is comprised of four basic features:

(1) catch pans that contain spilled liquid metal, (2) insulation between the catch pan plate and the surrounding concrete structure, (3) fire suppression decks that cover the catch pan open area, and (4) interconnections between adjacent catch pan cells that allow drainage of liquid metal from one cell to another. A steel grating above the fire suppression deck is provided where required to serve as a walkway for equipment and personnel access. The steel grating also acts to prevent damage to the fire suppression deck. A typical catch pan-fire suppression deck arrangement is shown in Figure 2-2.

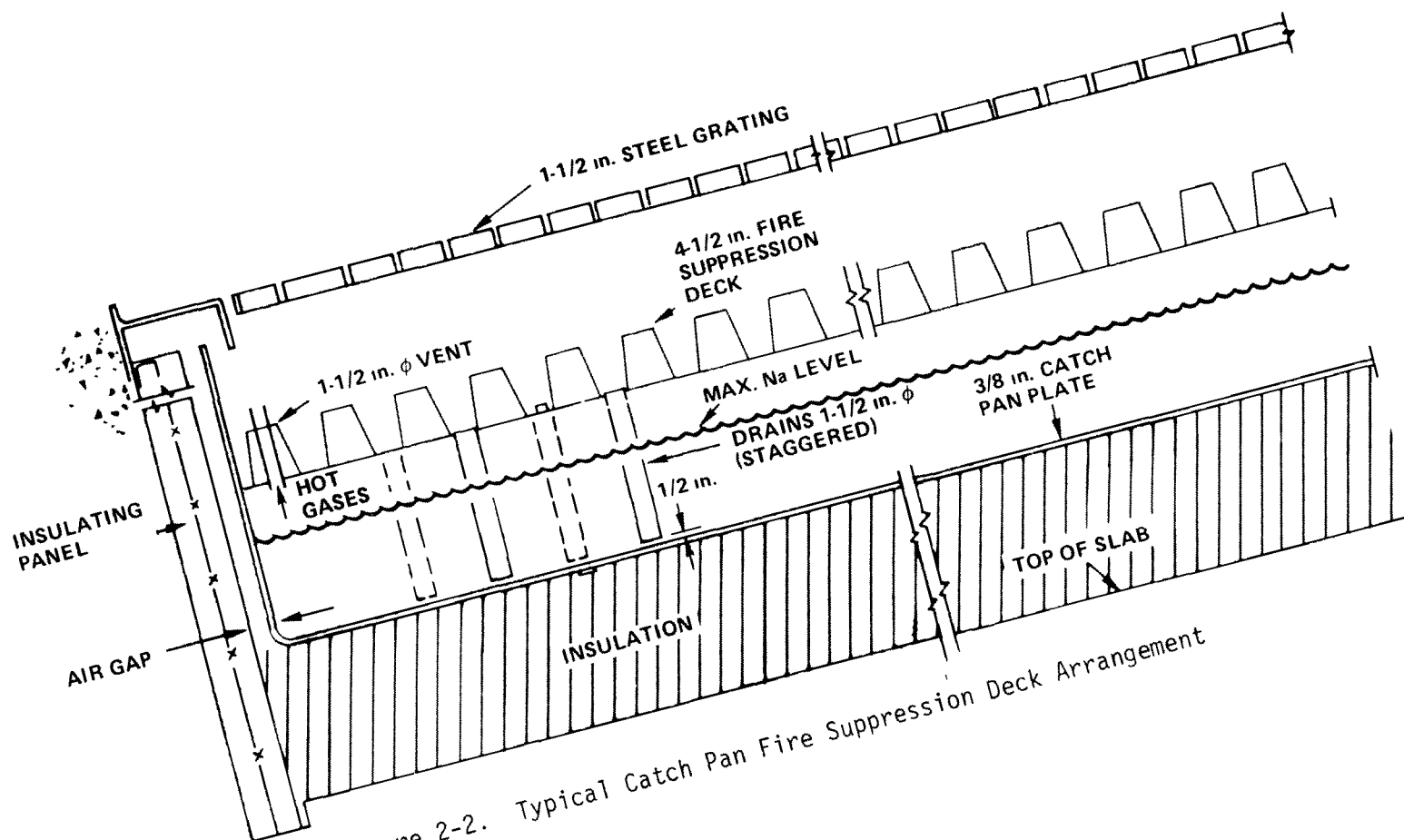


Figure 2-2. Typical Catch Pan Fire Suppression Deck Arrangement

2.1.2.2.1 Catch Pans

The catch pan consists of a carbon steel plate assembly. It covers the entire floor surface of the cell and extends vertically up the wall to a height sufficient to prevent spilled liquid metal from flowing over the edge of the plate into the area between the plate and the wall. A continuous lip plate is provided at the top of the catch pan side wall to prevent sodium or NaK from running down the structural concrete cell walls into the region behind the catch pan plate sidewalls. The catch pan is free floating and is supported above the concrete floor of the cell by a continuous layer of granular insulating material and by steel beams. Open catch pans (without fire suppression decks) are used in those cells where the postulated spill volumes are small and open pool burning does not result in concrete temperatures that degrade structural concrete or aerosol quantities that affect safety-related equipment in adjacent loops. In the event of a liquid metal spill, the catch pan contains the liquid metal and prevents contact between the liquid metal and the concrete structure.

2.1.2.2.2 Insulation

Insulation is provided beneath the catch pan plate, between the plate and the concrete floor, and behind the wall sections of the catch pans. Granular insulation is used beneath the catch pan plate.

The insulation behind the wall sections of the catch pans is provided in the form of a precast insulating concrete panel. The precast insulating concrete panels are secured to the structural concrete. An air gap between the panels and the vertical catch pan plate sidewall provides additional insulation, allows for relative movement between the concrete and the catch pan plate, and vents hot gases from behind the catch pan plate to the cell atmosphere to prevent pressure buildup behind the plate.

In the event of a liquid metal spill, the insulation acts as a thermal barrier between the hot liquid metal pool and the building structural concrete.

2.1.2.2.3 Fire Suppression Deck

In cells where liquid metal spills are contained and where open pool burning poses a challenge to the structural integrity of the building structure and/or to safety-related equipment, the catch pans are provided with fire suppression decks. The fire suppression deck covers the open area of the catch pan. The deck is supported above the catch pan plate surface by a structural (steel) framework supported at the edges of the cell by embedments in the structural concrete, and in the interior of the cell by stub columns supported on the top of the catch pan plate surface by embedments extending from the structural concrete floor to the underside of the catch pan plate. The fire suppression deck is connected to the support framing, with all edges sealed to form an essentially air-leak-tight cover over the open area of the catch pan. The catch pan plate is not connected to the embedments at the fire suppression deck support points in order to ensure free expansion of the catch pan plate under accident conditions.

Carbon steel drain pipes (downcomers) are welded to the deck and extend downward to a point just above the catch pan plate. The pipes are uniformly spaced to form a uniform array over the cell floor area. Vent pipes are welded to the fire suppression deck and extend below and above the deck. The vent pipes are provided to vent hot gases from the region below the deck to the cell atmosphere to prevent pressure buildup underneath the deck.

In the event of a liquid metal spill, the liquid metal flows from the surface of the fire suppression deck through the drain pipes into the catch pan. As the liquid metal drains into the catch pans, the drain pipes become partially filled, and the effective burning surface of the resulting liquid metal pool is limited to the cross-sectional area of the drain pipes. Burning is terminated when the pipes become plugged with combustion products and air is prevented from reaching the liquid metal surface.

2.1.2.2.4 Cell Interconnections

In certain cells, where the postulated spill volumes are large compared to the floor area of the cell such that consideration of cell penetrations and building structural loading make it impractical to contain the entire volume, open catch pans equipped with drains are provided. The catch pans are pitched toward the drains.

The drains are in the form of carbon steel pipes passing through the structural concrete of the cell. Horizontal pipes interconnect cells on the same level; vertical pipes interconnect cells on different levels.

In the event of a liquid metal spill, the catch pan prevents contact between the liquid metal pool and the structural concrete, and the liquid is drained into a cell, which has the capability for fire suppression (catch pan with fire suppression deck). This concept has been extended to include draining large upper level cells into lower elevation cells where economically justified, i.e., Cells 224, 225, and 226 that drain to Cells 207, 208, and 209, respectively, as shown in Figures 2-3 and 2-4.

2.1.2.3 Fire Extinguishers

Portable sodium carbonate (NaX) fire extinguishers, hand-held and wheeled-cart types, are provided and stored in locations convenient to spaces in which there is sodium and NaK equipment in the reactor service building (RSB), reactor containment building (RCB), steam generator building (SGB), and the sodium and NaK receiving station.

Some 58 portable fire extinguishers are located throughout the plant as shown in Table D-2 of System Design Description (SDD) 26A (Reference 1).

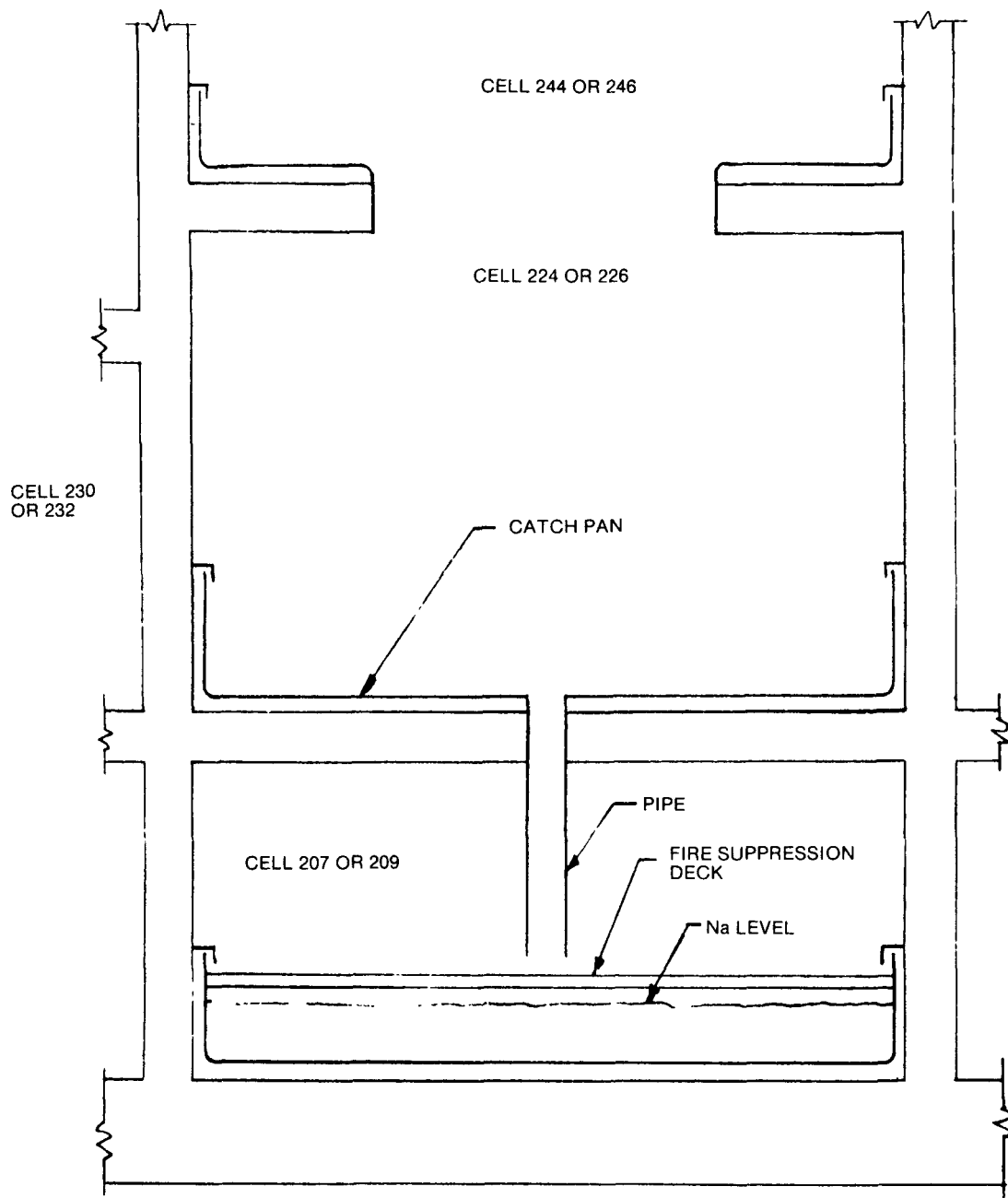


Figure 2-3. Catch Pan System Arrangement,
Steam Generator Loops 1 and 3

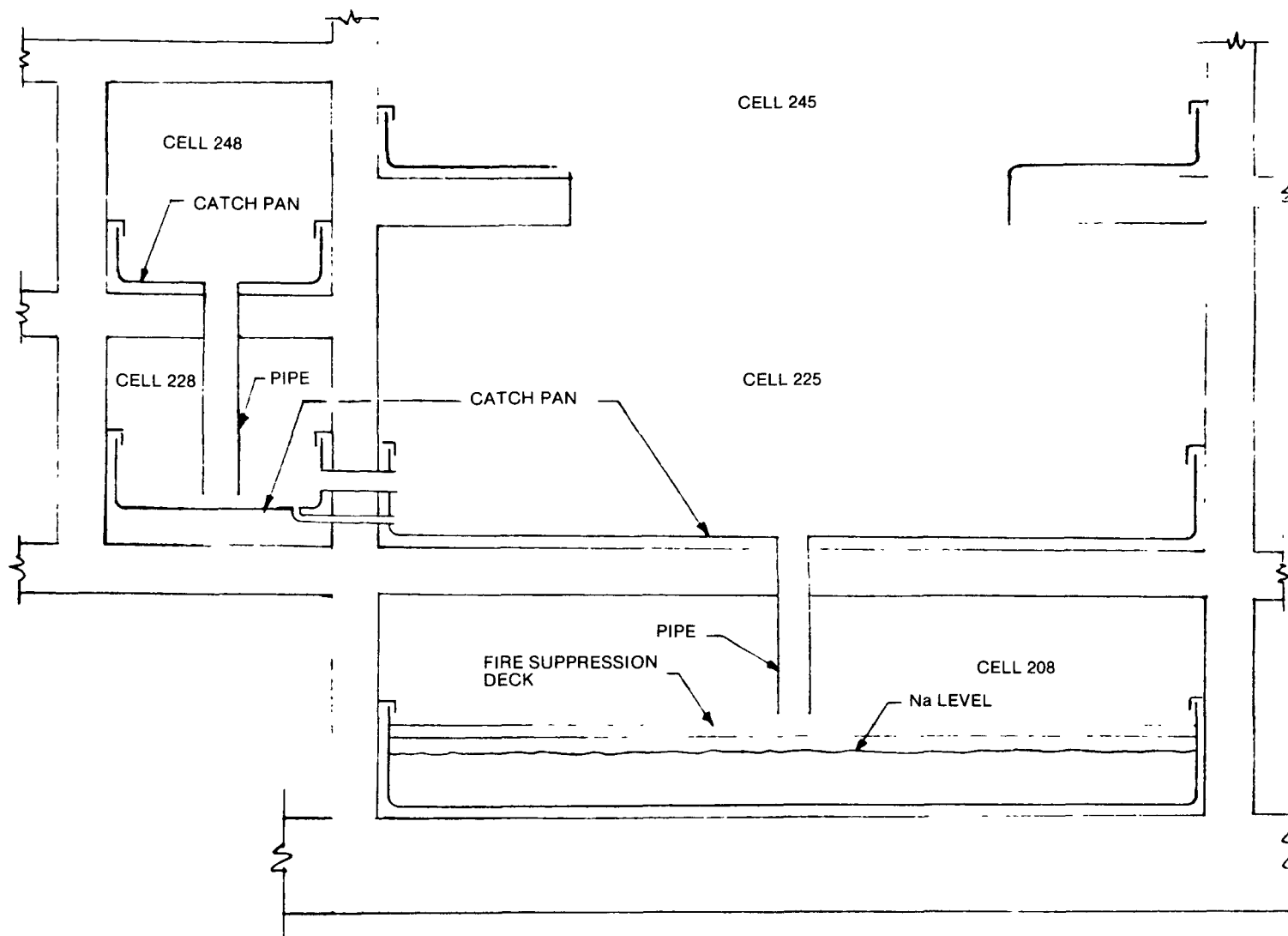


Figure 2-4. Catch Pan System Arrangement,
Steam Generator Loop 2

2.1.2.4 Instrumentation and Control

The SFPS instrumentation is designed to:

- A. Detect the presence of the location of incipient and existing sodium and NaK fires and provide this information to the plant operator
- B. Detect inoperative detectors and provide inoperative detector information to the plant operator
- C. Generate and transmit signals to interfacing systems in case of a sodium fire or aerosol detection.

Details of the sodium fire instrumentation and control system are provided in Section 2.2.3 of SDD 26A (Reference 1) and in Reference 2.

2.1.2.5 Safety Class

The system is a nonsafety class item. The catch pan fire suppression deck system is designed as an "engineered safety feature."

2.2 Evolution of System 26A Design

The design of the sodium fire protection subsystem began in 1973 and was still ongoing at the time of project termination. While the basic concept of the system changed only slightly over this time period, sufficient information did not exist initially to permit design of the fire suppression decks and determination of the fire consequences (structural concrete temperatures and aerosol releases). A development test program for verification of the effectiveness of the design features was required. A series of tests ranging from small- to large-scale sodium fires was conducted that resulted in changes to the design (this aspect is further discussed in Section 2.3.1).

2.2.1 Conceptual Design

A study of fire suppression tests reported in the literature developed information for the conceptual design of the fire suppression grating system and defined area and space requirements, complexity, cost, and suppression efficiency.

The basic liquid metal fire protection for the reactor containment building and the reactor service building consists of steel liners and inert atmospheres for the cells containing liquid metal. The steel liners will prevent contact between the liquid metal and the concrete structure, preventing a hydrogen-producing chemical reaction between liquid metal and the water in the concrete.

Inerting these cells will effectively prevent a self-sustaining fire and cool the spilled metal to the point where ignition no longer occurs.

In the steam generator building, the Sodium Fire Protection System is designed to contain sodium spills and extinguish potential sodium fires with steel pans, sized to accommodate the estimated sodium spill quantity for each pan location. Steel grating covers on the pans will permit sodium to drop through the grating into the pan but restrict the flow of oxygen from the air to the surface of the sodium. In addition, the pans will be supplied with nitrogen. In the event of a sodium spill, nitrogen will displace oxygen in the space below the grating and extinguish the fire.

Portable sodium carbonate fire extinguishers are provided for protection against fires, in areas not equipped with in-place fire protection equipment, to support maintenance, refueling, component handling and transport, and inspection operations where the potential for fires exists. They also provide protection against fires in normally inerted cells during periods when the cell is deinerted for maintenance and for sodium/NaK loading and unloading operations.

One of the primary problems for SFPS design was lack of data in the literature pertinent to determination of design parameters for the fire suppression deck (size and density of openings) and nitrogen flood rates. That lack made determination of system effectiveness uncertain. Analytical studies of system performance indicated that the size and density of openings in the deck were important parameters in determining sodium burning rates and drain times. Also, the effectiveness of the nitrogen flood, as employed in the experiments, had not been established.

A significant analytical effort was conducted during 1975 and 1976 in an attempt to characterize an adequate fire protection system for air-filled cells that would limit structural and environmental damage to acceptable levels for large sodium spills (equivalent to the complete rupture of the main intermediate heat transfer system). This effort was based on the development of an analytical model that calculated sodium pool burning rates, sodium temperatures, and concrete temperatures, and accounted for the oxygen limiting effects of the fire suppression deck. The studies indicated that, although the fire suppression deck is effective in reducing the sodium burning rate, the sodium pool can remain liquid and continue to burn at a low rate for very long periods of time. The potential effects on building structures due to elevated concrete temperatures, and the potential effects on safety-related equipment due to the sodium oxide aerosols produced, were judged to be unacceptable.

2.2.2 Preliminary Design

In 1977, an air-filled-cell task force was established for the purpose of conducting an overall review of sodium fire protection requirements and features, including the criteria for selection of design basis leaks (DBLs). The task force established that the SFPS must be designed to accommodate large sodium spills, based on the total volume of liquid metal that can be spilled into a given cell. The task force also recommended deletion of the nitrogen flood feature and redesign of the fire suppression deck to provide a more

positive, but still passive, means of extinguishing fires. Evaluation of the consequences of large sodium spills showed that additional plant design features were required to limit sodium oxide aerosol releases to a level such that the performance of safety-related equipment is not impaired.

Following those recommendations, the catch pan fire suppression deck system was redesigned. A design review was held on August 24, 1978, and the arrangement was approved subject to certain action items (Reference 3). A preliminary design review of the sodium fire detection system was held on November 7, 1977, at which time the design was approved subject to certain action items (Reference 4).

2.2.3 Development Testing

A development test program for verification of the effectiveness of plant design features for the accommodation of sodium and/or NaK spills was approved, and tests of the catch pan fire suppression deck were initiated in 1979 (References 5 and 6).

Several small-scale sodium fire tests were completed that indicated improvements in the design of the fire suppression deck, which was used on the basis for the design of a large-scale sodium fire test. The results of these tests are reported in Section 2.3.1.

Tests were also conducted to measure the thermal conductivity of the insulating aggregate used under the catch pan. The results verified the acceptability of this insulating material to limit structural concrete temperatures. The insulating concrete sidewall panels were replaced with alumina-silicate panels to avoid potential problems associated with release of water, under accident conditions, from the panels.

2.2.4 Final Design

The final design of System 26A was not completed prior to project termination. The final design review was scheduled to be held on March 14, 1984. Numerous design improvements resulting from the sodium fires testing were to have been incorporated via engineering change proposal (ECP) 26036 (refer to Section 2.4.3 and Reference 10).

2.3 Key Problems and Resolutions

2.3.1 Special Design Features Incorporated as a Result of Testing

Liquid sodium is used as the coolant in both the primary heat transport system (PHTS) (System 51A) and intermediate heat transport system (System 51B), and in addition, liquid sodium and potassium are used in the auxiliary heat removal systems (System 81). Since these liquid metals react readily with oxygen, water, and other compounds (e.g., concrete), special precautions are taken in the design, construction, testing, and maintenance of the sodium/potassium systems to ensure that the potential for leakage is very small. In spite of these precautions, Na/NaK leaks were postulated to occur.

To assure that the health and safety of the public is not imperiled, as well as minimizing economic damage to plant equipment, consideration was given to the potential impact of an accidental coolant spill on building structures, plant equipment, and the potential migration to the outside environment. A design basis leak (DBL) accident had to be defined as a basis for safety evaluations. Prudence dictated that the DBL be conservative, i.e., more severe than any leak that could reasonably be expected to occur in the course of actual plant service. For CRBRP, one such accident was the postulated failure of the intermediate heat transport system (IHTS) piping within the steam generator building (SGB). This accident would result in the discharge of 940°F sodium into the air-filled cell at an initial leak rate of approximately 1250 gpm. The total spill volume would be approximately

40,000 gal over the spill duration of approximately 1 h. Evaluations of the potential consequences of this very large, improbable accident showed that:

- A. Structural concrete temperatures could exceed the design allowables for maintaining cell integrity and the IHTS loop separation required to ensure reactor decay heat removal capability.
- B. The combustion product aerosols released to the atmosphere could pose a problem to the operation of plant safety-related equipment by being ingested in other plant areas, in the air intakes of air-cooled reactor decay heat removal equipment, and in the diesel generators.
- C. Unacceptably high concentrations of hydrogen in the cell could be generated as a consequence of the reaction of sodium with water vapor released from heated concrete.

The design concept to accommodate large design basis sodium and NaK leaks in the CRBRP was to provide a means for collecting the spilled sodium to prevent chemical reactions of the sodium pool with the concrete floor and to suppress pool burning. A simple, passive, catch pan fire suppression deck system was devised to perform this function. To protect the building from overpressurization, hot gases would be vented to the outside atmosphere.

The analytical codes developed for evaluating the consequences of design basis accidents indicated that, with the above design features, concrete temperatures were reduced to acceptable levels for structural design and that combustion product aerosol releases to the atmosphere resulted in acceptably low concentrations at the site boundary. Concerns remained, however, over the potential hazards associated with jet impingement on exposed concrete surfaces and with water release from the heated concrete. Concerns also existed regarding the validity of computer code calculations. Consequently, a test program was initiated to demonstrate the effectiveness of the catch pan fire

suppression deck system and to assess the validity of computer code calculations. The results of a large-scale sodium spray (LSSS) fire test would eventually show that the effects of spray burning on structural concrete temperatures and aerosol releases were being underestimated by a large margin.

The design of the catch basin system was to ensure that burning is suppressed by means of a fire suppression deck that covers the open area of the catch pan in air cells. Inert cell pan designs also were provided with a fire suppression deck.

The design functions of the catch pan systems were to:

- A. Collect spilled sodium and NaK in a leak-tight manner in the catch pan to preclude chemical reactions with the structural concrete.
- B. Limit sodium and NaK pool burning by designing the fire suppression deck to act as a barrier between the pool and the cell atmosphere to restrict the availability of oxygen.
- C. Limit the temperature imposed on the building concrete structure by limiting the sodium or NaK burning rate and by providing thermal insulation between the concrete surfaces and the catch pan.

The catch pan subsystem consisted of three basic design features: (1) catch pans with fire suppression decks, (2) open catch pans, and (3) open catch pans with drains.

A typical catch pan (with fire suppression deck) arrangement is shown in Figure 2-2. The catch pan consists of a 3/8-in.-thick carbon steel plate assembly which covers the entire floor surface of the cell and extends vertically up the wall to a height of 2.5 to 4.6 ft.

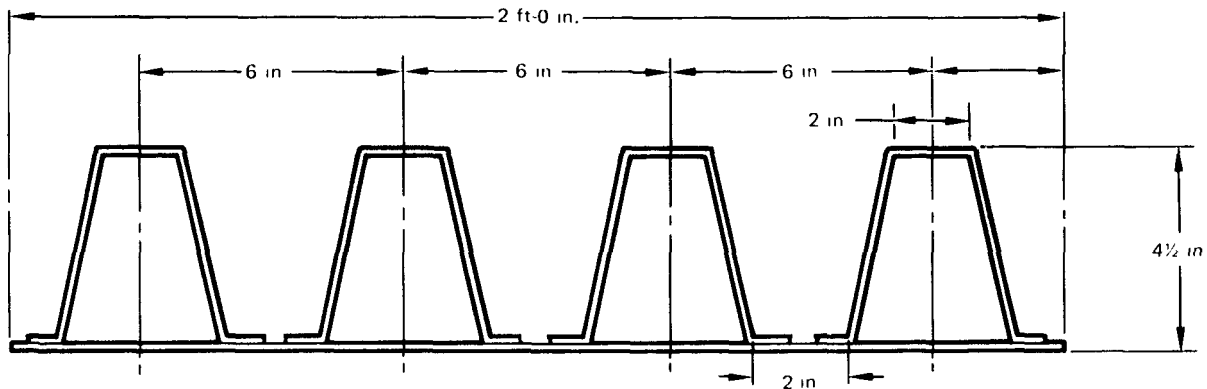
A continuous lip plate is provided at the top of the catch pan sidewall to prevent sodium of NaK from running down the structural concrete cell walls into the region behind the catch pan. The catch pan is free floating and is supported above the concrete floor of the cell by a continuous layer of insulating material. In the event of a liquid metal spill, the catch pan contains the liquid metal and prevents contact between the liquid metal and the concrete structure.

Insulation is provided between the catch pan plate and the concrete floor and behind the wall sections of the catch pans. The insulation behind the wall sections of the catch pans is provided in the form of a 3-in.-thick blanket of commercially available aluminum silicate-type insulation attached to the reinforced concrete sidewall. An air gap between the insulation and the vertical catch pan plate sidewall provides additional insulation, allows for relative movement between the insulation and the catch pan plate, and vents hot gases from behind the catch pan plate to the cell atmosphere to prevent pressure buildup behind the plate.

Insulation on the floor consists of commercially available "run-of-the-kiln" MgO aggregate extending to the bottom of the catch pan. The thickness is varied from 4 in. for open catch pans to 12 in. for catch pans with fire suppression decks.

In the event of a liquid metal spill, the insulation would act as a thermal barrier between the hot liquid metal pool and the building structural concrete.

The open area of the catch pan is covered with a carbon steel fire suppression deck. The deck is a commercially available "Q-deck" structure normally used in building construction to form composite floor structures. It consists of a 10-gauge flat steel plate onto which steel hat sections are spot welded. The hat sections run the entire length of the plate to form channels that restrict the spread of the impinging liquid metal and direct the liquid to drain pipes. Figure 2-5 is a sketch of the Q-deck arrangement. Carbon steel



"Q" DECK DETAIL

Figure 2-5. "Q" Deck Arrangement Detail

1-1/2-in.-diameter Schedule 10 drain pipes (downcomer) are welded to the Q-deck plate in each channel and extend downward to a point approximately 5/8 in. above the catch pan plate. The pipes are uniformly spaced over the cell floor area.

In the event of a liquid metal spill, the liquid metal flows from the surface of the fire suppression deck through the drain pipes into the catch pan. As the liquid metal drains into the catch pan, the drain pipes become partially filled, and the effective burning surface of the resulting liquid metal pool is limited to the cross-sectional area of the drain pipes. After the sodium has drained into the catch pan, burning is terminated when the pipes become plugged with the combustion products and air is prevented from reaching the liquid metal surface.

The Q-deck is supported 1.5 to 2.5 ft above the bottom of the catch pan on the bottom flange of W10 x 39 I-beams with metal-to-metal contact on all sides. The beams form a matrix of rectangular sections with approximately a 6-ft by 6-ft span between beams. The beams are supported separately from the catch pan on steel columns embedded in the floor concrete and by steel supports embedded in the cell wall concrete. A catch pan side wall is provided at each

floor support column to prevent sodium contact with the column or the embedment while providing flexibility at the column anchorage locations.

Schedule 10 pipes 1-1/2 in. in diameter are welded to the Q-deck plate and extend slightly below and above the deck. The pipes are provided to vent hot gases from the region below the deck to the cell atmosphere to prevent pressure buildup underneath the deck. The vent pipes are equipped with self-closing lids to prevent air ingress following the initial pressure pulse. The lids are essentially flat steel disks (1/8 in. thick) that rest by gravity on the open end of the pipe and ride up and down guide pins attached to the pipe. Figure 2-6 is a sketch of the vent pipe/vent lid configuration. A steel grating is provided above the fire suppression deck to serve as a walkway for equipment and personnel access. The grating also acts to prevent damage to the fire suppression deck.

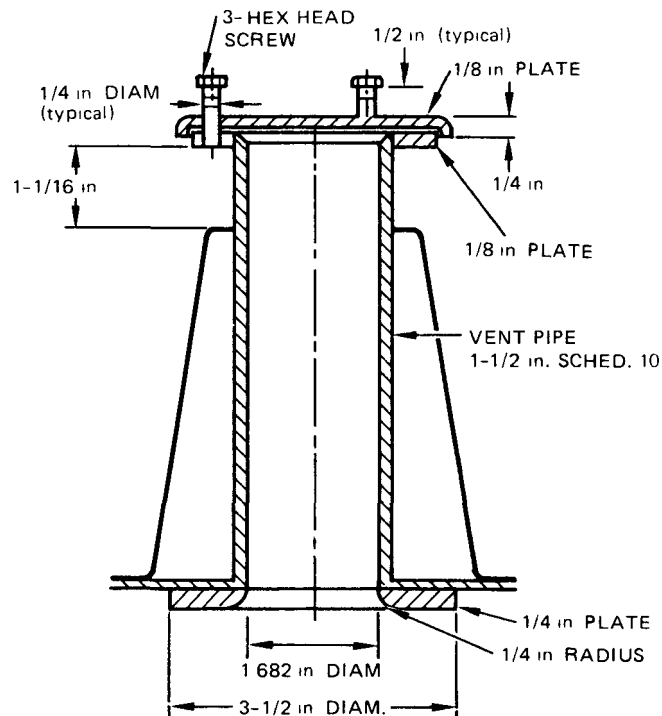


Figure 2-6. Vent Pipe

Open catch pans (without fire suppression decks) are used in those cells where the design basis spill volumes are small and open pool burning does not result in concrete temperatures that degrade structural concrete or release unacceptably high quantities of aerosols. The spilled liquid metal is contained in the catch pans and is allowed to burn to completion. The design of the catch pan is the same as the catch pans with fire suppression decks.

Open catch pans with drains are provided in those cells where the postulated spill volumes are large compared to the floor area of the cell such that considerations of pipe penetrations and building structural loadings make it impractical to contain the entire spilled volume. In the event of a liquid metal spill, the catch pan prevents contact with the floor concrete, and the liquid is drained into another cell provided with the capability for fire suppression (catch pan with fire suppression deck). This concept was extended to include draining large upper-level cells into lower-elevation cells where the upper-level cell has only a partial floor. The drains, in the form of large (nominally 12-in.-diameter) carbon steel pipes, welded to the open catch pan plate, extended downward to within approximately 3 in. of the top of the fire suppression deck in the lower-elevation cell. The open catch pan plates were to be pitched toward the drains with a nominal 1/4-in./ft slope. The number and size of the drains were selected to accommodate the maximum leak rates of the design basis spills.

The fire suppression deck subsystem was to be designed to contain the entire spillable volume that would result from a design basis piping leak. The spills postulated ranged in size from 500 gal in the reactor support building NaK equipment cells to 40,000 gal for the steam generator building cells.

In cells provided with catch pans with fire suppression decks, the pans were sized to contain the design basis spill volumes with the liquid level 4 in. below the bottom surface of the fire suppression deck. This provided assurance that the entire volume of liquid will be contained beneath the fire suppression deck with margin for uncertainties in the spill volume. The

vertical sections of the catch pan plate extended to a height above the fire suppression deck such that there was a 12-in. freeboard above the highest liquid metal level that could occur during a spill transient where the liquid level builds up on the surface of the fire suppression deck during the spill transient. The 12-in. freeboard provided protection against the splashing or washing of sodium over the sides of the catch pan plate. In cells equipped with open catch pans and drains, the vertical sections of the catch pan plate were likewise extended to a height such that there is a 12-in. freeboard above the highest liquid metal level that could occur during a spill transient. The drains are sized such that the catch pan lip elevation remains below the level of pipe penetrations through the cell wall.

In cells equipped with open catch pans without drains, the vertical sections of the catch pan extended to a height such that there was a 12-in. freeboard above the liquid level corresponding to the design basis spill volume.

The failure criteria for the catch pan was that it would be assumed to have failed if it would allow sodium/NaK to leak out of the pan into contact with the structural concrete. The failure criterion for the fire suppression deck was that it would be assumed to have failed if it should become distorted to the extent that open pool burning in the catch pan would result in unacceptably high structural concrete temperatures.

The catch pan plate, the fire suppression deck, and the fire suppression deck support framing were designed and analyzed to withstand the stresses and strains associated with the design basis spills. The system is designed to withstand the mechanical and thermal loads associated with the spill in combination with the safe shutdown earthquake load. The catch pans were designed for a maximum uniform temperature of 1100°F. The fire suppression deck and support framing were designed for a uniform temperature of 1400°F with local hot spot temperatures of 1600°F and a maximum temperature gradient between the bottom and top of the beams of 300°F.

To minimize the stresses and strains imposed on the system under accident conditions, the system was designed to allow free expansion or sliding of the catch pan plate. The plate rests on and is supported by a bed of well-compacted MgO aggregate insulation. The fire suppression deck was separately supported by steel columns embedded in the structural concrete. The supports are not connected to the catch pan plate to ensure that during a thermal expansion of the plate following a sodium/NaK spill accident, the plate shall not be restrained.

An air gap around the sides of the catch pan plate is provided to allow for relative movement of the plate and the concrete structure. The air gap also serves to allow hot gases to pass from behind the catch pan plate to the cell atmosphere to prevent pressure buildup behind the catch pan plate.

The fire suppression deck was provided with vent pipes to vent hot gases from the area under the deck to the cell atmosphere to prevent pressure buildup underneath the deck. The vent pipes are sized to limit the peak pressure to one-half or less of the static head of sodium filling the drain pipes. This ensured that the sodium will continue to drain during the short time interval of the pressure pulse.

To verify the design concept and establish confidence in the analytical approach, a series of tests were devised. These tests included (1) small-scale sodium fire suppression tests, (2) water spray tests, (3) small-scale sodium spray tests, and (4) a large-scale sodium spray fire test.

The small-scale fire suppression tests consisted of a series of tests to demonstrate and evaluate the fire suppression effectiveness of the catch pan fire suppression deck model of the system both as a spray and as a stream. The tests were performed in a 10-ft-diameter by 30-ft-high cylindrical steel vessel.

Based on an evaluation of the test data, it was concluded that:

- A. The burning sodium flows freely through the drain pipes into the catch pan without significant holdup.
- B. Following the sodium spill, the sodium in the drain pipes burns to form an oxide plug that prevents further combustion.
- C. The sodium in the catch pan continues to react with oxygen at a decreasing rate after the drain pipes plug as air flows into the catch pan through leakage paths at the Q-deck support framing interface.
- D. The sodium burning rate is reduced to less than 1/250 of an open pool within 20 h.

The test results demonstrated the effectiveness of the fire suppression concept sufficiently to justify testing a large-scale prototype. These results also provide useful input for the design of the system. Based on the results of the tests, it was decided to locate the gas vent pipes in the top sections of the Q-deck instead of in the channels in order to avoid the buildup of sodium residues around the pipes. It was also decided to equip the vent pipes with lids to prevent the development of natural air circulation patterns down some pipes and up others. It also lead to the reduction of the pipe schedule of the drain and vent pipes from Schedule 40 the Schedule 10 and to provide end covers for the open ends of the Q-deck top sections in order to avoid the accumulation of sodium residues inside the sections.

Water spray tests were then performed to select the worst case pipe leak configuration to use in the proposed large-scale sodium spray fire tests. Water was discharged at pressures and velocities prototypic of the design basis leak in the 24-in.-diameter IHTS piping, through rectilinear cracks and circular orifices, and impacted on both flat plate and curved steel surfaces.

Longitudinal as well as circumferential cracks were employed. The tests demonstrated that the worst case spray (smallest drop size and largest spray area) resulted from a discharge through a circular orifice impacting a curved surface representative of an adjacent pipe. The tests also demonstrated that the dimensions of the resulting spray pattern were large and that a full-scale jet test with sodium would exceed the test facility capabilities and limitations. That is, a large fraction of the spray would strike the walls of the test cell and would not be available for spray burning. In addition, the cell walls would require protection from impinging high-temperature sodium. For this reason, it was decided to use spray nozzles in the proposed large-scale spray fire test so as to control the spray pattern. Additional water tests were performed using the worst case spray configuration to determine the spray droplet size. In these tests, the water droplets were collected in liquid nitrogen dewars and then sized by screening. These data yielded an area mean droplet diameter of 0.3 cm.

Additional small-scale sodium spray tests were performed. The objective of these tests was to develop a spray nozzle that would produce a droplet distribution with an area mean diameter of 0.3 cm and to provide a benchmark for code comparisons. A preliminary sodium spray benchmark test was performed in air using a spray nozzle that had been calibrated with water. Subsequent sodium tests were performed in an inert atmosphere to confirm the nozzle calibration. In these tests, the droplets were collected in a lightweight oil and then sized by screening. The area mean droplet diameter was determined to be 0.15 cm.

On the basis of the above results, the nozzle design was modified and additional inert atmosphere tests were performed. The area mean sodium droplet diameter produced by the modified nozzle was confirmed to be 0.3 cm.

The large-scale sodium spray fire test was established in which sodium at 1000°F was discharged from a height of approximately 25 ft on a large-scale (30 ft by 20 ft) prototype of the catch pan fire suppression deck system.

These tests were performed at Rockwell International's field laboratories in an existing building specifically adapted for this purpose. Sodium was discharged through an array of 12 nozzles identical to the one developed in the small-scale spray tests. The test article was fabricated and installed in accordance with the design requirements of the CRBRP catch pans.

The test configuration is depicted in Figure 2-7. Figure 2-8 shows the test article configuration at various stages of construction. The test plan was to discharge 6600 gal of 1000°F sodium at a constant discharge rate of 72 gpm. The discharge rate and the spill volume were selected to be as large as possible within the limitations imposed by safety considerations and by facility capabilities. Table 2-3 shows a comparison of test parameters and parameters applicable to design basis leak accidents in the CRBRP SGB.

TABLE 2-3
COMPARISON OF TEST PARAMETERS AND THE SGB
DESIGN BASIS LEAK PARAMETERS

Item	Test	SGB Leak
Sodium temperature (°F)	1,000	936
Spray droplet size (cm)	0.3	0.3
Spray density (gpm/ft ³)	0.0014	0.22-0.0021
Spray height (ft)	25	18-105
Cell volume (ft ³)	49,000	5,000-500,000
Sodium pool depth (ft)	1.6	1.2-2.3

After test initiation, video recording of the spray manifold showed that all nozzles did not flow simultaneously. Intermittent flow was observed from several nozzles with others showing full flow. The test was aborted after approximately 6 min by a failure in the sodium supply system. Valves, located upstream of each nozzle, failed closed when the operating gas pressure was lost due to a leak in the gas supply line. Due to the abort and intermittent behavior of the nozzles, it was not possible to obtain an accurate estimate of

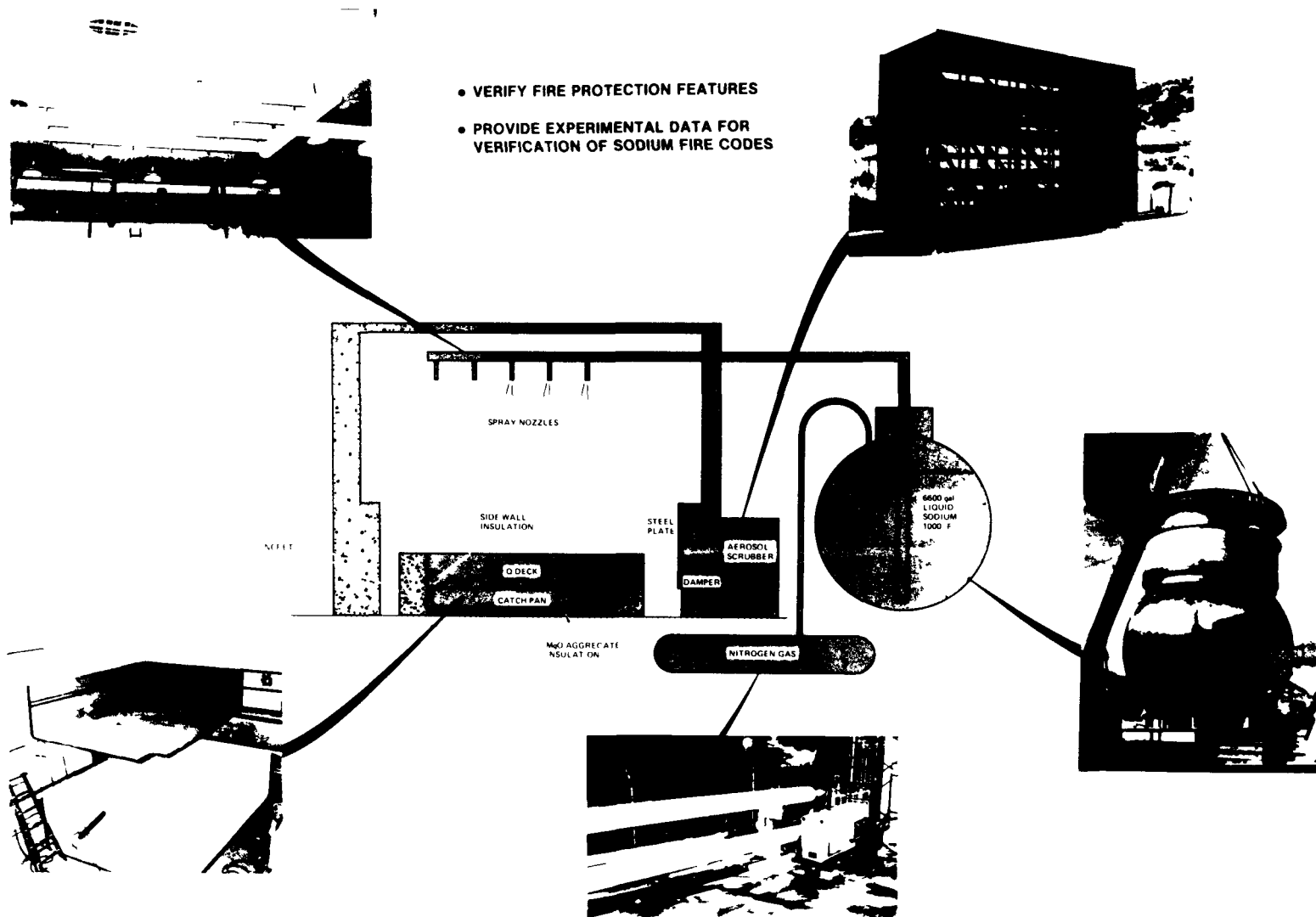


Figure 2-7. Test Facility Configuration

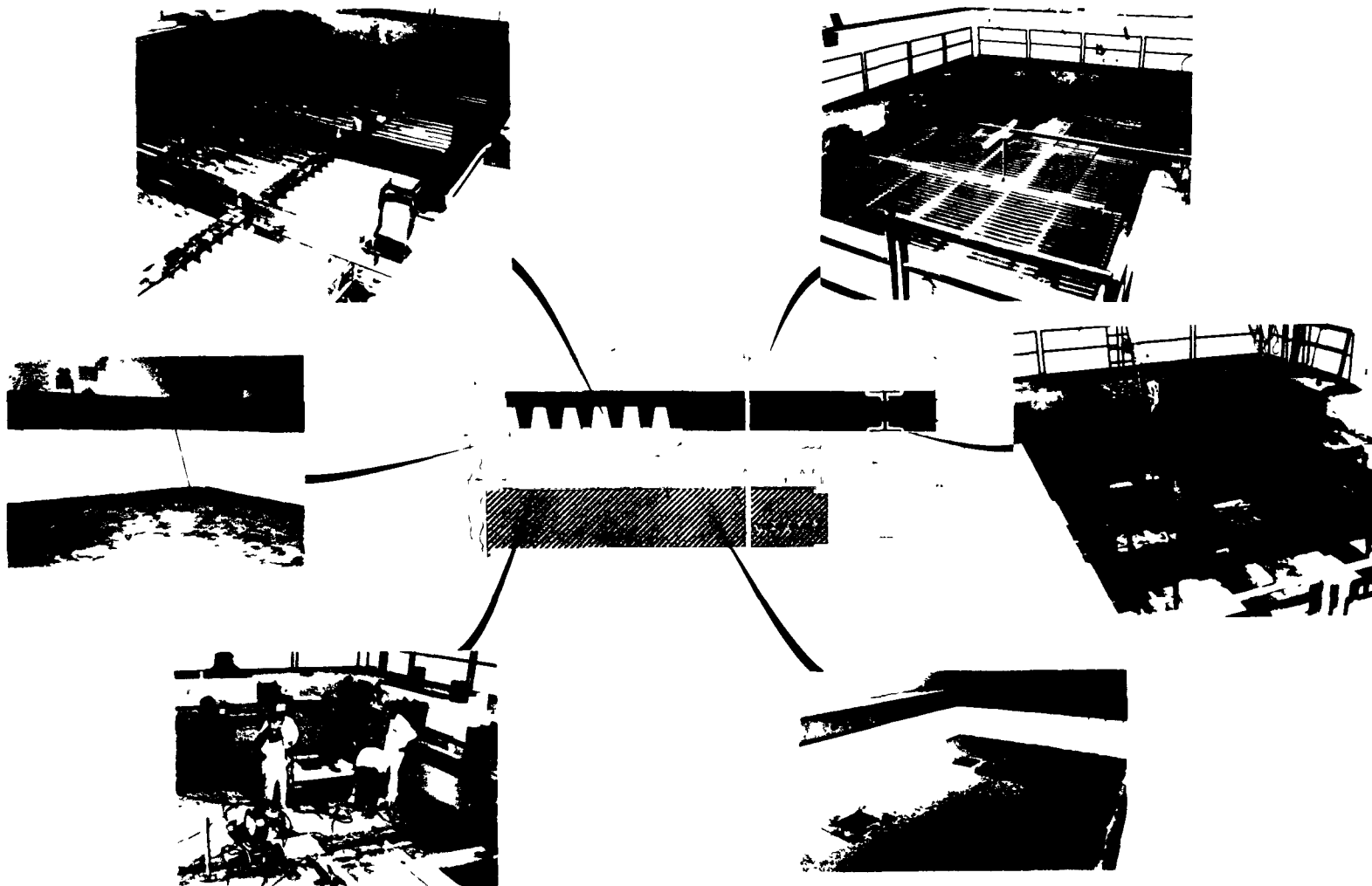


Figure 2-8. Test Article

the sodium flow rate or the total amount of sodium discharged. Best estimates are that no more than 215 gal of sodium were discharged with an overall average discharge rate of between 32 and 35 gpm.

During the test, gas temperatures up to 1246°F were measured. Gas circulation patterns are evident from the concrete temperature measurements. The ceiling surface temperature reached a maximum of 478°F whereas the maximum upper wall temperature was 242°F and the maximum lower wall temperature was 162°F. The effect of high catch pan system structural temperatures was observed in the deformation of the Q-deck support beams and the walking deck. A large fraction of the sodium had been consumed before reaching the catch pan.

Although the principal objective of the test (demonstration of the fire suppression effectiveness of the catch pan fire suppression deck system) was not achieved, this test along with the small-scale air benchmark spray tests provided useful information for code development. Table 2-4 shows a comparison of measured and predicted parameters for the large-scale test. The pretest predictions were made using the SPCA-I code. It is apparent from this comparison that the single gas node model employed in SPCA-I is inadequate to predict local temperature effects. Figure 2-9 shows a comparison of measured and predicted pressures for the second air benchmark spray test. The predictions were made with the SPRAY code, which provides input spray burning rates for the SPCA-I calculations. The comparison indicates that the spray burning rate was underpredicted by a significant factor.

To correct for the discrepancies in code predictions, the SPCA-II code was developed. This code not only combines spray burning and simultaneous pool burning effects but also includes a spray droplet burning model and a multinode gas circulation model that includes heat transfer from each gas node to adjacent concrete structures.

In view of the test and analytical results, two separate task force efforts were initiated. Task Force I, consisting of a panel of experts in sodium fire

TABLE 2-4
CRBRP FIRE TEST RESULTS

Parameter	Predicted		Measured	
	Value (°F)	Time (min)	Value (°F)	Time (min)
Peak gas temperature	434	4	1246	4.7
Peak concrete temperature				
Ceiling surface	199	48	478	6.4
Ceiling below surface	169		205 (1.0 in.)	9.5
Wall surface	195	54		
<u>East wall</u>				
20 ft elevation			185	
20 ft elevation			198	
10 ft elevation			162	
10 ft elevation			145	
<u>South wall</u>				
20 ft elevation			242	
20 ft elevation			230	
10 ft elevation			136	
10 ft elevation			152	

analysis, was charged with determining the applicability of the SPCA-II predictions. Task Force II, consisting of panel of experts in plant design, was charged with evaluating alternative design features for mitigating the leak consequence.

After a thorough review of the phenomenological processes modeled by SPCA-II and comparisons of SPCA-II code predictions with other codes and experiments, Task Force I concluded that although the code predictions appeared to represent an upper bound for sodium burned and pressure, no specific changes to the code could be recommended. In general, the committee felt that

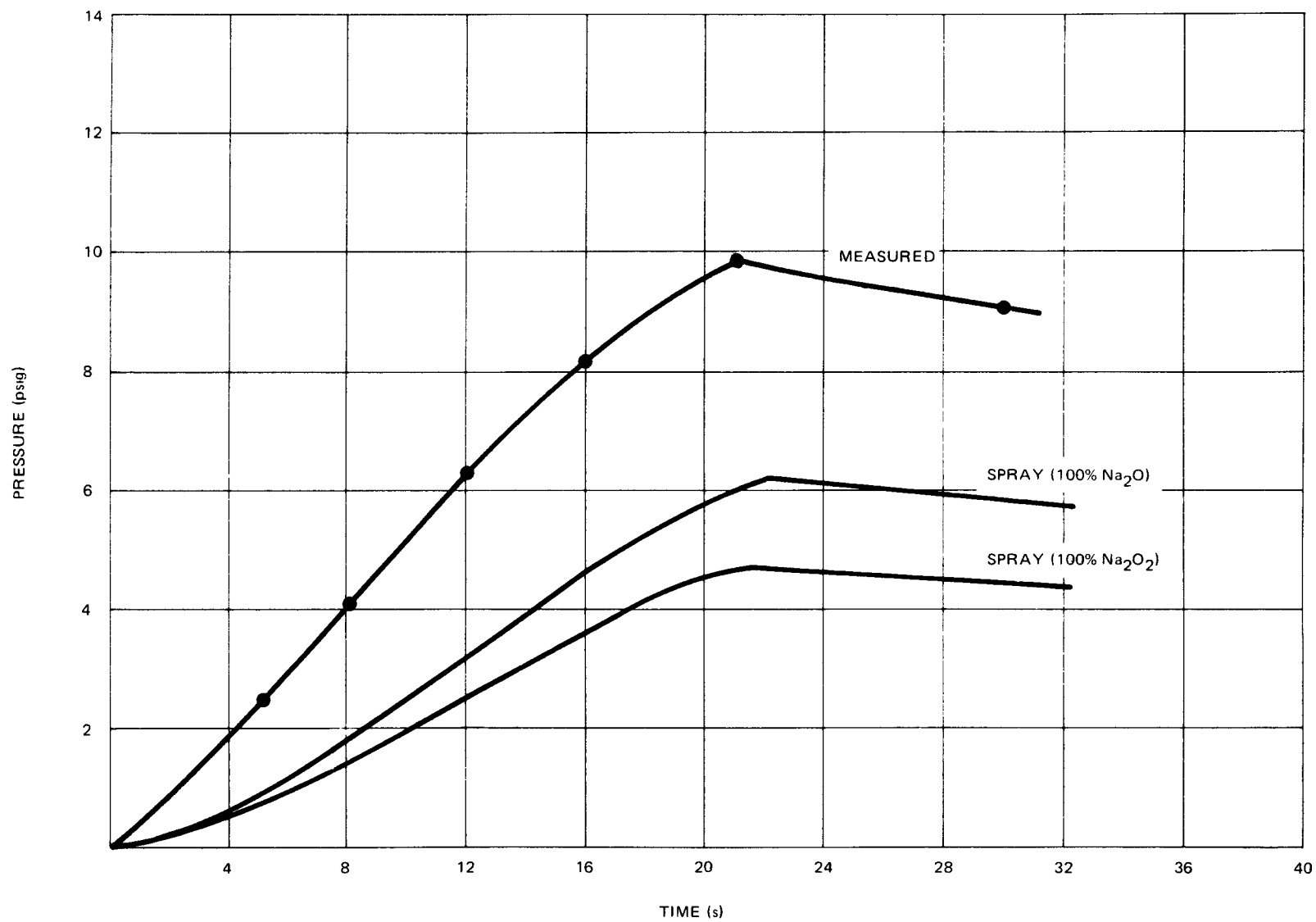


Figure 2-9. Air Benchmark Test No. 2

increased emphasis on improving ways to physically model the leak accident would have more payoff in reducing the severity of the accident than trying to justify a reduction in the level of conservatism in the code. Figure 2-10 shows a comparison of calculated and predicted pressures for Air Benchmark Test 2.

In support of the Task Force II deliberations, a large number of computer calculations were performed to assess the impact on the consequences of an IHTS accident due to:

- A. Changes in accident modeling
- B. Changes in leak parameters
- C. Changes in plant design.

The results are summarized in Table 2-5.

The base case accident scenario represents a leak in the 24-in.-diameter IHTS hot leg piping in Cell 226/246 of the steam generator building. Sodium at an initial temperature of 936°F is discharged from the pipe as a spray at an initial rate of 129 lb/s. The accident is modeled as a large area spray with a spray height of 105 ft and a mean drop diameter of 0.3 cm. The sodium drops are assumed to be evenly distributed over a 50-ft-diameter circular area at the source at the top of Cell 246. The total cell gas volume is 540,000 ft³, and the cell concrete surface area is 36,000 ft². The pool surface area formed by the falling drops is assumed to be 4,381 ft². Hot gases are discharged to the outside atmosphere through a 352-ft² vent in the ceiling of the cell. The flow discharge coefficient for the vent system is 3.7. The vent is assumed to open when the building pressure reaches 1.0 psig, and it is assumed to close when the pressure reduces to ambient.

Cases 1 through 11 were run to evaluate the effect of changes in accident modeling that could be justified either as an improvement in modeling or as a consequence of a plant design change.

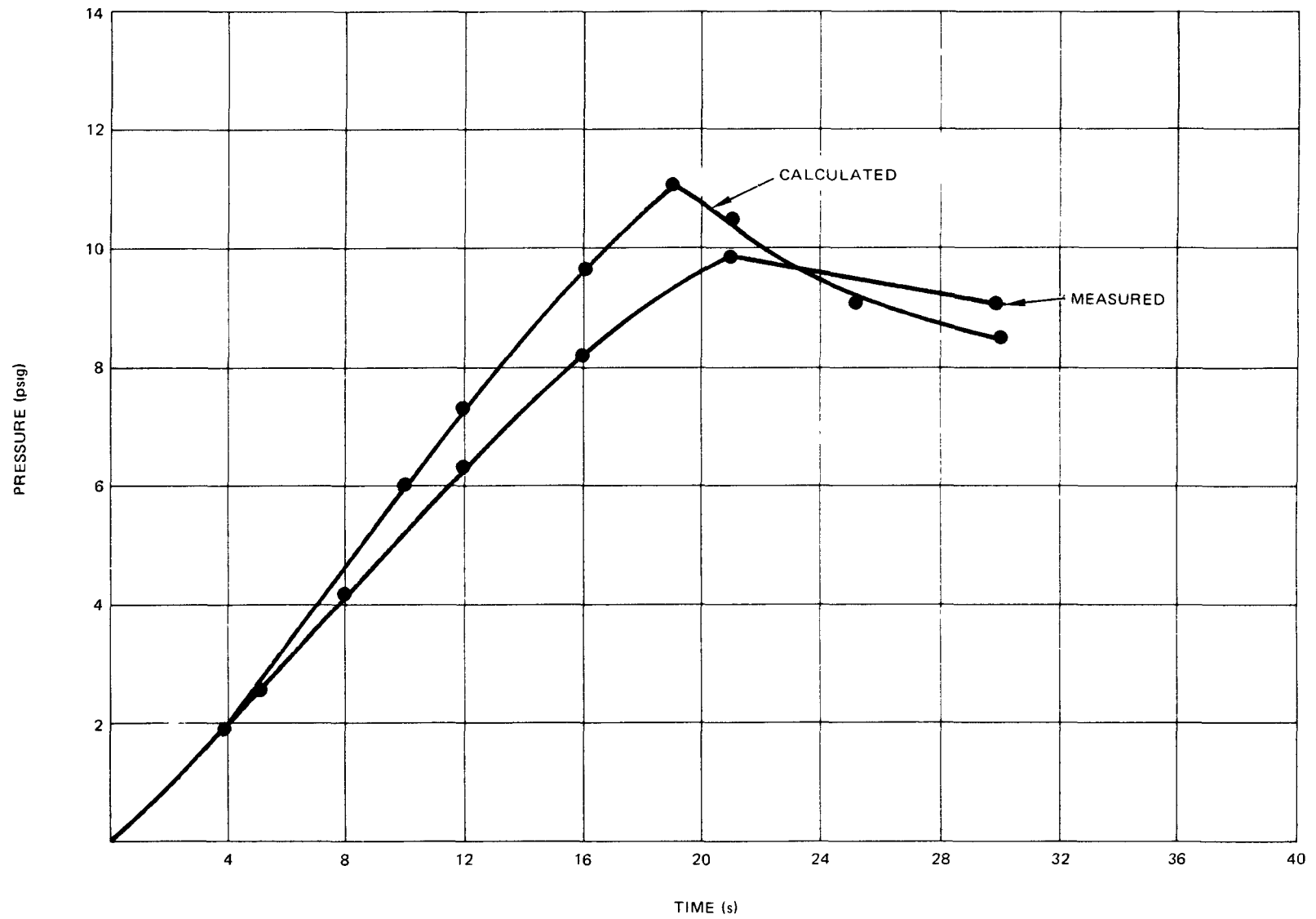


Figure 2-10. Air Benchmark Test No. 2

TABLE 2-5
SODIUM FIRE PARAMETER SURVEY

Parameters	Base Case	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	21s	22	23
<u>Variable</u>																						
Flow Rate	MEFS												Trip and Vent	Trip and Drain	1/2MEFS	Trip and Vent	Trip and Vent	Trip and Vent	MEFS	5% MEFS	6.7 lb/s	6.7 lb/s
Drop Diam (cm)	0.3	0.46			0.46												0.3/0.46	0.3/0.46		0.6		
Drop Height (ft)	105		30	15	30			30		30	30	30					30	30	70		65	65
Spray Diam (ft)	50						70		70		70									15		
Sodium Temp (°F)	936							650												40/10.5	40/1.5	40/10.5
Cell Vol. (ft³)	525,000								788,000							Vary	Vary	Vary			650	650
Cell Surface (ft³)	36,000								40,000								694,000					
Steel Mass (ton)	0									1000	1000						62,000					
Friction Factor	F											Fx10					669	519	519	519	519	519
Wetted Surface (ft³)	4381																			1000		
<u>Predictions</u>																						
Peak Pressure (psig)	1.1	1.1	1.1	1.1	1.1	23.6	11.9	1.1	12.3	1.1	1.1	1.1	1.1	1.1	1.1	1.1	1.1	1.1	1.1	0.01	0.17	0.2
Avg Gas Temp (°F)	1585	1320	1190	1090	1035	1650	1550	1100	1500	755	670	1315	1510	1530	1200	1500	450	660	1430	280	490	910
Ceiling Gas Temp (°F)	2715	1760	2470	1970	1650	2900	1980	2500	1950	1820	900	2620	2600	2650	1780	2300	1310	1550	2430	570	570	1020
Ceiling Surface Temp (°F)	1540	875	1170	900	860	1700	900	960	1090	680	420	1700	1000	1220	1030	1050	304	340	185	260	360	205
Ceiling 1-in.-Deep Temp (°F)	460	390	450	420	420	490	375	410	430	325	290	560	420	450	420	430	230	250	166	219	284	181
Upper Wall Surface (°F)	1010	820	815	750	740	1300	1000	700	1050	560	420	860	980	980	940	890	280	320	185	260	360	205
Lower Wall Surface (°F)	440	3900	340	380	380	340	400	310	360	290	320	275	370	350	380	350	210	250	175	160	290	205
Steel Temp (°F)										520	535						360	400	941	300	480	948
Aerosol Rel. (lb)	4000						7800			900	597	1890			1700		990		1760			
H2 Conc. (v/o) at 2 h						55					40						10			3.7	22	

*1-in. insulation on ceiling and wall.
Values have not peaked.
§Case 19 simulated an MEFS leak in Cell 227 of the SGB. Case 20 was in error.
Case 21 is corrected case.

In Case 1, the droplet size was changed to 0.46 cm.

In Case 2, the spray height was reduced to 30 ft. This is predicted on the assumption that the spray height could be reduced by a change in the building arrangement.

In Case 3, the spray height was further reduced to 15 ft.

In Case 4, the Case 1 and 2 changes were combined.

In Case 5, the vent area was reduced to 40 ft².

In Case 6, the spray diameter was increased to 70 ft, and the vent area was reduced to 40 ft².

In Case 7, the spray height was reduced to 30 ft, and the drop discharge temperature was reduced to 650°F to represent a leak in the IHTS cold leg piping.

In Case 8, the spray diameter was increased to 70 ft, the vent area was reduced to 40 ft², and the cell free gas volume was increased to 40,000 ft². This represents a modeling change to Case 6 designed to account for the increase in oxygen availability as a result of gas mixing between Cell 226/246 and the auxiliary bay cells.

In Case 9, the spray height was reduced to 30 ft, and a heat sink (1000 tons of steel) was added to determine the effect of heat sink on the results.

In Case 10, the spray height was reduced to 30 ft, the spray diameter was increased to 70 ft, and the structural steel heat sink was added.

In Case 11, the spray height was reduced to 30 ft, and the cell gas circulation friction factor was increased by a factor of 10. This represents

an arbitrary change in friction factor designed to scope the potential effect of the increased flow resistance associated with the equipment in the cell.

It was found that:

- A. The concrete temperature acceptable limits are exceeded in each case.
- B. The peak pressure acceptable limit is exceeded with a 40-ft² vent area and is reduced to an acceptable level with a 352-ft² vent area.
- C. Increasing the cell free volume (Cases 6 and 7) results in increased concrete temperatures. The interaction between connecting cells should be modeled in future analysis.
- D. Increasing the gas circulation friction factor results in increased concrete temperatures.
- E. Although confirming analyses were not made for all of the cases, it is expected that these changes will not result in a major decrease in the quantity of aerosols released to the atmosphere.
- F. Including the structural steel as a heat sink significantly reduces the concrete temperature and should be modeled in future analyses.

Cases 12 through 17 were run to evaluate the effectiveness of action to mitigate the leak rate time history.

In Case 12, it is assumed that the pump is tripped and expansion tank cover gas is vented at approximately 20 s.

In Case 13, it is further assumed that a system drain is initiated at 20 s.

In Case 14, it is arbitrarily assumed that the leak rate is reduced by a factor of 2.

In Case 15, the time-dependent sodium discharge temperature of Case 12 is included in the analysis.

In Case 16, representing the most optimistic assessment of what could be done without major plant design changes, it is assumed that pump trip and cover gas venting, a reduced spray height, an increased drop size, a time-dependent sodium discharge temperature, an expanded cell free gas volume with associated concrete heat sink, and a structural steel heat sink (revised estimate) are all combined.

The specific assumptions are that the pump trip and cover gas vent to atmosphere occurred in 20 s, which is probably optimistic. Due to the reduced sodium pressure at the leak and thus reduced leak velocity, the sodium droplet size was increased from 0.3 cm to 0.46 cm 20 s after pump trip. The rest of the assumed parameters were best judgments. The expanded cell volume and steel assumes the auxiliary bay is fully utilized in oxygen and heat sink.

The results of Case 16 show significant improvement in all areas. However, concrete temperatures are still relatively high, and the large steam vent louvers are probably needed to keep peak pressures below 3 psig. Hence, aerosol release exceeds the 600 lb criterion.

Case 17 is the same as Case 16 with the exception that the assumed cell free gas volume and associated heat sinks are representative of Cell 226/246 only. The real situation is probably somewhere between Cases 16 and 17.

It is seen that, with the exception of Cases 16 and 17, the concrete temperatures exceed the acceptable limit by a wide margin. In Cases 16 and 17, the concrete temperature exceeds 200°F, but by a relatively small margin. Calculations using the WATRE code were performed to assess the effect

of water release from the concrete and the subsequent buildup of hydrogen. The concrete temperature data from SPCA-II was used as input to WATRE. The results indicate that the peak hydrogen concentration in the cell would reach approximately 10% by volume at the end of 2 h.

As a result of task force deliberations, two concepts for mitigating the consequences of the accident were selected for further study. Concept 1 involves thermally insulating the ceilings and walls of the building to limit the concrete temperatures to acceptable levels with a full moderate energy fluid system (MEFS) leak. Concept 2 requires that the piping insulation be designed such that it remains intact during the accident and that 95% of the discharged sodium is directed in a semicontrolled manner to a catch pan equipped with a fire suppression deck. The remaining 5% would leak out of the pipe insulation and result in a spray fire. It was assumed that the concept would be applied to pipe sizes 4 in. in diameter and above. Thus, for the 3-in.-diameter auxiliary piping, the unmitigated MEFS leak would still apply.

Concept 1 is represented by Case 18, which assumes a full MEFS leak in the IHTS hot leg piping in Cell 226/246 and with 1-in. alumina silicate insulation covering all ceiling and wall surfaces. The results show that the concrete temperature is below 200°F and cell peak pressure is acceptable. However, the large steam vent louver is needed to reduce pressures and results in 1760 lb of aerosols discharged to the atmosphere. In addition, average gas and peak ceiling gas temperatures are very high and result in the average temperature of auxiliary steel to exceed 900°F.

Concept 2 is analyzed in Case 21, where it is assumed that 5% of the MEFS leak results in a 15-ft-diameter spray zone with average droplet size of 0.6 cm falling 15 ft. This represents a best estimate of droplet size for leakage out of the pipe insulation. The remainder of the leak (95%) falls as a liquid stream or is directed to a catch pan. In addition, only Cell 226/246 gas volume and steel heat sink is modeled with a small 40-ft² vent; by compartmentalization, the catch pan pool burning area is restricted to 1000 ft².

The results of Case 21 show that peak pressure, gas and steel temperature, and aerosol release are all acceptable. Only concrete temperatures exceed allowables.

Cases 22 and 23 analyzed an MEFS leak from one 3-in. pipe without any mitigating effect from pipe insulation. The leak was assumed to be a constant spray rate of 6.7 lb/s with 0.3-cm droplets. The difference between the two cases is that Case 23 assumed the ceiling and wall surfaces were covered with 1-in. insulation. The results of Case 22 show that the pressure and gas temperatures are acceptable, but concrete temperatures are above allowable. Aerosol release was not calculated but is believed to be acceptable. Case 23 indicates that the 1-in. insulation will result in acceptable concrete temperatures, but internal gas and steel temperatures have increased significantly.

It is apparent from a review of the analyses that Concept 1 does not mitigate the consequences of the accident in any respect other than reduce the concrete temperatures to an acceptable level. Thus, if Concept 1 were accepted as a design fix, the project would still have to deal with the problems of

- (1) designing the steam relief vent to withstand high temperatures,
- (2) designing the structural steel to withstand high temperatures, and
- (3) accommodating large quantities of released aerosols.

By contrast, Concept 2 reduces cell pressures, gas temperatures, structural steel temperatures, and aerosol releases to relatively easily managed levels. It remains, however, to deal with the fact that with Concept 2, the long-term concrete temperatures exceed 200°F. Several alternatives were available to deal with this problem. These include:

- A. Tripping the pump and venting the cover gas pressure
- B. Insulating selected portions of the ceiling and walls

- C. Limiting the spread of the discharged sodium (reduce pool burning)*
- D. Developing a data base for a quantitative assessment of the consequences of concrete temperatures that exceed 200°F.

Based on the result of these studies, Task Force II made the following recommendations:

- A. Design the piping insulation to mitigate the sodium spray
- B. Perform full-scale piping insulation tests with water to verify the insulation design
- C. Perform a large-scale sodium spill test under simulated conditions to demonstrate the catch pan fire suppression deck system performance under realistic thermal loadings.

Westinghouse-AESD embarked on the design of a modified piping insulation system. This design was such that it would function as follows:

- A. Control the pipe leakage so that a high-velocity "jet stream" does not impinge on cell structures or equipment to form a spray of droplets.
- B. Discharge the pipe leakage into the cell environment as a low-velocity liquid stream in a specified direction
- C. Limit the quantity of uncontrolled leakage into the cell environment to an acceptable level.

*For the Concept 2 cases, the heat generated by pool burning is equivalent to that from spray burning, whereas for Concept 1, pool burning effects are negligible.

Figure 2-11 illustrates the salient points of the modified design. This design was completed and water tested by Westinghouse-AESD and is fully described in References 8 and 9.

A large-scale test was performed in which over 4800 gal of 1100°F sodium was discharged over a 13-min interval onto the large-scale catch pan fire suppression deck test article. The test configuration is illustrated in Figure 2-12. The test was designed to provide an approximate simulation of the IHTS hot leg piping leak accident with modified piping insulation (leakage at the pipe clamp insulation jacket interface was not simulated). The discharge nozzle was designed to simulate the discharge from an 8-in.-diameter insulation annulus drain. The nozzle exit was positioned approximately 6 ft above the test article walking grating. A sodium temperature of 1100°F was selected to account for the potential increase in sodium temperature that would occur if sodium was leaked on an open catch pan and was drained on the catch pan fire suppression deck system. Fresh air was supplied to the cell by means of a blower to ensure that burning would not be extinguished as a result of oxygen depletion. In addition to the test article, the test cell contained various candidate sodium aerosol detectors and representative pipe hangers and snubbers. In this test, the cell walls and ceiling were insulated as a safety precaution. It was planned to discharge approximately 6600 gal of sodium at successively increasing rates, i.e., 60, 150, and 950 gpm. The low discharge rates were chosen so that the test article would be subjected to the buildup of oxides in the affected downcomers (with possible plugging) and the highest Q-deck temperatures because of spray rebound. The high discharge rate (950 gpm) would guarantee overflowing of the target Q-deck section into one or more adjacent Q-deck sections. Figure 2-13 shows a comparison of the planned and the actual sodium discharge time histories. Although the planned sodium discharge time history was not achieved, the principal objectives of the test were accomplished. That is, the test results demonstrated that the consequences of a large pipe leak are greatly reduced by mitigating spray formation and that the catch pan fire suppression deck is very effective in suppressing pool burning. Except for local hot-spot temperatures in the

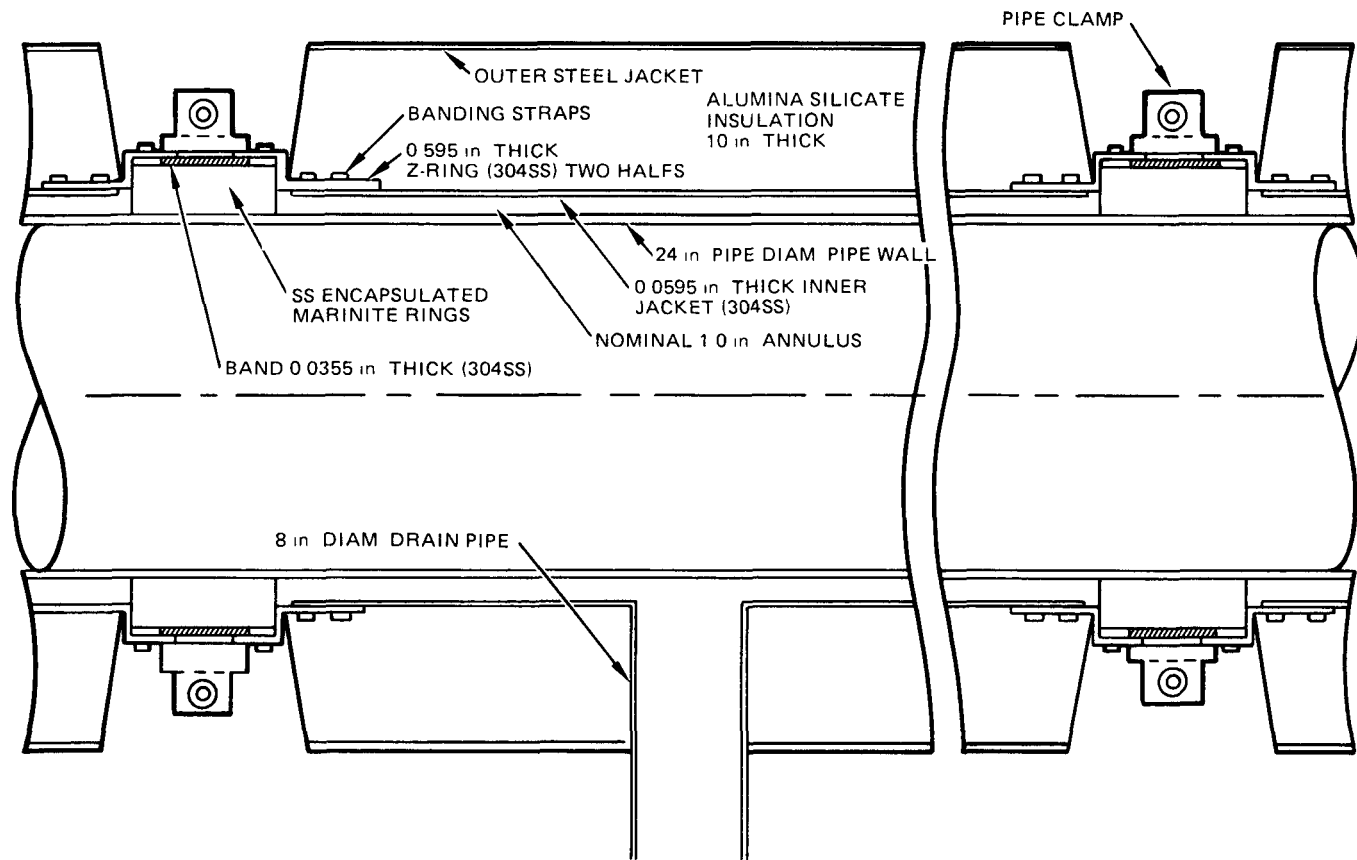


Figure 2-11. Modified Piping Insulation Design

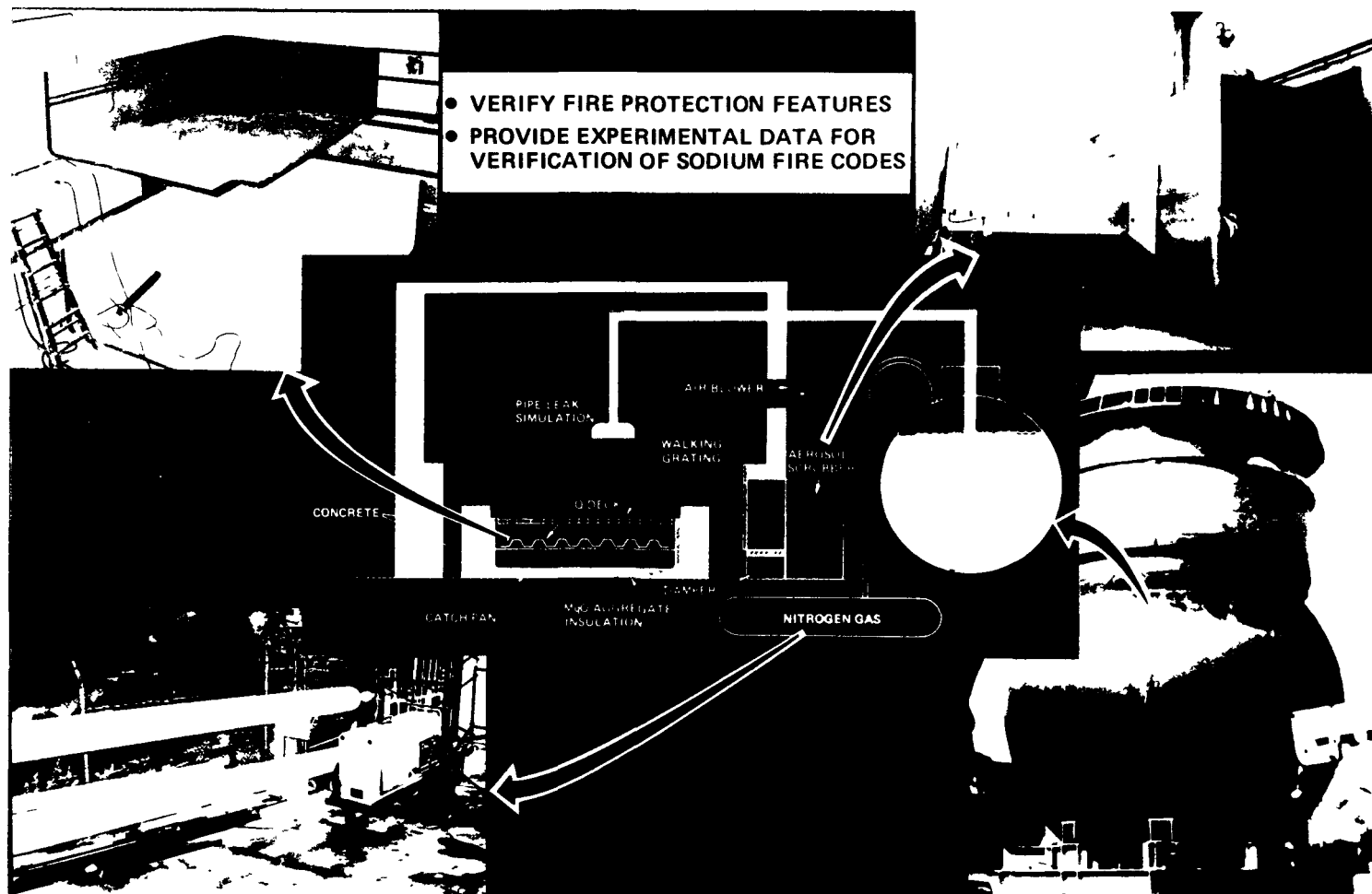


Figure 2-12. LSSS Test Article

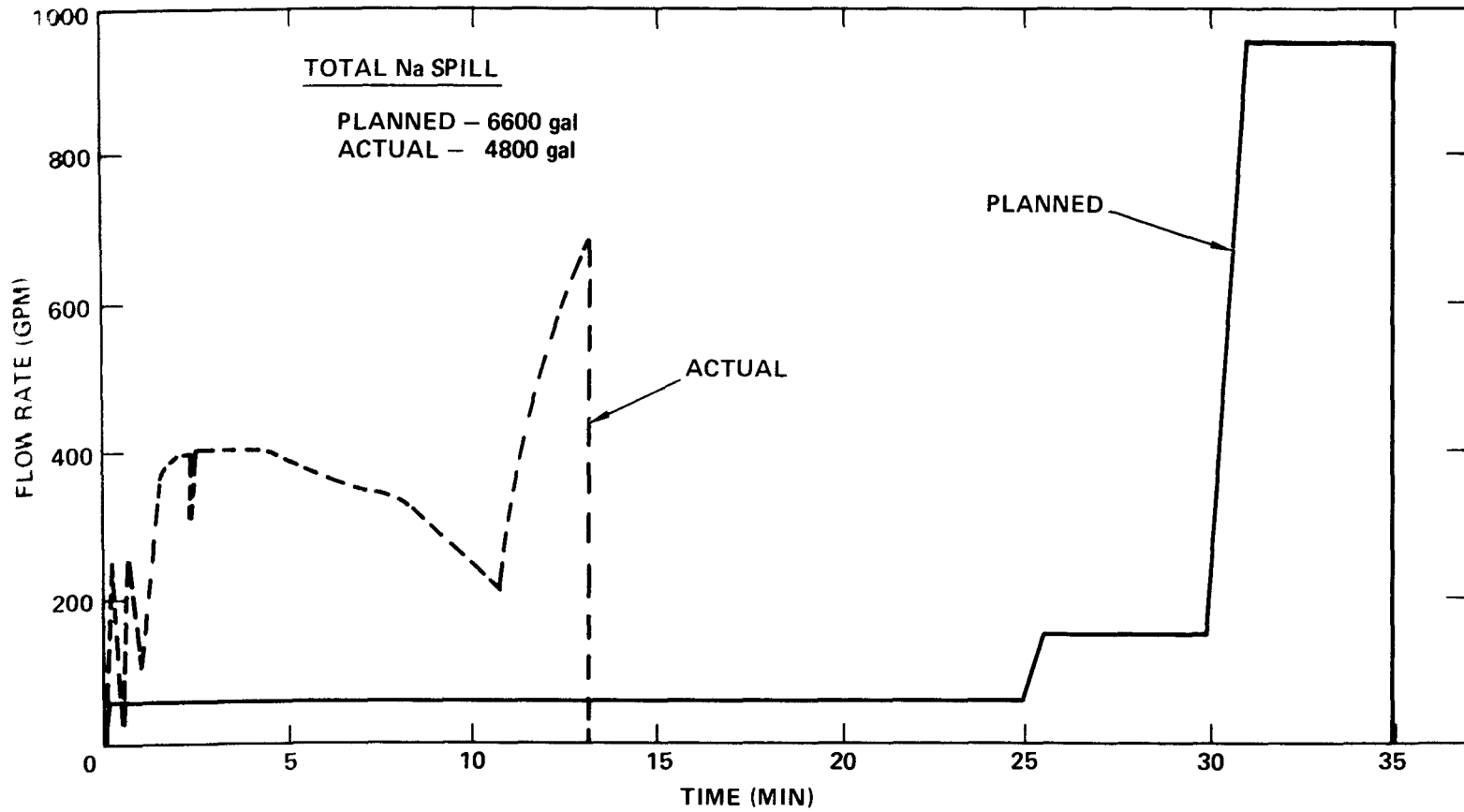


Figure 2-13. Large-Scale Sodium Fire Test, Sodium Flow Rate Versus Time

immediate vicinity of the sodium stream, the peak gas temperatures in the test were less than 700°F. By comparison, in the large-scale spray fire test, gas temperatures were in excess of 122°F over a significant volume of the cell. The temperature of the sodium in the catch pan reached a peak equivalent to the sodium discharge temperature and then decreased continuously to freezing. This trend indicates that there was insignificant burning beneath the fire suppression deck.

Pretest predictions of the thermal consequences of the test were made using the SPCA-II computer code. The calculation was repeated using the measured sodium flow rate data. Figure 2-14 shows a comparison of the calculated gas temperature 4 ft below the cell ceiling with the measured gas temperature at 5 ft below the cell ceiling. The calculated average gas temperature is also shown for comparison. Figure 2-15 shows a comparison of the long-term cell average gas temperatures. Figures 2-16 and 2-17 show comparisons of concrete surface temperatures for the wall and the ceiling, respectively. Figure 2-18 shows a comparison of the measured and calculated cell oxygen concentration. The good agreement shown in these comparisons demonstrated that the SPCA-II code can be used to adequately predict the transient thermal responses of the cell gas and concrete structures for this type of accident scenario.

Figure 2-19 is a comparison of the calculated and measured concrete floor (underneath the catch pan) temperature. Again, there is good agreement. It is seen that the concrete temperature peaks well below 200°F. Figure 2-20 is a comparison of sodium pool temperatures. It is noted that the predicted curve was obtained assuming that there was zero burning on the pool surface. The only heat input to the sodium pool was that associated with burning in the drain pipes. This corresponded to approximately 1/200 of an open pool burning rate and was assumed to continue for the first 10 h only. The closeness of the fit between the calculated and measured temperatures demonstrated the fire suppression effectiveness of the system. That is, pool burning is effectively limited to that represented by burning in the open area of the drain pipes over a 10-h period. Figure 2-21 provides further evidence of the fire

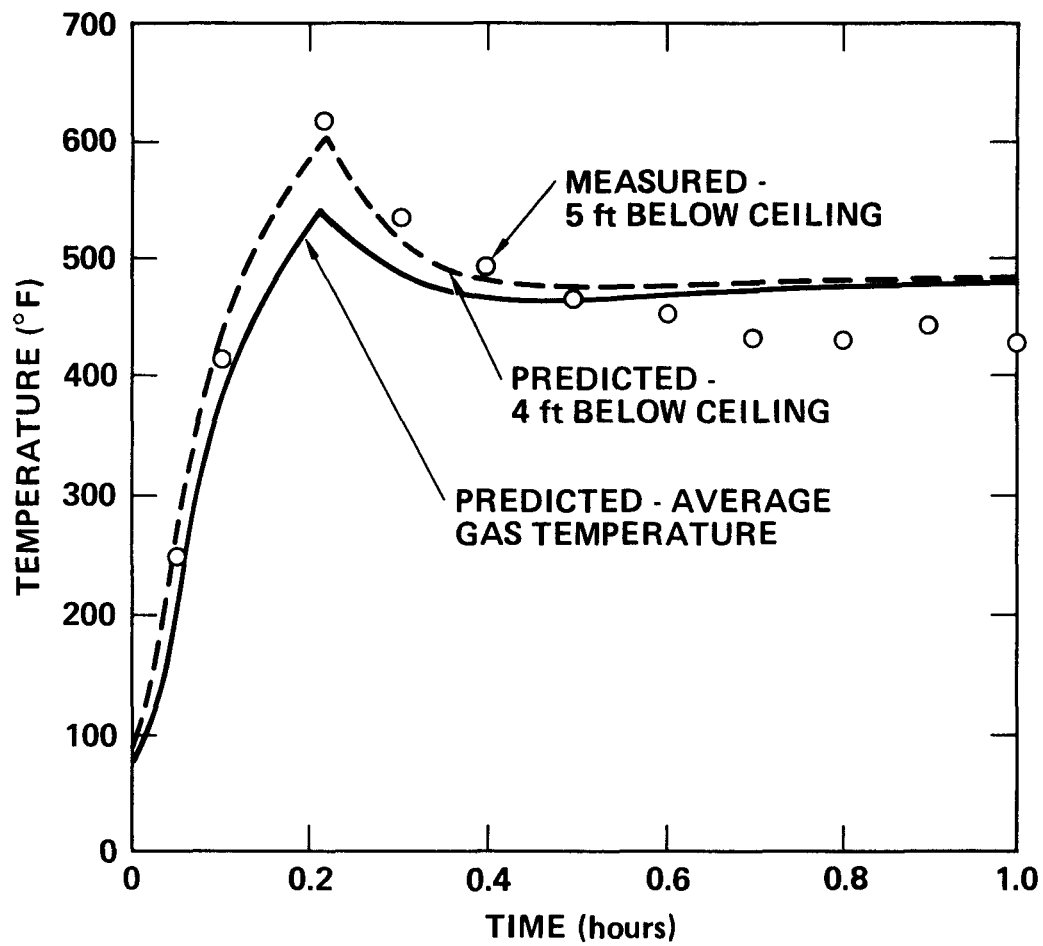


Figure 2-14. Sodium Fire Test, Cell Gas Temperature

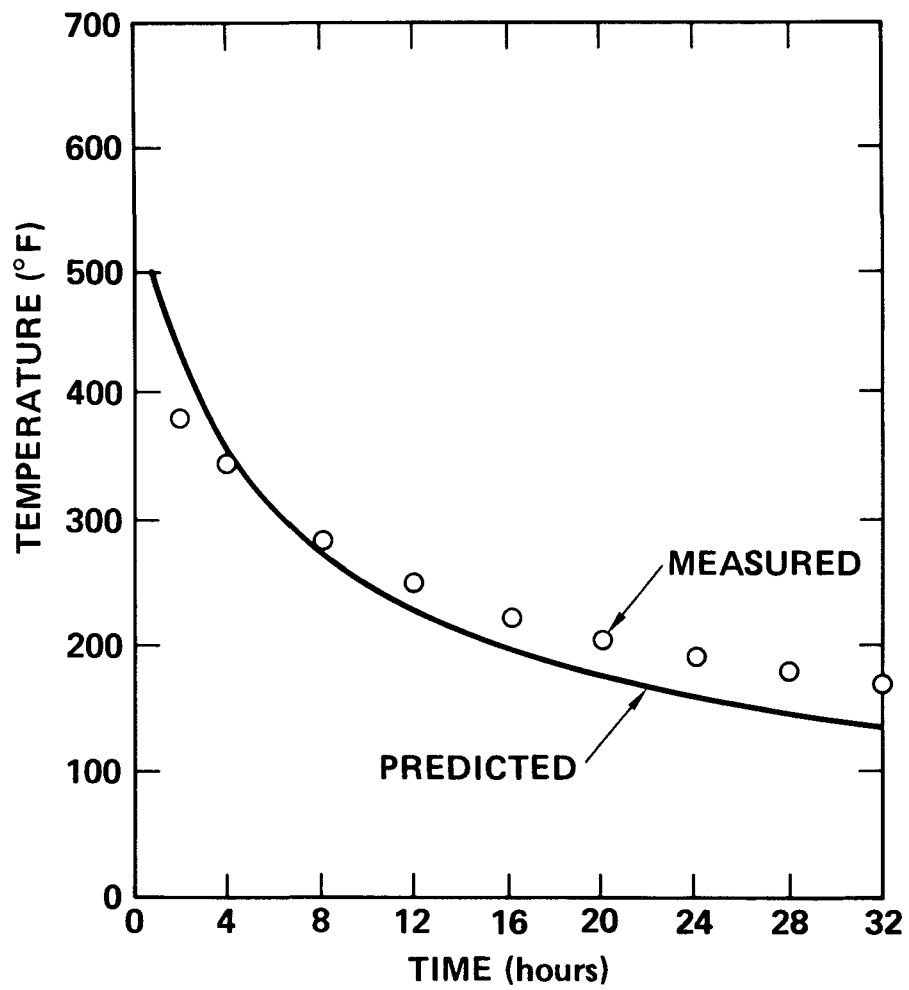


Figure 2-15. Sodium Fire Test, Cell Gas
Average Temperature

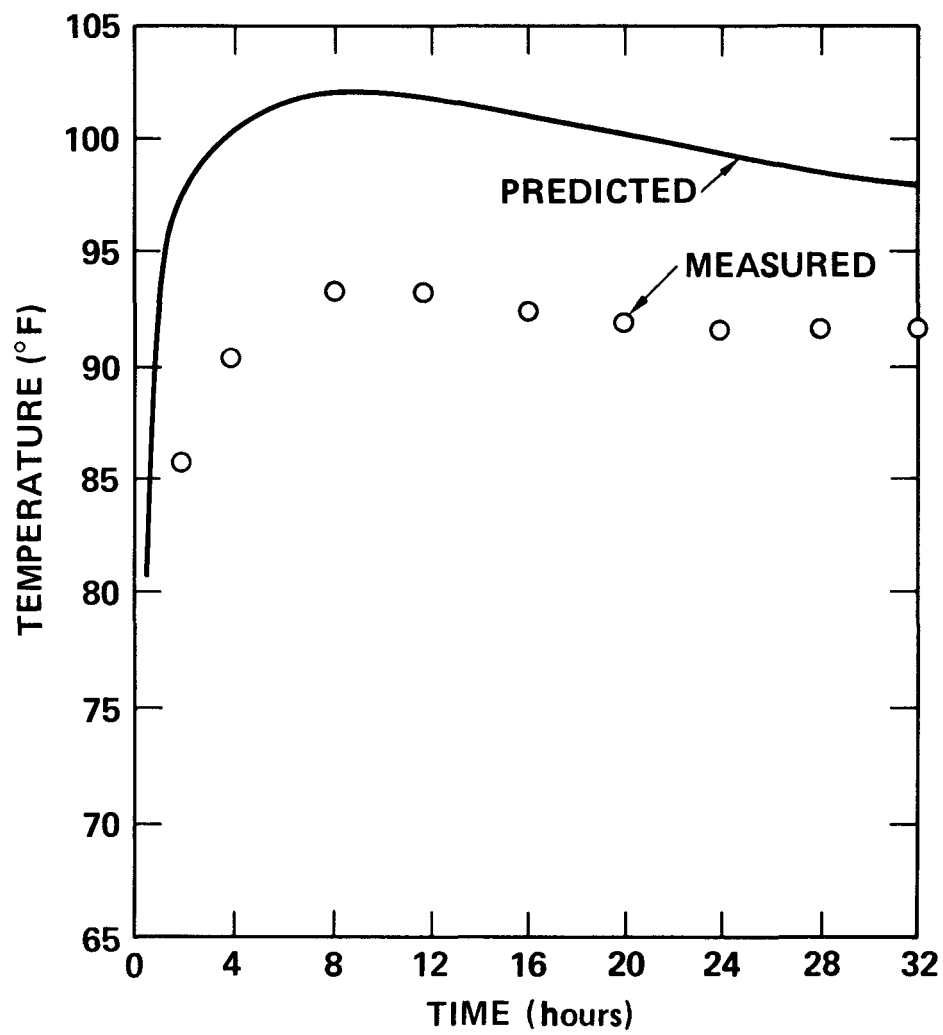


Figure 2-16. Sodium Fire Test, Building Concrete Wall Surface Temperature

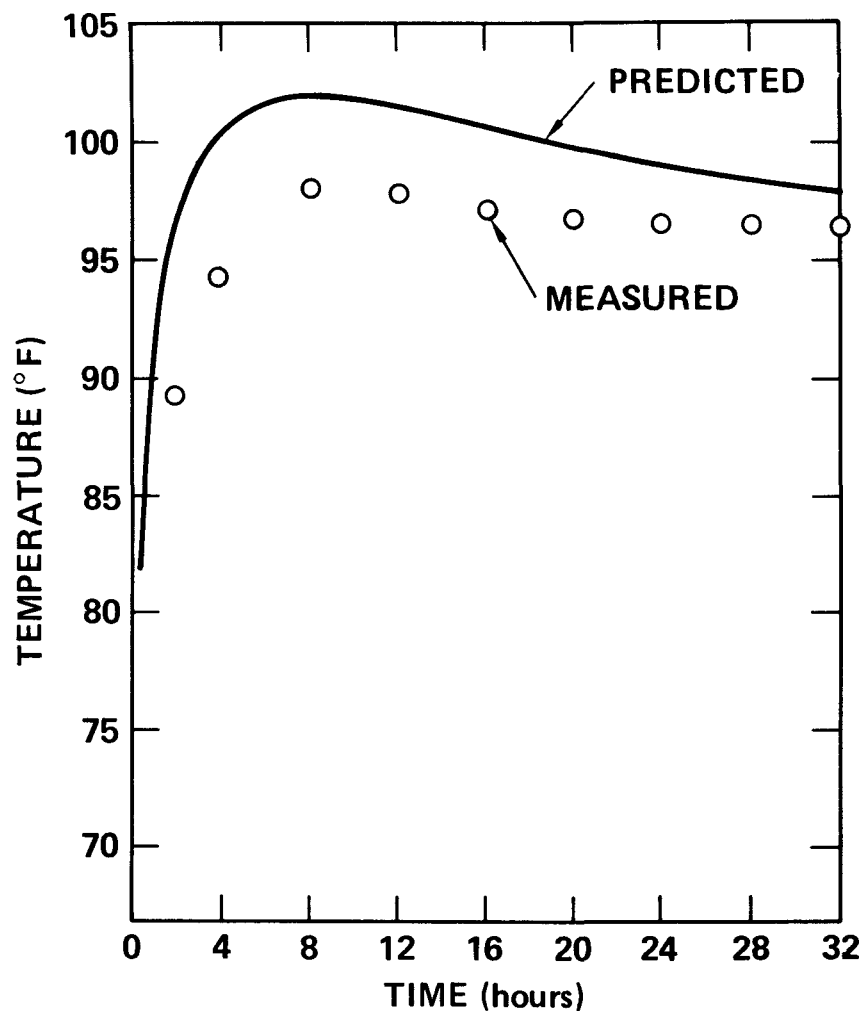


Figure 2-17. Sodium Fire Test, Concrete Ceiling Surface Temperature

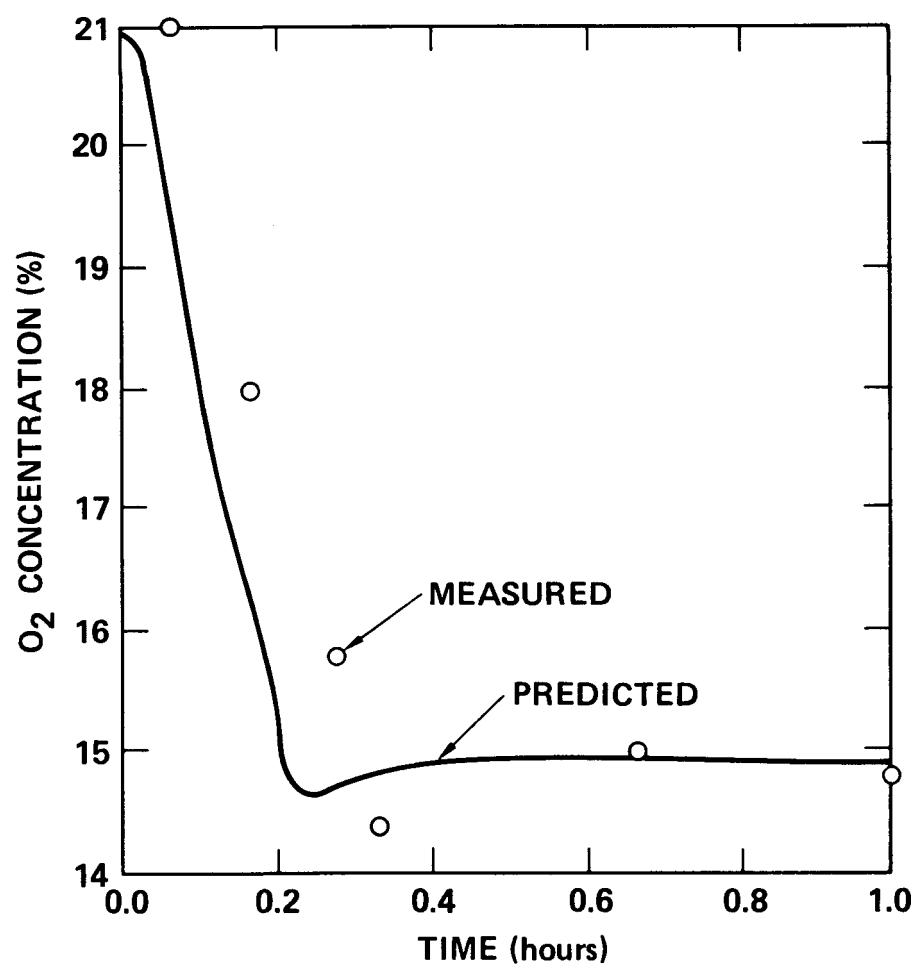


Figure 2-18. Sodium Fire Test, Cell Oxygen Concentration

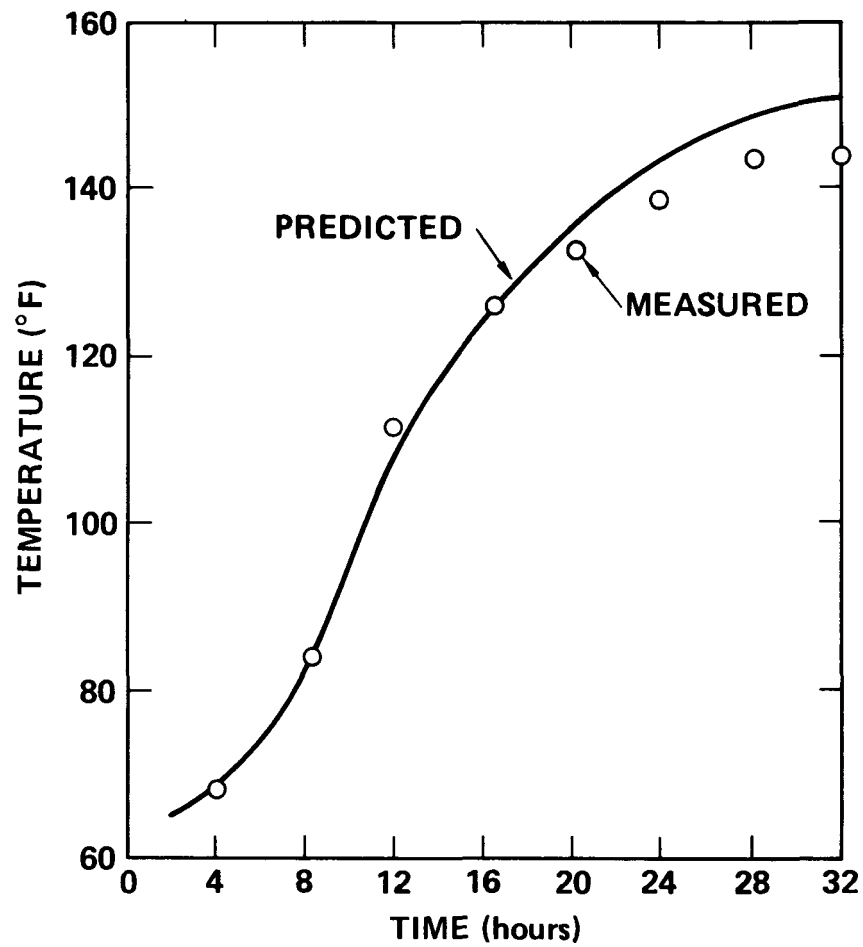


Figure 2-19. Sodium Fire Test, Concrete Floor
Surface Temperature Under Catch Pan

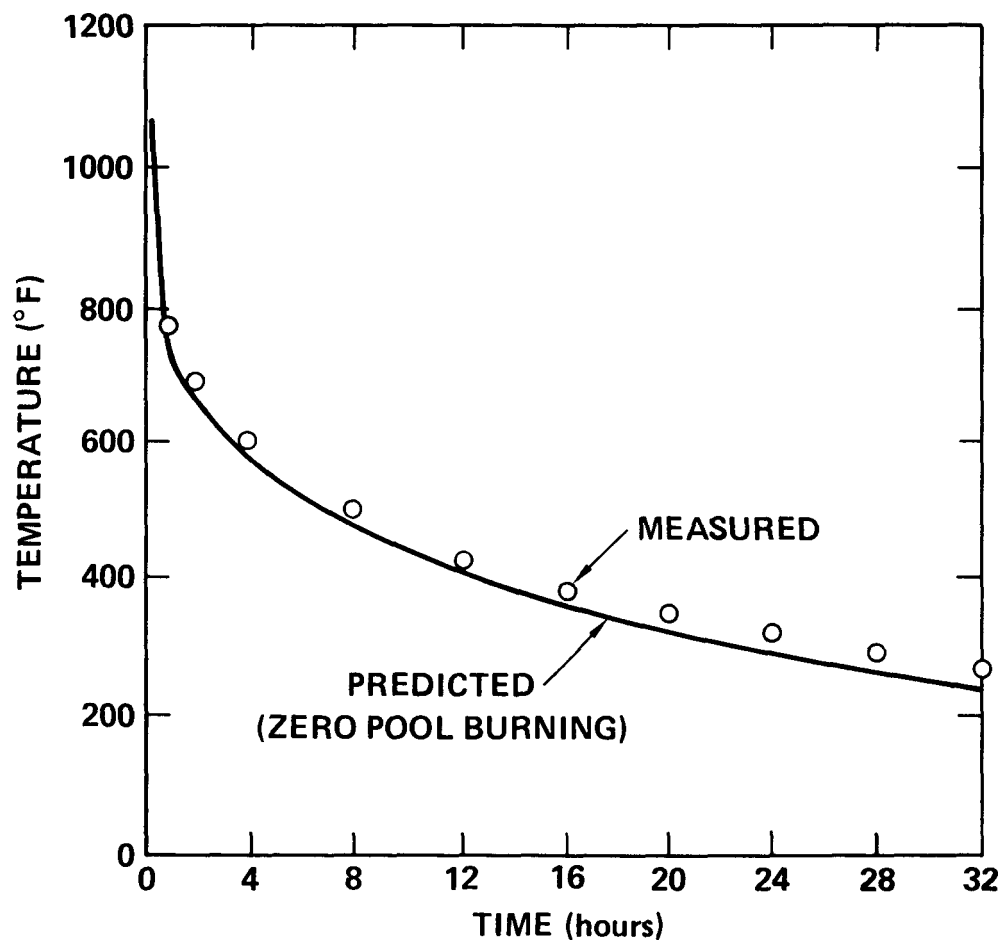


Figure 2-20. Sodium Fire Test, Sodium Pool Temperature

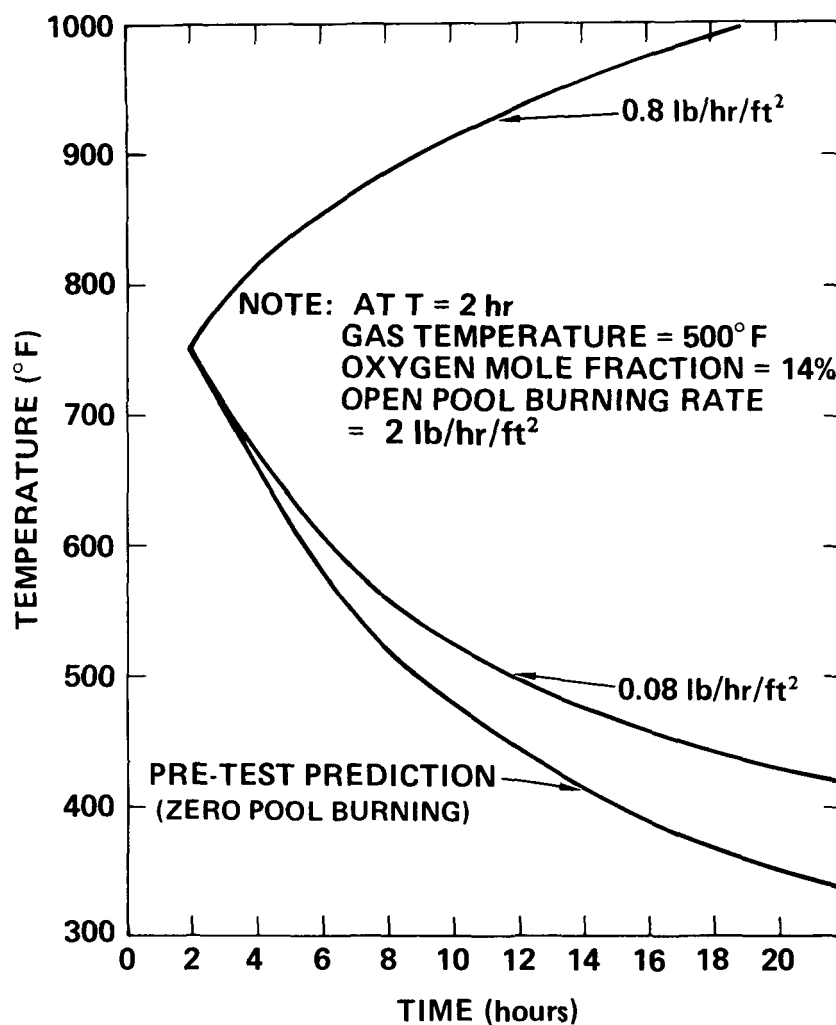
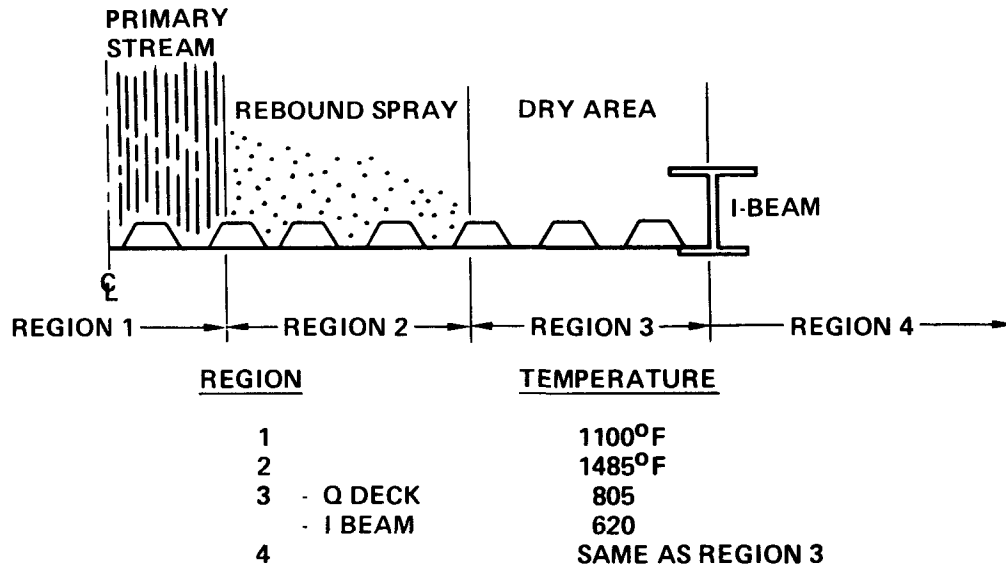


Figure 2-21. Effect of Pool Burning on Sodium Temperature

suppression effectiveness of the system by comparing the zero pool burning scenario with other assumed pool burning rates. The overall effectiveness of the system is demonstrated by the total quantity of sodium consumed. Of the approximately 33,000 lb of sodium discharged, it has been estimated, on the basis of posttest analysis, that a total of between 520 and 550 lb of sodium were consumed during the test, and only 80 lb was burned beneath the fire suppression deck.

Pretest estimates were made of the spread of sodium over the test article surface and of the expected maximum Q-deck and I-beam temperatures. These predictions are illustrated in Figure 2-22. Figure 2-23 shows the actual areas of the test article that were flooded. Figure 2-24 is a plot of measured Q-deck temperatures. The location of the thermocouple relative to the discharge target is illustrated. The plots show that the thermocouples closest to the target spot (11QD1, 12QD2) peaked out at near the sodium discharge temperature and remained at that level until the end of the discharge. On the other hand, the thermocouple located away from the target area (9QD1) reached a temperature well above the sodium discharge temperature and decreased near the end of the spray. The measured temperatures are in good agreement with the predictions illustrated in Figure 2-22 assuming that (11QD1, 12QD2) are in the flooded region of the Q-deck at all times and 9QD1 is in the spray rebound region during the early low-flow rate phase of the test. The drop in the temperature at 9QD1 after 10 min can be attributed to flooding of that area during the final high-flow rate phase. The increase in temperature following the termination of the discharge is attributed to burning of the residual sodium on the Q-deck surface. Figure 2-25 shows a similar behavior in I-beam temperatures. In this case, the I-beam is located at the end of the Q-deck channels and the lower flange is flooded during the entire sodium discharge period. These results demonstrate that those structures subjected to rebound spray will be heated to temperatures in excess of the sodium temperature. Figure 2-26 illustrates the temperature response of an I-beam that is outside the flooded area and is heated by radiation and conduction from the sodium pool.

● **LOW FLOW REGIME (60-150 GPM)**



● **HIGH FLOW REGIME (950 GPM)**

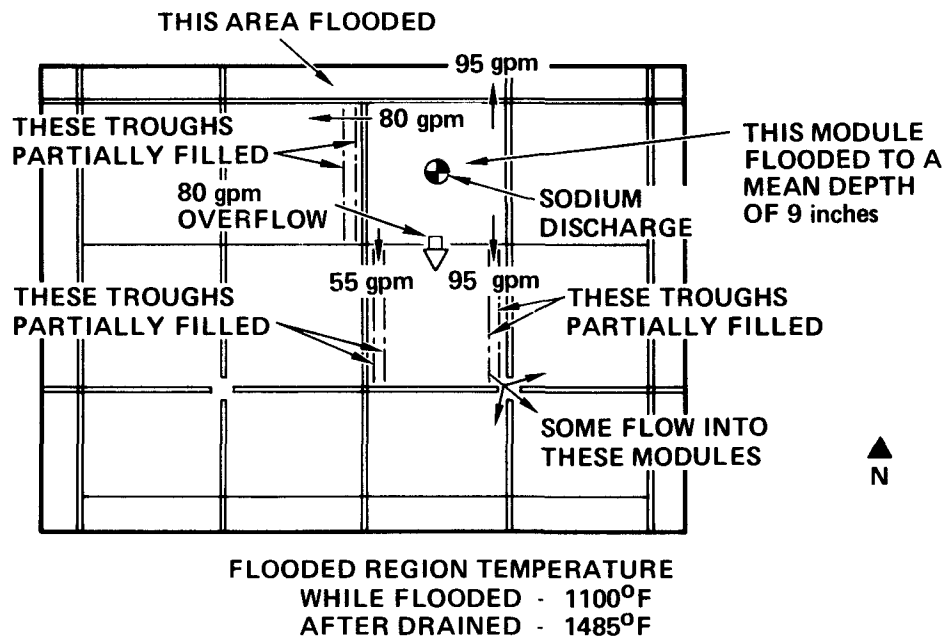


Figure 2-22. Structural Temperature Predictions

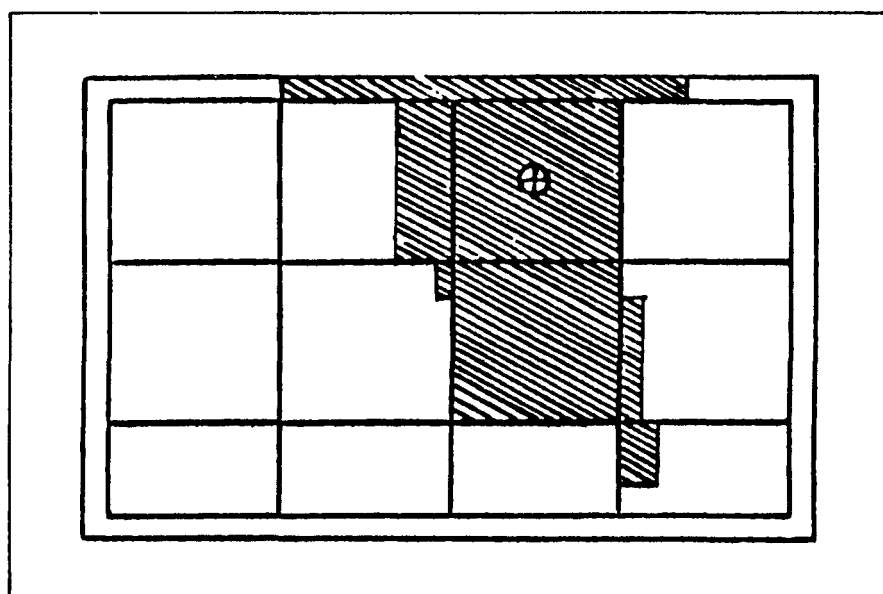


Figure 2-23. Sodium Spill Area on Q-Deck Surface

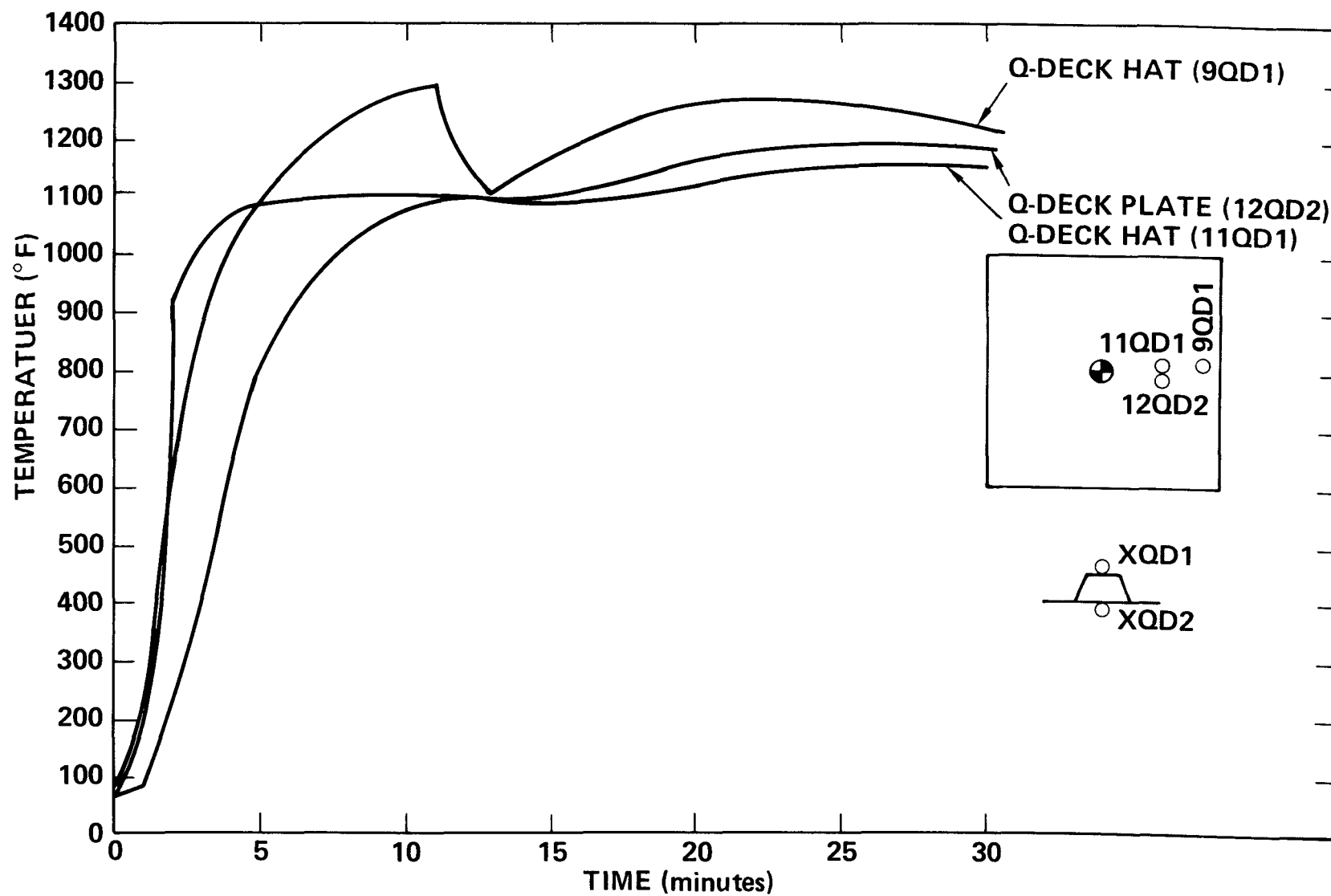


Figure 2-24. Fire Suppression Deck Temperatures

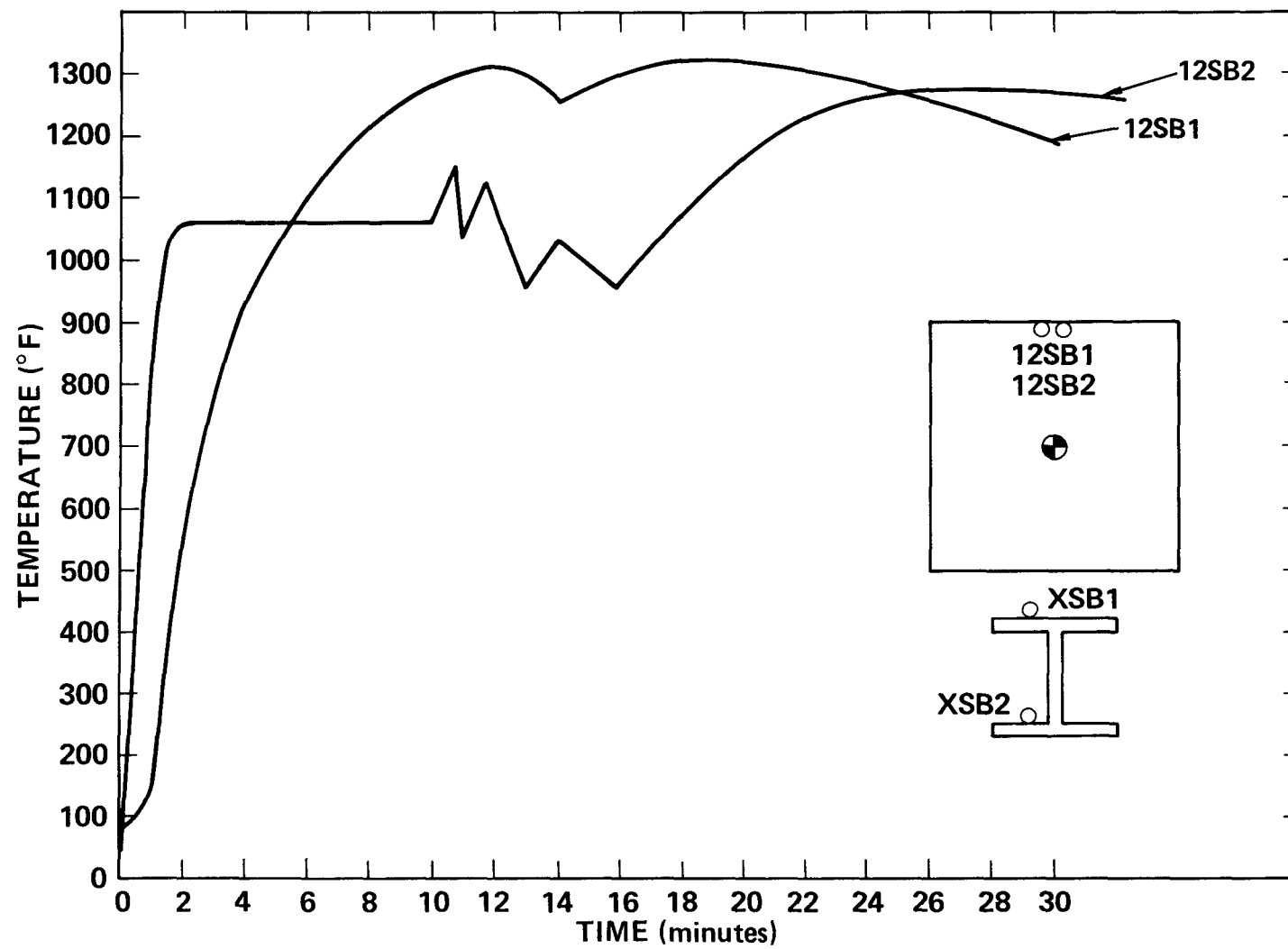


Figure 2-25. I-Beam Temperatures

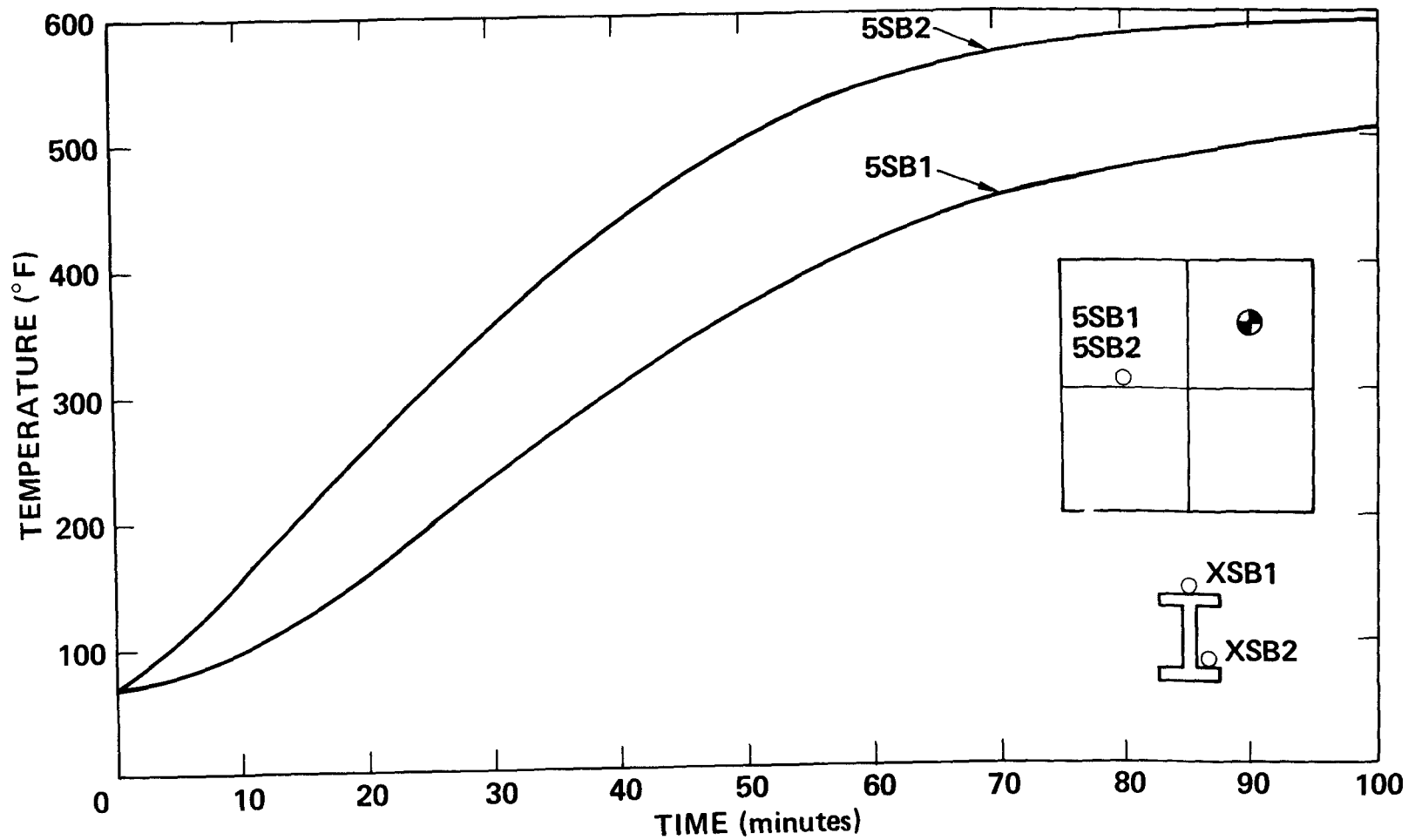


Figure 2-26. Sodium Fire Test, I-Beam Temperatures

The pipe hanger and snubber showed no significant change in performance, based on measurements before and after the test. All five photoelectric aerosol detectors responded to the sodium aerosols and transmitted an alarm prior to being functionally impaired by the fire environment.

In summary, the preliminary evaluations of the consequences of design basis sodium and NaK leak accidents in CRBRP indicated that the resulting concrete temperatures were acceptable from a structural design standpoint and that the aerosol releases to the atmosphere resulted in acceptable site boundary concentrations. Concerns remained regarding the potential hazards associated with water release from heated concrete and jet impingement onto exposed concrete surfaces.

In an attempt to verify the above conclusions, a large-scale sodium spray fire test simulating a design basis sodium leak in the IHTS piping was performed. The results of this test showed that existing computer code models were inadequate to predict the consequences of design basis leaks. The codes underpredicted the spray burning rate by a significant margin and failed to account for the local hot spot structural temperatures created by gas circulation. A modified computer code (SPCA-II) was developed to correct these deficiencies. Calculations performed with SPCA-II showed that design basis leaks resulted in unacceptably high structural temperatures and aerosol releases.

A comprehensive evaluation of alternative methods to reduce the severity of the accident consequences was performed. The effectiveness of changes in accident modeling, leak parameters, and plant design were evaluated. It was concluded that the only effective method to reduce the consequences of the accident to acceptable levels was to design to limit the potential for spray formation. A modified piping insulation system was designed to remain intake during a design basis accident and to direct the pipe leakage onto the catch pan as a columnated stream with less than 5% of the pipe leakage forming a spray. The effectiveness of this design was demonstrated by testing a

full-scale prototype with water under simulated sodium conditions. The effectiveness of the overall design approach for accommodating design basis leak accidents was demonstrated in a large-scale sodium fire test.

A detail description of the CRBRP sodium fire protection system design evolution and test results are described in "CRBRP Sodium Fire Protection System Design," ESG-DOE-13428 (Reference 13) and "SPCA-II Documentation and Users' Manual," ESG-DOE-13429 (Reference 14).

2.3.2 Features and Guidelines for Cleanup and Recovery from Large Fires

2.3.2.1 Background

In conjunction with the sodium fire suppression tests conducted under DRS 26.03/02.04.10 (Reference 20) at AI-ESG during FY 1982 and 1983, a prime test objective was to develop and demonstrate a safe and economical method of sodium cleanup and recovery following a large sodium spill and to provide practical information on sodium disposal techniques.

Previously experienced sodium spills have involved relatively small quantities of sodium spilled into air atmospheres and intermediate quantities spilled into inerted atmospheres. The AI sodium fire suppression test (Reference 21) involved the release of sodium from a "design basis sodium spill" into an air-filled cell. Approximately 5,000 gal of 1060°F sodium were discharged from a prototypical CRBRP pipe insulation drain plug on a one-tenth scale fire suppression/catch pan test article. During the test, the thermal characteristics of the test article were monitored. The test was considered to be completed when the temperature of the concrete under the catch pan passed through a maximum and started decreasing. During this period, freezing of the bulk sodium occurred.

Although the overall consequences of the large-scale fire suppression test were minimal, a cleanup and recovery effort resulted, which was larger than

any other industrial or commercial sodium user had experienced. The following sections discuss the techniques developed for the cleanup and recovery of this large sodium spill.

2.3.2.2 Sodium Spill Configuration

The sodium fire suppression test article was contained in a large test cell 48 ft long by 32 ft wide by 33 ft high. The primary test article consisted of a Q-deck/walking deck-catch pan assembly surrounded by wall and floor insulation and structural concrete. A photograph of the fire suppression test article is shown in Figure 2-27. The walking deck, which served as the surface of the test article, was placed at a prototypical height above the Q-deck that was the basic mechanism of the fire suppression system. Directly below the Q-deck assembly was the sodium catch pan, which was designed to contain sodium spills up to 7,000 gallons. A network of structural steel members of specific CRBRP design mounted directly in the structural concrete supported the Q-deck/walking deck structures. Each Q-deck assembly was provided with drain pipes through which sodium drained from the Q-deck surface into the catch pan. As the level of sodium increased in the catch pan the drain pipes became sealed thereby providing a seal so that air could not enter under the Q-deck and sustain sodium burning. A schematic layout of the fire suppression test article is shown in Figure 2-28.

2.3.2.3 Sodium Spill Conditions

The sodium was preheated to test conditions in a large, 10,000-gallon, preheat/storage tank. After the test conditions noted in Table 2-6 were established, the sodium was released onto the fire suppression Q-deck/walking deck-catch pan assembly at various discharge rates through a simulated CRBR sodium pipe insulation drain plug located 6 ft above the walking deck, see Figure 2-28.



Figure 2-27. Sodium Fire Suppression Test Article

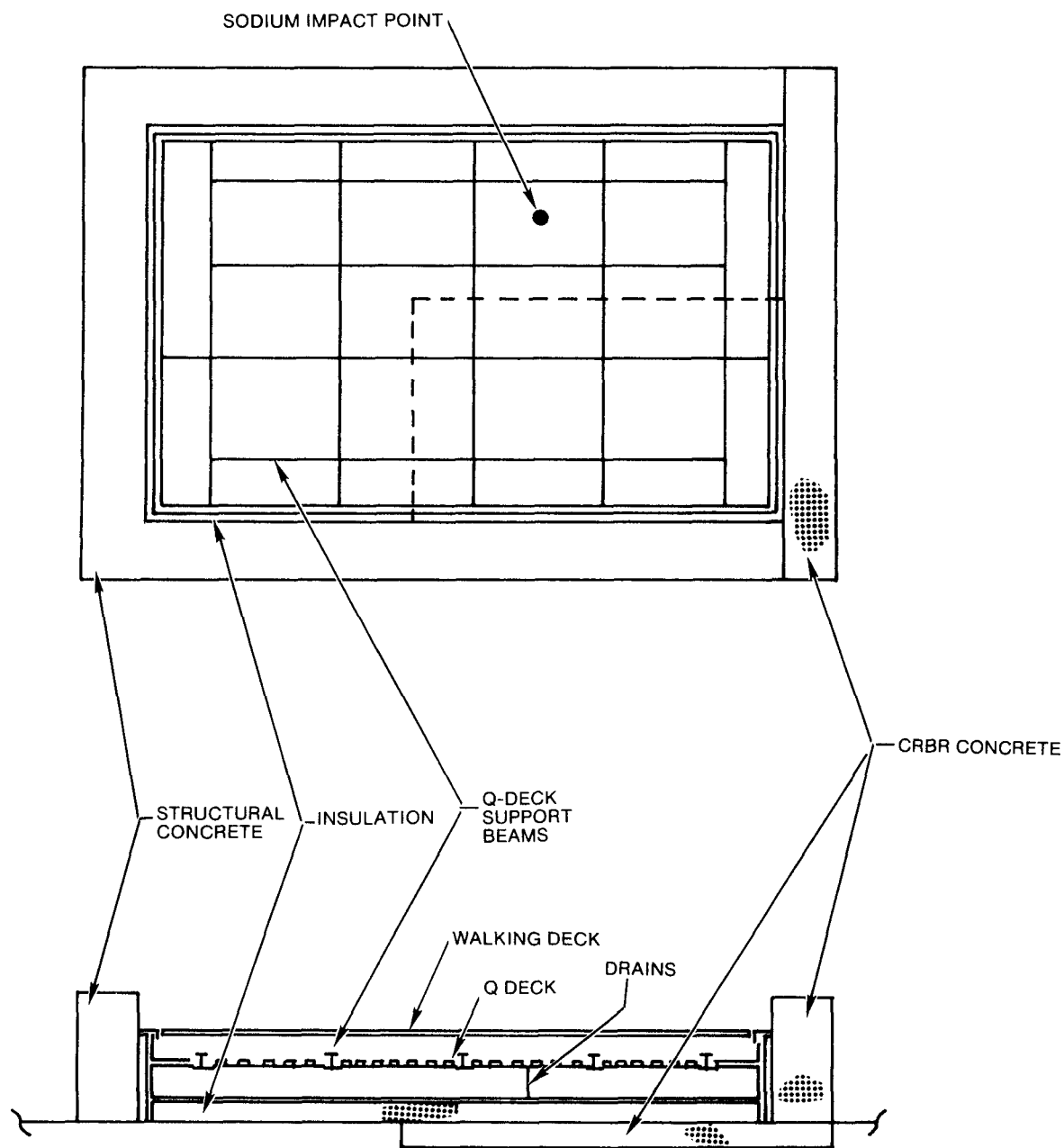


Figure 2-28. Schematic of Fire Suppression Test Article

TABLE 2-6
LARGE-SCALE FIRE SUPPRESSION TEST CONDITIONS

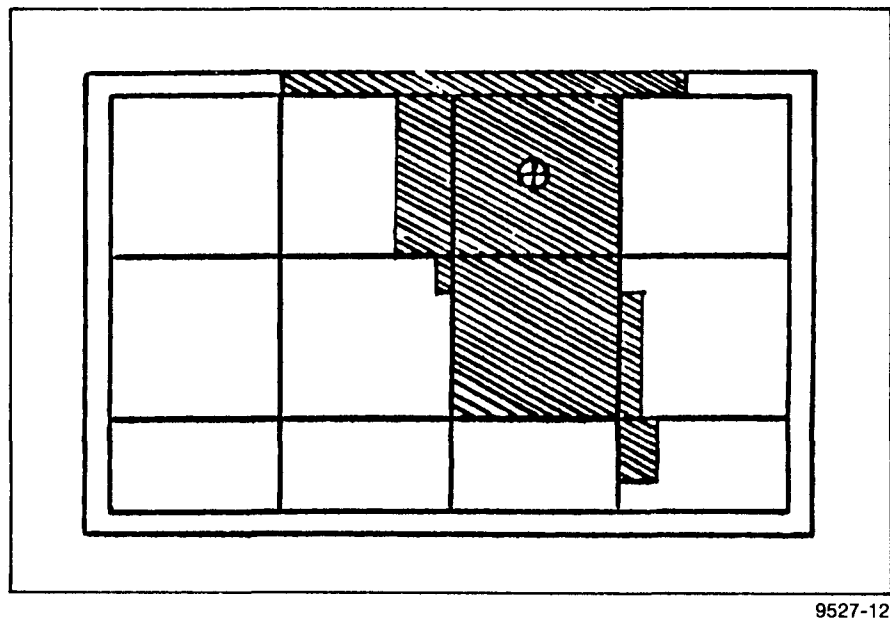
Cell gas temperature	80°F
Cell gas humidity	9% RH
Cell gas oxygen concentration	21%
Barometric pressure	716.8 mm Hg
Sodium temperature at release	1060°F
Sodium release rate, 4.5 min @	408 gpm
Sodium release rate, 6.2 min @	408-217 gpm
Sodium release rate, 2.8 min @	217-662 gpm

The release of 4,839 gallons of sodium during the 13.5-min period resulted in the system conditions noted in Table 2-7 (see Reference 21).

TABLE 2-7
SUMMARY OF FIRE SUPPRESSION TEST RESULTS

Maximum test cell pressure (vented)	0.057 psig
Maximum test cell gas temperature (near discharge)	1700-2200°F
Maximum Q-deck temperature	1300°F
Maximum catch pan temperature	1080°F
Maximum catch pan insulation (floor) temperature	700°F
Maximum catch pan concrete (floor) temperature	135°F
Occuring at	33 hr
Quantity of sodium burned on Q-deck	400 lb
Approximate burn time	20 min
Quantity of sodium burned under Q-deck	82 lb
Quantity of sodium aerosol released from test cell	7 lb

Approximately 7 days were required for the temperature of the test article to decay to a level which permitted personnel entry into the test cell. An assessment of the spill consequences were made during the initial entry to the cell. During the initial entry, the spill area was viewed. Figure 2-29 shows that only two Q-deck sections were completely covered by the sodium and four other sections only partially effected. The walking deck directly under the discharge nozzle was distroted up to 4 in. due to the impaction of the molten sodium. Although posttest analysis showed that 54 gal of sodium were burned on the surface of the Q-deck, evidence clearly showed that the remaining 4,785 gal freely drained into the catch pan during the spill. The majority of the sodium combustion products (sodium oxide aerosols) were swept from the test cell into a water scrubber system as only minimal quantities of aerosol deposits were found on the test cell floor, wall, and ceiling areas.



9527-122

Figure 2-29. Configuration of Sodium Spill

Shortly after the test was completed, the introduction of nitrogen gas under the Q-deck was initiated. This purge, approximately 3 cfm, was maintained until after sodium recovery and cleanup was completed.

2.3.2.4 Method of Cleanup and Recovery

The following method of cleanup and recovery of the large fire suppression test system was implemented:

- A. Maintain nitrogen gas purge under Q-deck until sodium recovery and cleanup is completed.
- B. Remove walking deck and clean Q-deck surface.
- C. Obtain sodium core samples from four catch pan areas; analyze for sodium oxide content.
- D. Replace walking deck; install heaters, flashing, and insulation.
- E. Remelt sodium in catch pan.
- F. Transfer sodium to sodium storage tank.
- G. Allow temperature of Q-deck/walking deck-catch pan to decay to ambient conditions.
- H. Remove insulation, flashing, and heaters.
- I. Remove one Q-deck panel from Section 4 and inspect catch pan.
- J. Remove bulk sodium and sodium oxide residues from Section 4, and replace Q-deck panels.

- K. Repeat previous step until all bulk residues have been removed from the catch pan.
- L. Rinse and dissolve remaining sodium residues in catch pan with water.
- M. Remove rinse water and dry catch pan.

2.3.2.4.1 Catch Pan Inerting

During periods of sodium temperature decay or when the sodium was to be contained in the catch pan for a period of time, a nitrogen gas purge was supplied to the catch pan under the Q-deck to prevent inleakage of air and thereby retard sodium surface oxidation.

2.3.2.4.2 Q-Deck Surface Cleanup

The bulk sodium and sodium oxide residues on the surface of the Q-deck panels were removed as soon as possible to prevent hydrolyzation. The removal technique was to break up the oxide crust using an electric chisel, placing all of the residues into a plastic bag in a 55-gal drum.

(NOTE: The use of an electric chisel is preferred to a pneumatic chisel because the high-pressure air which operated the pneumatic chisel generally contains moisture. Also the exhaust gas is directed toward the work area which results in rapid hydrolyzation of the exposed sodium oxides.)

To prevent buildup of hydrogen gas resulting from the hydrolyzation of sodium oxide on the Q-deck surface, dry ice was periodically placed directly in the work area to prevent the accumulation of sodium hydroxide.

(NOTE: This action is discussed thoroughly in Section 2.3.2.4.7.2.)

2.3.2.4.3 Sampling Sodium in Catch Pan

Core samples of the catch pan sodium were obtained through several Q-deck drain pipes. A coring tool was manually inserted into the pipe to secure a full-length sodium sample. The sample was removed from the coring tool and analyzed for oxide content using the mercury amalgamation process. The results of these tests provided an insight to the degree of sodium oxidization occurring while the sodium was retained in the catch pan and determines the overall sodium purity in the event the sodium was to be reused. Results of the coring samples taken after the sodium fire suppression test are listed in Table 2-8.

TABLE 2-8
OXYGEN CONTENT OF CORES FROM FROZEN
SODIUM IN THE CATCH PAN AREA

Depth Below Sodium Surface (in.)	Oxygen Content at Coring Location (ppm)		
	NE-4	C-7	SW-9
0	7,300	7,600	15,000
4	40	230	370
8	70	400	300
12	-	360	140

*Core designation:

NE-4 is from underneath the north-east Q-deck panel.

C-7 is from underneath the central Q-deck panel.

SW-9 is from underneath the south-west Q-deck panel.

2.3.2.4.4 Heater Installation

The walking deck panels were replaced on the test article, and a heating assembly was installed. The heating assembly consisted of 60 Cal-rod heaters equally spaced on the walking deck surface. Stainless steel foil, 5 mils thick, was then installed over the heater array to act as a reflector. A total of 10-in. Kao-wool insulation blanket was then installed on top of the stainless steel foil flashing forming as tight a seal around the concrete walls as possible. The heater assembly was powered by a 75-kW portable generator.

Each Cal-rod heater circuit was instrumented with thermocouples to provide temperature indication during the remelt operations.

2.3.2.4.5 Sodium Remelt

During the remelt operations, the temperature of the Cal-rod heaters was limited to 1400°F. Approximately 18 h were required to elevate the Q-deck, catch pan, and sodium to a mean temperature of 400°F. After the remelt was completed, the sodium transfer lines were preheated to 500°F and a 27 in. Hg vacuum established in the large sodium storage tank. The transfer line valves were opened to establish draining of the sodium in the catch pan. The entire transfer was performed at a transfer rate of 4 to 5 gpm, which took approximately 16 h to complete.

After the sodium transfer was complete, the transfer line valves were closed to isolate the sodium storage tank, the electrical power to the heater assembly was terminated, and the system allowed to cool to ambient temperatures. (A nitrogen purge was supplied under the Q-deck to maintain an inert atmosphere above the remaining sodium.)

After the system cooled to ambient temperatures, the insulation, flashing, and Cal-rod heaters were removed from the walking deck. Preparations for the initial catch pan inspection included removal of the walking deck and all Q-deck holddown clamps.

2.3.2.4.6 Initial Catch Pan Inspection

Three Q-deck panel sections were removed from Section 4 (northeast corner of the test article) to allow inspection of the catch pan. Upon entry it was found that only a thin layer of frozen sodium, which was covered with a thin layer of sodium oxide remained on the catch pan bottom. In the areas of the structural beam support columns, where air inleakage could occur, the degree of sodium oxidation was much greater. A series of photographs were taken to show the condition of the catch pan after the sodium draining. Figures 2-30 and 2-31 depict the general floor area and large sodium oxide deposits found during the initial catch pan inspection.

2.3.2.4.7 Catch Pan Cleanup

2.3.2.4.7.1 Material and Equipment Required for Cleanup

The material and equipment listed in Table 2-9 is recommended for cleanup after a sodium spill similar to the large fire suppression test.

2.3.2.4.7.2 Precautions During Catch Pan Cleanup

The precautions listed below should be followed during the removal of sodium and sodium oxides from a catch pan after a large sodium spill:

- A. Maintain an inert cover gas over the sodium and sodium oxide residues that are contained in a catch pan. This restricts further oxidization of sodium residues.

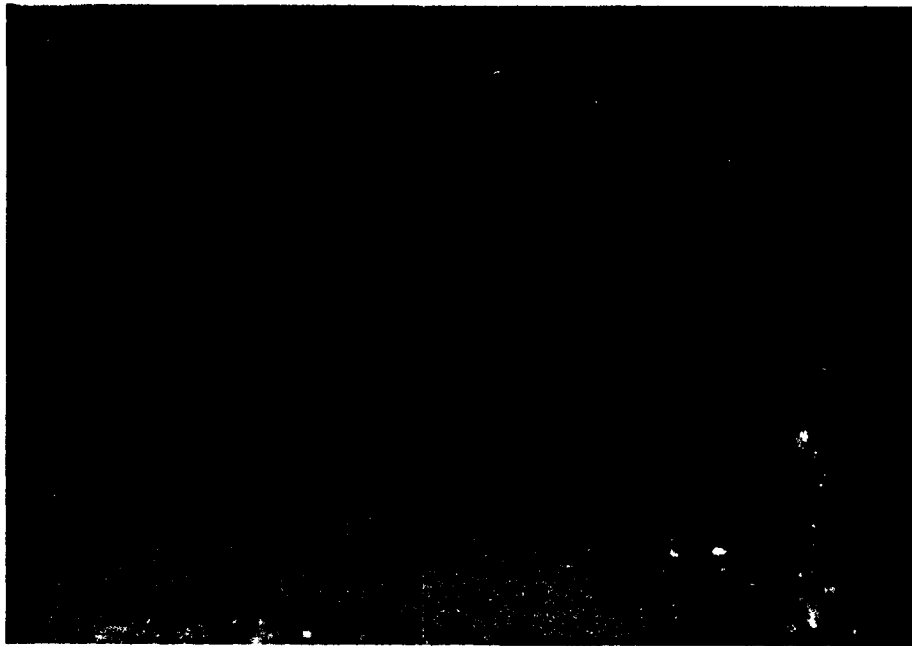


Figure 2-30. Initial Condition of Catch Pan Floor

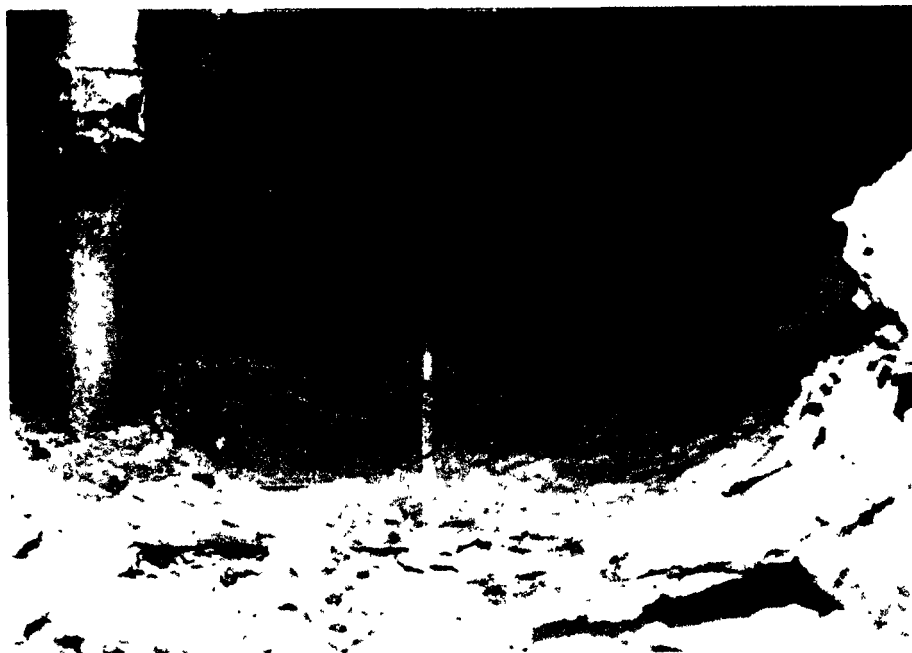
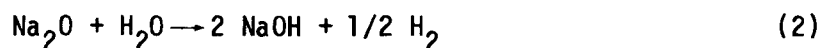


Figure 2-31. Initial Condition of Catch Pan Floor
Near Support Column

TABLE 2-9
MATERIAL AND EQUIPMENT RECOMMENDED FOR SODIUM CLEANUP

Item	Use
1. Fire-retardant coveralls, hood, gloves, hard hat (with full-face shield), dust masks, and rubber boots	Protection of personnel against caustic burns
2. Electric chisels (Black & Decker type Macho III)	Removing sodium and sodium oxides from catch pan
3. Portable oxygen analyzer	Determine oxygen concentration under Q-deck
4. 55-gal drums with plastic liners and lids	To contain residues removed from catch pan
5. Argon gas	To inert 55-gal drums during filling
6. Broad nose shovels	Residue removal
7. Q-deck panel removal tools	Q-deck panel handling
8. Port-A-Band saw	Drain pipe removal
9. Dry ice	Prevent buildup of NaOH (refer to Section 2.3.2.4.7.2)

- B. Exclude water from a catch pan containing sodium and sodium oxide residues. The result of water in the catch causes the release of hydrogen by the following chemical reactions:



The volume of hydrogen gas released from the above reactions, if not checked, can increase to a concentration level that is explosive (>4 v/o). These violent reactions can further spread local sodium fires.

- D. Maintain an inert atmosphere inside the 55-gal drum polyethylene liners, and cover the residue with calcium carbonate (a sodium fire extinguishant) during filling. This action prevents reignition of sodium residues while being contained in the drums (see Figure 2-33). When the drums become full with residues, seal the barrel liner and the drum.



Figure 2-33. Inerting Drum and Sealing Drum Liner

2.3.2.4.7.3 Removal of Residues from Catch Pan

All of the bulk sodium and sodium oxide residues were removed from the catch pan following the precautions listed in Section 2.3.2.4.7.2. The general approach was to remove only one Q-deck section at a time (three Q-deck panels), placing the portable oxygen analyzer sensor directly into the work area, then cleaning that respective area and replacing the panels either at the end of a work shift or when all residues were removed (see Figure 2-34).

- C. Prevent liquid sodium hydroxide from forming on the surface of the sodium-sodium oxide residues. The water of hydration of sodium hydroxide causes reignition of sodium contained in the residues. The hydrolysis of sodium hydroxide can be prevented by the addition of dry ice directly on the surface of the residues, Figure 2-32, causing the sodium hydroxide to be converted directly to sodium carbonate as shown in the following chemical reaction:

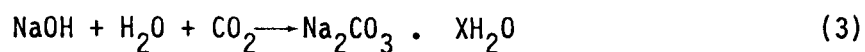


Figure 2-32. Addition of Dry Ice to Catch Pan Residues

It should be noted that the sodium carbonate can contain up to ten waters of hydration and still retain its crystalline form. Also, the sodium carbonate and dry ice acts as an extinguishant when on the surface of elemental sodium.



Figure 2-34. Opened Q-Deck Section for Cleaning Catch Pan

(Due to extensive warpage of the catch pan metal during the spill, many of the Q-deck drain pipes were embedded in the residual sodium.) To expedite removal of both the sodium and sodium oxides and the transferring of Q-deck panels during the cleanup, all of the drain pipes connected to the panels and support beams were removed using a Port-A-Band saw (see Figure 2-35).

When the removal of all the bulk sodium and sodium oxides was completed, the remaining sodium oxide film covering the catch pan walls and floor was reacted and brought into solution by misting these areas with a fine water spray. The caustic solution, which collected on the catch pan floor, was diluted with approximately 2,000 gal of water, then pumped into a commercial waste disposal truck. Final cleanup of the catch pan was completed by dry mopping any remaining liquid.



Figure 2-35. Q-Deck Drain Pipe Removal

2.3.2.4.7.4 Removal of Residues from Walls, Ceiling, and Floor

The walls and ceiling of the test cell were protected with a layer of insulating material to prevent damage to the structural concrete during the test. All deposited residues on the walls and ceiling were removed with the insulation after the test. The fallout residues on the test cell floor were swept up and disposed as waste.

Following the test, the CO_2 , added as dry ice to the catch pan, converted all the residues on the walls and ceiling to sodium carbonate. This prevented the oxides from hydrolyzing and running down the walls or dripping from the ceiling.

2.3.2.4.7.5 Waste Disposal

All the 55-gal drums, which were filled with bulk sodium and sodium oxide residues, were transferred to the AI-operated Sodium Waste Disposal Facility. This facility converts all elemental sodium to sodium oxide by burning, then the oxides are dissolved in water and treated as industrial waste.

2.3.2.5 Summary

The overall safety aspects of cleanup after a large sodium spill have been greatly enhanced by use of dry ice during all cleanup stages. Previous experience witnessed numerous hydrogen explosions during cleanup, which frequently halted cleanup efforts due to spreading local sodium fires. During the cleanup after this sodium spill, no hydrogen explosions were experienced.

Workmen were able to work directly in the catch pan removing the sodium and sodium oxides at the rate of 30 to 60 ft² per day. (No injuries or burns were experienced during the entire cleanup effort.)

With the use of dry ice and gaseous nitrogen as a cover gas, it was demonstrated that large quantities of sodium could be contained in catch pan/fire suppression Q-deck configuration as used during this test.

2.3.2.6 Recommendations

Following a large sodium spill either in an experimental sodium loop or in a larger LMFBR system, AI-ESG presents the following recommendations:

- A. Use dry ice extensively as an agent to convert sodium hydroxide to the stable end-product sodium carbonate during the cleanup.
- B. Consider injecting gaseous carbon dioxide into cells or vaults where a sodium spill is occurring to enhance the formation of sodium carbonate (Reference 22).

2.4 Final System Status

2.4.1 Status at Project Termination

At project termination, the published status of the System Design Description SDD 26A was as follows:

Sections 1, 2, and Appendices	Rev. 23
Section 3	Draft 2
Section 4	Draft 2
Section 5	Draft 2
Section 6	Draft 2
Maintenance Outline Procedures (Appendix G)	Draft 2

The catch pans, fire suppression decks, insulation, etc., are part of the buildings in which they are located and, as such, are final designed by the architect-engineer. The design specification for these items is N099SS260001, Revision 4 (Reference 15).

The procurement specifications for the detail design, fabrication, and supervision of installation of the sodium fire detection System (Reference 11) were completed and approved. No procurement actions had been taken due to lack of funding.

2.4.2 Outstanding Approved Changes

At termination, there were three approved ECPs that had not been completely incorporated into project documents. They are as follows:

- B26B-067 - Integrating and Upgrading Sodium and Nonsodium Fire Detection Systems to meet NEPA Requirements (Reference 16)
- L10149 - Surveillance Requirements (Reference 17)
- L10161 - Update of HAA Best Estimate Dose Rate (Reference 18)

2.4.3 Outstanding Activities to Complete

ECP A26036, Accommodation of Na/NaK Leak Accidents in Air-Filled Cells, was sent to the lead reactor manufacturer for approval on October 18, 1983 (Reference 10). It was in the approval process at the time of termination.

The purpose of this ECP was to establish design requirements and interfaces for the accommodation of Na/NaK leak accidents in air-filled cells. This ECP would:

- A. Baseline interface requirements for plant design features required to limit the environmental consequences, cell temperatures, and aerosol releases, to acceptable levels.
- B. Baseline new design basis Na/NaK leak parameters.
- C. Baseline the cell environments, including the aerosol releases to the atmosphere, resulting from the postulated spills.

The large-scale sodium fire test was completed in May 1983. The test verified the structural integrity and fire suppression capability of the catch pan-fire suppression deck design. The water tests of the modified piping insulation design were completed in July 1983. These tests verified the structural integrity of the design and demonstrated the effectiveness of the design in reducing the uncontrolled leakage fraction to <5% of the design basis MEFS leak. Modifications to the SPCA-II code were completed in August 1983 and a cell-by-cell analysis of the consequences of Na/NaK leak accidents was completed in September 1983. Additional changes to the baseline design were identified as necessary to reduce the environmental impact of the accidents to acceptable levels. The objective of ECP A26036 is to baseline the requirements for a modified piping insulation design and the additional requirements derived from the cell-by-cell analysis. In addition, it was the intent of this ECP to provide design features, as required, to limit aerosol

releases to the atmosphere to levels that will eliminate the need for qualification testing of electrical and mechanical equipment to the plant-wide "nonsevere" aerosol environment.

Also outstanding was ECP A26034, "System 26A Catch Pan Inservice Inspection and Surveillance Requirements" (Reference 19), which would baseline requirements necessary to establish the catch pans as "Engineered Safety Features." It was awaiting project approval at termination.

2.4.4 Recommendations for Future Design

Considerable information and knowledge was gained during the 10 years of CRBRP design regarding the safe handling of Na/NaK fires and leaks. Any new plant design would be able to begin with this level of knowledge and, thus, be better able to integrate into the plant design the various features required. The CRBRP design as it eventually evolved, assuming approval and incorporation of ECP A26036 and ECP A26034, was current with the state of the art. However, it went through many costly iterations to get there. A new plant should benefit immensely from this experience.

2.5 References

1. SDD 26A, Revision 23, "System Design Description - Sodium Fire Protection System," March 11, 1983
2. N099ER260002 - Sodium Fire Detection and Aerosol Release Mitigation, System 26A Final Design Report, June 11, 1980
3. N099DRR260003 - Preliminary Design Review, Catch Pan Fire Suppression Deck, System 26A, June 4, 1979
4. N099DRR260002 - Preliminary Design Review, Sodium Fire Detection, System 26A, March 16, 1978
5. DAD 26.03 - Sodium/NaK Fire Suppression Tests, March 6, 1978
6. DAD 26.04 - Aerosol Plugging of Generic Linear Leakage Paths Typical of LMFBR Penetrations, December 9, 1980

7. N099DRS26001, Revision J - Sodium Fire Tests, May 6, 1983
8. Clinch River Breeder Reactor Plant Insulation System Design Concepts for MEFS Pipe Leak Mitigation," AESD-TME-3174, January 1983
9. Clinch River Breeder Reactor Plant IHTS Insulation Fire Mitigation Leak Test Report," AESD-TME-3190, September 1983
10. Letter 83ESG-6196, ECP 26036, Revision D, October 18, 1983
11. ST099N601001, Specification for the Sodium Fire Detection System, CRBRP, January 5, 1982
12. N40057, Specification for the Aerosol Release Mitigation System, October 28, 1983
13. ESG-DOE-13428, "CRBRP Sodium Fire Protection System Design," September 10, 1984
14. ESG-DOE-13429, "SPCA-II Documentation and Users' Manual," September 7, 1984
15. N099SS260001, Revision 4, "Liquid Metal Fire Detection and Suppression," November 17, 1982
16. Letter LA830194, Approval of ECP B26B-067, Revision 0, "Integrating and Upgrading the Sodium and Nonsodium Fire Detection Systems to Meet NFPA Requirements," June 15, 1983
17. Letter LW810478, Approval of ECP L10-149, Revision 1, "Surveillance Requirements," November 23, 1981
18. Letter LA820049, System 41 Implementation of ECP L10-161, "Update of HAA Best Estimate Dose Rate"
19. Letter 83ESG-1370, ECP A26034, Revision 0, dated February 25, 1983
20. N099DRS26001 (DRS 26.03/02.04.10), "Sodium Fire Tests, Rev. H," K. E. Buttrey, September 7, 1982
21. 351TRO00014, "Test Report for Large-Scale Fire Suppression Test No. 1 (DRS 26.03)," R. P. Johnson, November 2, 1983
22. R. N. Newman, "The Role of Carbon Dioxide in the Combustion of Sodium in Air," Proceedings of the LMFBR Safety Topical Meeting, ENS, July 19, 1982, Lyon-Ecully, France

3.0 REACTOR

3.1 Fuel Rod and Assembly Design

3.1.1 Summary of Functions and Description

The functional requirements of the fuel assembly are given in Table 3-1. These functional requirements have been satisfied by design in the manner presented in the design description of both the fuel rod and fuel assembly.

A further requirement of the fuel assembly design was that the initial core draw upon information available from Fast Flux Test Facility (FFTF) experience and Liquid Metal Fast Breeder Reactor (LMFBR) fuel development programs. As will become evident the Clinch River Breeder Reactor Plant (CRBRP) fuel rod and assembly designs are based upon FFTF driver fuel rod and assembly designs with only those modifications made that were necessitated by the operational and initial breeding requirements of the CRBRP. Hence the conceptual design of the CRBRP fuel assembly was based on that of the FFTF.

The design of the CRBRP fuel assembly is shown in Figure 3-1. Coolant flow is controlled by orifice plates within the assembly. A detailed comparison of the CRBRP and FFTF fuel assembly design details is given in Table 3-2 together with the reason for the difference in design. Welded into a compact structural unit, the fuel assembly can be handled by the refueling machines and provides a controlled path for the primary sodium coolant. As shown in Figure 3-1 each fuel assembly is comprised of the following subassemblies:

- o an inlet nozzle,
- o a shield and orifice region,
- o a rod attachment assembly,
- o 217 rods that contain fuel and axial blanket pellets and
- o an outlet nozzle

Table 3-1

Fuel Assembly Functional Requirements

- **Provide and position the nuclear fuel of the fast breeder reactor core to produce heat for the reactor heat transport system**
- **Provide neutrons for breeding plutonium in the fuel and blankets**
- **Provide a compact structural unit that can be handled in and out of the reactor by the refueling machines**
- **Provide a controlled channelled path for the primary sodium coolant of the reactor heat transport system**
- **Provide flexibility for utilization of light water reactor derived plutonium**

CRBRP FUEL ASSEMBLY

209

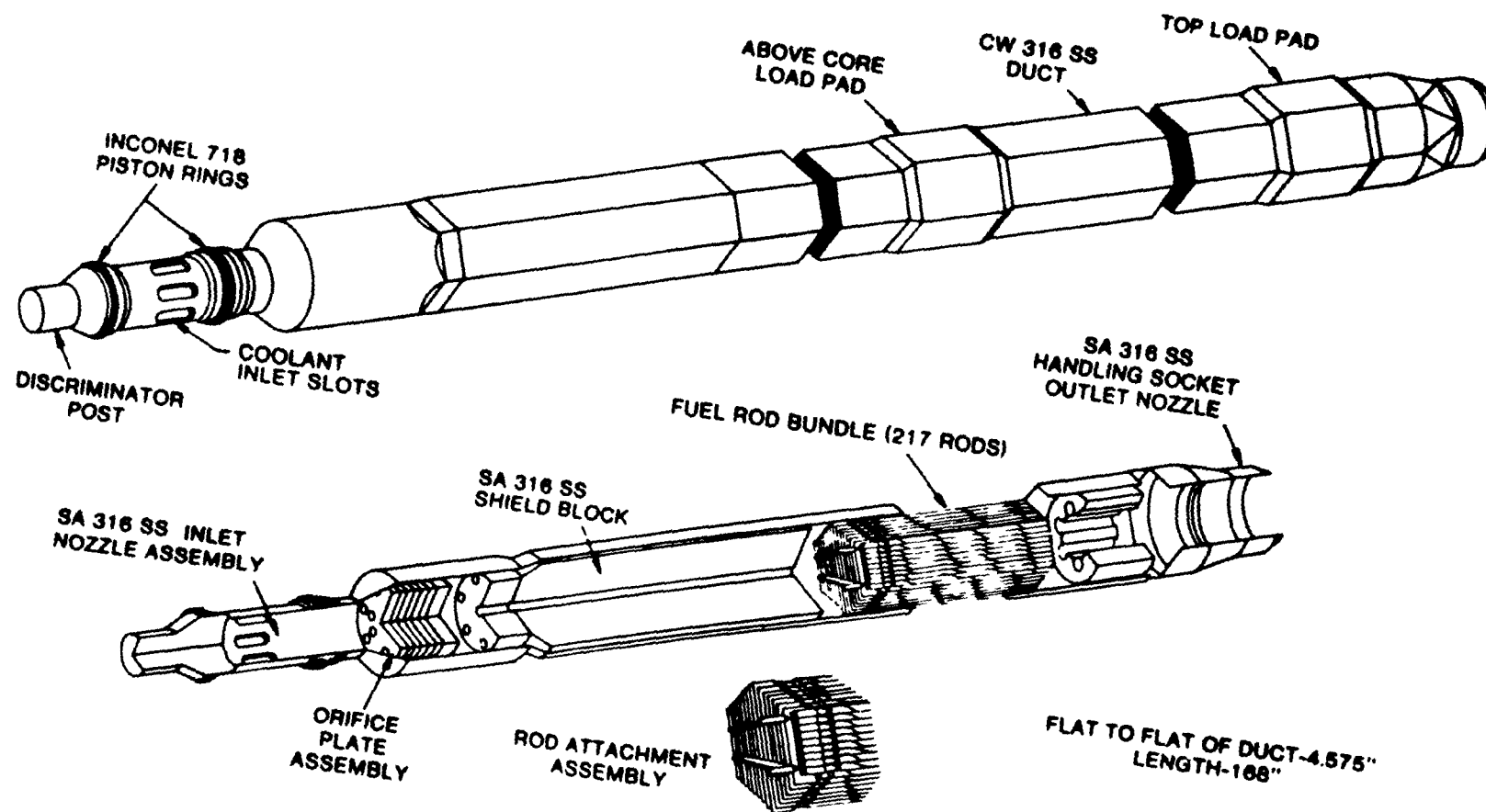


Figure 3-1

Table 3-2

CRBRP FUEL ASSEMBLY COMPARISON WITH FFTF

<u>Design Parameter</u>	<u>CRBRP Value</u>	<u>FFTF Value</u>	<u>Reason for Difference</u>
Types of discriminators (orificing zones)	6	3	Core arrangement and core size
Lower shielding length (inches)	200 (1 piece)	21.5 (3 piece)	FFTF closed loop cooling not required in CRBRP
Duct load pad (inches)			
—Outside dimension	4.745	4.715	Accommodate larger seismic loads in larger core
—Wall thickness	0.205	0.190	} Provide more space for irradiation induced deformation in higher burnup reloads
Fuel rod growth clearance (inches)	2.10	1.00	
Type of top load pad (outlet nozzle)	Fixed	Floating collar	} Evolution of creep and swelling equations for core restraint
Misaligned grapple pickup capability (inches)	1.75	1.25	
			} Allow for more tolerance stackup in larger CRBRP core

The fuel rod design is illustrated in Figure 3-2. The mixed plutonium-uranium dioxide sintered powder dished fuel pellets supply heat and neutrons for breeding, and an upper plenum containing fission product gases generated during operation. Plenum volumes larger than FFTF's are required to allow for the higher fission gas pressures caused by somewhat higher operating temperatures and by potential operation to higher equilibrium burnup levels. A comparison of the CRBRP fuel rod with that of FFTF is presented in Table 3-3 together with the reason for the difference in design. CRBRP fuel rod design details are given in Table 3-4. Pertinent features of the CRBRP fuel rods, similar to those of FFTF are:

- o The fuel pellet column is retained in position during preirradiation shipping and handling by a preloaded helical compression spring made of American Iron and Steel Institute (AISI) Type 302 Stainless Steel. After insertion of the fuel rod into the reactor, the spring is flexible enough that it will not restrict the axial expansion of the fuel pellet column.
- o Each fuel rod contains a gas tag capsule which identifies breached fuel rods by a unique gas mixture for each assembly.
- o A wire wrap spacer system is employed to maintain required lateral spacing during normal and transient operation.

The mixed uranium/plutonium dioxide fuel contained within the Fuel Assemblies satisfies the first two functional requirements of Table 3-1. That is, it produces heat for the Primary Heat Transport System and provides neutrons for breeding plutonium in the fuel itself and in the depleted uranium of the radial and axial blankets.

The fuel enrichment for the initial core loading has been selected on the basis of promoting a uniform power generation across the core and utilization of FFTF-grade (low Pu-240) plutonium. The Fuel Assemblies and the Reactor System can accommodate incorporation of light water reactor discharge plutonium in later cycles.

CRBRP FUEL ROD

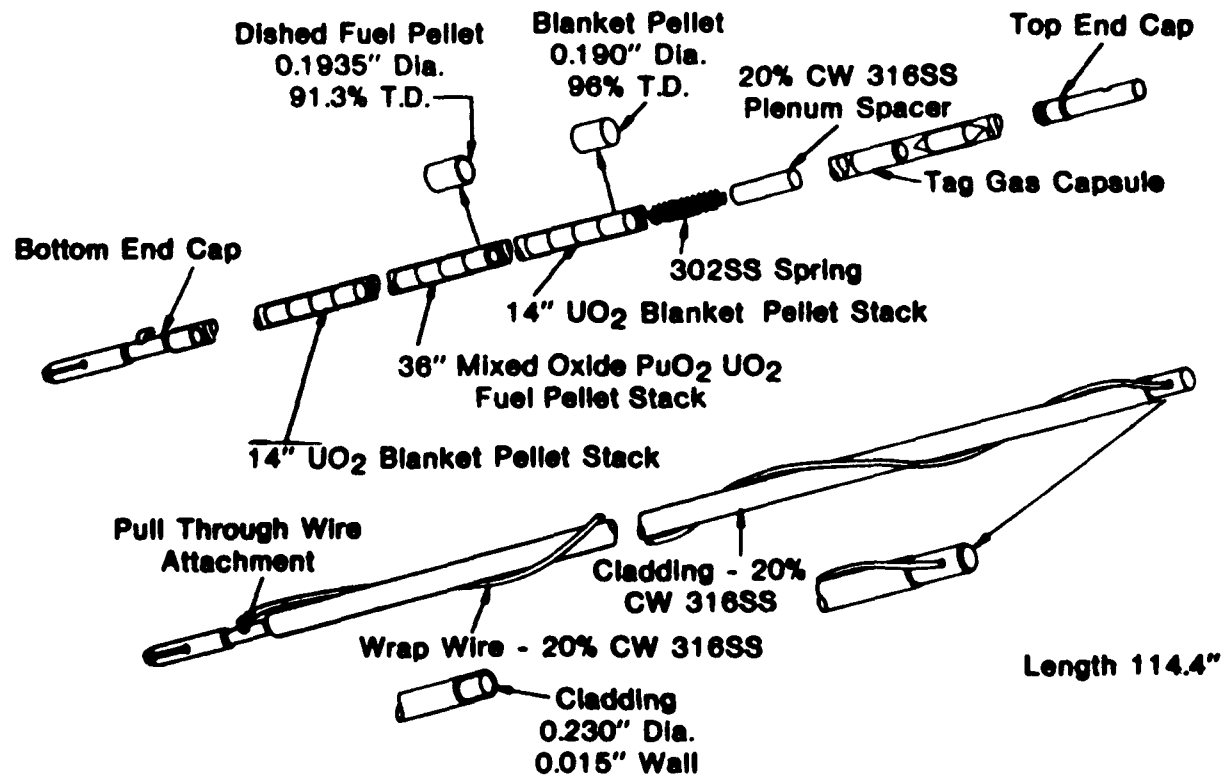


Figure 3-2

Table 3-3

CRBRP FUEL ROD COMPARISON WITH FFTF

<u>Design Parameter</u>	<u>CRBRP Value</u>	<u>FFTF Value</u>	<u>Reason for Difference</u>
Pellet PuO_2 content	0.33	0.225/0.275	More power per assembly in CRBRP heterogeneous core
Pellet density (percent of theoretical)	91.3	90.4	} Reduced FCMI for same smeared density
Pellet diameter (inch)	0.1935	0.1945	
Axial blanket stack lengths (inch)	14.0	0.8	Breeding requirements of CRBRP
Inconel reflector lengths (inch)	0.0	5.7	Shielding provided by axial blankets
Fission gas plenum length (inch)	48.0	42.0	Provide more space for accommodation of fission gas in higher burnup reloads
Overall rod length (inch)	114.4	93.4	As above

TABLE 3-4
CRBRP FUEL ROD DESIGN DETAILS

<u>Parameter</u>	<u>Nominal Value</u>
Rod Outside Diameter, in.	0.230
Rod Radial Spacing	0.056-inch diameter wire wrapped around fuel rod cladding in clockwise helical spiral with pitch of 11.9 inches
Rod Triangular Pitch, in.	0.2877
Clearance Between Fuel Rods at Wires, Nominal, in.	0.0017
Clearance Between Fuel Rods Nominal, in.	0.0577
Cladding Thickness, Nominal, in.	0.015
Cladding Thickness, Minimum, in.	0.014
Rod Axial Support	17 key-shaped rails through keyhole slots in bottom end caps
Rod Length, in.	114.40
Pellet Column Length, in.	64, including two 14-inch axial blankets
Fission Gas Plenum Length, in.	48.0
Fission Gas Plenum Available Volume-Cold, in ³	1.274
Upper/Lower Axial Blanket Length, in.	14
Upper/Lower Axial Blanket Composition	Depleted Uranium Oxide
Axial Blanket Pellet Diameter, in.	0.1935
Axial Blanket Pellet Length, in.	0.31 to 0.46
Pellet Density, % Theoretical	97.1
Fuel Region Length, in.	36
Fuel Pellet Material	Plutonium-Uranium Oxide

TABLE 3-4 (Continued)

<u>Parameter</u>	<u>Nominal Value</u>
Fuel Pellet Diameter, in.	0.1935
Pellet Length, in.	0.205 to 0.283
Density:	
Nominal, % Theoretical	91.3
Cold Smeared, % Theoretical	85.5
Plutonium Content:	
First Cycle, wt %	32.8

Positioning of the 36-inch long stack of fuel pellets and 14-inch long stack of the upper and lower axial blanket dished pellets--located above and below the fuel pellets--is provided by the fuel rods. The structural support and hermetic seal for the pellets are provided by the cladding and end caps.

A fission gas plenum is provided in the top of the fuel rod to limit internal gas pressure buildup caused by gaseous fission products. Its length was selected based upon the design conditions and the damage limits for normal operation. Within this plenum is a spring-type axial support system to prevent pellet stack movement during preirradiation shipping and handling operations. Also in the fission gas plenum is a unique tag gas that is used to identify an assembly having a breached fuel rod.

In addition to a schematic of the Fuel Assembly relating the fuel rods and the other assembly components (Figure 3-1) further details are given in Table 3-5. There are 217 fuel rods in each Fuel Assembly, spaced in a triangular array by spiral wire wraps of approximate 12-inch axial pitch along the fuel rod length. A rod attachment subassembly, welded to the shield block and located directly below the fuel-rod bundle, provides uniform flow distribution and axial restraint for the fuel rods. The attachment subassembly rails mate with keyhole slots in the fuel-rod bottom end cap. The entire fuel-rod bundle is contained within a hexagonal duct. The duct also serves to guide the sodium coolant through the assembly and to protect the bundle during all handling operations, which satisfies the remaining functional requirements of Table 3-1.

The shield-orifice subassembly, located just under the fuel-rod attachment subassembly and welded to the duct, is designed to reduce the neutron flux and resultant damage to the permanent Core Support Structure and the Lower Inlet Modules as required. Additionally, the shield-orifice subassembly provides variable flow-impedance capability which is used to offset the core radial power distribution. This assists improvement of fuel performance in the core by providing more flow to the higher power Fuel Assemblies and less flow to the lower power Fuel Assemblies. The objective is to protect the hottest fuel rod in each assembly. The shield-orifice subassembly is designed to satisfy

TABLE 3-5
CRBRP FUEL ASSEMBLY DESIGN DETAILS

<u>Parameter</u>	<u>Value</u>
Number of Assemblies	156
Rods Per Assembly	217
Rods, Total Number of Fuel	33,852
Inlet Nozzle Discrimination Post, Max. Dia., in.	1.956
Number of Orifice Zones	6
Maximum Number of Orifice Plates	4
Number of Shield Blocks	1
Shield Block Total Length, in.	20.0
Shield Block Effective Length, in.	14.7
Shield Block Outside Hex., in.	4.695
Load Pad, Outside Dimension Across Flats, in.	4.745
Duct, Inside Across Flats Dimension, in.	4.335
Duct Wall Thickness, in.	0.120
Load Pad Thickness, Nominal, in.	0.205
Fuel Rod Growth Clearance, in.	2.1
Outlet Nozzle Inside Diameter, in.	3.60
Outlet Nozzle Outside Diameter, in.	1.75
Misaligned Grapple Pickup Capability, in.	1.75
Weight, lb	
In Air	~450
In Sodium at 400°F	~390

the maximum sodium velocity limits of Section 3.13. The circular-to-hexagon transition surfaces on the shield block orient the assembly into the hexagon shaped hole in the core during refueling.

The inlet nozzle, welded to the bottom of the shield-orifice subassembly provides: (1) radial positioning, (2) axial support within the Lower Inlet Modules, (3) multiple coolant inlet passages, (4) piston ring seals to minimize bypass flow leakage, (5) hydraulic balance for axial restraint, and (6) core location discrimination. The discrimination feature prevents insertion of a Fuel Assembly into a Control Assembly position or into any other position where the Fuel Assembly would be undercooled.

The outlet nozzle, located above the top of the fuel rods, is a complicated piece because of its many functional requirements. Welded to the duct, the outlet nozzle completes a compact structural unit that can be handled by the refueling machines and provides a controlled, channelled path for the primary sodium coolant. A secondary axial restraint is provided by the geometric interface of the outlet nozzle with the Upper Internals Structure.

The outlet nozzle geometric features that assist refueling are:

- o An internal groove to receive the refueling machine grapple fingers.
- o A large exit diameter to guide a misaligned refueling machine grapple.
- o Tapers to guide adjacent reactor assemblies during insertion and withdrawal refueling operations.
- o Transition surfaces to rotate an assembly into the correct azimuthal orientation during insertion.

The outlet nozzle coolant exit is designed to meet the maximum sodium velocity limits of Section 3.13, minimize the potential for vibration and direct the flow to the Upper Internals Structure which contains the outlet thermocouples used for plant control, surveillance and design verification purposes. The fluted internal geometry precludes fuel rod damage in the unlikely event that

an assembly is inadvertently inserted into an occupied position during refueling operations.

After an assembly has been completely withdrawn from the core, axial grooves on the outlet nozzle can be interpreted by the refueling machine to verify a specific type of core assembly, its serial number and the reference orientation.

The load pads on the outlet nozzle and duct interface with similar features on adjacent assemblies to provide radial support and seismic load-carrying capabilities as dictated by core restraint system design.

With the exception of the fuel and blanket pellets, the Fuel Assembly components are primarily made of austenitic stainless steels. These materials have been selected because of their compatibility with the CRBR operating conditions. Their selection is further supported by the availability of prototypic operating experience that is now being developed in the LMFBR fuels program and the precedent of the FFTF Driver Fuel Assembly design and successful operation. The fuel rod cladding, end caps and wire wraps, as well as the Fuel Assembly duct, are Type 316 SS, nominally 20% cold worked. Because the required strength and irradiation degradation are less, the inlet and outlet nozzles and shield-orifice subassemblies are made from solution-annealed Type 316 SS. Development tests of the FFTF Driver Fuel Assembly piston ring indicate that Alloy 718 chrome-plated piston rings on an Alloy 718 Lower Inlet Module receptacle provide the best combination for low wear, leakage and friction coefficients. To minimize the potential for galling and wear, the mating surfaces of the inlet nozzle are chrome plated; the load pads on the duct and the outlet nozzle are chrome carbide coated.

3.1.2 Fuel Assembly Design Process

The design process at Westinghouse Advanced Energy Systems Division (WAESD) for Core Assemblies (i.e., fuel, blanket, and removable radial shield) uses a disciplined and iterative approach. The design process is outlined schematically in Figure 3-3. Fuel (core) assembly design functions and

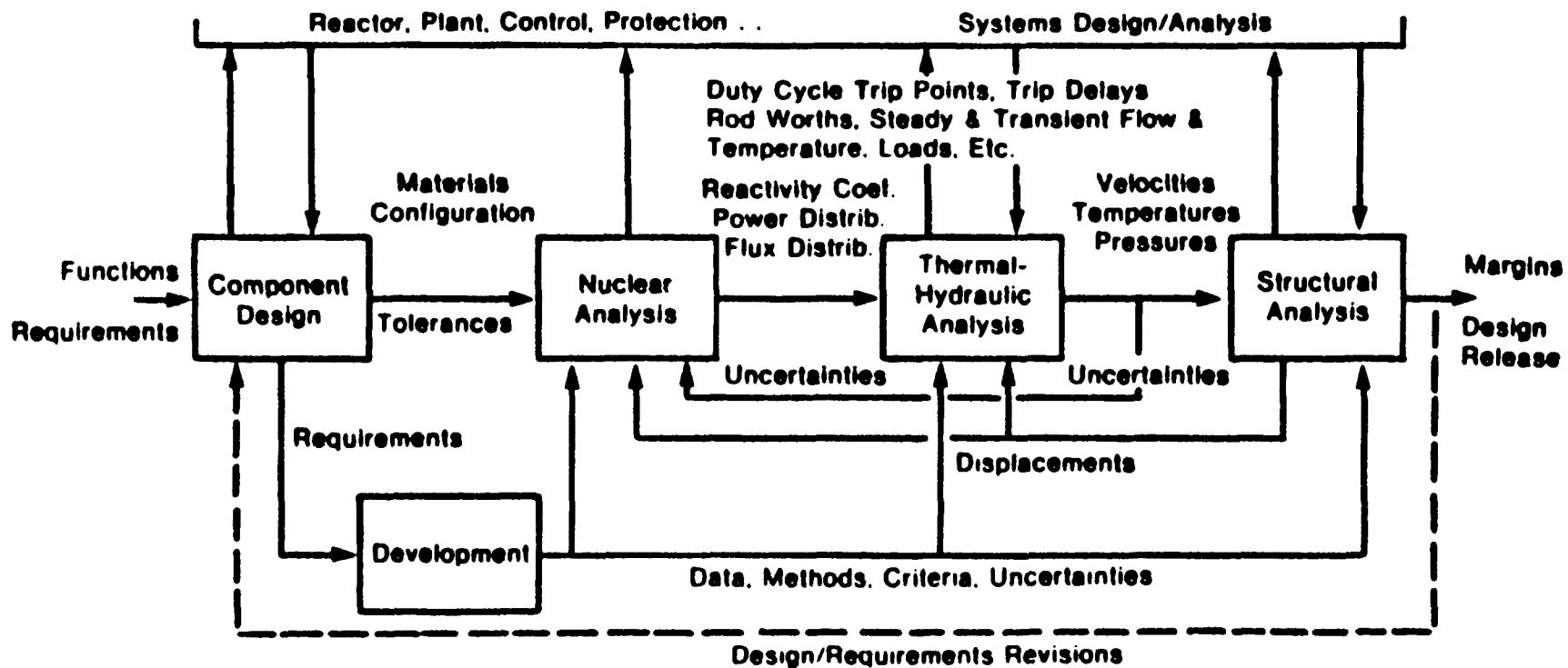


Figure 3-3 Core Assembly Design Process

requirements are factored into the component design together with Reactor, Plant, Control and Protection Systems design information. This allows component configuration and materials selection which provides dimensions on which to start the initial design. During this phase necessary development requirements for testing, fabrication, demonstration, etc. are formulated and submitted to acquire the necessary development results in a timely and cost effective manner. Some of the development activities are generic in nature to the LMFBR program and as such are often funded out of Base Technology budgets. Other development activities are CRBRP plant specific and, as such, are funded by Applied Base-CRBRP budgets. The developments can vary greatly, from a critical experiment in the Zero Power Plutonium Reactor (ZPPR), to fuel nozzle orifice flow testing at the Hanford Engineering Development Laboratory (HEDL), to steady-state and transient fuel rod/ assembly irradiation testing in Experimental Breeder Reactor (EBR)-II, Transient Reactor Test (TREAT), and FFTF. Information from these activities are fed into the nuclear, thermal-hydraulic and structural (mechanical) analysis of the component (fuel assembly/rod) in the form of data, methods, criteria and uncertainties. Meanwhile, the configuration, material, and tolerances start a series type design process beginning with the nuclear analysis which establishes such things as the reactivity coefficient, power/flux distribution, and uncertainties. This information is utilized in the thermal-hydraulics analysis to provide velocities, temperature and pressure values together with uncertainties. The nuclear and thermal-hydraulic information is then evaluated by structural (mechanical) analysis to provide mechanical damage values and allow an evaluation of margins of safety. Early on, and in parallel, working with the plant systems design/analysis, duty cycle trip points, trip delays, rod worths, steady-state and transient flow and temperature and loading values, etc. are iterated with each of the analysis disciplines. If during the course of design there are design/requirements revisions necessitated by engineering or plant changes (e.g., the heterogeneous core), the complete design loop must be reevaluated. The outcome of this design process is the approved design release and the necessary documented high quality design and development information necessary to satisfy the U.S. Nuclear Regulatory Commission (NRC) and Advisory Committee on Reactor Safeguards (ACRS) to secure a construction permit and operating license.

3.1.3 Damage Severity Limits and Design Transients

The inter-relationship of expected loading conditions, consequences of component failures and required design margins have been considered in the design criteria through the response of the overall reactor plant to various degrees of service events. For example a multi-level design approach has been used in the FFTF and CRBRP as discussed in Reference 1. For the fuel rod and assembly the damage severity limits for various event categories are shown in Table 3-6. As the probability of a given type of event decreases, the allowable impact on plant performance (or damage severity) increases. As an example, during normal operation and anticipated faults, the assembly ducts must exhibit a high degree of structural integrity to assure acceptable impact on plant performance, whereas the fuel rods need exhibit only a moderate degree of structural integrity. During unlikely faults, a moderate degree of confidence in the duct structural integrity is required, whereas little or no structural integrity requirements are required for the fuel rods. For extremely unlikely events, there are no structural integrity requirements for either the ducts or rods, however it must be possible to maintain coolable geometry.

Examples of fuel rod normal, anticipated, and unlikely operational events are given in Tables 3-7, 8, 9, respectively. The descriptions of the events have been given in Appendix B of the CRBRP Preliminary Safety Analysis Report. The frequency of the event is shown in these tables to illustrate the character and expected probability of the event.

As a result of iterative design calculations it was possible to develop design basis or enveloping transients. These enveloping transients are shown in Table 3-10 together with a description of the enveloping transient. The most damaging upset overpower transient occurs for an assumed uncontrolled rod bank withdrawal from 100% power with a delayed manual reactor trip. Operation is assumed at 115% overpower for five minutes. The frequency of this assumed occurrence may be obtained from Table 3-8; 47 such events in 30 years. Similarly, the other umbrella transient events listed in Table 3-10, the Operating Basis Earthquake (OBE), the E-16 and 60¢ step reactivity insertion

Table 3-6

REQUIREMENT - DAMAGE SEVERITY LIMITS

<u>Event Category</u>	<u>Fuel Damage Severity Level</u>
Normal operation (Normal)	No significant loss of effective lifetime
Anticipated events (Upset)	No reduction of effective lifetime below the design values
Unlikely events (Emergency)	A general reduction in the fuel burnup capability and, at most, a small fraction of fuel rod cladding failures
Extremely unlikely events (Faulted)	Maintain coolable configuration

Table 3-7

**CRBRP FUEL ROD DUTY CYCLE NORMAL OPERATION
FUEL DAMAGE SEVERITY LIMIT—NO
SIGNIFICANT LOSS OF EFFECTIVE LIFETIME**

<u>Event⁽¹⁾</u>	<u>Name</u>	<u>Number In 30 Years</u>
N-2	Normal startup	840
N-3	Normal shutdown	270
N-4	Load following	Note 2
N-5	Step load changes Of $\pm 10\%$ of full load	750
N-6	Steady state temperature fluctuations	30×10^6
N-7	Steady state flow induced vibrations	1×10^{10}

Notes:

(1) Description of events given in PSAR, appendix B, general plant transient data

(2) Daily load follow is not a design requirement, rate of power change is to be determined

Table 3-8

CRBRP FUEL ROD DUTY CYCLE ANTICIPATED (UPSET) EVENTS

Fuel damage severity limit—no reduction of effective lifetime below the design value

<u>Event</u>	<u>Name</u>	<u>Type</u>	<u>Number (1) In 30 Yrs.</u>
U-1	Reactor trip from full power	Overcooling	503
U-2B	Uncontrolled rod bank withdrawal from 100% power with delayed manual trip	Overpower	47
U-2C (D)	Uncontrolled rod withdrawal from startup with automatic (delayed manual) trip	Overpower	20
U-2E	Plant loading at maximum rod withdrawal rate	Overpower	10
OBE	Operating basis earthquake (30 cent step reactivity insertion and loss of off-site power)	Overpower	10

Note:

(1) Number of normal and upset events for fuel rod design life:

- Cycle 1 (128 FPD) - 1/30 of number in 30 yrs. to nearest larger whole no.
- Cycles 1&2 (328 FPD) - 2/30 of number in 30 yrs. to nearest larger whole no.
- Cycles after 1&2 are 274 FPD, no. of transients/cycle = 1/30 of no. for 30 yrs.

Table 3-9

CRBRP FUEL ROD DUTY CYCLE UNLIKELY (EMERGENCY) EVENTS

Fuel damage severity limit — A general reduction in the fuel burnup capability and, at most, a small fraction of fuel rod cladding failures

<u>Event</u>	<u>Name</u>	<u>Type</u>	<u>Number</u>
U-18 (S)	Loss of preferred and alternate preferred power (with secondary system shutdown) (1)	Undercooling	} 1 Of Worst
E-7	One loop natural circulation heat rejection from initial two loop operation	Undercooling	
E-16	Three loop natural circulation	Undercooling	
—	60 cent step reactivity insertion (PPS design evaluation transient)	Overpower	

Note:

(1) Concurrent failure of primary shutdown system shifts fuel damage severity limit to next highest level

Table 3-10

CRBRP DESIGN BASIS (ENVELOPING) TRANSIENTS

<u>Event Category</u>	<u>Simulated CRBRP Event</u>	<u>Description</u>
Upset	U-2B	Uncontrolled rod bank withdrawal from 100% power with delayed manual trip. Operation at 115% power for 5 minutes. This is most damaging upset overpower transient
Upset	OBE	Operating basis earthquake with 30 cent step reactivity insertion and loss of off-site power. Potentially significant fuel contact pressure
Emergency	E-16	Three-loop natural circulation. Most damaging undercooling emergency event
	—	60 cent step reactivity insertion (Plant Protection System design evaluation transient)

frequencies may be obtained from Tables 3-8, 9. These transient events, which umbrella all others, were used to evaluate the performance of fuel (and blanket) rods against their mechanical design limits.

3.1.4 Fuel Rod Criteria and Mechanical Design Limits

Current design limits and procedures for evaluation reflect state-of-the-art in overall structural performance relative to integral fuel assembly component performance data. As such, it should be remembered that the development of design criteria (and limits) and the models and material properties used to evaluate them are both inter-related and in a continual state of development. The very satisfactory performance of the FFTF driver fuel to and beyond goal burnup attest however, to the methodology and limits chosen.

Extensive EBR-II irradiation experience has shown that the major structural component is the fuel rod cladding. The predominant cladding loading mechanisms during normal steady-state operation are fuel cladding mechanical interaction (FCMI) and fission gas pressure loading. Tests of design basis type transients also indicate that FCMI loading is most damaging during reactivity insertion (overpower) events whereas fission gas loading is the principal cladding loading mechanism during undercooling type transients. Experience indicates that life limiting failure mechanisms are plastic and thermal creep damage. Based upon experience, three major criteria have been developed; fuel melting, ductility limited strain and cumulative damage function (CDF). To preclude cladding breach through normal operation, all anticipated events and the worst unlikely event, these criteria have been used even though a limited number of rod breaches are allowed under these conditions. Table 3-11 lists the applicability, parameter, design limit and bases for the CRBRP fuel and blanket rod. Following is a discussion of the criteria and limits chosen for CRBRP.

Table 3-11

CRBRP FUEL AND BLANKET ROD
MECHANICAL DESIGN LIMITS

<u>Applicability</u>	<u>Parameter</u>	<u>Limit</u>	<u>Bases</u>
Steady state	Ductility limited strain (DLS, thermal creep & plasticity)	0.2% fuel 0.1% blanket	Used for FFTF driver fuel design, verified by FFTF operations
	Power at 15% overpower	Power to melt fuel	Provide margin for termination of severe overpower transients
	Primary equivalent stress	Proportional elastic limit	No plastic strain contribution to DLS
	Flow channel closure (bundle-duct interaction)	One wire diameter	Design temperatures remain conservative
NORMAL AND ALL ANTICIPATED AND WORST UNLIKELY EVENTS	Ductility limited strain	0.3% fuel 0.2% blanket	FFTF used 0.7% for driver fuel design, verified by FFTF operation
	Cumulative damage function (CDF, creep rupture, plastic, fatigue damage)	1.0	Alternate criteria to DLS, based upon latest material properties instead of design recipe.

A. Fuel Melting

During steady state operation, the power and corresponding fuel temperature in the peak power fuel rod is to be less than the value for incipient fuel melting with a 15% overpower margin.

This power-to-melt criterion is not a structural design limit in reality, but it is based upon structural considerations. Fuel melting would result in volumetric expansion of the fuel and some additional release of fission gas stored in the fuel during overpower transients. These effects would increase the two primary cladding loading mechanisms. In conjunction with the Plant Protection System (PPS), 15% is the maximum overpower primary system trip setting for CRBRP. If the fuel rod is designed to satisfy this criterion, cladding loading would be minimized during overpower transients. In providing the design margin between normal operation and incipient fuel melting, the task of terminating severe design overpower transients where given fuel melting or relocation can be important, is easier. This is the only purpose of the no-melting limit.

Fuel rods (reference FFTF type) irradiated in EBR-II and tested in TREAT to simulate secondary control system shutdown of reactivity insertion type transients in the range of 3¢ to \$3 per second have shown small transient damage. This has demonstrated the success of the power-to-melt limit to preclude excessive loading of the cladding during overpower transients.

B. Fuel Rod Cladding Strain

The fuel rod cladding strain criterion considers four strain components: thermal creep, plastic deformation, swelling and irradiation creep. Irradiation creep is believed to be non-damaging. Primary swelling strains are also believed to be non-damaging. However, the effects of swelling can lead to secondary effects (increase in local stresses, change in elastic modulus, etc.) which can increase cladding damage. Therefore, strains associated

with irradiation creep and swelling per se are not used to calculate cladding damage but the effects of these strains on other damaging mechanisms (stress-rupture, plastic deformation, etc.) are fully taken into account.

A fuel rod cladding damage limiting criterion based upon thermal creep and plastic deformation was developed for use in FFTF and CRBRP fuel rod design. This simple criterion was developed in the years before high-fluence tensile, stress-rupture or burst test data were available. For CRBRP the criterion simply states that the calculated steady-state thermal creep diametral strain shall be less than 0.2%, and the total thermal creep and plastic diametral strain including design transients shall be less than 0.3% independent of temperature and fluence. The design procedure for steady-state creep analysis is given in Table 3-12. The strains are calculated using conservative equations for thermal creep and tensile properties (unirradiated solution annealed Type 316 SS), hot channel fuel rod temperatures, worst case fission gas and FCMI loading and a very conservative wastage allowance. This recipe for ductility limited strain has been a successful criterion for preventing fuel rod cladding breach in the high temperature portion of the fuel rod.

In addition, in evaluating fuel rod bundle-duct interaction (BDI) fuel channel closure is limited to one wire wrap diameter (0.056 inch) in order to insure that design temperatures are conservative and stresses do not build up.

C. Fuel Rod Cumulative Damage

A fuel rod cladding lifetime criterion based primarily on stress-rupture correlations during steady-state operation and tensile properties during transients have been developed for CRBRP fuel rod design [2]. Unlike the strain limits, the cumulative damage function contains several damage models, each attempting to sum damage over

Table 3-12

DESIGN PROCEDURE FOR STEADY STATE CREEP ANALYSIS

<u>Calculated Parameter</u>	<u>Required Assumptions</u>	<u>Comment</u>
Strain rate	Thermal creep rate for solution annealed 316 stainless steel	Overpredicts irradiated 20% CW 316 SS creep rate
Temperature	Upper 2 σ local hot spot over design life Total gradient totally relaxed by thermal creep	97.5% of time temperature is below this requirement Primarily relaxed by non-damaging irradiation creep
Fission gas	Upper 2 σ limit, linear by cycle	Linear behavior more conservative than actual exponential increase
Fuel loading	Worst prediction considered	Worst combination of irradiation induced creep and swelling and fabrication variables
Cladding wastage	Upper limit on tolerance, wear, defects, sodium corrosion and fission product attack	Approximately 40% of original cladding thickness is assumed not effective at end of design life
Stress limit	Below proportional limit	No plastic strain allowed

the variable temperature, stress and fluence history of the fuel rod cladding. Table 3-13 lists the salient features of the cumulative damage function.

The CRBRP fuel rod design uses stress-rupture and tensile property correlations in conjunction with a life-fraction rule to calculate damage during steady-state operation and transient events. The cold-worked AISI Type 316 SS cladding is assumed to decarburize in the flowing sodium at a rate equal to or greater than solution annealed AISI Type 316 SS. This conservative decarburization model greatly reduces the strength of the cladding during steady-state and transient events. The damage correlations are used at their worst 99% confidence levels, and steady-state and transient temperatures used in the correlations are 2σ and 3σ hot spot temperatures, respectively. The combination of the conservative confidence level correlations and hot spot temperatures gives a very conservative design lifetime. Figure 3-4 presents an example of this conservatism. The CDF is plotted as a function of steady-state operation, anticipated events and a worst case emergency (unlikely) event. For design level temperature and property values fuel rod breach would be expected at 485 days. If design level temperatures and nominal property values were used the fuel rod breach would be expected at 705 days. If, however, nominal temperature and property values are used, fuel rod breach would not be expected to occur in well over 850 days at full power. Fuel rod cladding stresses are calculated due to fission gas pressure (approximately 100% gas release) and steady-state and transient fuel-cladding mechanical interaction (FCMI). Reduction of the cladding thickness due to chemical attack by the fuel is also modeled. The net result is that the CRBRP design lifetime criteria are conservative but still allow CRBRP design lifetime goals to be attained. For example, fuel adjacency effects during temperature transients were not well understood during the development of the CRBRP fuel rod cladding

CUMULATIVE DAMAGE FUNCTION (CDF)

- The CDF evaluates the cladding mechanical damage due to the temperature and stress history during the steady-state and transient duty cycle
- CDF considers creep rupture and plastic damage
- When the CDF = 1, including allowances for design uncertainties, maintenance of cladding integrity can no longer be assured
- CDF models for tensile properties, time to rupture, and cladding internal and external wastage are statistically derived from measured data
- The CDF models consider variations due to temperature, fluence, strain rate, fuel, cladding and sodium composition at the worst statistically derived confidence bound
- Conservatism of CDF has been demonstrated using multi-phase stress rupture tests and biaxial creep tests followed by a uniaxial tensile or burst test

CUMULATIVE MECHANICAL DAMAGE FUNCTION DUE TO STEADY STATE OPERATION, ANTICIPATED EVENTS AND UNLIKELY EVENT EXAMPLE

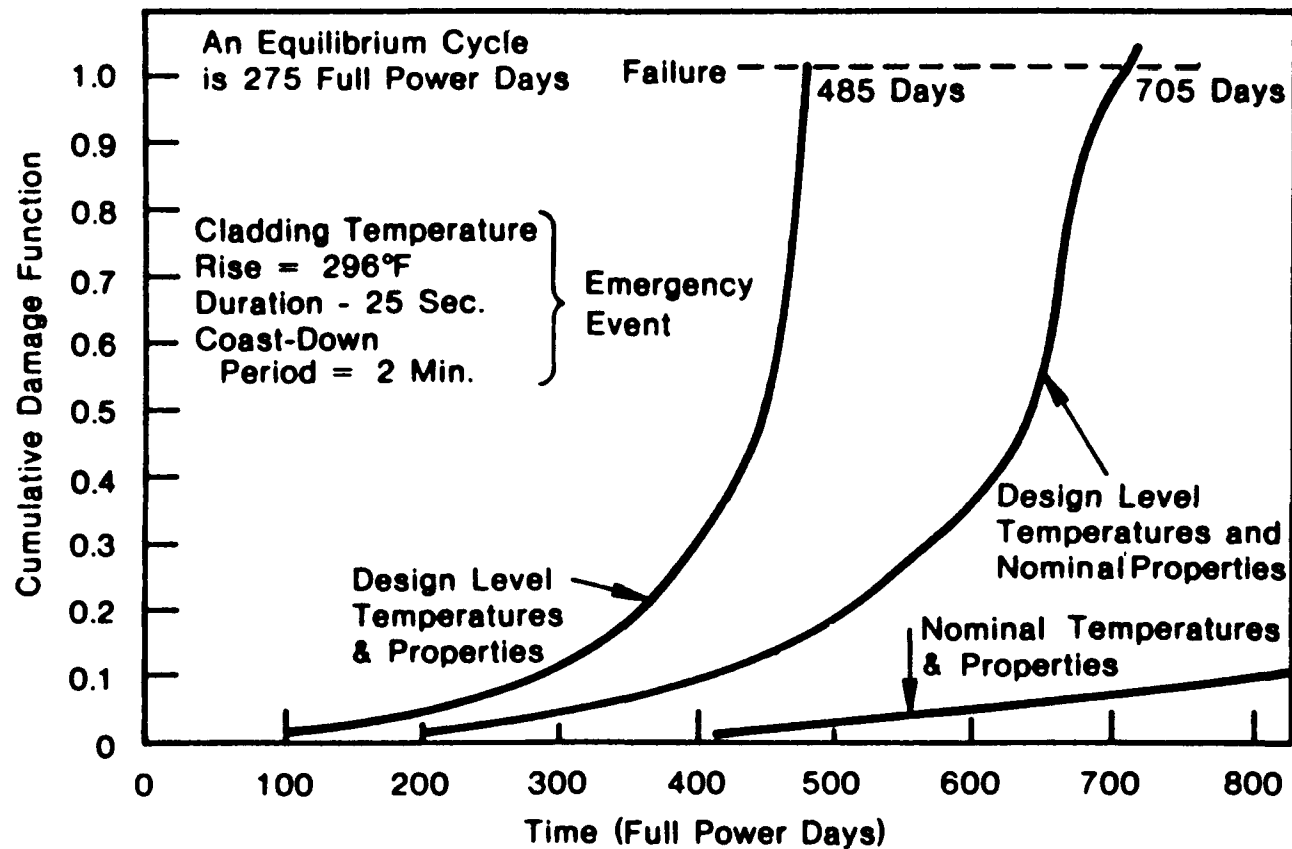


Figure 3-4

design criteria. However, due to the highly conservative criteria, the negative effects of fuel adjacency, which are not directly modeled, are enveloped.

3.1.5 Fuel Assembly Criteria and Limits

The fuel assembly structure contains and supports the fuel rods with the components shown in Figure 3-5. The functions and features of each component determine the specific structural integrity and performance criteria for that component. Most assembly component dimensions are dictated by deformation limits which are derived from requirements on fuel positioning, core restraint, and shielding. Consequently, design margins on structural limits are generally larger than those on deformation limits. The result is that assembly component structural failures are rare and assembly performance limits are generally reached by exceeding deformation limits.

Figure 3-5 lists the loading and deformation mechanisms on each component. These assembly loading mechanisms are characterized by large uncertainties due to uncertainties in the material swelling and creep rates, temperature uncertainties, the interaction of many core assemblies, and calculated loads based on simplified, conservative material property models. Consequently, the loading conditions for structural analysis are selected to envelop the worst expected steady state and transient loading condition.

Similar to the fuel rods, the assembly component loads and conditions are generally developed into time histories for normal operation, with periodic anticipated events and one worst unlikely event. The loads, temperatures, and fluences to be considered in the assembly structural analyses are simplified by enveloping the worst conditions. These simplifications often add the largest degree of conservatism without taking any credit in the design criteria and limits.

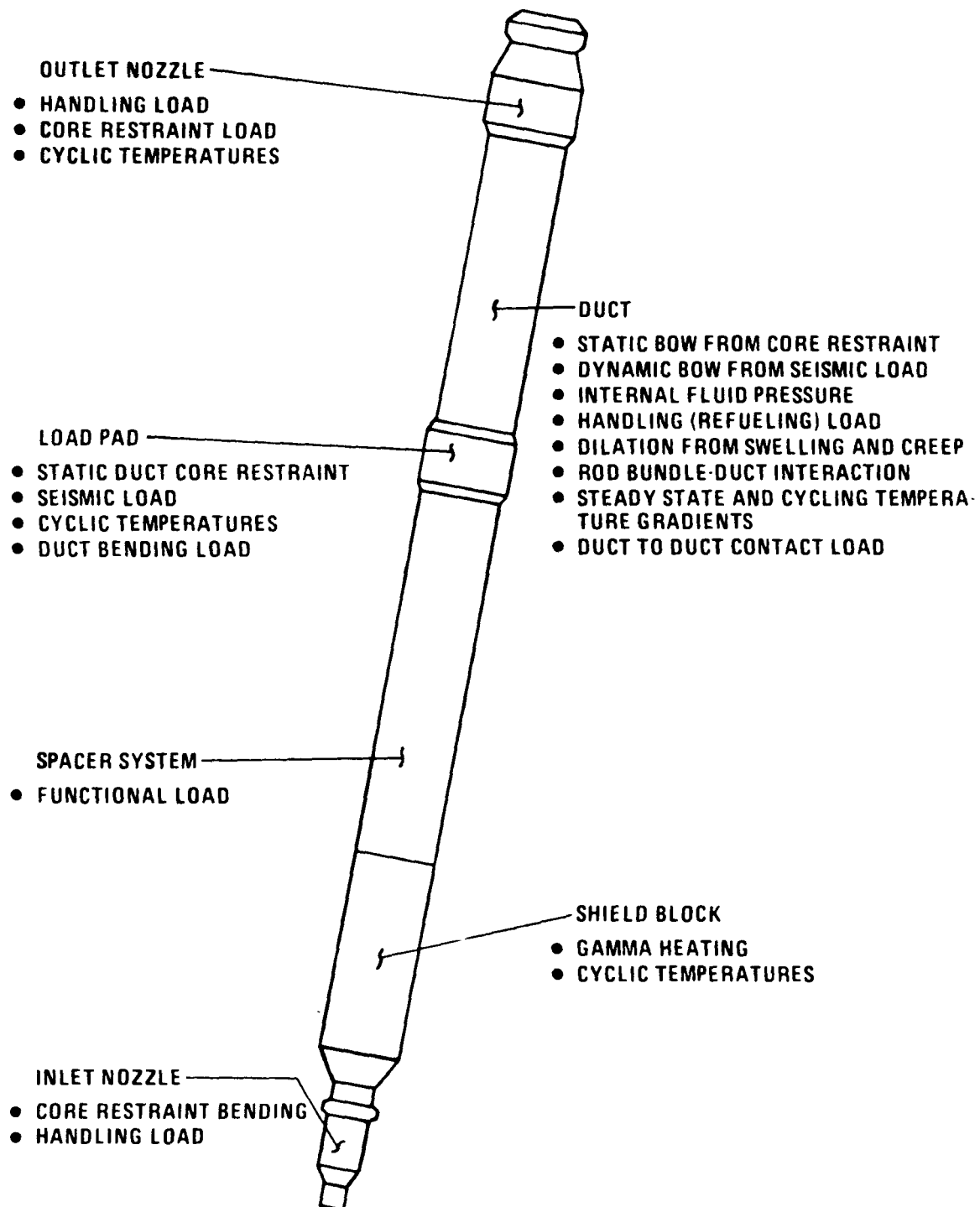


Figure 3-5
Fuel Assembly Loads

The fuel assembly structural loads are given in Table 3-14. They are comprised of core restraint loading, seismic loading, thermal loading and other loading caused by such things as coolant pressure, component weight, rod-bundle interaction and refueling loads.

The principal core (fuel) assembly structural criteria are satisfied by detailed inelastic analysis in most cases or by experimentation, as shown in Table 3-15. Crack initiation has a criterion based on local ductile rupture and creep-fatigue interaction with limits on fracture strain and uniform elongation and rupture time and cycles to failure, respectively. A failure mode of excessive deformation has criterion based on peak plus accumulated deformation and residual deformation which each have limits imposed by component functional requirements and effects of such deformation on structural performance.

The bases for the mechanical design limits are shown in Table 3-16. Guidelines of the national LMFBR core components structural criteria committee have been used as well as FFTF precedent and supporting experimental information. Nozzles have been found to have enough residual ductility and are covered by American Society of Mechanical Engineers (ASME) Boiler and Pressure Vessel Code rules. Finally, the duct mechanical properties are within the cladding scatter band and hence cladding experience can be used.

Guidelines for structural analyses developed for CRBRP are shown in Table 3-17. This guideline for the structural analyses includes worst case environments, dimensions, material strength, combined irradiation induced creep and swelling and worst transient to umbrella less severe events. In combination this process undoubtedly produces conservative values to evaluate safety margins.

Finally, the inelastic criteria and limits are shown in Table 3-18. The numerical values from the structural analyses limits are used to evaluate the design and are reported in Section 3.1.6

Table 3-14

FUEL ASSEMBLY STRUCTURAL LOADS

Core restraint	— Bending moment, axial force, lateral loads
Seismic	— Bending moment, axial force, lateral loads
Thermal	— Operational, transient, refueling thermal stresses
Others	— Coolant pressure loads, component weight, refueling loads, rod—bundle interaction

Table 3-15

CORE ASSEMBLY STRUCTURAL CRITERIA

<u>Required Method</u>	Detailed Inelastic analysis	
<u>Failure Mode</u>	<u>Criteria</u>	<u>Limits</u>
Crack initiation	Local ductile rupture	Fracture strain Uniform elongation
	Creep-fatigue interaction	Rupture time Cycles to failure
Excessive deformation	Peak + accumulated deformation	Based on component functional requirements and effects of deformation on structural performance
	Residual deformation	Same as above

Table 3-16

BASES FOR CURRENT MECHANICAL DESIGN LIMITS

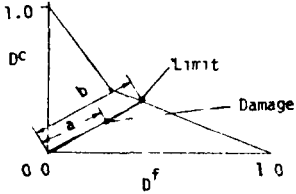
- FFTF precedent and supporting experiments
- Guidelines of national committee on LMFBF core component structural criteria
- Unirradiated duct mechanical properties within cladding scatter band
- Nozzles have sufficient residual ductility and material is covered by ASME code

Table 3-17

GUIDELINES FOR STRUCTURAL ANALYSES

- **Maximum temperature and flux over life**
- **Worst transients to umbrella less severe events**
- **Applied loading for minimum design margin**
- **Minimum load bearing dimensions**
- **Minimum material strength with degradation due to sodium exposure and irradiation**
- **Worst combination of irradiation induced creep and swelling**

Table 3-18
CRBR CORE ASSEMBLY STRUCTURAL INELASTIC CRITERIA AND LIMITS

Type of Failure	Mode	Criteria	Limit
Crack Initiation	Local Ductile Rupture	$F_{DR} = \text{Max of } \left\{ \begin{array}{l} \bullet \frac{(\epsilon_{\text{max principal}})^{TF}}{0.3 \epsilon_f} \\ \bullet \frac{(\epsilon_{\text{max principal}})^T}{\epsilon_u} \end{array} \right\}$ where, ϵ_f = True Min. Fracture Strain ϵ_u = True Min. Uniform Elongation TF = Triaxiality Factor $TF = \frac{\sqrt{2} (\sigma_1 + \sigma_2 + \sigma_3)}{\sqrt{(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2}}$ $\sigma_1, \sigma_2, \sigma_3$ = Principal Stresses $\epsilon_{\text{max principal}}$ = Maximum Principal Strain (Peak + Accumulated)	1
	Creep Fatigue	$F_{CFD} = a/b = \text{Minimum of } \left\{ \begin{array}{l} \bullet 7/3 D^C + D^f \\ \bullet D^C + 7/3 D^f \end{array} \right\}$  where, D^C = Creep Damage Factor $D^C = \int \frac{dt}{t_r}$ t_r = Rupture Time Based on Equivalent Stress or Max. Positive Principal Stress D^f = Fatigue Damage Factor $D^f = \sum \frac{n}{N_f}$ n = No. of Cycles N_f = Allowable No. of Cycles Based on range of equivalent or Max. Principal Strain	1
Excessive Deformation	Peak + Accumulated	$\epsilon^{P+A} / PADL$ where, PADL = Peak + Accumulated Non-Uniform Deformation Limit, Excluding Irradiation Creep and Swelling	1
	Residual	δ^R / RDL where, RDL = Residual Non-Uniform Deformation Limit, Excluding Irradiation Creep and Swelling	1

3.1.6 Results of Fuel Rod and Assembly Final Design Evaluations

The preliminary design review of the fuel assembly was held on June 25 and 26, 1975. This design review was based on the initial CRBRP homogeneous core design but produced actions that were in some cases relevant to the referenced heterogeneous core design. These actions were included in preparation for the Final Design Review held on December 6 to 8, 1977.

The key evaluations from the final design review presentations and documentation are described below:

A. Fuel Rod

Cladding damage is within design limits. As shown in Table 3-19 the steady-state and transient ductility limited strain values indicated a 75% and 8% margin when compared to the 0.2% and 0.3% ductility limited strain design values. Similarly, the cladding cumulative damage values had margins; 35% for steady-state and 2% for steady-state plus transient damage.

Rod internals were also evaluated as shown in Table 3-19. An early design change to reduce the diameter of the blanket pellet adjacent to the fuel column had shown that the reduced diameter when combined with axial migration of cesium to that location would not cause significant additional cladding strain.

An analysis was also done for a dished fuel pellet which indicated that at power the pellet would not exhibit doming caused by thermal conditions and that the maximum amount of ridging was less than 0.001 inch. Similarly no ridging or doming were found to occur in the flat ended axial blanket pellets.

The holddown spring was shown to be capable of maintaining a 6g preload without overstress and can accommodate at least one inch of fuel column growth before approaching solid height.

DESIGN EVALUATIONS - KEY FUEL RESULTS

Cladding Damage

- **Cladding damage is within design limits**
 - **35 percent margin on steady-state cumulative damage**
 - **75 percent margin on steady-state ductility limited strain**
 - **2 percent margin on steady-state and transient cumulative damage**
 - **8 percent margin on steady-state and transient ductility limited strain**

Rod internals

- **Axial blanket pellet/cesium interaction will cause no significant additional cladding strain**
- **For dished fuel pellet, no doming occurs; maximum ridging is less than one mil**
- **No ridging or doming occurs in the flat ended axial blanket pellets**
- **The holdown spring is design to maintain a minimum 6G preload without overstressing the spring at the maximum working load. The spring can accommodate at least one inch of fuel column growth before approaching its solid height**
- **The plenum spacer margin of safety on stress is 9.5**
- **Eighty-five percent (85%) of the fuel rod plenum is available for fission gas accommodation**

DESIGN EVALUATIONS - FUEL KEY RESULTS (Cont.)

Wire wrap

- Maximum wire wrap stress and strain are well below limits of 21 ksi and 0.6%, respectively
- Maximum wire slack is 0.1 inch out of 116 inches and considered acceptable

Bundle/duct interaction

- Maximum bundle/duct interference of 0.020 inches is well below the limit of 0.056 inches to preclude excessive cladding stress or flow redistribution
- The maximum bundle/duct clearance of 0.04 inches is less than the 0.054 inches (6 mils/ring) limit to preclude excessive fretting wear

Duct dilation

- The maximum duct dilation is ~80 mils which is less than the limit of 108 mils required to exceed the pitch line

A very large safety margin was obtained for the plenum spacer and the fission gas space available was 85% of the fuel rod gas plenum thereby meeting design objectives.

A summary of the thermal transient information for the fuel rods is given in Table 3-20. Maximum cladding, fuel, and coolant temperature are given together with the time to reach the maximum temperature and length of time the temperature is above steady state conditions. The cladding does not reach a design guideline temperature of 1600°F, the fuel does not melt, and the sodium does not boil for any of the transient events.

The wire wrap was analyzed and the resultant stress and strain are well below allowable values of 21 ksi and 0.6%, respectively, while the maximum wire slack of 0.1 inches (out of 116 inches) calculated is insignificant.

B. Fuel Rod Bundle/Duct

The maximum fuel rod bundle-duct clearance and interference were calculated with the results shown in Figure 3-6 as a function of time. The maximum clearance of 0.04 inch is less than the 0.054 inch (6 mils/ring) limit to preclude excessive fretting wear as determined by experimentation. The maximum interference value of 0.02 inch is well below the limit of 0.056 inch (one wire wrap diameter) to preclude excessive flow redistribution or excessive cladding stress.

The maximum duct dilation along the length of the duct is given in Figure 3-7 and is ~80 mils. This value is below the limit of 108 mils required to exceed the assembly pitch line.

Table 3-20

SUMMARY OF THERMAL TRANSIENT DATA FOR F/A RODS

EVENT	CLADDING			FUEL			COOLANT		
	A	B	C	A	B	C	A	B	C
U-1									
#6 Assembly	1359°F	0 Sec.	0 Sec.	4480°F	0 Sec.	0 Sec.	1285°F	0 Sec.	0 Sec.
#8 Assembly	1387°F	0 Sec.	0 Sec.	4322°F	0 Sec.	0 Sec.	1330°F	0 Sec.	0 Sec.
U-2b									
#6 Assembly	1469°F	300 Sec.	~302 Sec.	4856°F	300 Sec.	~301 Sec.	1380°F	300 Sec.	~302 Sec.
#8 Assembly	1501°F	300 Sec.	~302 Sec.	4682°F	300 Sec.	~302 Sec.	1434°F	300 Sec.	~302 Sec.
30¢ OBE (U-16)									
#6 Assembly	1451°F	1.27 Sec.	2.64 Sec.	4601°F	1.053 Sec.	1.39 Sec.	1366°F	1.59 Sec.	3.3 Sec.
#8 Assembly	1477°F	1.38 Sec.	2.75 Sec.	4432°F	1.053 Sec.	1.4 Sec.	1412°F	1.69 Sec.	3.4 Sec.
3-Loop Nat. Circ.									
#6 Assembly	1532°F	84.2 Sec.	~101 Sec.	4480°F	0 Sec.	0 Sec.	1524°F*	96.2 Sec.	~173 Sec.
#8 Assembly	1575°F	87.2 Sec.	~116 Sec.	4322°F	0 Sec.	0 Sec.	1577°F*	102 Sec.	~173 Sec.
U-18S									
#6 Assembly	1518°F	2.52 Sec.	4.84 Sec.	4486°F	2.0 Sec.	2.12 Sec.	1433°F	2.84 Sec.	6.05 Sec.
#8 Assembly	1548°F	2.63 Sec.	5.07 Sec.	4328°F	2.0 Sec.	2.13 Sec.	1482°F	2.96 Sec.	6.3 Sec.
60¢ Step									
#6 Assembly	1449°F	.72 Sec.	1.66 Sec.	4831°F	.512 Sec.	1.34 Sec.	1354°F	.922 Sec.	2.02 Sec.
#8 Assembly	1473°F	.72 Sec.	1.73 Sec.	4643°F	.513 Sec.	1.34 Sec.	1400°F	1.03 Sec.	2.1 Sec.
\$2/Sec.									
#6 Assembly	1454°F	.81 Sec.	1.76 Sec.	4858°F	.61 Sec.	1.4 Sec.	1358°F	1.02 Sec.	2.14 Sec.
#8 Assembly	1477°F	.81 Sec.	1.85 Sec.	4667°F	.61 Sec.	1.44 Sec.	1403°F	1.12 Sec.	2.26 Sec.
60¢ SSE (F-1)									
#6 Assembly	1550°F	1.43 Sec.	~4.0 Sec.	4924°F	1.11 Sec.	1.83 Sec.	1446°F	1.66 Sec.	~4.6 Sec.
#8 Assembly	1574°F	1.49 Sec.	~4.0 Sec.	4728°F	1.11 Sec.	2.10 Sec.	1492°F	1.77 Sec.	~5.0 Sec.

A - Maximum hot spot temperature attained (for cladding at top of active core, for coolant at top of upper blanket and for fuel at center of active core).

B - Time to reach maximum temperature.

C - Length of time temperature is above initial steady state.

* - The cladding and core exit coolant temperature would be approximately equal at exit of top blanket position since the heat flux is small.

MAXIMUM FUEL ROD BUNDLE-DUCT DIAMETRAL CLEARANCE AND INTERFERENCE VS. TIME - F/A 45. FIRST CORE

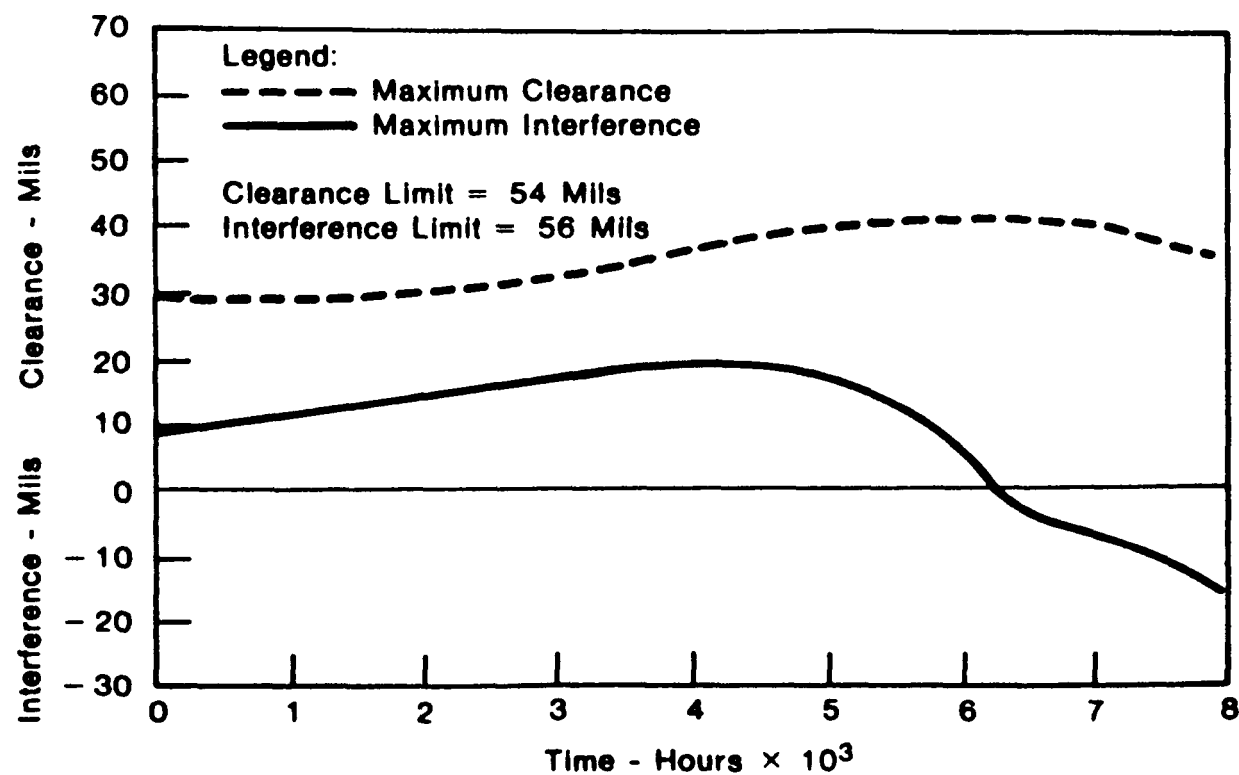


Figure 3-6

**MAXIMUM DUCT RADIAL DILATION ACROSS FLATS
VERSUS AXIAL LOCATION AT END-OF-LIFE. FOR F/A 45**

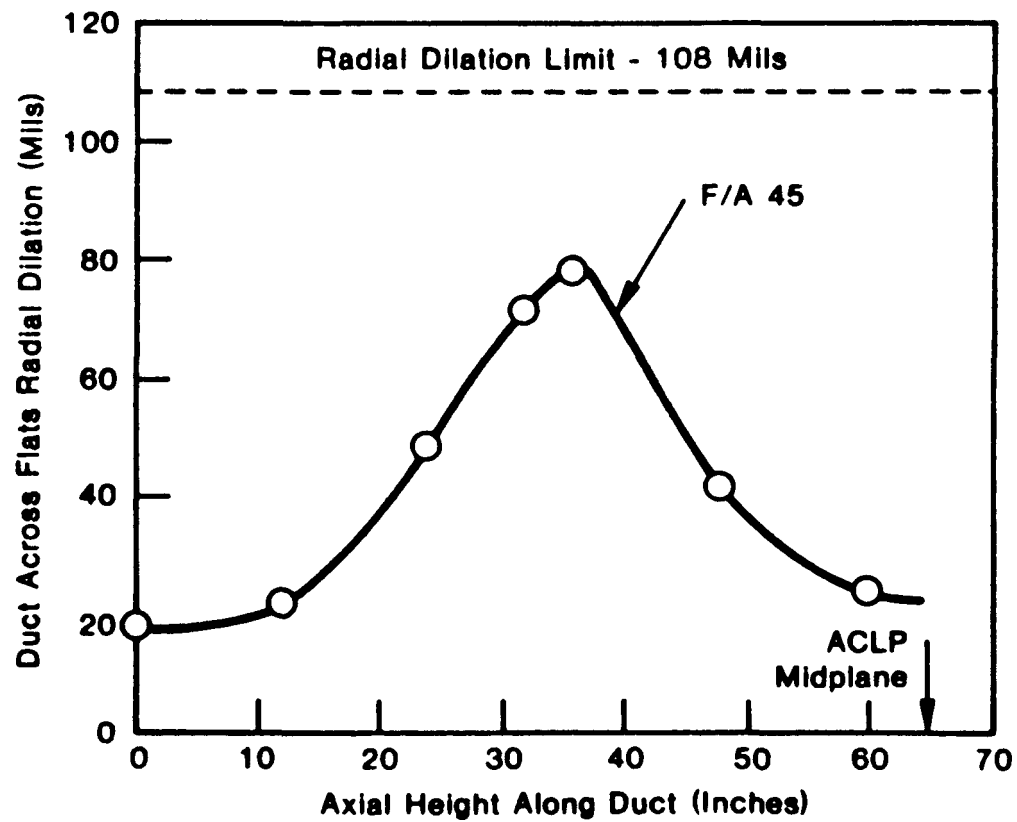


Figure 3-7

C. Fuel Assembly

In the structural analysis of the fuel (and core) assemblies design conditions considered for the evaluations are given in Table 3-21 for the regions/components shown. The results of the analyses including the minimum margin of safety and the failure mode are given in Table 3-22. All margins of safety are acceptable.

Additional considerations of thermal striping for the core assemblies are given in Section 3.12 and confirm that for the design conditions the assembly outlet nozzle design and material is acceptable.

3.1.7 Design and Methodology Evolution

As mentioned earlier, the fuel rod and assembly conceptual design was patterned after the FFTF and only those modifications made that were necessitated by the operational and initial breeding requirements of the CRBRP. During design there were significant occurrences which caused modifications to be made to both the design/analysis methodology and to the design itself. Some of the more important evolutionary changes follow:

A. Heterogeneous Core

With the core modification to the heterogeneous design the plutonium content of the fuel was increased to ~32%; significantly above the available EBR-II data base for FFTF prototype fuel rods. This higher plutonium content produces an attendant lower solidus temperature which, when coupled with power jumps caused by the insertion of fresh fuel near "burned-in" fuel, design transients, and rapid design reactor start-up rates, caused a condition which violated the "no-melting" criterion. To alleviate this problem a unique solution was found. Since the fuel pellets restructure rapidly during irradiation and this restructuring (which forms a central hole and closes the fuel to cladding gap) lowers the fuel temperature, it was postulated that a slower programmed start-up rate would mitigate the melting problem. Modeling of the restructuring phenomenon was done

Table 3-21

CORE ASSEMBLY STRUCTURAL ANALYSIS

Regions/components evaluated

- Orifice plate
- Rod attachment assembly
- Shield block
- Duct at mid-core
- Duct load pad
- Outlet nozzle

Design conditions considered

- Fast fluence
- Uniform temperature
- Cross-duct temperature gradient
- Inter-assembly temperature gradient
- Core restraint bending moments
- Coolant pressure
- Transient power and flow variations
- Seismic loads

Table 3-22

CRBRP FUEL ASSEMBLY STRUCTURAL ANALYSIS SUMMARY

<u>Assembly Region</u>	<u>Minimum Margin*</u>	<u>Failure Mode</u>
Orifice plate	0.43	Peak plus accumulated deformation
Rod attachment	10.11	Peak plus accumulated deformation
Shield block	2.13	Residual deformation
Duct		
• Core midplane	12.76	Local ductile rupture
• Load pad	1.58	Residual deformation
Outlet nozzle	0.29	Combined creep-fatigue damage

$$*Margin \text{ (of safety)} = \frac{\text{Allowable limit}}{\text{Calculated value}} - 1.0$$

with the LIFE National Code for various start-up rates and design operation conditions. Using this code an acceptable start-up rate was determined, which if followed whenever new fuel was put in the reactor, would not violate the "no-melting" criterion. This programmed start-up rate was then specified for a period of ~100 hours to plant operations and the problem was solved.

B. NRC Interaction

In interactions with NRC it became obvious that it would be necessary to have the capability to "predict" how many rods might fail in the core for various design basis conditions. To that time, deterministic methods were used for the Ductility Limited Strain (DLS) and Cumulative Damage Function (CDF) which were not amenable to this type of probabilistic analysis. A design procedure (FPAN) was formulated for the CDF criterion which would allow deterministic and probabilistic analysis of fuel rod performance. This procedure was formulated by D. C. Jacobs and described in Reference 3. It was a major step in fuel rod analytical procedures.

Another requirement that NRC had was that whatever fuel performance codes were used, they must be qualified against an irradiated fuel rod data base. In 1982-1983 WAESD undertook an effort to qualify the new CDF procedure (FPAN) and a modification of the DLS criterion against the EBR-II data base. This attempt at qualification was not successful primarily for two reasons. First the number of breaches in EBR-II was small and secondly the breaches were believed to be caused by over-temperature conditions which could not be well characterized. As an outcome of this exercise however, the fuel design group chose the CDF methods as giving the better fit to the experimental data and would have used this method for analysis of statistically significant information that was just beginning to come from the FFTF. With large numbers of rods which have been taken to beyond-goal values of peak burnup in FFTF it was confidently anticipated that qualification could have been accomplished.

C. Adverse Experimental Results

Two examples of adverse experimental results may be reviewed to illustrate design/methodology changes necessitated by the results. Early on, cesium was found to migrate to the colder ends of rods in EBR-II and to effectively increase the pellet diameter in the insulator pellet, thereby causing additional cladding strain in this region. A design modification was made in the axial blanket pellet diameter, to decrease the diameter a few mils, and thereby accommodate this potential problem. Analytical efforts confirmed the "fix" was feasible without noticeable breeding penalty. The change was made and analytically shown that it would work.

Adverse power-to-melt information was obtained from early FFTF experiments. Both HEDL and WAESD reviewed the information, evaluated the impact on reactor operation, and consulted on additional experiments needed to show margin in power-to-melt throughout life. The WAESD evaluation for CRBRP yielded results which indicated small but acceptable margins which were to be confirmed by further experimental evidence in future FFTF tests.

D. Improved Type 316 SS Cladding

It became evident from careful examination of EBR-II results that minor elemental modification to cold worked (CW) Type 316 SS cladding would improve its properties and, in fact, potentially allow operation in CRBRP for two equilibrium cycles (~550 EFPD). On the basis of these evaluations the cladding/duct Type 316 SS specifications were modified for CRBRP and project approval received for such procurement. Before the analysis could be done to determine whether the improvement would allow operation for 550 EFPD, the project was cancelled.

E. SAF Line Fabricated Fuel

Because it was in the national interest to fabricate fuel for CRBRP and FFTF future cores in a secure automated facility (SAF) in a coordinated manner, joint HEDL/WAESD efforts were made to simplify fuel and fuel rod design. Examples of changes made were a magnetic pulsed end cap weld, and relaxed fuel pellet quality procedures on various pellet surface conditions. This effort was completed. Additional experiments in FFTF were initiated to confirm that the changes were acceptable. Finally, these changes were made in the fuel rod specifications and drawings and were approved.

These examples are but a few of the changes made to the fuel rod and assembly of CRBRP. The examples serve to illustrate the evolutionary process in the design during the course of such a project.

3.1.8 Final Status of Fuel Rod and Assembly Design

During the time between the fuel assembly final design review and the project termination major changes occurred which necessitated reevaluation of the fuel rod and assembly design. These changes involved reorificing of the core to mitigate thermal striping concerns (Sections 3.11, 3.12), fuel and fuel rod specification changes to be compatible with SAF line production, cladding and duct material specification changes to obtain an improved cold-worked Type 316 stainless steel, methodology improvements and changes in the fuel rod analysis procedures, unsuccessful attempts at qualifying fuel rod design procedures with the EBR-II data base, adverse power-to-melt information from FFTF testing, and numerous interactions with NRC, the ACRS and the Atomic Safety and Licensing Board in the attempt to obtain a construction permit.

It had always been planned to prepare and hold a performance analysis review of the fuel rod and assembly prior to release for fabrication. This review was scheduled for March of 1984. Preparations for the analytical effort were already started and reactor reorificing had been completed in mid-1983. New methodology had been used (FPAN) for rod analysis in the core reorificing. Property evaluations for the improved Type 316 SS had been almost completed.

Power-to-melt preliminary evaluations based on FFTF data had indicated small margins for design conditions which remained to be tested for the reorificed core. The designers were confident that the evaluations to be done for the performance review would yield acceptable margins based on preliminary evaluations. What remained was the analysis and review to support this confidence.

3.1.9 References

1. "Fuel Pin and Assembly Design Limits and Criteria," R. M. Vijuk, D. S. Duff, J. D. Stephen, International Conference on Fast Breeder Reactor Fuel Performance, Monterey, CA, March 5-8, 1979, Pg. 725.
2. "The Development and Application of a Cumulative Mechanical Damage Function for Fuel-Pin Failure Analysis in LMFBR Systems," CRBRP-ARD-0115, D. C. Jacobs, May 1976.
3. "FPAN User's Manual (Revision 2)," LRA-82-249, D. C. Jacobs, July 1, 1982.

3.2 Blanket Assembly Design

3.2.1 Summary of Functions and Description

The functional requirements of the Inner and Radial Blanket Assemblies (IBAs and RBAs, respectively) are presented in Table 3-23. These functional requirements have been satisfied by the design in the manner shown by the design description which follows. Unlike the fuel assembly the design of the blanket assemblies was not based on FFTF experience (since FFTF does not contain blankets) but was a new design for U.S. LMFBR operating reactors.

Plutonium is bred from the depleted UO_2 in the rods of the assemblies. The RBAs contribute to the reactor radial shielding and with the IBAs, to the reactor thermal power. The blanket assembly design consists of a wire wrapped bundle of 61 rods in a triangular pitch enclosed in a hexagonal duct as shown in Figure 3-8. An inlet nozzle, shield and orifice are attached to the bottom of the duct. The top of the assembly is capped by an outlet nozzle that can be handled by the refueling machine. The blanket assembly thereby provides a controlled channeled path for the primary coolant of the reactor system thereby fulfilling all the functional requirements.

The design features of the BA structure are similar to those of the Fuel Assemblies and are given in Table 3-24. The sodium coolant enters each BA at the inlet nozzle and flows upward through the shield block (Figure 3-8) and a stack of orifice plates. Upon leaving the orifice plates, the sodium coolant flows through the rod bundle and then out of the assembly through the outlet nozzle. Piston rings on the inlet nozzle are used to limit the flow which can bypass the assembly.

The inlet nozzle positions the BA in the Lower Inlet Modules. The shield block, located between the duct and inlet nozzle, provides adequate shielding for the Core Support Structure and the Lower Inlet Modules, serves as an attachment point for the blanket rods, and controls the sodium coolant flow into the assembly.

Table 3-23

CRBRP BLANKET FUNCTIONAL REQUIREMENTS

<u>Core Component</u>	<u>Functional Requirements</u>
Blanket Assemblies	● Provide and position the fertile material within the core for conversion to plutonium ● Provide a compact structural unit that can be handled in and out of the reactor by the refueling machines ● Provide a controlled channelled path for the primary sodium coolant of the reactor heat transport system

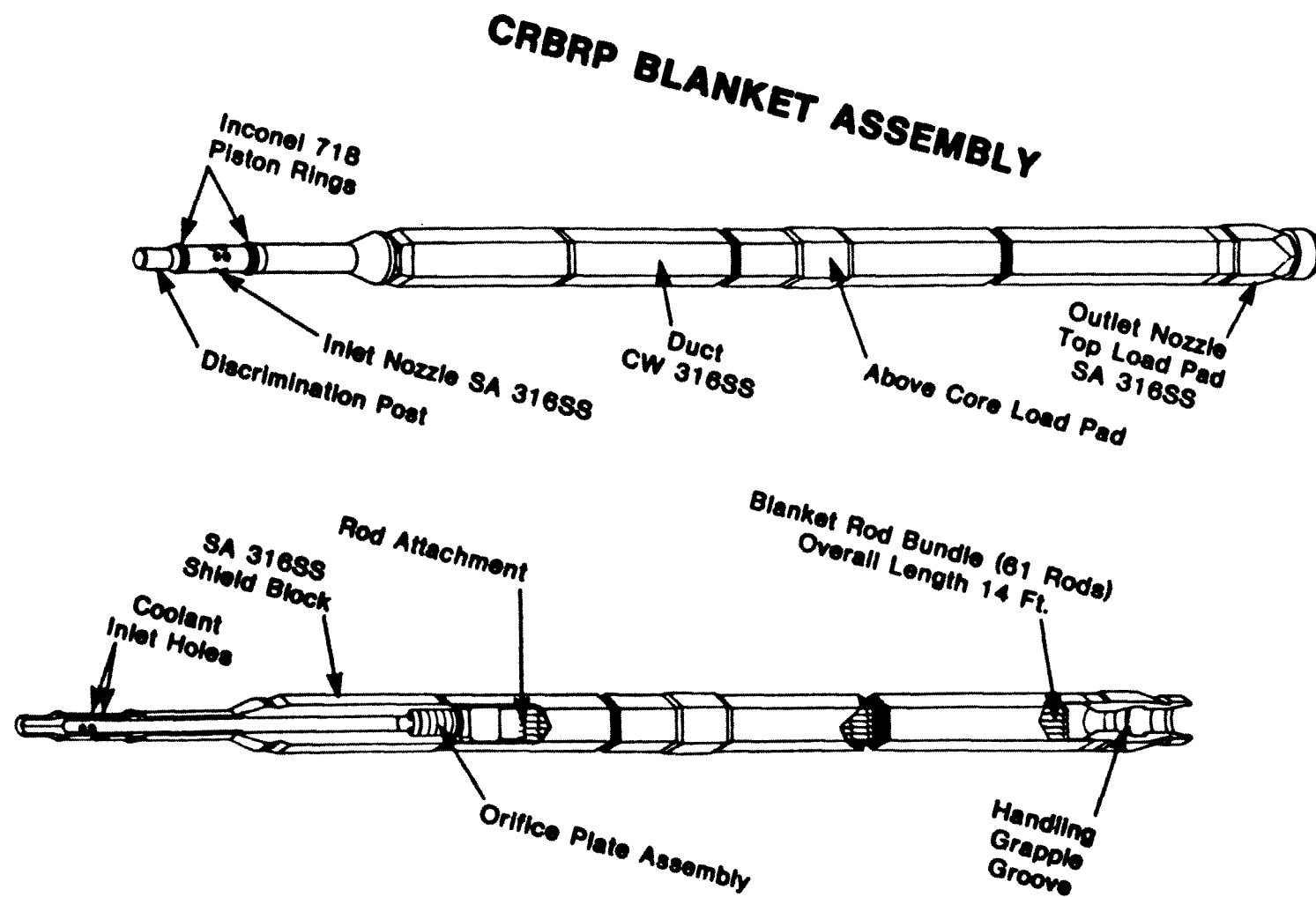


Figure 3-8

TABLE 3-24
CRBRP BLANKET ASSEMBLY DESIGN DETAILS

<u>Parameter</u>	<u>CRBRP Value</u>
Number of Assemblies	208(2)
Blanket Rods Per Assembly	61
Assembly Length, in.	168
Duct Material	Type 316 Stainless Steel, 20% Cold Worked
Inlet and Outlet Nozzles and Shield Block Material	Type 316 Stainless Steel, Solution Annealed
Distance Across Flats on Duct Inside, in.	4.335
Duct Wall Thickness, in.	0.120
Duct ACLP ⁽¹⁾ Thickness, in.	0.205
Assembly Pitch, in.	4.760
Maximum Shield Block Length, in.	20 (73% Min. Volume Fraction)
Weight, lb	
In Air	~536
In Sodium at 400°F	~469

(1) ACLP: Above core load pad.

(2) Includes the six Inner Blanket Assemblies that are alternated with Fuel Assemblies.

Each rod is formed from a stack of solid cylindrical dished blanket pellets encapsulated by a cladding tube, as shown in Figure 3-9. The blanket fuel pellet density maximizes the fuel volume fraction yet maintains adequate voidage for UO_2 swelling. Selected blanket rod parameters are given in Table 3-25 while Table 3-26 presents these parameters as well as other parameters of general interest.

The cladding wall thickness was selected to contain the blanket pellets and associated gases. A void is provided above the pellet stack inside each rod which serves as a plenum for holding the gases formed by the fission process. Initially, each rod is filled with helium bonding gas to facilitate pellet-to-clad heat transfer. Each plenum contains an encapsulated, unique tag gas for a given assembly used for detection and identification of any breached BA rods within an assembly. A helical compression spring in the fission gas plenum is provided for holding the pellets in place during handling prior to insertion of the BA into the reactor. The blanket rods are spaced within the assembly by wires wrapped on the rods, each in the form of a helix. This spacer concept was selected as a simple and proven design, and the four-inch axial pitch of the wires was selected to promote flow mixing within each assembly.

The rods are mechanically attached to the shield as shown in Figure 3-8. Two support bars with integral inverted U-shaped legs are welded to the top of the shield. These bars are slotted to receive the attachment rails that span the two support bars. The bars and rails are predrilled so that two locking pins inserted into the two bars hold the nine rails to the bars. The rail cross section engages a keyhole-shaped slot in the bottom of the blanket rod. From five to nine rods are slid onto one rail. The duct locates the rods and pins in place.

A groove within the outlet nozzle is provided for engagement by the handling grapples of the Reactor Refueling System. The BA is constructed to withstand any loads that may be imposed during shipping or refueling. Raised pads on the side of the outlet nozzle and duct are designed as contact points between the various core assemblies. Any radial loads on the reactor are received and transmitted by these load pads as part of the core restraint system.

CRBR BLANKET ROD

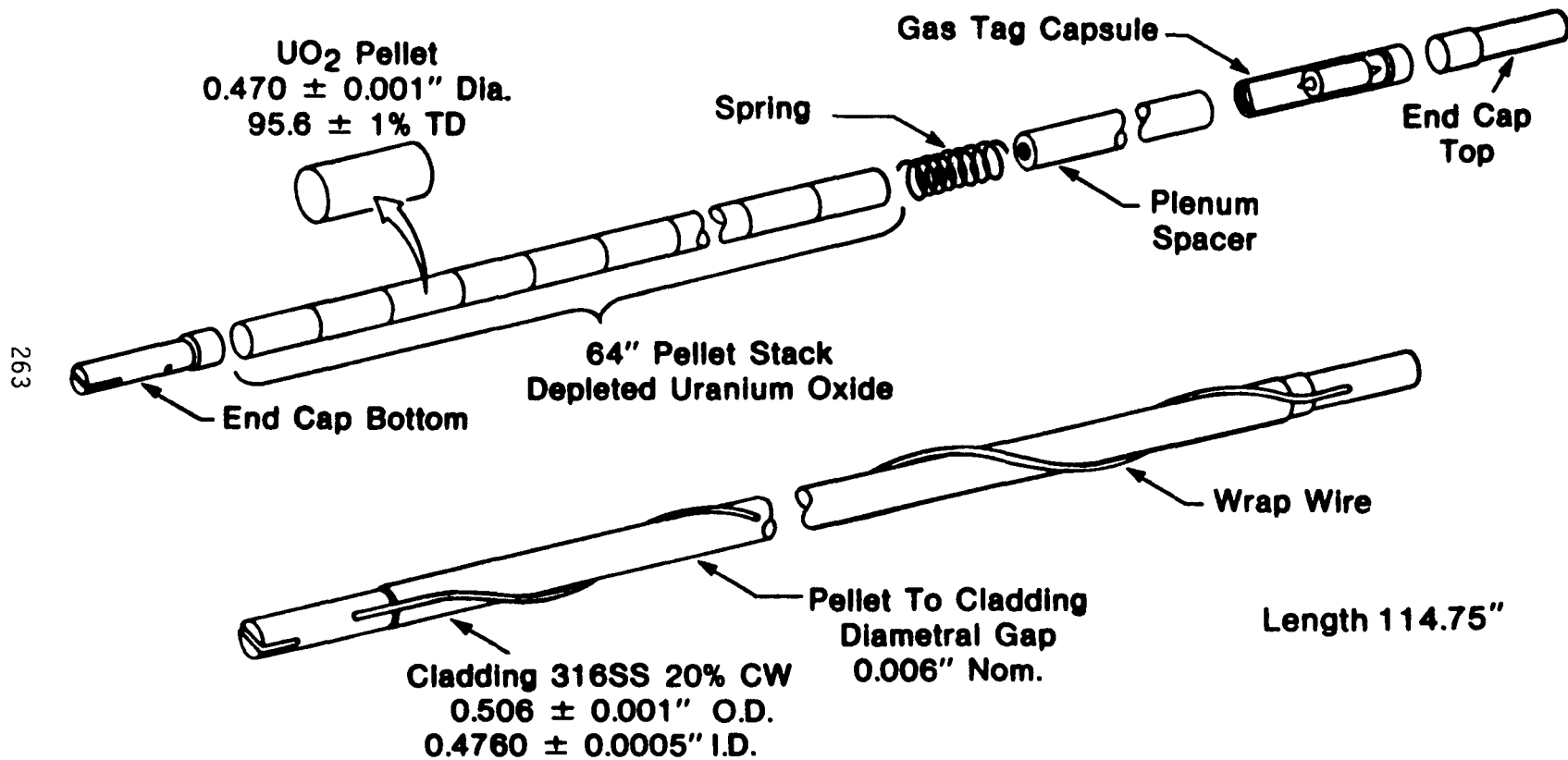


Figure 3-9

Table 3-25

BLANKET ASSEMBLY DESIGN DESCRIPTION

Blanket assembly design is the same as the fuel assembly except for blanket rods and the shield/inlet nozzle for the radial blankets

Inner and radial blanket rod parameters:

Cladding diameter	0.506 inches
Cladding wall thickness	0.015 inches
Fuel-cladding gap	0.006 inches
Pitch-to-diameter ratio	1.076
Fuel material	Depleted UO ₂
Fuel column height	64 inches
Blanket rod length	114.75 inches
Wire wrap diameter	0.033 inches
Wire wrap axial pitch	4.0 inches

TABLE 3-26
CRBRP BLANKET ROD DESIGN DETAILS

<u>Parameter</u>	<u>CRBRP Value</u>
Pitch to Diameter Ratio	1.072
Blanket Rod Spacing Device	Wire-Wrap
Blanket Rod Length, in.	114.75
Cladding Material	Type 316 Stainless Steel, 20% Cold Worked
Rod Outside Diameter, in.	0.506
Cladding Thickness, in.	0.015
Blanket Pellet Column Length, in.	64
Pellet Material	Depleted UO ₂
Pellet Diameter, in.	0.470
Pellet Length, in.	0.5 to 1.0
Pellet Density, % Theoretical	95.6
Smear Density, % Theoretical	93.7
Cladding-Pellet Diametral Gap (Initial, Cold), in.	0.006
Fission Gas Plenum Volume, in ³	8.0
Fill Gas	He
Fill Gas Pressure, Atmos.	1.0
Spacer Wire Material	Type 316 Stainless Steel, 20% Cold Worked
Spacer Diameter, in.	0.033
Spacer Wire Pitch, in.	4.0

After being properly inserted into a reactor lattice position, each BA is held in place by its own weight and hydraulic balance.

All BAs incorporate a discriminator on the inlet nozzle to prevent insertion of an assembly into an incorrect reactor lattice location. Notches cut into the outlet nozzles provide a feature for remote identification of individual assemblies, by type and serial number, by the Reactor Refueling System machines.

BA surfaces subject to sliding friction with interfacing structures are protected against potential galling by chrome or chrome carbide coatings. Ample and strategically-placed chamfers on the BAs guide misaligned assemblies into position, rotate assemblies to the correct azimuthal alignment, and provide transitions between various cross sectional changes in shape and size of the BAs.

The structural materials in the BAs are mostly components fabricated from Type 316 Stainless Steel. The blanket rod cladding, wire wraps, and ducts are cold worked to minimize radiation swelling. The piston rings and pins for the rod attachment are made of Alloy 718 to prevent galling. In addition, the rings are chrome plated to provide the best combination for low wear, leakage and friction coefficients with the Alloy 718 receptacles of the Lower Inlet Modules.

3.2.2 Blanket Assembly Design Process

In Section 3.1.2 the core assembly design process was given schematically in Figure 3-3. In that section the iterative process was described and therefore will not be repeated in this section. The process given is essentially the same for the blanket assembly design.

3.2.3 Damage Severity Limits and Design Transients

The damage severity limits and design transients and the enveloping transients used in design analyses were given previously in Section 3.1.3. This section

is likewise applicable to the blanket assembly and for this reason a description is not repeated and the reader is referred to Section 3.1.3.

3.2.4 Blanket Rod Criteria and Mechanical Damage Limits

The major differences between the fuel rod criteria and mechanical damage limits is associated with the continually increasing linear heat generation rate (LHGR) in the blanket rod and the longer residence time associated with the radial blankets. As the LHGR rate increases the cladding temperature also increases with time as plutonium is bred into the blankets. This differs from the fuel rod which shows decreasing fuel and cladding temperatures with time. As shown in Table 3-11 the only differences between the limits chosen are in the ductility limited strain (DLS) allowable for steady-state and transients. For the blanket rods a DLS value of 0.1% and 0.2% was chosen primarily to account for the longer residence time. In the blanket rod analyses it was necessary to modify the LIFE code for property values applicable to UO_2 at start of life. Utilization of EBR-II irradiation information on early blanket rod tests were used to develop and refine gas release, blanket fuel swelling and other parameters affecting rod performance. Final proof of the design methodology was to be obtained from ongoing FFTF and EBR-II irradiations such as the WBA-40, 41 experiments presently under irradiation in FFTF and the WBA-24 operational transient test in EBR-II (completed). Of major interest was the power-to-melt information which would be obtained from these and other tests scheduled for FFTF (WBA-45/46).

3.2.5 Blanket Assembly Criteria and Limits

Because of the similarity in design of the CRBRP fuel and blanket assemblies the structural criteria and limits are also the same as those for the fuel assembly given in Section 3.1.5. For this reason the reader is referred to that section.

3.2.6 Results of Final Design Blanket Rod and Assembly Analyses

The preliminary design review of the blanket assembly was held on July 14 to 17, 1975. The final design review of the blanket assembly was held in February 1979 and included pertinent actions from the preliminary design review. During the interim period a number of significant design changes had taken place. The blanket rod diameter had decreased from 0.520 inch to 0.506 inch, the concept of using thinned elliptical wires in the outer row of the blanket rods to promote even radial temperature distribution had been changed to one of using the same wire diameter on all rods (based on experimental results), and a new rod attachment assembly had been designed and drop tested to insure its structural integrity. By the time the design review was held two successful blanket rod irradiation experiments without rod breach had been done in EBR-II (WBA-20, 21), which lent considerable credence to the blanket rod design methods. These experiments aided in providing modification to the fuel rod LIFE code which would allow it to calculate fission gas pressures and FCMI for the blanket rod. This modification is necessary because of the higher pellet density, different fuel properties, different (increasing constantly) steady state cladding temperature history, and constantly increasing fuel temperature as plutonium is bred into the depleted uranium dioxide in CRBRP.

The key blanket rod and assembly evaluations from the final design presentations and documentation are described below:

A. Blanket Rod

The cladding damage is within design limits. The minimum margin for inner and radial assemblies are given in Table 3-27. Depending on which criterion, the CDF or the DLS, the minimum margin ranges from 9 to 600% respectively for steady-state and transient damage. It should be remembered that, because of the longer time in-reactor of radial blanket rods than fuel rods, the DLS criterion is less than the fuel rod cladding (0.1% for steady-state and 0.2% for steady-state plus transient). In addition, it was shown that the effects of power jumps in the heterogeneous core do not decrease this margin due to FCMI, as shown in Table 3-28.

DESIGN EVALUATIONS - KEY BLANKET RESULTS

Cladding Damage

- Cladding damage is within design limits
 - 68 percent margin on steady-state cumulative damage (Radial)
 - 250 percent margin on steady-state ductility limited strain (Radial)
 - 9 percent margin on steady-state and transient cumulative damage (Inner)
 - 600 percent margin on steady-state and transient ductility limited strain (Radial)
 - Margins are not reduced due to FCMI from a mid-life power jump

Wire wrap

- Maximum wire wrap stress and strain are well below limits of 21 ksi and 0.3%, respectively
- Maximum wire slack is <0.1 inch and considered acceptable.

Bundle/duct interaction

- Maximum bundle duct interference of ~0.013 inches is well below the design guideline value of one wire wrap diameter (0.033 inch)
- Maximum bundle/duct clearance of ~0.065 INCH is adequate based on vibration testing
- Adequacy of design due to unique blanket features (stiffness) will be obtained from EBR-II and FFTF irradiation testing (WBA-40,41,45)

Duct dilation

- The maximum duct dilation is 67 mils for the IBA and 82 mils for the RBA which is less than the limit of 108 mils required to exceed the pitch line

Table 3-28

**STEADY STATE CDF VALUE FOR THE HOT ROD OF
BLANKET ASSEMBLY 201 OF THE FIRST CORE**

<u>Axial Location</u>	<u>Case I*</u>	<u>Case II*</u>
0.46	0.0106	0.0137
0.62	0.187	0.205

*Case I does not include effect of power jump

**Case II includes effect of power jump

Analysis also showed that the wire wrap would perform well. The calculated stresses and strains are well below their design limit, and a maximum possible slackness of <0.1% calculated for the full length of a wire wrap is considered acceptable.

Rod internals were also evaluated. The holddown spring, plenum spacer and fission gas plenum size were all shown to be within acceptable limits.

A summary of thermal transient data is given in Table 3-29. Maximum cladding, fuel and coolant temperatures are given together with the time to reach the maximum temperature and the length of time the temperature exceeds steady state conditions. For any of the umbrella transient events the cladding does not exceed the design guideline of 1600°F, and the coolant does not boil. In all cases but one the blanket fuel does not reach melting. As noted in the footnote for the U-2b event incipient melting was calculated. However, further analysis confirmed that melting would not actually be reached for burned-in gap conductance values.

The bundle-duct interaction (BDI) was evaluated for maximum BDI interference and clearance. The results of these analyses are shown in Figure 3-10 where the interference and clearance is shown as a function of time. The maximum interference occurs for a radial blanket assembly and is ~0.013 inch. This value is well below the design allowable of one wire wrap diameter, or 33 mils. The maximum bundle/duct clearance calculated was for an inner blanket assembly and was ~0.065 inches. Based on vibration testing results this value is considered acceptable.

The maximum duct dilation versus the axial length for inner and radial blanket assemblies are shown in Figure 3-11. For the IBA the dilation has a maximum of 67 mils and for the RBA the maximum is 82 mils. Both of these values are well within the limit of 108 mils necessary to exceed the pitch line.

Table 3-29

SUMMARY OF THERMAL TRANSIENT DATA
FOR BLANKET ROD REGION

EVENT	CLADDING			FUEL			COOLANT		
	A	B	C	A	B	C	A	B	C
<u>U-1</u>									
IB/A 94	1397	40	85	4597	0	0	1397	40	85
IB/A 100	1412	45	95	4525	0	0	1412	45	95
RB/A 201	1356	20	67	3728	0	0	1353	45	95
RB/A 206	1476	60	126	2850	0	0	1476	60	126
RB/A 212	1429	80	169	2258	0	0	1429	80	169
<u>U-2b</u>									
IB/A 94	1541	324	405	5132**	300	309	1541	324	405
IB/A 100	1545	342	408	5060	300	305	1545	342	408
RB/A 201	1483	318	383	4246	300	305	1474	342	412
RB/A 206	1578	351	434	3275	300	305	1578	351	434
RB/A 212	1519	366	476	2568	300	304	1519	366	476
<u>OBE</u>									
IB/A 94	1433	21	90	4620	1	1.5	1433	21	90
RB/A 201	1400	15	68	3749	1	1.5	1389	24	97
<u>E-16</u>									
IB/A 94	1501	120	>860*	4597	0	0	1501	120	>860*
IB/A 100	1498	151	>574*	4515	0	0	1498	151	>574*
RB/A 201	1438	130	>600*	3733	0	0	1438	130	>600*
RB/A 206	1551	246	565*	2855	0	0	1551	246	565*
RB/A 212	1580	382	>800*	2256	0	0	1580	382	>800*

A - maximum 3σ hot spot temperature attained, °F.

B - time to reach maximum temperature, sec.

C - length of time temperature is above initial steady state value, sec.

* Extrapolated values.

** In the order of 1% molten fuel might exist for this temperature; however, using LIFE-III burnt-in gap conductance values will lower the temperature substantially below this temperature calculated with P-19 fresh rod data.

MAXIMUM CRBR BLANKET ASSEMBLY BUNDLE - DUCT INTERFERENCE AND CLEARANCE VS. TIME

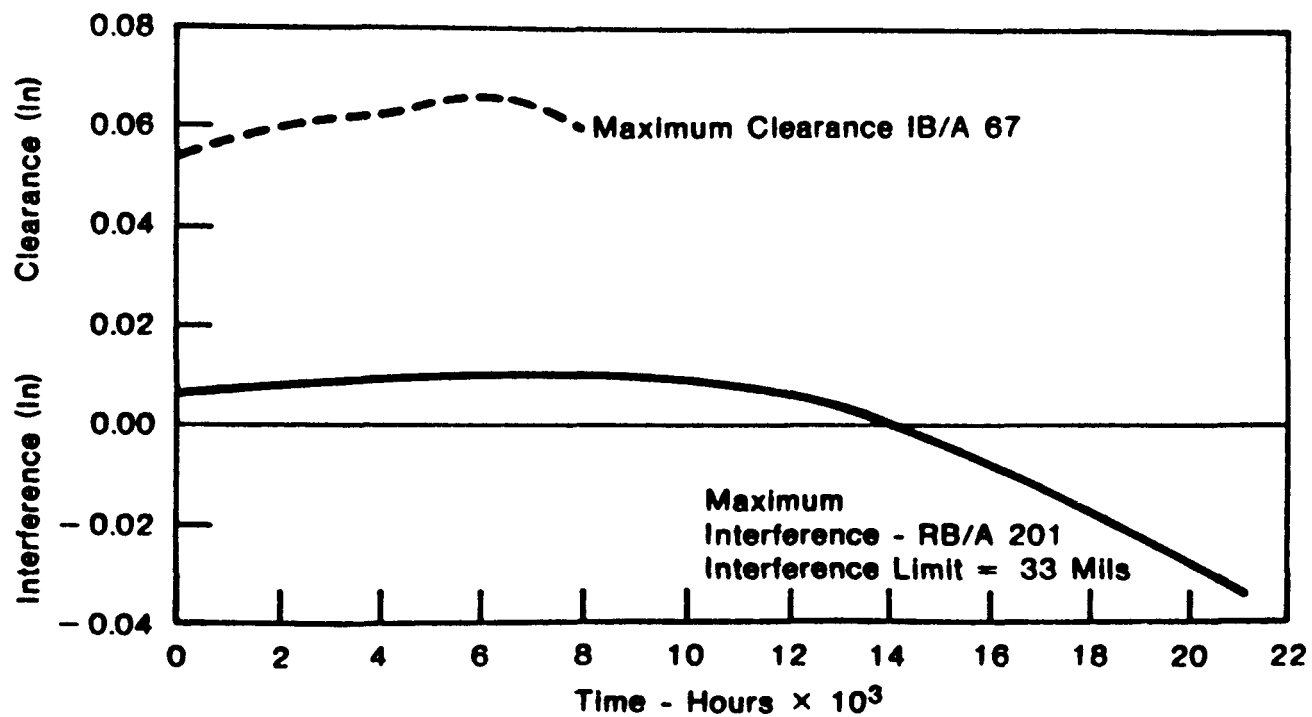


Figure 3-10

DUCT DILATION VS. AXIAL HEIGHT FOR IB/A 67 AND RB/A 201

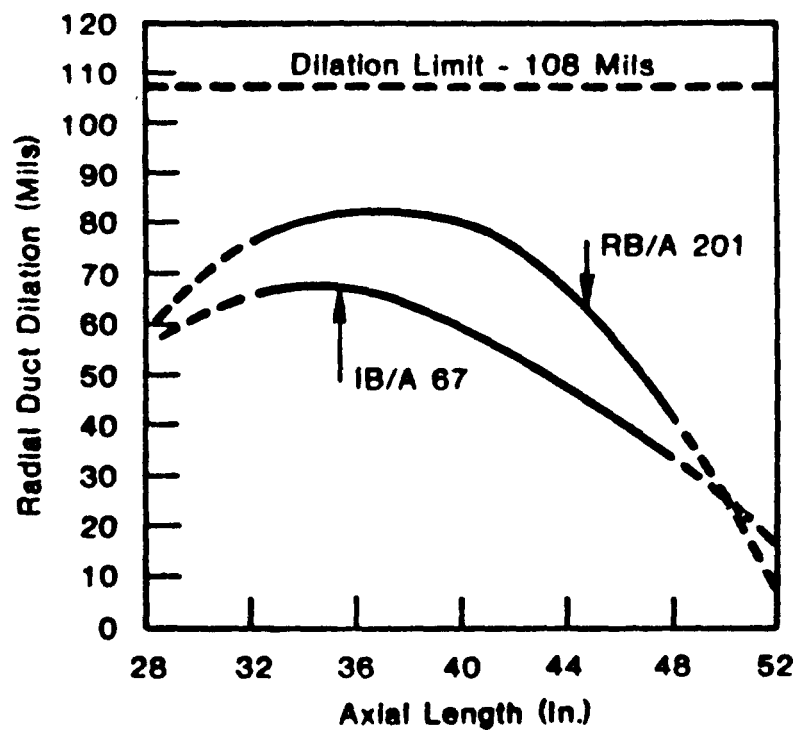


Figure 3-11

B. Blanket Assembly

In the structural analysis of the blanket (and core) assemblies the regions/components of the assembly which were evaluated and the design conditions considered in the evaluations are given in Table 3-30. Table 3-31 presents the results of the analyses. The margins of safety are shown together with the failure mode. All margins of safety are acceptable.

Thermal striping considerations for the core assemblies are presented in Section 3.12 and confirm that for the design conditions the materials and outlet nozzle duct design is acceptable.

3.2.7 Design and Methodology Evolution

The blanket assembly design and methodology evolution is interconnected with experiments and tests used to prove out particular design features or analytical techniques. Unlike the fuel assembly it was not possible to rely on development work done in support of FFTF/CRBRP fuel design, which were in many cases very similar.

Following is a summary of some of the more significant development work which assisted and impacted the blanket assembly design.

3.2.7.1 Blanket Heat Transfer Testing

A. Objectives

Blanket Heat Transfer Testing - the principal objective of the blanket heat transfer tests was to reduce uncertainties in prediction of temperature and flow in blanket assemblies by providing experimental data to validate and calibrate thermal-hydraulic codes. This was to be achieved by a test program whose aim was to:

Table 3-30

CORE ASSEMBLY STRUCTURAL ANALYSIS

Regions/components evaluated

- Orifice plate
- Rod attachment assembly
- Shield block
- Duct at mid-core
- Duct load pad
- Outlet nozzle

Design conditions considered

- Fast fluence
- Uniform temperature
- Cross-duct temperature gradient
- Inter-assembly temperature gradient
- Core restraint bending moments
- Coolant pressure
- Transient power and flow variations
- Seismic loads

Table 3-31

CRBRP BLANKET ASSEMBLY STRUCTURAL ANALYSIS SUMMARY

<u>Assembly Region</u>	<u>Minimum Margin</u>	<u>Failure Mode</u>
Orifice plate	Greater than fuel assembly	
Rod attachment	>0.0	Per drop test results
Shield block	0.48	Peak plus accumulated deformation
Duct		
• Core midplane	Greater than fuel assembly	
• Load pad	0.07	Residual deformation
Outlet nozzle	0.14	Local ductile rupture

*Margin (of safety) = $\frac{\text{Allowable limit}}{\text{Calculated value}} - 1.0$

- o Measure experimentally temperature distribution in a 61-rod full-size electrically heated blanket assembly model over a wide range of sodium flow, power and power gradients expected for typical blanket operating conditions, both steady state and transient.
- o Determine the effect of inter-assembly heat transfer.
- o Compare data with thermal-hydraulics code predictions to calibrate and validate codes.
- o Investigate stability of flow and cooling during low and transient flow conditions.
- o Establish a friction factor versus flow correlation for blanket bundles.

In a blanket assembly a high volume fraction of fertile material is required for efficient breeding. To achieve this, the blanket assemblies consist of large diameter blanket fuel rods closely spaced on a triangular pitch and contained within a hexagonal duct. To minimize hot spots and permit adequate cooling, the rods are separated by spacer wires wound helically around each rod. The blanket assemblies differ from fuel assemblies by using larger diameter, closer spaced rods with a shorter wire wrap spacer lead and pitch length.

For a safe design, the temperature distribution and in particular the peak temperatures in the coolant must be known at all operating points. However, the prediction of these temperatures is difficult because of the complexity and wide range of operating conditions which may occur in blanket assemblies. These complexities are imposed by the function, operating environment and inherent features of the LMFBR blanket assemblies.

Coolant flow through individual blanket assemblies may be in the turbulent, transition or laminar flow regime depending upon the assembly heat generation rate. Radial blanket assemblies occupy the outermost or peripheral positions surrounding the core. These are characterized by lower total power but steep flux, power and temperature gradients. Inner blanket assemblies are located in concentric rings within the reactor core and have higher power levels but flatter, though more complex, gradients.

The radial blanket assemblies have gradients decreasing radially outward with distance from the core axis; a few gradients are oriented across the rows of the hexagonal array, others across corners. Because of the steep temperature gradients and the wide range of coolant flow velocities, heat transfer from rods to coolant may take place in the forced, natural or mixed convection, and buoyancy effects and secondary recirculating flow patterns may arise which further complicate prediction of temperatures within the assembly. The difficulties are further compounded since, in addition to temperature gradients within an assembly, there are also significant temperature differences between adjacent assemblies. This results in heat transfer by conduction between adjacent assemblies, which tends to further distort flow and temperature patterns.

The coolant temperatures within a wire wrapped blanket assembly can be predicted by subchannel codes of the type which start with the temperature at the assembly inlet and progressively calculate the temperature at successive locations along the assembly. However, these codes contain parameters whose values must be obtained from or checked by calibration against test data. No test data existed which were directly applicable to calibration of the codes for use with blanket assemblies.

Therefore, large uncertainties were associated with predicted temperatures. This test program was undertaken primarily to obtain test data which would be used to reduce the uncertainties in predicted peak temperatures, temperature distribution, pressure drop and flow stability during transient and low flow conditions. Results obtained from this experimental program are given below for the various primary considerations:

B. Assembly Pressure Drop

Laminar, transition, and turbulent parallel flow pressure drop across the wire wrapped hexagonal 61-rod bundle positioned inside a duct were determined in tests using water, sodium, and air. A smooth transition region from turbulent to laminar flow which occurred over the 5000 to 400 Reynolds number range characterized the resulting friction factor behavior. The long, continuous transition region could be explained in terms of the fraction of the flow area in turbulent flow.

Laminar friction factors calculated from individual subchannel measurements could be correlated by the same expression found for rod bundle averaged conditions. In the laminar range, the friction factor was correlated by the expression $f = 99/Re$, in the turbulent range by $f = 0.48/Re^{.25}$, and in the transition range by

$$f = \frac{99}{Re} \sqrt{1-\Psi} + \frac{0.48}{Re^{.25}} \sqrt{\Psi}$$

$$\text{where } \Psi = \frac{Re-400}{4600}$$

The data were based on bundle average flow rate and hydraulic diameter and agreed with the correlation within $\pm 15\%$ over the range tested.

C. Temperature Rise Peaking with Radial Blanket Gradients

(1) Forced Circulation Peaking

Of principal interest to the thermal design of blanket assemblies is the highest (peak) temperature rise in the rod bundle which usually occurs at the outlet of the heated zone. Most tests were performed with heat input gradients from 1:1 to 4.6:1 oriented row-by-row across flats. Tests were made with forced (pumped) circulation and with natural circulation around

a loop containing an airblast cooler. Temperature distributions were mapped from recorded data at selected horizontal planes (levels), and transverse temperature rise profiles were plotted. The peak temperature rise values were compared with code predictions and their location in the bundle was determined.

The effect of heat input gradient on the temperature profiles at constant total power input and flow was observed. The peak temperature rise values were cross plotted against power-to-flow ratio, Reynolds number, and heat input gradient. Buoyancy flattened the temperature profiles in the laminar and transition flow regions. In fully turbulent flow, peaking approaches a maximum value for each input gradient.

The peak coolant temperature rise in the blanket assembly depends on the heat input gradient. For uniform heat input (1:1), the temperature rise profile is nearly symmetrical, with the peak occurring near the center of the rod bundle. With increasing gradient, the peak is displaced towards the high heat input side and its value increases. Because of conduction to the higher side subchannel flow, peaking does not occur next to the first row of rods which have the highest heat input for the range of input gradients tested.

When the blanket assembly heat input gradient is across corners, the maximum-to-minimum heat input ratio is greater than that for the across-flats gradient of the same peak-to-average heat input ratio because of the longer radial distance. This gives rise to slightly higher temperature peaking than observed with the across-flats (row-by-row) gradient. A correction to the peaking was calculated which accounts for the difference in the conductivities of the ceramics in the rods, UO_2 in the reactor and BN in the test assembly. For the peak channel, it is of the magnitude of 3.5% and a weak function of heat input gradient.

(2) Peaking with Natural Circulation

Under normal conditions and during controlled shutdown, heat removal is by sodium pumped through a circuit comprised of the core and the intermediate heat exchangers (IHX). Upon a postulated failure of all pumps, the decay heat is transported from the core to the IHX by natural circulation. The question arose whether the temperature distributions in a blanket assembly predicted for the pumped mode of operation was the same or different when the motion of the fluid was induced solely by temperature (i.e., density) differences between the core and the IHX.

Tests were performed of the assembly with a cooler in a bypass loop, isolated from the main loop. The temperature distributions obtained were plotted. For operation of the test section with natural circulation, higher temperature rises were required than with forced circulation to induce the desired flow rates. These flow rates were found to be a single valued function of test section heat input, independent of heat input gradient. In comparison with pumped circulation, slightly lower peaking was observed at the same flow rates. The physical location at which peaking occurred differs somewhat in the two modes of flow circulation.

D. Peaking with Inner Blanket Gradients

In CRBRP blanket assemblies are placed within the fuel assembly region. The heat generation rates of these inner blanket assemblies are higher than those of the radial blanket. The heat input pattern is affected by the adjacent control assemblies and is therefore not symmetrical.

The heat input patterns also are flatter than in radial blankets. Peaking is reduced and can occur at more than one location. Two peaks may occur for symmetrical across-flats and across-corners input. Skewed gradients with a minimum between wall and center rod produces non-symmetrical peaking. When the heat input pattern is very flat, representing conditions adjacent to control assemblies, the resulting temperature rise pattern is very similar to that found for uniform heat input. It could be shown that temperature rise gradients generated by power input peaks of less than 1.1/1 maximum to average (1.2/1 maximum/minimum) will be flattened by the strong mixing effects of turbulence and wire wrap pumping, and that in these cases the peak temperature rise does not exceed that obtained with uniform heat input (1.27 times the average).

E. Peaking During Transient Operation

A series of loss-of-flow transient heat transfer tests was performed. First, steady-state conditions of power input, flow, temperatures were established; then power input and sodium flow were decreased at preprogrammed rates. The test series included five tests with uniform heat input to all rods, four with uniform heat input to four rows (26 rods) only and five with a row-to-row heat input gradient of 2.8:1, maximum: minimum. The transients simulated both undercooling and overcooling events.

The system responded as expected. The peak temperatures followed the peaks in normalized power-to-flow ratio. They occurred about 15 seconds to 70 seconds after the power-to-flow ratio peaked, increasing lag reflecting the decrease in sodium velocity during the latter part of the transient. The peak temperatures were higher during the initial, higher flow rate part of the transient even when the highest power-to-flow ratio occurred during the latter part of the transient. This is believed to reflect buoyancy-induced flow redistribution which had been observed in low flow steady-state tests. Heat loss and thermal conduction also are contributing factors.

High temperature gradients occurred within the rod bundle during power and flow turn down when only part of the bundle was heated. However, no oscillations or other indication of local flow instabilities were observed, notwithstanding the steep temperature gradients existing at the interface between the heated and the unheated portions of the rod bundle.

F. Inter-Assembly Heat Transfer Effect on Peaking

An auxiliary duct containing five heater rods was located next to two opposite faces of the hexagonal main duct. By adjusting flow and heat input to these auxiliary ducts, heat could be transferred to and from the main duct; simulating the effect of an adjacent assembly on the temperature profiles in the main rod bundle. The effect observed was an increase or decrease of the temperatures in the flow subchannels next to the wall facing the auxiliary duct, depending on the temperature level. The change in temperature was sensed in several subchannel rows inboard of those near the duct wall and affected the peak value of the temperature rise in the rod bundle, and, also, because of the circumferential swirl, all side channel temperatures.

G. Summary

The information obtained by the heat transfer test program led to significant improvement in the understanding of heat transfer under the complex conditions existing in the CRBRP blanket assembly. The uncertainty in the predictions of flow and temperature patterns on the CRBRP blanket was much reduced. A data base was established against which steady-state and transient subchannel codes could be calibrated and verified. The relation between flow rate and bundle pressure drop was found to be single valued over the whole range of operating flow rates. The effects of inter-assembly heat transfer and of buoyancy on temperature profiles was demonstrated. Stability

of flow and temperature patterns was proven over the whole range of test conditions including coastdown to natural circulation flows with steep heat input gradients within the rod bundle.

The results [1] aided the blanket design immeasurably by not only decreasing uncertainties but also providing confirmation of analytical tools that applied thermal loading to the assemblies and allowed analysis of gradient limits which allowed the blanket assembly handling sockets to mate with the refueling systems (see Section 3.10). These experiments also showed that the mixing enhancement of the short pitch cylindrical wire wrap produced sufficient mixing of the coolant to alleviate gradient concerns.

3.2.7.2 Load Following Experiment

Although not a prerequisite for the initial core of CRBRP, the project recognized the ultimate need for load following capability and had intended to demonstrate such capability in reload cores for fuel as well as for blankets. Early analyses of cyclic thermal stress effects on cladding showed that irradiation creep is predominantly a relaxation mechanism. Since irradiation creep is believed to be superplastic, it was postulated that load following was possible. In addition, later LIFE code predictions also indicated that load following could yield acceptable cladding damage. Therefore, load follow was conceptually feasible. However, the damage mechanisms remained to be demonstrated experimentally.

The load following characteristics for a typical day of CRBRP power operation is shown in Figure 3-12. The cycles consist of 5 fluctuations from 80 to 100% power and one from 40 to 100% power. In addition, overpower conditions to ~115% power were periodically postulated for anticipated (upset) events.

At the time of this experiment no test data were available to support the operation of blanket rods undergoing overpower transients and repetitive cyclic overpower operation. The operation of the EBR-II reactor in a transient mode during run 112 (February - March 1981) presented a unique

AVAILABILITY = 85%, LOAD CAPACITY=75%

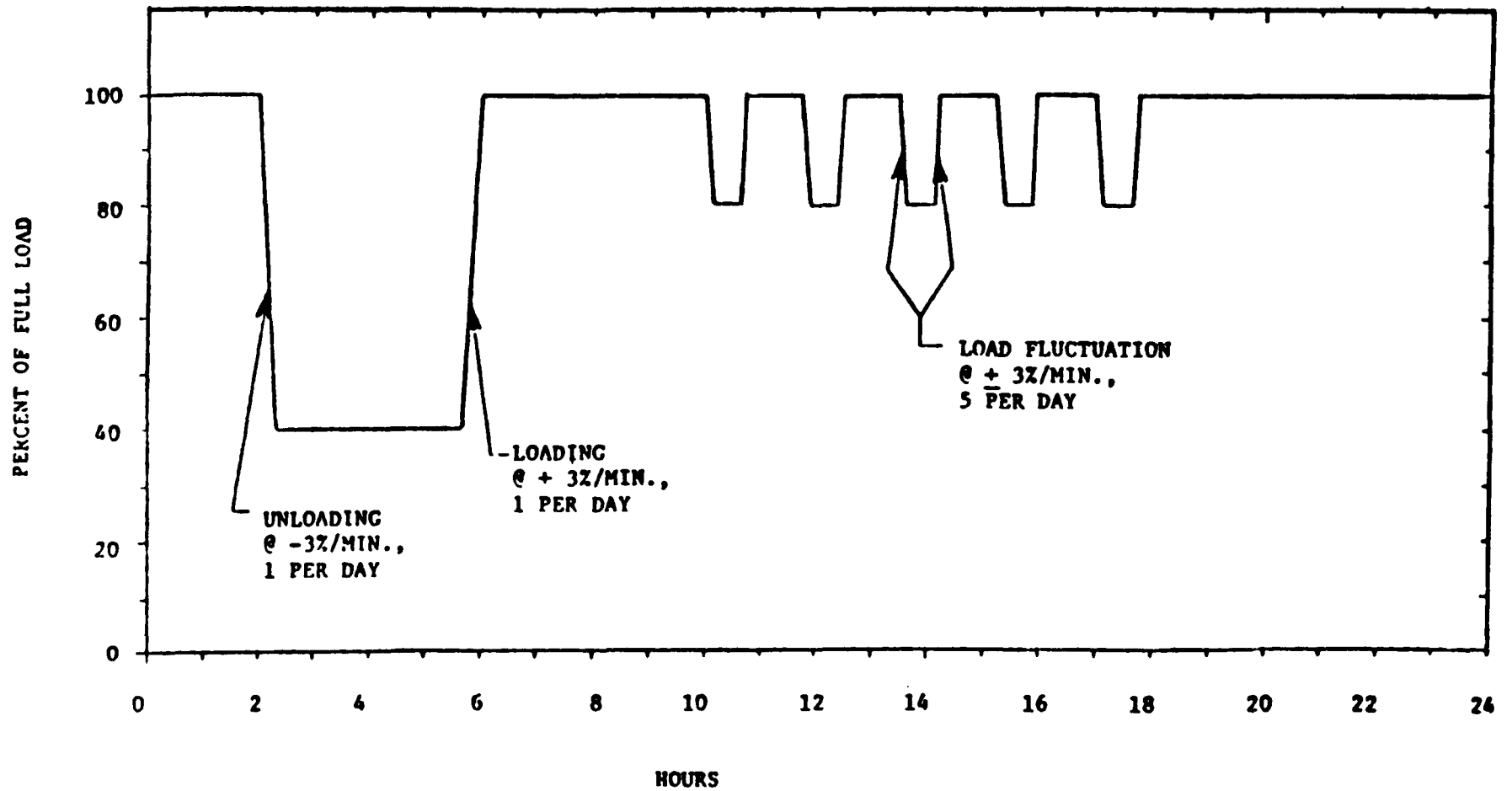


Figure 3-12

CRBRP Duty Cycle

Load Following Characteristics for Typical Day of Power Operation

opportunity to obtain the needed data value. The WBA-24 blanket rod experiment, consisting of seven wire-wrapped large diameter (0.506") oxide blanket rods of the earlier WBA-21 blanket rod design, was designed to provide information on the behavior of blanket rods and assemblies during load-following operation and overpower transient events and to support the design requirements to address these types of events.

WBA-24 (X363) subassembly was irradiated in the 5B2 position in EBR-II starting in run 112A. At the start of the irradiation, the test experienced a 21% overpower excursion and two transient events. The cyclic portion of the test consisted of 54 transient power cycles in which the power increased from 46% to 121% of the steady-state power experienced prior to run 112. Figure 3-13 schematically shows the transient cycles of the experiment. During the final overpower transient portion of the test, the WBA-24 experiment was relocated to the 3D2 position and operated at 92% of the prior steady-state power for four hours before undergoing one final transient to 151% of the prior power. At discharge, the peak preirradiated rod had accumulated a peak burnup of 3.1% and a peak fluence of $1.07 \times 10^{23} \text{ n/cm}^2$ ($E > 0.1 \text{ Mev}$) and the peak unirradiated rod had accumulated a peak burnup of 0.1% and a peak fluence of $3.0 \times 10^{21} \text{ n/cm}^2$ ($E > 0.1 \text{ Mev}$). Following nondestructive examination of the seven rods, three rods were destructively examined. The results of the examination showed satisfactory performance of the blanket rods. The swelling and mechanical strain of the cladding and the fission gas release behavior of the blanket fuel were evaluated. Particularly noteworthy in this experiment was the melting observed in the preirradiated rods and the occurrence of incipient melting of the blanket fuel in the unirradiated rods [2].

Results of this experiment provided experimental evidence for the viability of the load following concept and for the potential of operation of the CRBRP blanket in the load following mode with upset events. Additional experiments are presently underway in the Operational Reliability Testing (ORT) program under joint U.S. Department of Energy/ Power Reactor and Nuclear Fuel Development Corporation (DOE/PNC) sponsorship.

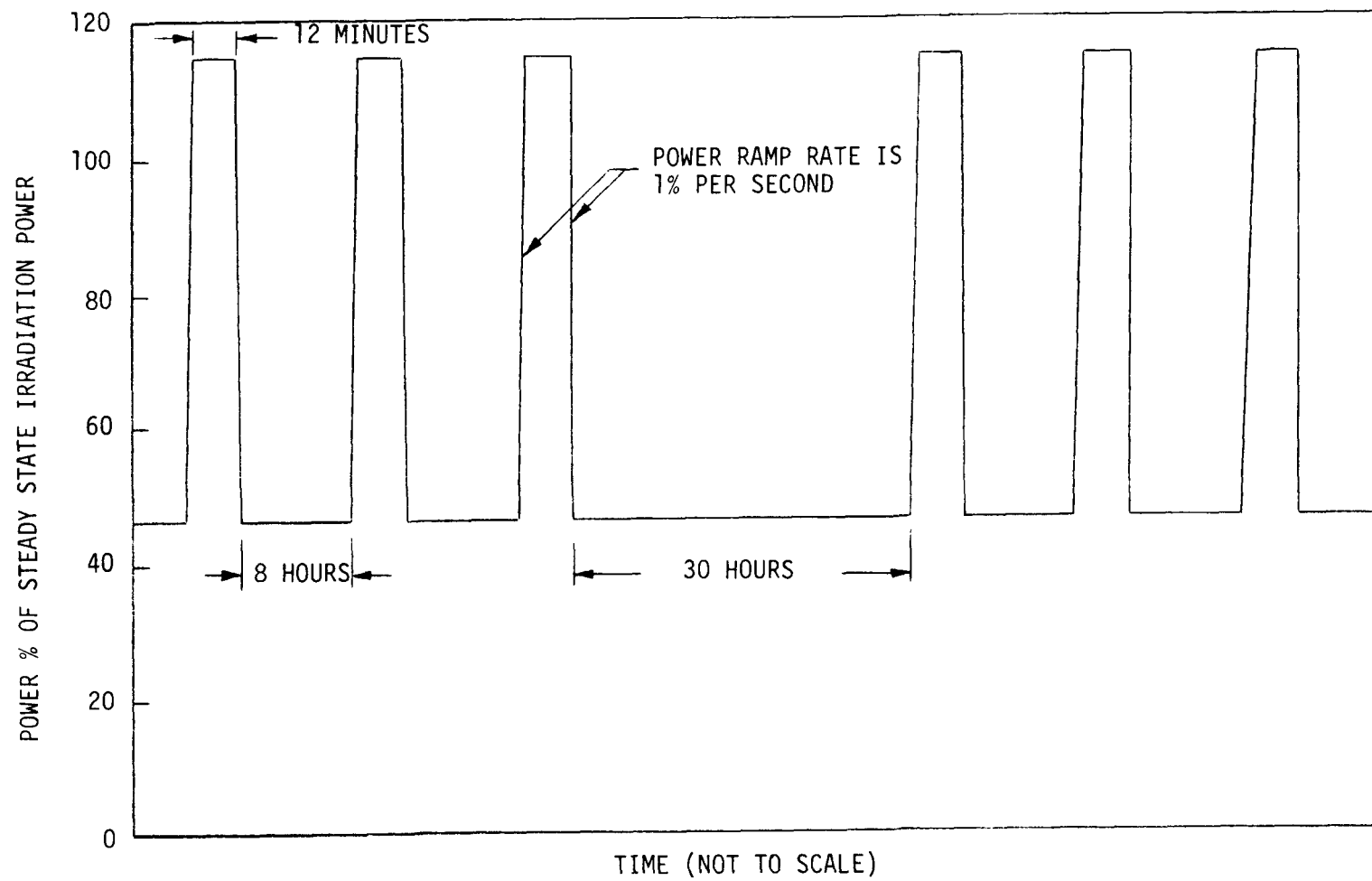


Figure 3-13 Typical EBR-II Transient Cycles During Run 112 After Startup and Hold Time

3.2.7.3 EBR-II Steady-State Irradiations

The principal purpose of a radial blanket in a fast breeder reactor (FBR) is to convert fertile material into fissile material. An efficient blanket will maximize the fissile material production consistent with satisfying the design and operating constraints and providing optimum economy. Radial blankets should be capable of withstanding a wide range of neutron and thermal fluxes and concomitant thermal and mechanical stresses which change very significantly with time. The wide range of operating conditions and differences in design features from those of core fuel assemblies necessitate the experimental verification of satisfactory performance of a radial blanket assembly in prototypic conditions.

A. Seven Prototypic FBR Rod Experiment

The experiment, WBA-20, consisted of seven large diameter (0.520 inch) oxide blanket rods and was the first U.S. experiment designed to evaluate the performance of prototypic FBR blanket rods. This diameter was somewhat larger than the final CRBRP design diameter of 0.506 inch. The diameter evolution was decided upon principally due to concerns over end-of-life power-to-melt and tight rod bundle/duct considerations. The purpose of the test was to generate data to assist in reducing the uncertainties associated with designing blanket rods for long residence times and to evaluate the effects of fuel management schemes on blanket rod behavior. This experiment was mainly to support design of the CRBRP radial blanket.

The WBA-20 (X196) subassembly was irradiated in the 7C6 position in EBR-II starting in run 69B. At the end of run 89, after accumulating 53,225 megawatt-days (MWD) exposure, the subassembly was relocated to the 6B1 position with a 60° rotation, which gave rise to power jumps ranging from 5 to 26% in the blanket rods. The subassembly was irradiated in the latter position for one run, accumulating an additional 2538 MWD exposure and was subsequently discharged for post-irradiation examination. At discharge the peak rod in the

subassembly had accumulated a peak burnup of 1.6% and a peak fluence of $9.9 \times 10^{22} \text{ n/cm}^2$ ($E > 0.1 \text{ MeV}$). Following nondestructive examination of the seven rods, three rods were destructively examined.

The results of the examination [3] showed satisfactory performance of the blanket rods. The swelling, mechanical strain, and fission gas release behavior were evaluated.

Some of the conclusions drawn from the WBA-20 test are:

- o The WBA-20 oxide radial blanket rods successfully operated without breach for ~900 full power days at power levels ranging from 14 to 25 kW/m to a peak fluence of $9.9 \times 10^{22} \text{ n/cm}^2$ ($E > 0.1 \text{ MeV}$) and a peak burnup of 1.6%.
- o The peak total cladding strains ranged from 0.4 to 1.2% change in diameter.
- o Fission gas releases were observed to be low (1-5%).
- o Plutonium content in the blanket pellets increased from 2.5% at the beginning-of-life to 4.2% at the end-of-life due to breeding.
- o End-of-life power jumps of 5-25%, which simulated the effect of a fuel management scheme, resulted in no effect on cladding strain.
- o Features of the profilometry were related to spacer wire-cladding and pellet-cladding interactions. The WBA-20 rods showed evidence of tight wire and tight bundle spacer wire-cladding interactions.

- o Fuel-cladding mechanical interaction occurred in the bottom half of the rod as evidenced by the profilometry data (ridging and mechanical strain).
- o No restructuring of the pellets was observed.
- o Fuel-cladding chemical interaction and actinide redistribution were not observed.

B. Thirteen Prototypic CRBRP Rod Experiment

The experiment, WBA-21, consisting of thirteen large diameter (0.506-inch) oxide blanket rods, was designed to investigate the performance of blanket rods and assemblies at peak power and temperature conditions typical of breeder reactor plant internal blanket assemblies.

Significant differences exist between the design and operating conditions of fuel and blanket rods and assemblies. Longer exposure times and differences in blanket and fuel oxygen to metal ratio and fluence to burnup ratios increase the uncertainties in the prediction of blanket rod behavior based on fuel rod tests. The larger diameter blanket rods are stiffer than fuel rods and operate with large temperature, power and flux gradients across the assemblies. Thus, the reactor environment coupled with the large blanket rod stiffness may introduce larger rod bundle-duct interaction loads than those which occur in fuel assemblies. This potentially-greater interaction is further complicated by the low pitch to diameter ratio of blanket rod bundles. Irradiation testing is needed to demonstrate the satisfactory performance of blanket assemblies at the higher loading conditions. The WBA-21 experiment was designed to provide information on the behavior of blanket rods and assemblies at conditions similar to that of internal blanket assemblies.

The WBA-21 (X321) subassembly was irradiated in the 5D4 position in EBR-II starting in run 94A. The experiment resided in this position to the end of run 109, accumulating 40,653 MWD exposure at peak power levels of 50 kW/m and was subsequently discharged for post-irradiation examination. At discharge the peak rod in the subassembly had accumulated a peak burnup of 3.0% and a peak fluence of $1.0 \times 10^{23} \text{ n/cm}^2$ ($E > 0.1 \text{ MeV}$). Following nondestructive examination of the thirteen rods, two rods were destructively examined.

The results of the examination [4] showed satisfactory performance of the blanket rods. The swelling, mechanical strain, and fission gas release behavior were evaluated.

Conclusions drawn from the WBA-21 experiment are:

- o The WBA-21 oxide blanket rods successfully operated without breach for 674 full-power days at power levels of 50 kW/m to a peak fluence of $1 \times 10^{23} \text{ n/cm}^2$ ($E > 0.1 \text{ MeV}$) and a peak burnup of 3%.
- o The peak cladding strains ranged from 2 to 3% change in diameter with rods nearest the core center exhibiting the higher strains.
- o Fission gas releases were ~60%.
- o Fuel-cladding mechanical interaction occurred along the entire fueled length as evidenced by cladding ridging.
- o The WBA-21 rods showed tight-wire and tight-bundle mechanical interactions.
- o The predominant fuel-cladding chemical interaction was oxidation of the cladding inner surface, resulting from the hyperstoichiometric composition of the fuel.

- o Porosity distribution measurements showed agglomeration of large pores at the central void edge.
- o Significant actinide redistribution was observed in one of the rods.
- o The plutonium content in the peak power region increased from 4.2% at the beginning of life to 5.4% at the end of life.

3.2.7.4 Summary of Irradiation Tests

These irradiations (WBA-24, 20, 21) provided confidence in the new area of blanket rod/assembly design. The results were used as confirmatory information on the performance of CRBRP blankets and to provide input to analytical techniques which describe such things as fission gas release, rod bundle-duct interaction and fuel cladding chemical and mechanical interaction. Additional tests were either underway for the FFTF and are in various stages of preparation. These include the steady-state WBA-40 and 41 tests presently under FFTF irradiation and the WBA-45/46 natural circulation test already designed but now on "hold" with the cancellation of CRBRP.

The examples given are just a few of the experimental program results that contributed to the design and methodology evolution of the blanket assembly. As with the fuel rod, additional changes in the blanket rod analytical methodology were taking place which incorporated the evolved fuel rod methodology given in Section 3.1.7.

3.2.8 Final Status of the Blanket Assembly Design

During the time between the blanket assembly final design review and project termination changes occurred which necessitated reevaluation of the blanket rod and assembly design. These changes involved cladding and duct material specification changes to obtain an improved CW Type 316 SS, methodology improvements and changes in the fuel and blanket rod analytical procedures, and interactions with the NRC and ACRS in the attempt to obtain a construction permit.

It had been planned to review the performance of the blanket rod and assembly prior to release for fabrication. Preparation for such a review had been started with the reactor reorificing effort completed in mid-1983. This reorificing mitigated thermal striping concerns (see Section 3.11, 3.12). Property evaluations for the improved Type 316 SS had almost been completed. New methodology had been used (FPAN) for both fuel and blanket rod analysis in the core reorificing NRC observation that some transient testing of blankets in TREAT should be done had been used to secure a test (EBTB) which was run in early 1984. The evaluation is presently underway. All documentation was completed on specifications, procedures, etc. and the project was to release procurement of the improved Type 316 SS.

The designers were confident that the confirmatory evaluations to be done in the review would give margins which were acceptable based on preliminary analysis. The completion of the analyses remained to be done. Final proof of the design, before CRBRP operation, would be obtained from the WBA-40, 41, 45/46 tests in FFTF.

3.2.9 References

1. "Blanket Heat Transfer Tests Final Report," F. C. Engel and B. Minushkin, WARD-RB-94000-6, September 1982.
2. "Irradiation Behavior of the WBA-24 Oxide Blanket Transient Test," U. P. Nayak and T. P. Soffa, WAESD-RB-94000-7, September 1983.
3. "Performance Evaluation of the WBA-20 Radial Blanket Irradiation Test," U. P. Nayak, T. S. Roth and C. E. Schenone, WARD-RB-94000-4, February 1981.
4. "Irradiation Behavior of the WBA-21 Oxide Blanket Test," U. P. Nayak and T. P. Soffa, WARD-RB-94000-5, November 1982.

3.3 Removable Radial Shield Assembly Design

3.3.1 Summary of Functions and Description

The functional requirements of the Removable Radial Shield Assemblies (RRSA) are given in Table 3-32. These functional requirements are satisfied by design as discussed in the design description.

RRSAs are located around the radial blanket. Their principal function is to provide radial neutron shielding to limit radiation damage to permanent reactor structures. This shielding assures that the fracture toughness and ductility of the permanent reactor structures will not be degraded, by neutron radiation and/or nuclear heating, beyond acceptable limits during the design life of the reactor.

Each RRSA (Figure 3-14) utilizes FFTF fuel duct technology. The reference design uses 19, one-inch diameter, solid, closely-packed, Type 316 Stainless Steel rods to achieve a solid metal volume fraction of 0.81. The hexagonal duct is Type 316 Stainless Steel and both duct and shield rods are 20% cold-worked to minimize the radiation-induced swelling that may occur during the service life of the RRSA. In any selected RRSA location the option is available to replace the central shield rod with a surveillance specimen container fabricated from 1.00-inch O.D. tubing and to accommodate surveillance samples.

The shield rods extend from the elevation of the bottom end of the lower axial blanket of the fuel assembly to the above core load pad (ACLP), which is at the elevation of the top end of the upper axial blanket. An exception to this arrangement is made in 14 RRSA locations in the outer row--seven at each of two locations, 90° and 270° -- where additional shield rods are used above the ACLP in the vicinity of the Core Former Structure Support Ring welds to provide additional shielding.

The RRSAs are supported by and located by the Bypass Flow Modules and some of the Lower Inlet Modules from which they receive ~1.5% of the total reactor coolant flow. Each inlet nozzle receives coolant flow through an axial hole

Table 3-32

CRBRP CORE COMPONENT FUNCTIONAL REQUIREMENTS

<u>Core Component</u>	<u>Functional Requirements</u>
(Removable radial) Shield Assembly	● Provide shielding which, when coupled with other shielding, contributes to the design life specified for other installed reactor structures and vessel ● Provide for transmittal of loads between the core former rings and the core assemblies ● Provide a means for locating surveillance specimens within the shield region and a method for their expedient recovery

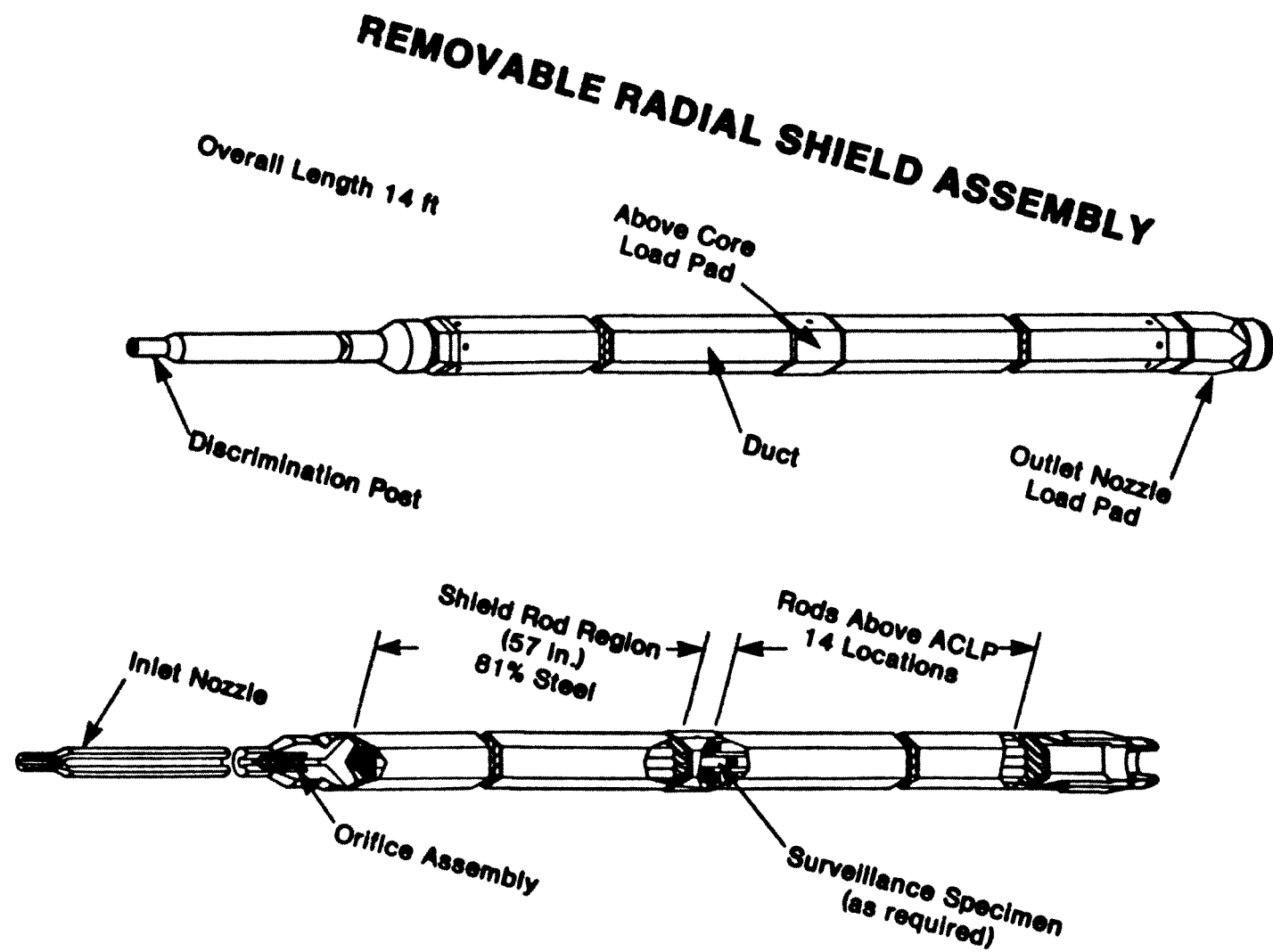


Figure 3-14

and is sealed to its respective module by a piston ring. An orifice assembly above the inlet nozzles provides a coolant pressure drop of approximately 20 psi to preclude potential reverse flow through the RRSAs.

The outlet nozzle-handling socket and the external features of each RRSA contain the same interfacing features required for handling, insertion and removal as the Fuel, Blanket and Control Assemblies.

A unique interfacing feature of the RRSAs is that they are designed to be flexible (0.060-inch duct wall) so that they will be compatible with the operation of the core restraint system. A close-fitting support block is inserted inside the duct at the ACLP to provide axial restraint for the shield rods and to absorb seismic loads that are transmitted through the ACLPs to the Core Former Structure.

The RRSAs are held down by gravity only. About 70% of the RRSAs are provided with secondary holddown by the bottom plate of the Upper Internals Structure.

3.3.2 Removable Radial Shield Design Process

In Section 3.1.2 the core assembly design process was given schematically in Figure 3-3. In that section the iterative design process was described and therefore will not be repeated in this section. The process given is essentially the same for the removable radial shield assembly (RRSA) design.

3.3.3 Design Transients

The U-2b event was initially considered as an umbrella transient event. However, inspection of the DEMO data showed that it could be umbrellaed by the U-18 transient and would not require a separate analysis. The F-4c event had been formerly designated as an emergency event E-4c. It is similar to, but more conservative than the E-16 event which umbrellas the emergency events, and hence the F-4c thermal analysis was used to umbrella the E-4c and other emergency events. Thus, the transient events analyzed and their 30-year frequencies are as follows:

<u>Event</u>	<u>Frequency</u>
U-18	542
U-2b	10
F-4c	7

For a scram type transient like the U-18 and the second portion of the U-2b, the interstitial flows were determined for 100°F, 200°F and 300°F temperature gradients between the core and RRSAs, and then were used during the transient based on the largest temperature difference between the core inlet temperature and the hottest node in the model. This type of modeling resulted in a conservative estimate of the interstitial flows, and therefore, a conservative estimate of the cooling effects during the transient. For a non-scram type transient, like the first 300 seconds of the U-2b (15% overpower), the interstitial flows were also determined conservatively by assuming that the maximum temperature difference between the core and the RRSAs stay constant during the transient. Results of the transient temperature distributions are presented in Reference 1 as plots showing the RRSA duct inside and outside surface temperatures. Further considerations of thermal striping are given in Sections 3.11 and 3.12.

3.3.4 Mechanical Design Limits

The bases, guidelines, criteria and limits for CRBRP core assemblies has previously been given in Section 3.1.5 in Tables 3-16 through 3-18 and discussion will not be repeated in this section. Unlike the fuel and blanket assemblies where detailed analysis of the fuel and blanket rods must and have been done, detailed analysis of the shielding rods of the RRSAs is not necessary. The only loading of the RRSA shielding rods is derived from moderate gamma heating and potential non-damaging swelling. Hence the shielding rods themselves were not analyzed in any detailed manner since it was not necessary.

3.3.5 Results of the Removable Radial Shield Assembly Design Evaluations

The preliminary design of the RRSA was held on January 25, 1977. The final design review was held on December 19, 1979. Pertinent actions from the preliminary design review were incorporated in the final design review preparation. The principal action from the final design review was an action to resolve thermal striping concerns for the outlet nozzle. This action was subsequently followed up by experimental and analytical efforts which mitigated the effects of thermal striping on the outlet nozzle (see Sections 3.11 and 3.12) and answered the concerns. The thermal striping effects completed the outstanding RRSA final design actions in mid-1983 and a release-for-fabrication was obtained from the project office. At the time of the CRBRP termination ~90 RRSA ducts had been fabricated.

Detailed results of the structural analysis of the CRBRP removable radial shield assembly (RRSA) were presented in Reference 2. The report presented a comprehensive structural analysis and evaluation. The analysis was performed by calculating stresses, strains and damage at the duct load pad due to seismic, thermal (steady-state and transient) and core restraint loads. Appropriate computer models and analyses methods were used and the analysis was performed on a "worst case" basis where all the peak loads are assumed to act simultaneously at one single assembly subjected to the most severe environmental conditions. The results of the minimum margins obtained are shown in Table 3-33. In the report the outlet nozzle thermal striping analysis yielded negative results. The thermal striping mitigation of outlet nozzle conditions are reported in Sections 3.11 and 3.12 which showed that outlet nozzle conditions had been found to be acceptable with core reorificing. All other margins of safety were acceptable as shown in Table 3-33.

3.3.6 Design Evolution of the RRSA

The reactor system is designed to provide radiation shielding to insure the structural integrity of the Core Former Structure (CFS), Fixed Radial Shield (FRS), Core Barrel (CB), Fuel Transfer and Storage Assembly (FT&SA) and the Horizontal Baffle (HB). The reactor system design requirements are to provide

Table 3-33

CRBRP REMOVABLE RADIAL SHIELD ASSEMBLY
STRUCTURAL ANALYSIS SUMMARY

<u>ASSEMBLY REGION</u>	<u>MINIMUM MARGIN*</u>	<u>FAILURE MODE</u>
INLET HARDWARE	GREATER THAN FUEL ASSEMBLY	
DUCT		
● LOAD PAD	5.7 0.14	BRITTLE FRACTURE CREEP-FATIGUE DAMAGE
OUTLET NOZZLE	COVERED BY THERMAL STRIPING EVALUATION	

$$*\text{MARGIN (OF SAFETY)} = \frac{\text{ALLOWABLE LIMIT}}{\text{CALCULATED VALUE}} - 1.0$$

adequate shielding to limit the 30 year end-of-life residual elongation to those values shown in Table 3-34. The reactor system radial shield configuration, consisting of removable and fixed shielding, assures that these permanent structures are protected from excessive neutron irradiation damage consistent with achieving their design lifetimes. A schematic of the axial shielding configuration is shown in Figure 3-15.

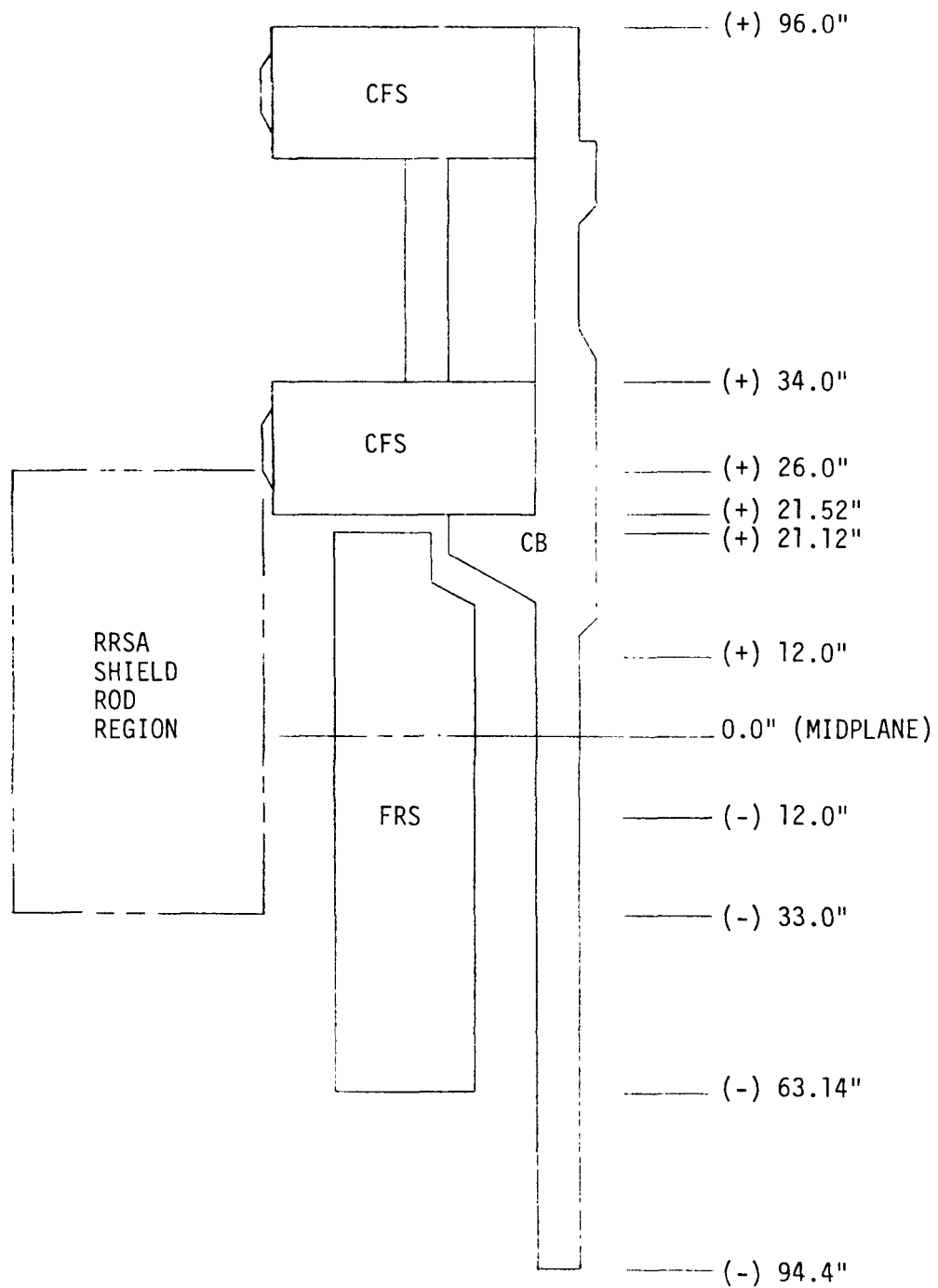
The design evolution of the RRSA was primarily one governed by the use of shielding materials within the assembly in order to provide adequate shielding for the permanent structures. Originally, Inconel was the material selected for the shield rods within the assemblies. This selection was based on obtaining a material of reasonable cost to provide adequate shielding within the confines of the assemblies. As the shielding work and calculations progressed it became evident that less expensive CS Type 316 SS could be used together with smaller diameter filler rods between the 1 inch diameter shield rods. Upon further refined shielding analyses it was possible to remove the interstitial rods and still obtain adequate shielding of the permanent structure which resulted in a stainless steel assembly density of 81%. Thus the evolution of the RRSA shield material and its optimization was the result of the refinement of the shielding calculations and knowledge of the radiation effects on the materials used for shielding. The final configuration and material was an outgrowth of the optimization process.

Comparison of the structural component material fluence and fluence limits [3] showed that the CFS, FRS, CB, FT&SA and HB were adequately protected by the radial shield system, including the RRSA. The removable shielding provided a design margin (fluence limit/fluence) of 2.2 for the CFS (lower core former ring), and a design margin of 1.6 for the FRS (segment at mid-plane and upper retention pin). The RRSA in conjunction with the fixed shielding protected the CB with a margin of ~1.0 at the circumferential weld 8.75" above the core mid-plane with fuel stored in the FT&SA. FT&SA and HB margins are 2.6 and 16.7, respectively. The minimum end-of-life ductilities of the CFS, FRS, CB and FT&SA are 18%, 9%, 10% and 15%. The low fluence level of the HB will result in little noticeable degradation of material ductility. Later results

Table 3-34

REACTOR SYSTEM DESIGN REQUIREMENTS

Structural Component	End-of-Life Minimum Residual Total Elongation	
	Base Metal	Weld Metal
CFS	10%	10%
FRS	5% & 10%	NA
CB	10%	10%
FT&SA	10%	NA
HB	10%	10%



NOTE: Axial Dimensions Are Relative to Reactor Core Midplane

Figure 3-15 Removable and Fixed Radial Shielding Configuration;
Elevation View

which also accounted for helium embrittlement also showed acceptable damage levels, however it was decided to utilize surveillance samples for permanent structures to provide additional assurance of radiation damage.

By axially adding shield material in the outer row of 14 RRSAs, as shown in Figure 3-16 and 3-17, seven at each of two locations, 90° and 270°, above the above core load pad (ACLP) the core former structure (CFS) support ring welds were adequately protected. This axial shielding feature for special location shielding was a novel and unique evolutionary feature.

Provisions had been provided for RRSA material surveillance samples of a sub-size charpy-V notch design and a tensile specimen design (see Figure 3-18). These specimens were to be fabricated from archive duct material and would be used to monitor irradiation degradation in lead assemblies. The lifetime of assemblies were to be determined from this testing. The charpy (C_v) and tensile (T) specimens were to be located within a central tube as shown in Figure 3-19. These specimens were, in turn, to be axially located within the upper end of the core region as shown in Figure 3-20.

3.3.7 Final Status of the Removable Radial Shield Assembly Design

At the time of CRBRP termination the fabrication of RRSA ducts were underway and approximately 90 had been fabricated. All drawings, analyses, drawing and procedures had been completed and approved. The thermal striping redesign and analyses had shown the outlet nozzle concerns had been mitigated by the reorificing, and the nozzle design was acceptable. Provisions had been made for surveillance sample location of permanent structure and RRSA materials to be periodically performed. Maintenance and operating procedures had been written for the assembly. Details of the permanent structure surveillance specimens to be included in the assemblies for various components had yet to be formulated, however, adequate provisions for their incorporation had been made.

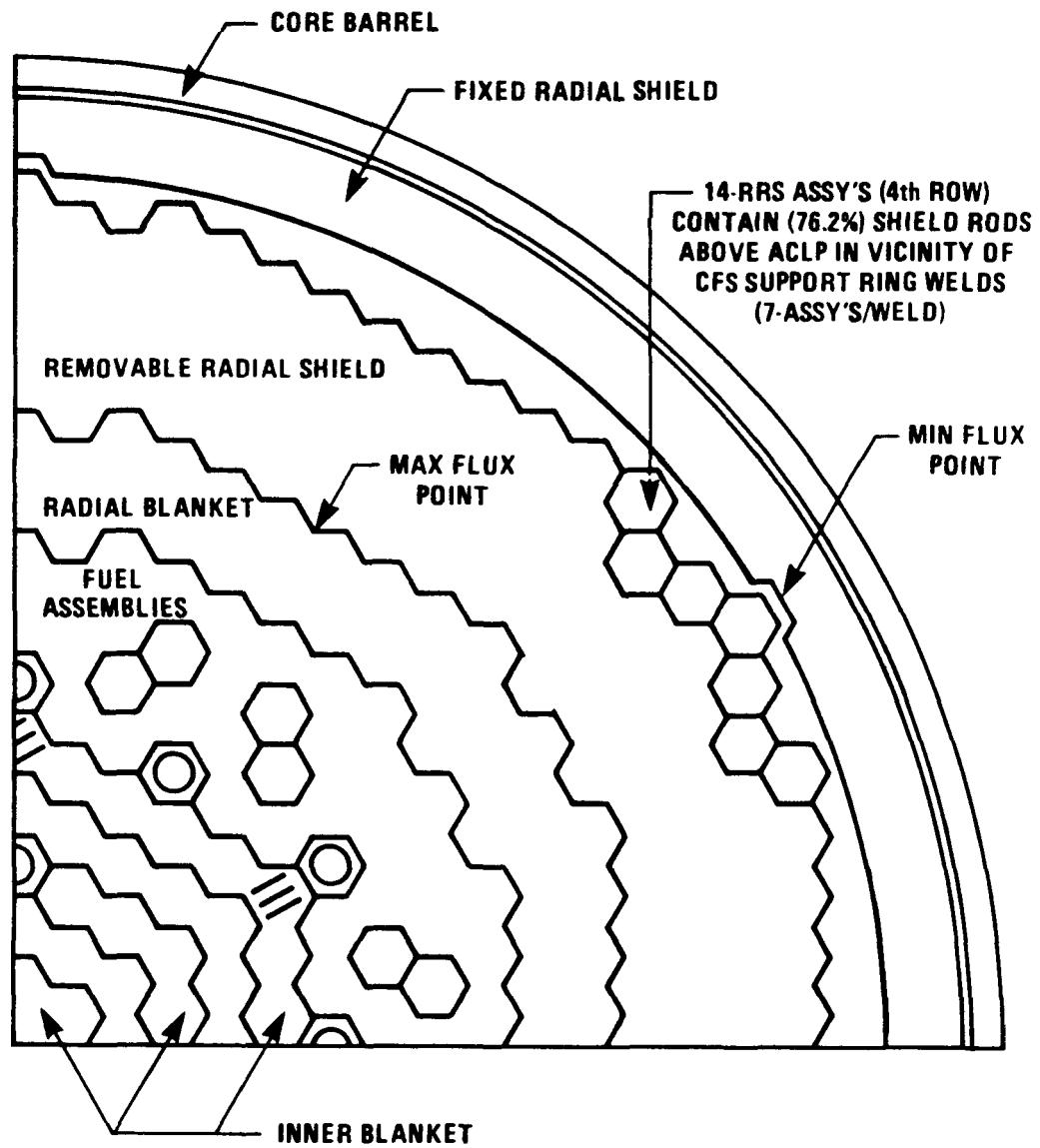


Figure 3-16 Location of Removable Radial Shield Assemblies

14-RRS ASSEMBLIES (ROW FOUR) CONTAIN (76.2%) SHIELD RODS ABOVE THE ACLP IN VICINITY OF CORE FORMER STRUCTURE SUPPORT RING WELDS (7-ASSEMBLIES/WELD).

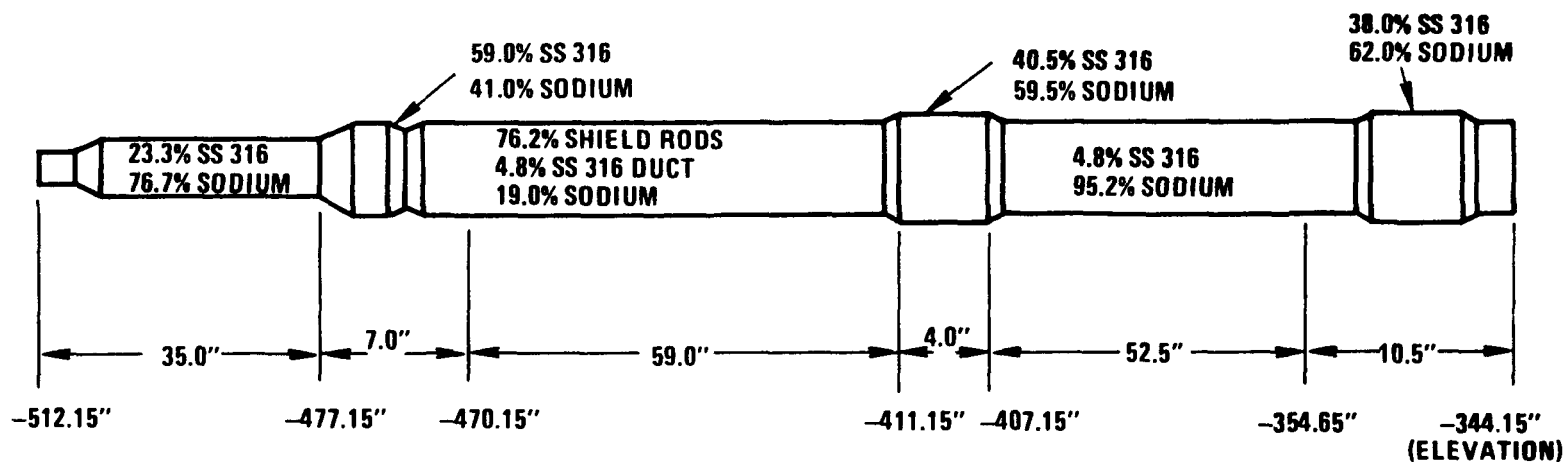


Figure 3-17 RRS Material Configuration



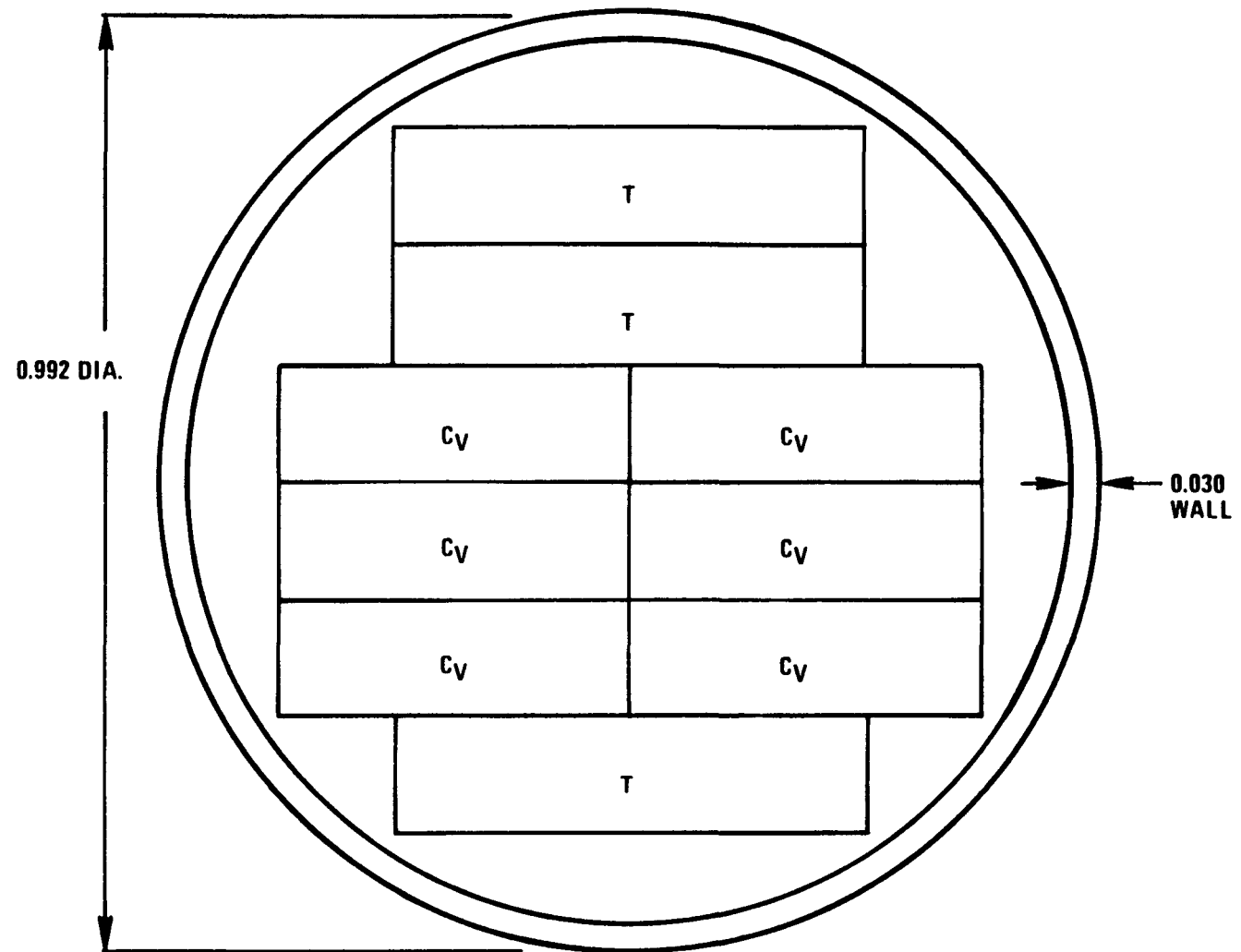


Figure 3-19 Nesting of Tensile and Charpy V Notch Specimens in Tube

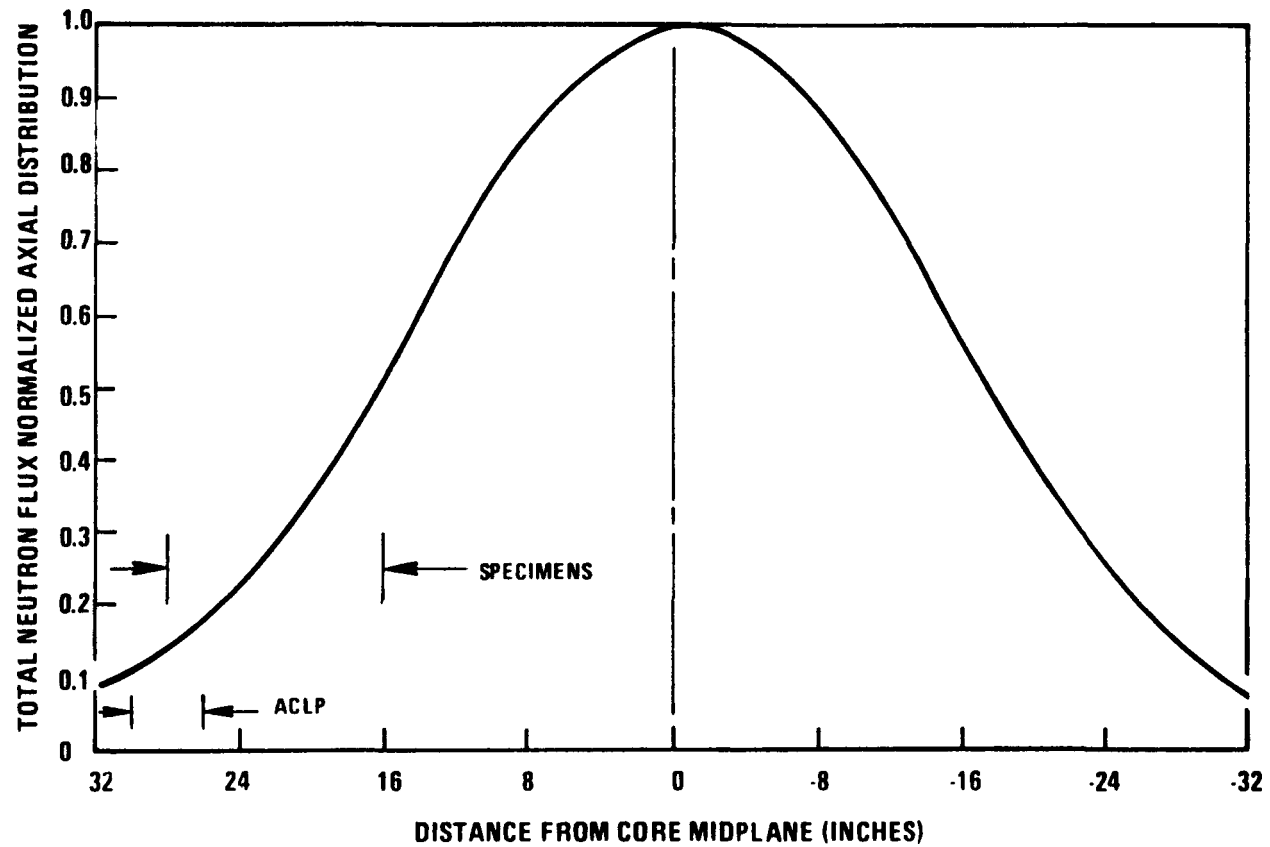


Figure 3-20 Location of Duct Surveillance Specimens

3.3.8 References

1. LRA-79-756, "Thermal-Hydraulic Evaluation of the CRBRP Removable Radial Shield Assemblies for the Final Design Review," A. J. Friedland, K. B. Kittredge, D. Molnar (approved by R. A. Markley), December 1979.
2. LRA-79-773, "Structural Analysis of CRBRP Removable Radial Shield Assembly," V. K. Sazawal, J. A. Belak, and A. D. Sane, December 1979.
3. LRA-79-751, "Radiation Environment and Shielding Performance Information for RRSA Final Design Review," G. W. Wrights, December 5, 1979.

3.4 Heterogeneous/Homogeneous Core Comparison

3.4.1 Introduction

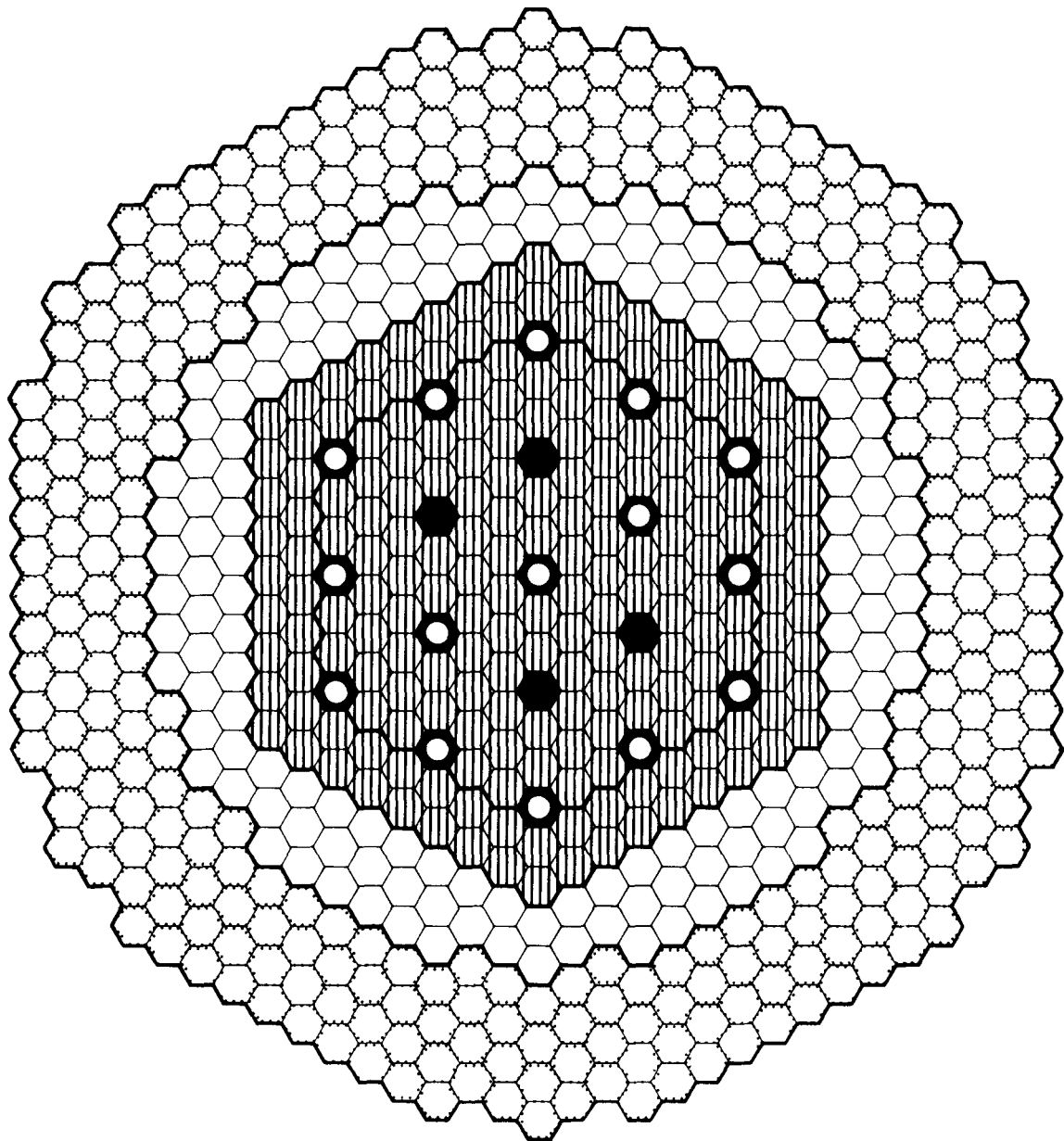
The evolutionary development of the CRBRP radially heterogeneous core design is summarized in the paragraphs that follow. This design was one of the most innovative features of the Clinch River Breeder Reactor and set the stage for worldwide attention and adoption of this concept for the large (commercial size) LMFBR plant designs in the United States because of its superior safety and performance capabilities.

The so-called "homogeneous" core design which was originally developed in the early 1970s for CRBRP is shown in Figure 3-21. The reactor core configuration is relatively conventional with two fuel enrichment zones surrounded by radial and axial blankets. The CRBRP fuel and blanket assembly designs were discussed in Sections 3.1 and 3.2 of this report. The core arrangement consists of 198 fuel assemblies, 108 of which are approximately 17% "enriched" with PuO_2 in UO_2 and located near the center of the core, and the remaining 90 of which are approximately 25% "enriched" with PuO_2 and located around the core periphery to promote a flatter radial power shape.

Fourteen-inch thick axial blankets, containing depleted UO_2 , are located above and below the 36-inch active core. The fuel is surrounded by 150 depleted UO_2 radial blanket assemblies. Fifteen primary control rods (for burnup reactivity control and for primary reactor shutdown) are located at the center of the core and in the 4th and 7th fuel rows. Four secondary control rods (for secondary reactor shutdown only) are located in the 4th fuel row. The fuel management consisted of a scatter replacement of one half of the fuel assemblies annually.

In addition to the usual reactor performance and safety criteria, the CRBRP core nuclear design requirements imposed by OPDD-10 (paragraph 2.1.4) included the following:

"The fuel breeding performance goal for the CRBRP shall be to achieve an overall breeding ratio of 1.2 using mixed oxide (PuO_2 and depleted UO_2) fuel assemblies and UO_2 blankets."



	FUEL ASSEMBLIES	198
	BLANKET ASSEMBLIES	150
	RADIAL SHIELD ASSEMBLIES	324
	PRIMARY CONTROL ASSEMBLIES	15
	SECONDARY CONTROL ASSEMBLIES	4

Figure 3-21 Clinch River Breeder Reactor Homogeneous Core

Furthermore, the Reactor System SDD-31 design criteria (#1.4.1 developed from the original Demonstration Plant Design Guidelines) required that "the design of the CRBRP fuel element and fuel assembly shall take the greatest practical advantage of the FFTF fuel experience." This criteria was widely interpreted to mean that the CRBRP fuel rod OD should be the same as the FFTF value (0.23 inches). The homogeneous CRBRP core satisfied all of the design and safety requirements placed upon it with the exception that the breeding ratio could not be maintained above the 1.2 value under certain conditions. Table 3-35 summarizes the breeding ratio history for the CRBRP homogeneous core for the first and equilibrium cycles using the required FFTF-based fuel assembly design (0.23 inch rod O.D.) which is not optimized for breeding performance.

The CRBRP breeding ratio exceeds the 1.2 design goal in the initial core with LWR plutonium in Table 3-35. However, because of competition from parasitic neutron absorptions in fission products and the higher required equilibrium fissile loadings corresponding to longer burnup intervals, the equilibrium breeding ratio falls below 1.2 even with LWR plutonium. Furthermore, with low-240 grade plutonium* similar to that used to fuel the initial cores of FFTF, the breeding ratio never attained the 1.2 goal. This breeding ratio shortfall is basically the result of a less-than-optimum core heavy metal density (i.e., too small a fuel rod diameter) with the FFTF-based fuel design. A study was performed in 1974 which examined a myriad of ways in which the core breeding performance could be improved. The results of these studies were reported [1 - 3] at the Atlanta ANS Topical Meeting on Advanced Reactors: Physics, Design and Economics in September 1974. Proposed reload fuel assembly modifications were aimed at increasing the fertile material (U^{238}) content of the core, and it was shown that the 1.2 breeding ratio goal could be maintained throughout the equilibrium cycles, even with low-240 plutonium. The difficulty with a reload fuel assembly design which differed from the FFTF-based design was the potential expense of replicating the rather extensive FFTF fuel irradiation test data base.

*Low-240 grade plutonium (~12% Pu^{240}) is obtained from the Hanford-N reactor.

TABLE 3-35
CRBRP BREEDING RATIO
HOMOGENEOUS CORE

	Breeding Ratio	
	<u>Low-240 Pu</u>	<u>LWR Pu</u>
Cycle 1	1.15	1.21
Equilibrium	1.08	1.14

3.4.2 Development of the Radially Heterogeneous Core

At the time when the CRBRP Project was evaluating the ramifications of changing the CRBRP reload fuel design, a paper [4] was published in April 1975 at the European Nuclear Conference in Paris by Mougnoit et. al., proposing the following merits of a radially heterogeneous* LMFBR core configuration:

- o increased breeding ratio
- o decreased doubling time
- o decreased sodium void reactivity coefficient
- o decreased core fluences (increased fuel lifetime)
- o potential for improved (flatter) power distribution using a single fuel enrichment.

The French radially heterogeneous core studies resulted in renewed interest in heterogeneous and modular LMFBR concepts which had been examined [5] in the 1960s by Westinghouse, Allis Chalmers and General Electric and then abandoned in favor of the conventional homogeneous (fuel only) core design. The impetus for this early work with heterogeneous and modular core designs had been their capability for lowering the sodium void reactivity. However, the ramifications of a positive sodium void reactivity, from a commercial licensing standpoint, were not yet clear in the 1960s, and the design teams favored the less-complex homogeneous-fuel configurations. The CRBRP core design team undertook a comprehensive evaluation of the heterogeneous concept

*A radially heterogeneous core differs from a conventional homogeneous core configuration in that blanket assemblies are interspersed with the fuel assemblies throughout the central regions of the core.

in 1975, and developed a radially heterogeneous core design for the Clinch River Breeder Reactor [6] which met all of the design requirements; including a breeding ratio higher than 1.2 in all cycles with low-240 plutonium and FFTF-type fuel assemblies. The heterogeneous core concept had opened up an additional avenue for core design optimization, by way of variations in the number and locations of fuel and blanket assemblies, which allowed an optimum breeding core to be developed using the proven FFTF fuel design. Based upon the preliminary evaluation of the performance of the heterogeneous core in CRBRP, an Engineering Change Proposal (ECP-W0491) was submitted to the Project to initiate the formal process of changing the baseline CRBRP core design from a homogeneous to a heterogeneous configuration.

Because of a combination of circumstances*, the following constraints were placed on the development of the CRBRP heterogeneous core:

- o Overall safety performance must not be degraded (in particular, the core modifications should not jeopardize the Projects' position of less than 660 MW-sec energy release calculated for the worst case Hypothetical Core Disruptive Accident).
- o The mechanical designs of the fuel, blanket and control assemblies should not be changed (with the allowed exception of inlet orificing changes and fuel and control enrichment changes). This was particularly important because of the national investment in the fuels irradiation program and the commitment to the FFTF-based fuel design (0.23 inch rod OD).
- o Blanket assembly shuffling was not allowed (at least in the preliminary design studies) in order not to complicate the core refueling procedures.

*These circumstances included the advanced stage of CRBRP hardware design, the ongoing licensing review by the NRC, and the close ties of the Project with the U.S. fuels irradiation program.

- o Control rod locations in the core could not be changed so as not to impact the reactor head penetrations. However, the relative number of primary and secondary control rods, and their respective locations in the core, could be changed.
- o Fluence limits for permanent reactor components could not be increased.
- o Power output, annual refueling requirements, etc. could not be degraded.
- o Linear power limits of 16 kw/ft (525 w/cm) for fuel and 20kw/ft (655 w/cm) for blankets could not be exceeded at upper 3σ uncertainty limits and 115% of full rated power.
- o Low 240 plutonium should be used throughout the 5-year Demonstration Period to assure that breeding goals could be met with any plutonium composition.

The heterogeneous core configuration which emerged from the preliminary design study, and how that configuration evolved into the reference, optimized CRBRP heterogeneous core, which was formally adopted by the Project, is the subject of the succeeding section of this report. In the following paragraphs, the physics characteristics of this reference heterogeneous core are compared with those of the homogeneous core, and the major advantages of the heterogeneous system, from the CRBRP Project standpoint, are summarized.

3.4.3 Comparison of Heterogeneous and Homogeneous CRBRP Core Characteristics

The reference CRBRP heterogeneous core configuration is shown in Figure 3-22. Reference 7 contains some more detailed discussions of the performance characteristics of the CRBRP heterogeneous core. These include core physics and shielding, thermal/hydraulic, fuel and blanket mechanical design and irradiation testing, core restraint, and safety. The initial core consists of 156 fuel assemblies containing a single fuel enrichment zone, 76 inner blanket assemblies dispersed heterogeneously throughout the central regions of the core, and 126 radial blanket assemblies. The upper and lower axial blankets

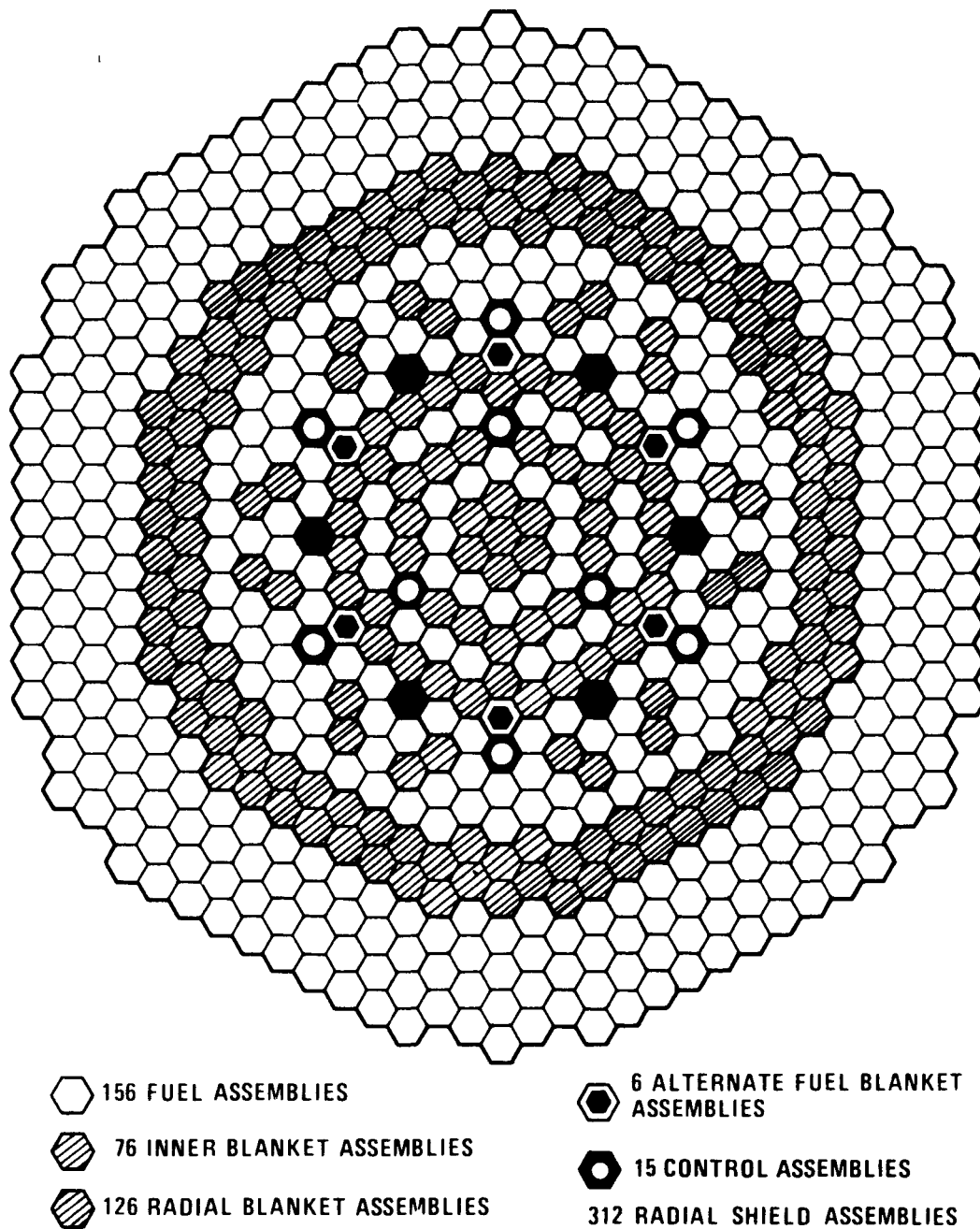


Figure 3-22 Clinch River Breeder Reactor Heterogeneous Core

are each 36 cm (14 in.) thick. Nine fully enriched (92% B¹⁰) primary control assemblies are located in the 6-row 7 corner and 3-row 4 positions. The 6-row 7 corner control rods, operated as a bank, serve to control the reactor throughout each cycle. The remaining primary and secondary control rods are normally fully withdrawn for all full power operations. The total number of primary and secondary control rods (15) is reduced compared to those in the homogeneous core (19), but the heterogeneous core control rods are fully enriched in B¹⁰.

The CRBRP fuel management is a batch refueling scheme with a two-year lifetime for fuel and inner blanket assemblies. Annual refueling is planned for all operating cycles. The first cycle is based on operation at 975 MWt for one calendar year at 35% capacity (128 effective full power days). The capacity factor is increased to 55% (200 efpd) during the second cycle and to 75% (275 efpd) during the third and subsequent cycles. At the end of the first cycle, three inner blanket assemblies in alternating row 6 corner positions are replaced with fresh fuel assemblies for the second burnup cycle. The three refueled assemblies are the same enrichment as the initial load. At the end of the second cycle, all of the fuel and inner blanket assemblies are discharged and replaced with a fresh core. Under equilibrium conditions, the fresh fuel batch is burned for one cycle (275 efpd) at which time six inner blanket assemblies are replaced with six fresh fuel assemblies to supply the additional excess reactivity for the next 275 efpd burnup cycle. The entire fuel and inner blanket batch is replaced at the end of the second cycle, and the scheme is repeated in two-year intervals. The radial blanket assemblies reside in place for four or five years, respectively, in the first and second radial blanket rows. This batch fuel management scheme makes more efficient utilization of the initial core load than does a scatter fuel management which causes the first 1/2-core batch to be discharged at only half burnup. The combination of batch fuel management scheme, and the smaller initial load of fuel assemblies (156) in the heterogeneous core, saves more than 200 fuel assemblies (at the expense of cheaper blanket assemblies) over the 5-year Demonstration Period.

Table 3-36 compares the principal neutronic characteristics of the CRBRP homogeneous and heterogeneous core designs. The heterogeneous core breeding ratio substantially exceeds the homogeneous core value, and it exceeds the 1.2 goal in all cycles (even with the FFTF-based fuel assembly design and low-240 plutonium). This breeding ratio increase is attributable to the higher core-average fertile material (U^{238}) density associated with the presence of the inner blankets in the heterogeneous core. Similar breeding ratio values would be attainable in a homogeneous core configuration with larger fuel rod diameters (higher heavy metal volume fraction), but optimum breeding performance was obtained in the heterogeneous core using the well-tested FFTF fuel design by varying the number of fuel and blanket assemblies in the core, rather than requiring development of a new fuel design. Furthermore, only a single fuel enrichment is required in the heterogeneous core, and the power shape is flattened both radially and azimuthally by arranging the distribution of neutron sources (fissile plutonium, primarily in fuel assemblies) and absorbers (fertile U^{238} , primarily in blankets). The higher core-average U^{238} density, combined with the effect of concentrating the fissile fuel near the lower reactivity worth outer core periphery, caused the required core fissile loading to be somewhat higher in the heterogeneous core than in the homogeneous core. Nevertheless, the compound system doubling time⁽²⁾, which is a measure of the growth rate potential of the fissile fuel inventory, is considerably improved in the heterogeneous core.

The radial power distribution in the heterogeneous core is flatter than that in the two-enrichment-zone homogeneous core due to the flexibility in power shaping afforded by the almost unlimited number of heterogeneous fuel and blanket arrangements. The peak fuel linear power in Table 3-36 is higher for the heterogeneous core. This is not a generic characteristic of fuel/blanket heterogeneity, but rather it reflects an evolutionary increase in the CRBR fuel power-to-melt limit which resulted in a fuel cost savings of several million dollars over the first 5 years of operation by permitting the core to be designed with fewer fuel assemblies (156 in the heterogeneous core compared with 198 in the homogeneous core). The peak (inner) blanket end-of-life power in the heterogeneous core is approximately the same as the peak (radial)

TABLE 3-36
COMPARISON OF THE MAJOR NEUTRONIC CHARACTERISTICS
OF THE CRBRP HOMOGENEOUS AND HETEROGENEOUS CORES

	<u>Homogeneous</u>	<u>Heterogeneous</u>
Breeding Ratio		
Initial Cycle (low-240 Pu)	1.15	1.27
Equilibrium Cycle (low-240 Pu)	1.08	1.22
Initial Cycle (LWR-recycle Pu)	1.21	>1.3
Equilibrium Cycle (LWR-recycle Pu)	1.14	1.28
Fissile Plutonium Loading, Initial Core (kg)	1230	1529
Fuel Enrichment, Initial Core, low-240 Pu (w/o)	0.174 (Inner) 0.252 (Outer)	0.336
Compound System Doubling Time (yrs)		
Low-240 Plutonium	~95	33
LWR-Plutonium	62	28
Power Peaking Factor, Initial Core, Fuel		
Radial	1.34	1.18
Axial	1.24	1.27
Peak Linear Power, $3\sigma + 15\%$ Overpower (kw/ft)		
Fuel	14	16
Blankets	20	20
Peak Fast Flux (n/cm^2 sec)		
Total	7.4×10^{15}	5.4×10^{15}
Fast ($E > 0.1$ MeV)	4.2×10^{15}	3.4×10^{15}

TABLE 3-36 (Continued)
COMPARISON OF THE MAJOR NEUTRONIC CHARACTERISTICS
OF THE CRBRP HOMOGENEOUS AND HETEROGENEOUS CORES

	<u>Homogeneous</u>	<u>Heterogeneous</u>
Peak Fuel Burnup, Initial Core/Equilibrium Core (MWd/kg)	63/104	77/110
Sodium Void Reactivity		
Flowing Sodium in Fuel-plus-Upper Axial Blanket, Fuel/Inner Blanket (\$)		
BOC	+1.98	-0.11/+1.01
EOC	+1.75	+1.12/+1.18
Maximum Positive (Sum of Positive Nodes), Fuel/Inner Blanket (\$)		
BOC	+3.90	+1.51/+1.40
EOC	+4.00	+2.31/+1.64
Doppler Constant, Initial Core ($-Tdk/dT \times 10^4$)		
Fuel	55.9	25.8
Inner Blanket	--	44.0
Radial Blanket	7.0	11.8
<u>Axial Blanket</u>	<u>4.4</u>	<u>2.6</u>
Total	67.3	84.2

blanket end-of-life power in the homogeneous core. Blanket power sharing is higher in the heterogeneous core which allows more efficient utilization of the fuel.

One result of the higher fissile loading in the heterogeneous core is that the neutron flux levels (at full reactor power) are lower than comparable homogeneous core values. The consequent reduction in fast neutron fluence in the heterogeneous core enables the CRBRP fuel lifetime* capability to be increased. Peak fuel burnup in the heterogeneous design is only slightly higher than the homogeneous core values, reflecting the increase in linear power.

Sodium void reactivity is the important driving force for the Hypothetical Core Disruptive Accident (HCDA). The reactivity worth of coolant voiding is calculated from the difference between positive (spectral) components of the neutron balance, associated with the hardening of the neutron energy spectrum in the core when sodium moderation is removed, and negative (leakage) components which occurs predominantly in flux-gradient regions near the core periphery. The placement of fuel assemblies in high leakage regions of the heterogeneous core, and the preferential exclusion of fuel assemblies from the center of the core, results in a substantial reduction in the magnitude of the fuel sodium void reactivity. The kinetics of the void reactivity accident are also considerably different in the heterogeneous and homogeneous cores due to thermal response incoherencies between fuel (with small rod diameter) and blankets (with larger rod diameter). This incoherence results in a delay in voiding the inner blanket assemblies (in the highly positive-reactivity-worth central core regions) during the HCDA sequence. The combination of lower magnitude sodium void reactivity and void incoherence (which reduces the void reactivity ramp rate) was extremely beneficial to the CRBRP Project during the NRC licensing review process in limiting the envelope of predicted energetic

*In CRBRP, the fuel lifetime is fast fluence limited because of the low-swelling (316-SS) cladding and duct material.

consequences for the worst-case HCDA and reducing the sensitivity of this value to methodology and phenomenological uncertainties. It is primarily this generic reduction in sodium void reactivity, and the consequent increased level of inherent reactor safety, which has made the radially-heterogeneous core configuration so attractive for large LMFBR plant designs (where the sodium void reactivity potential for a homogeneous core configuration can approach 9%).

This same placement of fuel near the outer core periphery in the heterogeneous configuration reduces the fast-response fuel Doppler constant relative to the homogeneous core value. The total reactor Doppler constant is somewhat higher (due to the higher core-average U^{238} density) in the heterogeneous core, and the core transient performance is not degraded substantially as a result of this redistribution of Doppler between fuel and blankets.

Other, somewhat smaller, impacts of the adoption of the CRBRP heterogeneous core arrangement on shielding requirements, core internal structures behavior, core radial restraint characteristics, and others are summarized in a paper by P. W. Dickson at the 1977 BNES Conference on Optimisation of Sodium-Cooled Fast Reactors [8] in London.

3.4.4 Confirmatory Critical Experiments

Critical experiments at the Zero Power Plutonium Reactor (ZPPR) at ANL (West) have served as a test of the accuracy of fast reactor design calculational methods and cross section data and as a basis for the development of nuclear design bias factors and uncertainties for application to calculated CRBRP core physics characteristics. The CRBRP Critical Experiments Program was a joint venture of Argonne National Laboratory, Westinghouse Advanced Energy Systems (Advanced Reactors) Division, and General Electric Advanced Reactor Systems Department, under the direction of the U.S. Department of Energy.

The ZPPR-7 experiments [9] in 1976 served as an important proof-of-principle of the predicted physics performance of the CRBRP heterogeneous core. These benchmark experiments were the first such in the world for a CRBRP-type

radially heterogeneous configuration. Key physics integral parameters, such as k_{eff} , control rod subcritical reactivity worth, important isotopic fission and capture rates supporting the prediction of power distribution and breeding ratio, and sodium void reactivity near the center of the core, were measured in ZPPR-7. The Westinghouse analysis of these benchmark measurements is reported in Reference 10. The analyses of the ZPPR-7 critical experiment measurements served to support the development of fuel enrichment specifications and fissile loading requirements, control rod shutdown margin determination, development of power distribution parameters, and prediction of safety and HCDA-related parameters in CRBRP. The principal motivations for the development of heterogeneous core configurations include the higher breeding gain and lower sodium void reactivity. Both of these parameters were examined in the ZPPR critical experiments. The reaction rate ratio that is the largest contributor to the breeding gain is the ratio of U^{238} capture to Pu^{239} fission. This ratio was predicted in the ZPPR-7 heterogeneous core mockup with the same accuracy as in comparable homogeneous core mockups (e.g., ZPPR-4), and the magnitude of the breeding gain was considerably larger, thus validating the breeding improvement of the heterogeneous configuration. The measurements also confirmed the substantial reduction in sodium void reactivity in the heterogeneous configuration. This reduction was a combination of a reduction in the specific worth of sodium ($\text{\$/kg}$) and a reduction in the mass of sodium in the core associated with the reduced number of fuel assemblies in the heterogeneous reactor. The ZPPR mockup of the heterogeneous CRBRP core appeared on the May 1982 cover of Nuclear News under the banner: "Heterogeneous Core for CRBR Tests Well on ZPPR."

3.4.5 Summary

The CRBRP heterogeneous reactor core configuration allowed the 1.2 breeding ratio goal to be met for all cycles using the FFIF-based fuel assembly design and the most conservative low-240 plutonium composition. This increase in breeding gain is a generic characteristics of the heterogeneous configuration. Along with the increase in breeding potential, the required core fissile loading was also increased. For CRBRP, the compound system doubling time was substantially improved. Subsequent studies have shown that comparable system doubling times can be attained in heterogeneous and

homogeneous reactor systems if the fuel design in the homogeneous system is optimized for this performance. However, an advantage of the heterogeneous system is that optimum breeding and doubling time performance can be achieved in a more cost efficient manner, even for a non-optimum, but well proven, fuel assembly and fuel rod design, by varying the number and placement of fuel and blanket assemblies.

The heterogeneous core configuration opens up an additional degree of freedom* for optimizing the radial and azimuthal power distribution in the core. Control rod shutdown reactivity can also be increased by clustering fuel assemblies around control rod positions. Lower peak core fluence values in the heterogeneous configuration are beneficial to longer fuel lifetime in a CRBRP-type environment employing low-swelling cladding and duct materials.

Finally, substantial reductions in the net positive reactivity, associated with voiding fuel assemblies of sodium coolant, greatly reduces the initiating reactivity for Hypothetical Core Disruptive Accidents. For CRBRP in particular, the worst case HCDA could be accommodated in the structural design, even with the larger void reactivity in the homogeneous core. The reduction in sodium void reactivity with the heterogeneous CRBRP core design therefore served to increase the margins to phenomenological and analysis uncertainties. However, for larger (1000 MWe) commercial-size LMFBRs, this generic reduction in heterogeneous core sodium void reactivity makes an enormous difference in predicted HCDA energetics. The licensing advantage of the heterogeneous configuration for 1000 MWe plants has been the principal incentive for adoption of this design configuration for commercial LMFBR plant studies in the United States.

The heterogeneous core configuration was formally accepted as the Reference Design for the Clinch River Breeder Reactor in March 1979.

*That degree of freedom being the number and placement of fuel and blanket assemblies.

3.4.6 References

1. A. R. Buhl, et. al., "Nuclear Design of the Clinch River Breeder Reactor System," in Advanced Reactors: Physics, Design and Economics, J. M. Kallfelz and R. A. Karam, Eds., Pergamon Press (1974).
2. J. A. Lake, et. al., "Breeding Ratio and Doubling Time Characteristics of the Clinch River Breeder Reactor," *ibid.*
3. N. C. Paik, R. A. Doncals and J. A. Lake, "Fuel and Blanket Characteristics of the Clinch River Breeder Reactor," *ibid.*
4. J. C. Mougnoit, et. al., "Breeding Gains of Sodium-Cooled Oxide-Fueled Fast Reactors," Paris Nuclear Conference, ORNL-tr-2994 (1975).
5. P. W. Dickson and R. A. Doncals, "Heterogeneous Core Designs for Liquid Metal Fast Breeder Reactors," pp. 33-91 in Advances in Nuclear Science and Technology, Jeffery Lewins and Martin Becker, Eds., Plenum Press (1980).
6. P. W. Dickson, Jr., and W. H. Arnold, "Design of the Clinch River Breeder Reactor Plant Heterogeneous Core," International Conference on Nuclear Power Experience, Vienna, IAEA-CN-42/277, Conf.-820914-3, International Atomic Energy Agency (1982).
7. "CRBRP Heterogeneous Core Final Report," WARD-D-0182, Rev. 2 (1980).
8. P. W. Dickson, Jr., "Engineering Aspects of Heterogeneous and Homogeneous Reactors," International Conference on Optimisation of Sodium Cooled Fast Reactors, London, pp. 269-78, ISBN-0-7277-00545, British Nuclear Energy Society (1977).
9. M. J. Lineberry, et. al., "Physics Studies of a Heterogeneous Liquid-Metal Fast Breeder Reactor," Nuclear Technology, **44**, pp. 21-43 (1979).
10. R. V. Rittenberger and J. A. Lake, "ZPPR-7 and 8F Cooperative Analysis Program: Critical Experiments," CRBRP-ARD-0237 (1979).

3.5 Assembly Arrangement in the Heterogeneous Core

The heterogeneous arrangement of fuel and blanket assemblies opened a new degree of freedom in core design optimization; one that involves shaping the azimuthal as well as the radial flux and power distribution, and one that changes throughout core life as the core fissile material shifts from the fuel to the blankets. To a large extent some of the early criticism of the heterogeneous core concept, especially by ANL, was based upon conclusions drawn from non-optimum core arrangements. This chapter is devoted to a discussion of the early development of the CRBRP heterogeneous core and the evolutionary process which led to the reference heterogeneous configuration which was finally adopted by the Project.

The ground rules for the evaluation of the heterogeneous concept were outlined in Section 3.4. A variety of core arrangements were explored in order to identify the one which was capable of satisfying the concurrent goals of reducing costs through maximizing fuel burnup capability, achieving an equilibrium breeding ratio of 1.2 with the FFTF-based fuel assembly design and low-240 plutonium, and maintaining or enhancing reactor safety and control rod shutdown margins. Since the number of fuel assemblies in the heterogeneous core (156) had been reduced compared with the homogeneous core (198), thereby increasing the core-average linear power in the fuel, local power peaking was one of the most commonly encountered design limits. Furthermore, control rod worths were found to be sensitive to local core constituents, and the shutdown margin therefore varied from configuration to configuration along with the radial and azimuthal flux shape. The core design optimization took advantage of this control rod worth variation.

The fuel management scheme in the CRBRP homogeneous core involved a more or less conventional scatter replacement of one half of the core annually. The burnup cycle lengths in the Demonstration Period varied from 128 effective full power days (EFPD) to 274 EFPD (75% capacity factor) under equilibrium-cycle conditions. Discharging one half of the first core fuel assemblies after the initial 128 EFPD cycle was recognized as an inefficient utilization of the fuel's burnup capability. Therefore, for the heterogeneous

core, it was decided to evaluate a batch fuel management scheme which avoided the premature half-core discharge. In the batch fuel management scheme, the entire core is burned as a batch and discharged together at the end of its design life, and a totally fresh core (fuel and blankets) is loaded. In a batch fuel management, there is no convenient way to add excess reactivity if it is needed during the cycle. Even with the substantial increase in heterogeneous core breeding gain, the burnup reactivity decrement for 2×274 EFPD equilibrium cycles was too large to load the entire amount of excess reactivity at start of cycle for a straight-through burn. This would have compromised the available control rod shutdown margin.* A less costly alternative to adding more control rods or shortening the burnup cycle is to replace a few (6 in this case) partly burned blankets with fuel assemblies midway in the 2×274 EFPD equilibrium burnup cycle. In this case, shutdowns are still scheduled annually, but only 6 core assemblies (excluding control rod replacements as required) are replaced every other year, and the entire core batch is refueled in alternate years. The blanket/fuel substitution is characterized by the removal of a low reactivity worth absorber (blanket) and insertion of a positive reactivity worth fuel, adding approximately 10% to the excess reactivity inventory. The resulting sawtooth excess reactivity history therefore has maxima which are constrained within available control rod shutdown margins. The advantage of this batch scheme is that all fuel batches, including the initial core, are efficiently utilized to goal burnup. This was the fuel management mode selected for the CRBRP heterogeneous core.

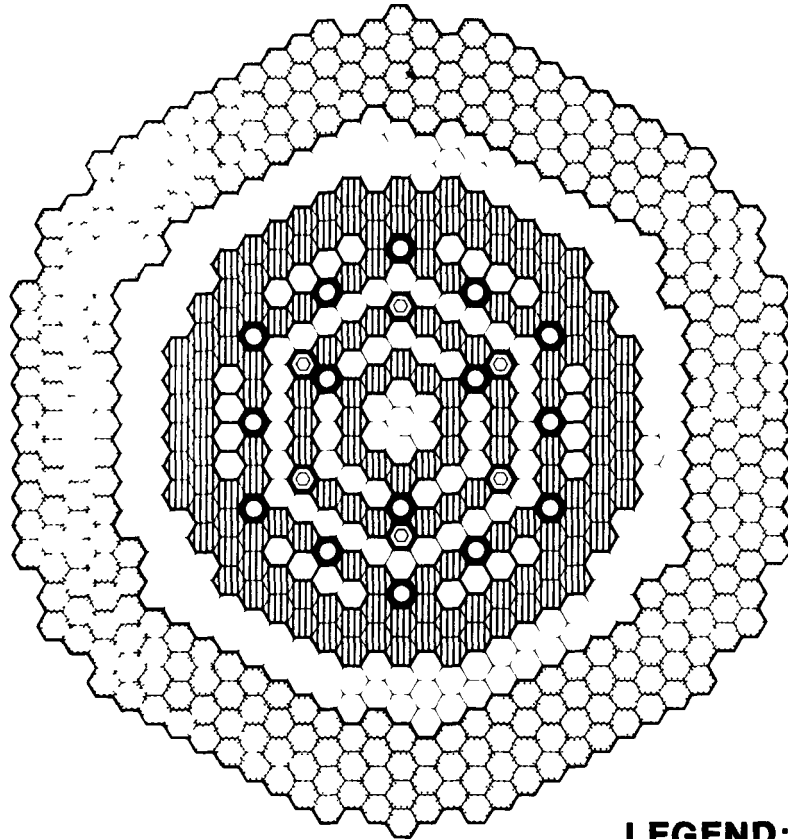
A nearly optimum configuration was developed which met most of the design requirements. This design contained 168 fuel assemblies and 72 inner blankets arranged in alternating rings. Six secondary control rods were located in the 4th assembly row (as in the homogeneous core) and 7 primary control rods were located at the core center and in the 7th assembly row. The high breeding gain in the inner blankets in this design caused a substantial number of these assemblies to exceed linear power limits at the end of 2×274 EFPD equilibrium cycles. Options to alleviate this problem included reducing the

*More primary control assemblies could have been added for start of cycle excess reactivity suppression, but the remaining available core locations were low reactivity worth positions.

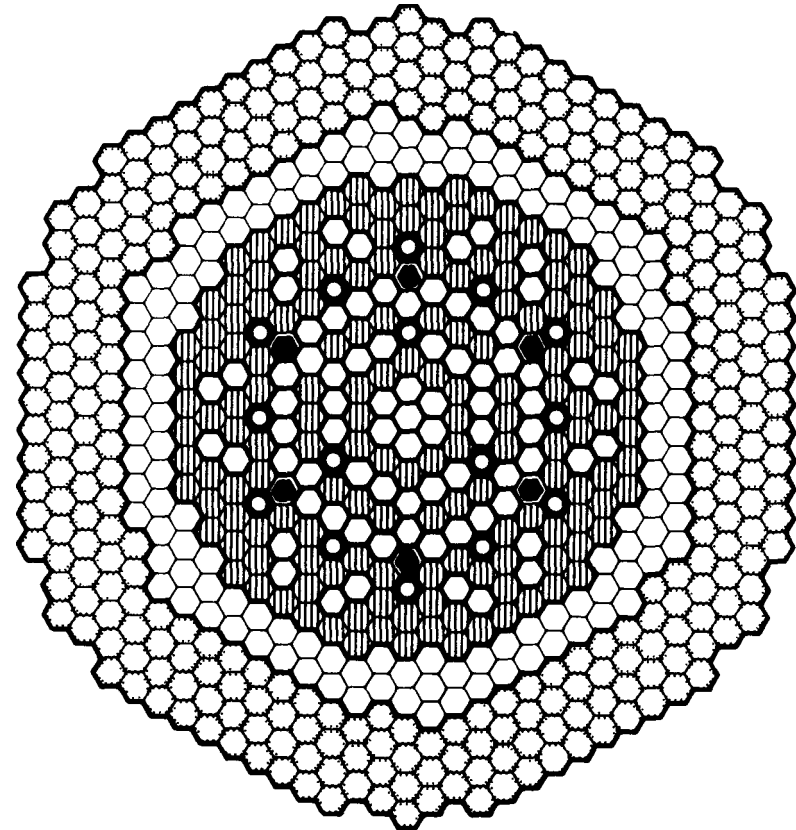
blanket assembly lifetime to one year (too expensive), shortening the equilibrium burnup interval to something like 3/4 of a year (violates annual refueling requirements and compromises maximum utilization of fuel burnup capability), development of a 91-pin reload blanket assembly design to replace the initial-core 61 pin design (expensive), or lowering the central core flux levels to reduce the blanket breeding and thereby reduce the end of life blanket plutonium-fission power. The latter was accomplished by further substituting blanket assemblies for fuel in the core interior and shifting the flux peak further from the core center and out toward the periphery. The depression in the core interior flux levels at beginning of life caused the reactivity worth of the secondary control rods to be reduced and they were therefore moved from the 4th to the 7th assembly row. The so called Alternative Fuel Management Scheme was the outcome of this conceptual heterogeneous core evaluation, and this configuration was selected for more detailed design studies and for experimental verification at the Zero Power Plutonium Reactor (ZPPR) critical mockup.

The core configuration for the CRBRP Alternative Fuel Management Scheme (AFMS as it was called at the time) is pictured in Figure 3-23. The AFMS consists of 156 fuel assemblies with a single plutonium enrichment, and 76 inner and 132 radial blanket assemblies. Six locations in the 5th assembly row contain blanket or fuel assemblies for mid-term excess reactivity makeup in alternating odd or even numbered cycles, respectively. The fuel management is the 2-year batch scheme described in Chapter 3.4. The radial blanket assemblies are burned in place for 4 (inner row) or 5 (outer row) years. Blanket assembly shuffling may have been capable of extending the blanket assembly lifetime, but it was not considered for this study. Most of the control rods were preferentially located near the flux peak in the 7th assembly row. No central control rod was used since its reactivity worth would have been very low in the central blanket. Only 3 (primary) control rods are located in the 4th assembly row and these are normally fully withdrawn for power operation. The reactivity worth of the row 4 control rods is relatively low in the depressed start of cycle fluxes, although these fluxes steadily increase (along with the row 4 control worth) throughout the burnup cycle as plutonium is bred in the inner blanket regions.

AFMS



AFMS-M



LEGEND:

-  **FUEL ASSEMBLIES**
-  **BLANKET ASSEMBLIES**
-  **ALTERNATE FUEL BLANKET ASSEMBLIES**
-  **RADIAL SHIELD ASSEMBLIES**
-  **CONTROL ASSEMBLIES**

Figure 3-23 Evolution of the CRBRP Heterogeneous Core Arrangement

The AFMS configuration was the one that was modeled and successfully tested in the ZPPR-7A heterogeneous core proof-of-principle critical experiments at ANL in 1976. There was, however, a potential problem remaining with AFMS as configured in Figure 3-23. The replacement of radial blanket assemblies at the end of cycles 4 and 5 increased the burnup reactivity deficit. This deficit had to be covered by the excess reactivity requirements for the 3rd core (cycle 5) fuel enrichments. This same excess reactivity must be suppressed at the start of cycle by the primary control rod system. In AFMS, the primary control rod shutdown margin was not large enough to assure this shutdown capability with adequate allowance for uncertainties. Therefore, the required excess reactivity could not be loaded in the fuel enrichments to guarantee the equilibrium cycle burnup without compromising control rod shutdown margin. Several potential improvements in core performance could have been expected to take place by the 5th operating cycle. These included reductions in control rod worth uncertainties and/or comparable reductions in criticality and burnup reactivity uncertainties, as well as the presumed availability of LWR reprocessed plutonium fuel. Several combinations of these would have allowed the core to be operated at full capacity with all uncertainties covered and without compromising shutdown margin. However, a much more assured option, that of enhancing control rod shutdown reactivity worth, was explored. This examination led to a modification of the AFMS heterogeneous configuration (called AFMS-M) which is also pictured in Figure 3-23. The modification consisted of moving the alternating blanket/fuel (refueling) location from the 5th to the 6th assembly row and opening up the partial blanket ring in row 8 around the secondary control rods. This seemingly minor change, and the resulting radial and azimuthal flux redistribution, had a dramatic effect on the CRBRP control rod worths.

Table 3-37 compares several key core physics parameters for AFMS and AFMS-M. The primary control system reactivity worth increased somewhat (primarily because of an increase in the worth of the 3 control rods in the 4th assembly row), and the secondary control rod reactivity worth increased substantially. The later secondary control rod worth change is associated with concentrating more fuel assemblies around the row 7 location in AFMS-M. The combination of higher primary and secondary control rod reactivity worths, and a smaller burnup reactivity decrement, allowed the AFMS-M equilibrium burnup reactivity

TABLE 3-37
COMPARISON OF MAJOR CRBRP NUCLEAR DESIGN PARAMETERS*
IN AFMS AND AFMS-M CONFIGURATIONS

	<u>AFMS</u>	<u>AFMS-Modified</u>
Control System Reactivity Worth (%ΔK)		
Primary (3R4 + 6R7C)	6.58	7.29
Secondary (6R7F)	2.65	4.16
Burnup Reactivity, 2 Equilibrium Cycles(%ΔK)	5.22	4.66
Breeding Ratio		
First Core (Cycles 1 & 2)	1.26	1.29
Equilibrium	1.21	1.24
Equilibrium Plutonium Enrichment (w/o)	33.4	32.9
Peak Linear Power (kw/ft)		
Fuel (beginning of life)	15.9	16.0
Blanket (end of life)	19.7	20.0

*Calculated with consistent design methods and cross-section data for the sake of comparison. As such, the AFMS-M data is not exactly the same as the latest, final design CRBRP data.

to be guaranteed, with uncertainties included, for all cycles (including cycle 5). This same change in core configuration also brought about small improvements in core breeding performance and core fissile loading requirement, compared with the AFMS. Neither configuration was predicted to exceed peak fuel or blanket linear power limits.

The AFMS-M control rod reactivity worth enhancement, utilizing azimuthal as well as radial shaping of the real and adjoint fluxes, illustrates an important aspect of heterogeneous core optimization which is not present in the "equivalent homogeneous core." The more optimum AFMS-M configuration meets or exceeds all the design requirements placed upon it, and it is the one that was formally adopted by the Project as the reference configuration for the CRBRP core design.

3.6 Flux Tilt Considerations

3.6.1 Introduction

This chapter discusses the impact of local power (flux) tilt effects associated with single control assembly withdrawal events. Design Duty Cycle 'Upset Event' U-2b in OPDD-10 and certain events in the PSAR Accident Analysis involve accidental withdrawal of one control rod at full power. Early analyses of these events were based on the core average power response. However, the withdrawal of a single control assembly causes an asymmetric redistribution of the neutron flux which leads to local power peaking in fuel assemblies near the withdrawing rod. When these flux tilt considerations were included in the analysis, it was found that acceptable fuel design limits for these events were exceeded, and the Plant Control System (PCS) design did not therefore fully satisfy the criteria.

A task force was formed to examine this problem and make recommendations for its solution. A design change to the Plant Control System which reduces the likelihood of a single rod event was subsequently incorporated. Recommendations were made for modifying the definition of the U-2b event and for reclassifying the single rod withdrawal. The design change, along with the recommended classification changes, effectively resolved the problem.

The analysis which was performed in support of these actions provides a significant contribution to the safe operation of all fast reactors. The problems and their solutions that have been identified and the subsequent reactor control system modifications should have a positive impact on the licensability of LMFBRs.

A description of the supporting analysis, the PCS design change, the justification for the recommended reclassifications, and the current status of the situation are given in the following sections.

3.6.2 Analysis of Events of Interest

Considerable analysis was performed to characterize the flux tilt phenomenon associated with rod withdrawal. In particular, two specific cases of single control assembly withdrawal events were analyzed. In the first case, it was assumed that the rod withdrawal continues unmitigated until it is terminated by scram and there is no control system action to compensate the reactivity addition during the transient. The event was studied parametrically to examine the clad/fuel temperature responses, and fuel failure for the worst combination of assumptions was analyzed. These results are documented in Section 3.6.2.1 of this report.

In the second case, it was assumed that the rod withdrawal continues until fully withdrawn. In addition, it was also assumed that the reactivity addition caused by the withdrawing rod was continually being compensated for by the action of the control system. A worst-case initial condition was used for this study, and the attendant fuel failure was estimated. In conjunction with this case, a cursory study was undertaken to examine the realism of the assumption that the control system as designed would act to compensate continually the reactivity addition caused by the withdrawing rod. The results of this study are provided in Section 3.6.2.2 of this report.

3.6.2.1 Assessment of Single Control Assembly Withdrawal at Power - Uncompensated Case

Nuclear calculations were performed to determine the magnitude of power perturbations caused by an uncompensated control rod runout at beginning of life (BOC1) and end of life (EOC4) in CRBRP. The rod runout power perturbations were determined from a sequence of full-core, two dimensional power distribution calculations. In the unperturbed calculation, the row seven corner primary control rod bank was symmetrically inserted (18 inches in the BOC1 calculation and approximately 8 inches in the EOC4 calculation). The perturbed calculation has one row seven corner rod partially withdrawn from the average bank insertion by an amount which is equivalent to 15¢ in reactivity, with the other five row seven corner rods remaining at the unperturbed bank position.

Figure 3-24 shows the percent change in 36-inch integrated beginning-of-life power by triangular mesh interval in the fuel, inner and radial blankets associated with a 15¢ rod runout (~1.1 inches above 18 inches-inserted bank position) in the sector of the core containing the runout rod. The corresponding power perturbations in the remainder of the core are either near zero or negative. Figure 3-25 shows the power perturbations (percent change in 36-inch integrated power) for the same 15¢ rod runout (~2.1 inches above 8 inches-inserted bank position) at the end of cycle four. The data in Figures 3-24 and 3-25 represent radial power perturbations and are intended to be superimposed on the normal rod powers associated with a rod runout reactivity addition. An additional power perturbation of +0.5% (per 15¢ reactivity insertion) should be included in the axial power shape (axial peak to average) for the assemblies directly adjacent to the runout control rod. For transient reactivity insertions larger than 15¢, it should be assumed that the power shape perturbations in Figures 3-24 and 3-25 (plus the axial peak to average perturbation) scale linearly with rod-runout reactivity. This scaling is valid only over the range of a few inches.

In order to assess the uncertainty in the calculated rod runout power perturbation, a measurement was performed in the CRBRP Engineering Mockup Critical (ZPPR-11). The unperturbed power distribution was determined with the reactor critical with the six row seven corner control rod bank half inserted. Another irradiation was then performed with one row seven corner control rod withdrawn 6 inches from the half-inserted bank. An evaluation of this experiment indicates that it is not necessary to apply any additional uncertainty to the tilted power above that already applied to the normal steady state power distribution.

The calculated power tilts as shown in Figures 3-24 and 3-25 are used for the core thermal and hydraulic analyses described in the following paragraphs. These paragraphs discuss the fuel assembly cladding and fuel temperature response to the uncompensated control rod withdrawal event. FORE-2M [1] code predictions are presented which show the effect of flux tilt perturbations in the core. It should be noted at this point that the reference analytical technique used for the following results is based on a modified point-kinetics approach using the FORE-2M code. This analysis technique employs point-kinetic

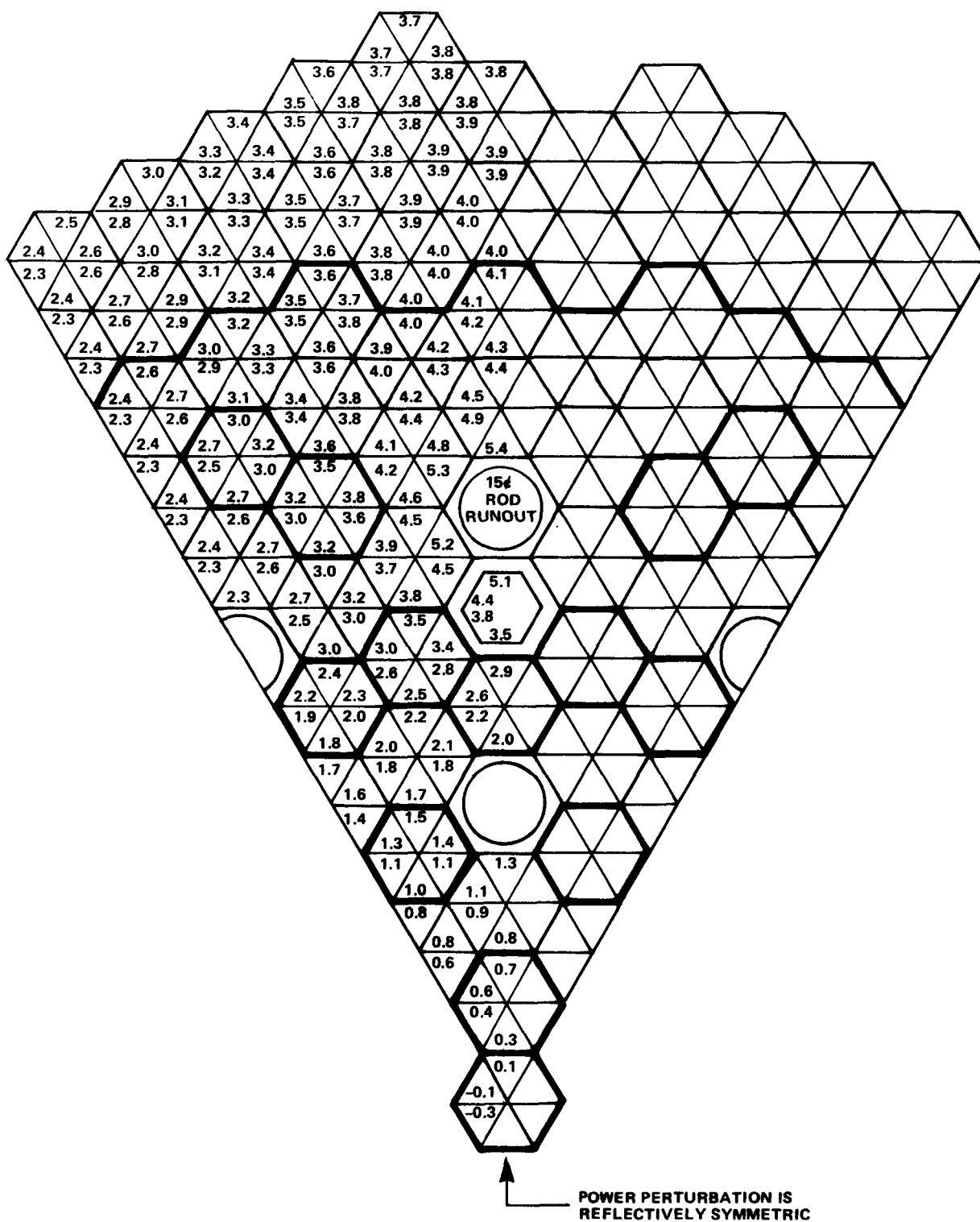


Figure 3-24 Percent Increase in 36-Inch Integrated Power Shape Due to 15¢ Uncompensated R7C Control Rod Runout (~1.1 Inches From Half Inserted). CRBRP (4), Beginning of Cycle One

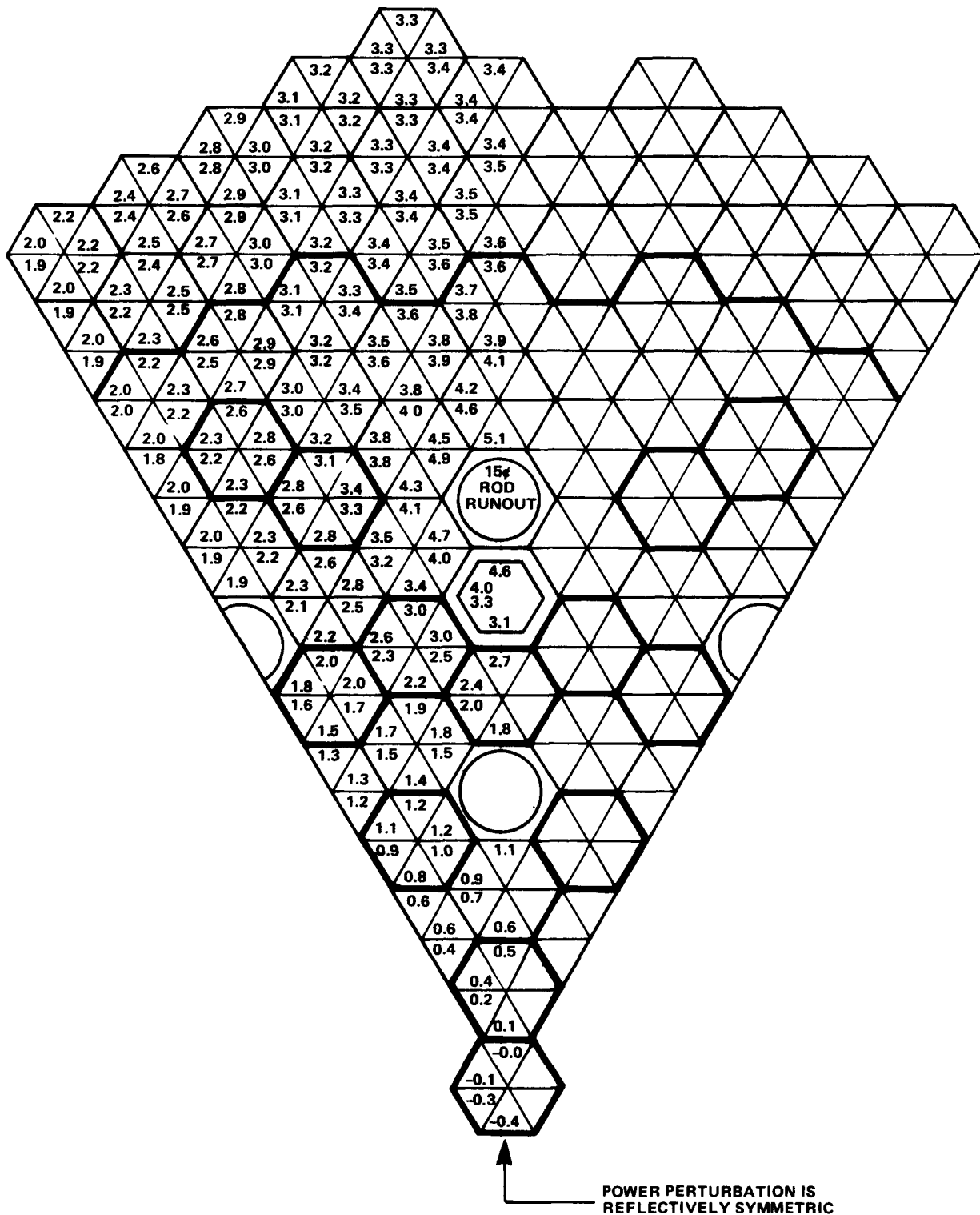


Figure 3-25 Percent Increase in 36-Inch Integrated Power Shape Due to 15¢ Uncompensated R7C Control Rod Runout (~2.1 Inches From 8 Inches Inserted). CRBRP (4), End of Cycle Four

calculations to determine the effect of the transient on fuel, inner blanket and radial blanket hot rod maximum cladding and maximum pellet temperatures. The localized power peaking due to the flux tilting, based on steady-state nuclear analyses, is linearly combined with the increase in average reactor power. This modified point-kinetics technique has been verified by FX2-TH [2] space-time kinetics calculations.

Figure 3-26 shows the power variation used in the studies for the flux tilt versus non-tilt cases. The localized power increase (over reactor average power) due to tilting is represented as δ_p . Values of 0, 3, 6 and 9 percent were studied parametrically for δ_p . Various trip times, τ_T , were also investigated as indicated on the figure. The rate of average reactor power rise for any case in %/sec is $15/\tau_T$. Results for the hottest cladding temperature in fuel assembly FA #52 are given in Figures 3-27 and 3-28 for cladding and fuel temperatures, respectively. These figures provide the increase in temperature above steady state as a function τ_T . The formation indicates that flux tilt effects are attenuated for ramp power increases greater than 3%/second. This would encompass reactivity rates greater than 3 to 6¢/sec, depending on assumptions used in the nuclear kinetics.

Parametric studies were not performed for the blanket rods. These type rods would show an ever larger attenuation of the flux tilt effect for larger rates of power increase due to their significantly larger heat capacity.

Assuming approximately 0.5% core power rise for each 1¢ reactivity insertion, to reach 15% overpower will thus require a reactivity insertion of about 30¢. Using worst case δ_p values from Figures 3-24 and 3-25 for highest fuel temperature, the results shown in Table 3-38 were obtained for long τ_T cases where maximum temperatures are attained.

An evaluation of the fuel rod damage due to localized power tilting effects was performed. A conclusion from this study is that, for power tilting of 6.1% (i.e., for $\delta_p \sim 3.05\%$ in Figures 3-24 and 3-25), the calculated Cladding Damage Function (CDF) in the worst fuel rods is equivalent to the design limit of 1.0. Using the calculated power tilting results as

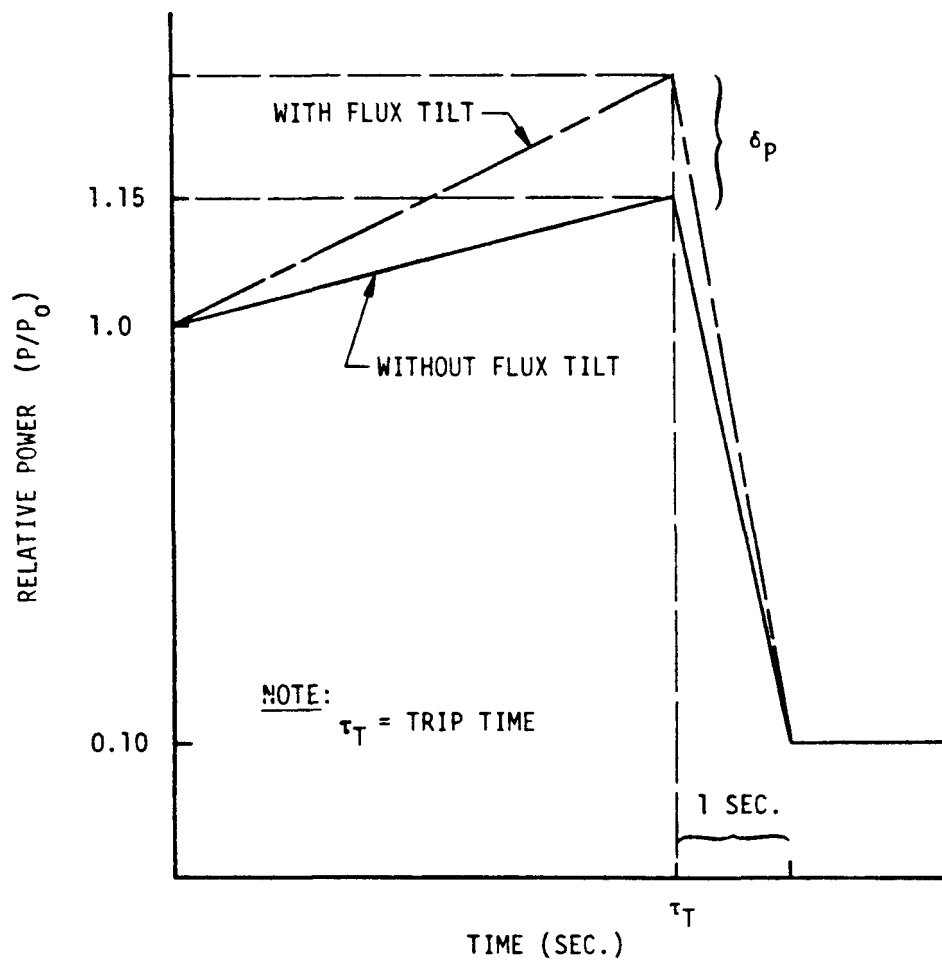


Figure 3-26 RELATIVE POWER VARIATION FOR FUEL HOT ROD CASES WITH AND WITHOUT FLUX TILT EFFECT

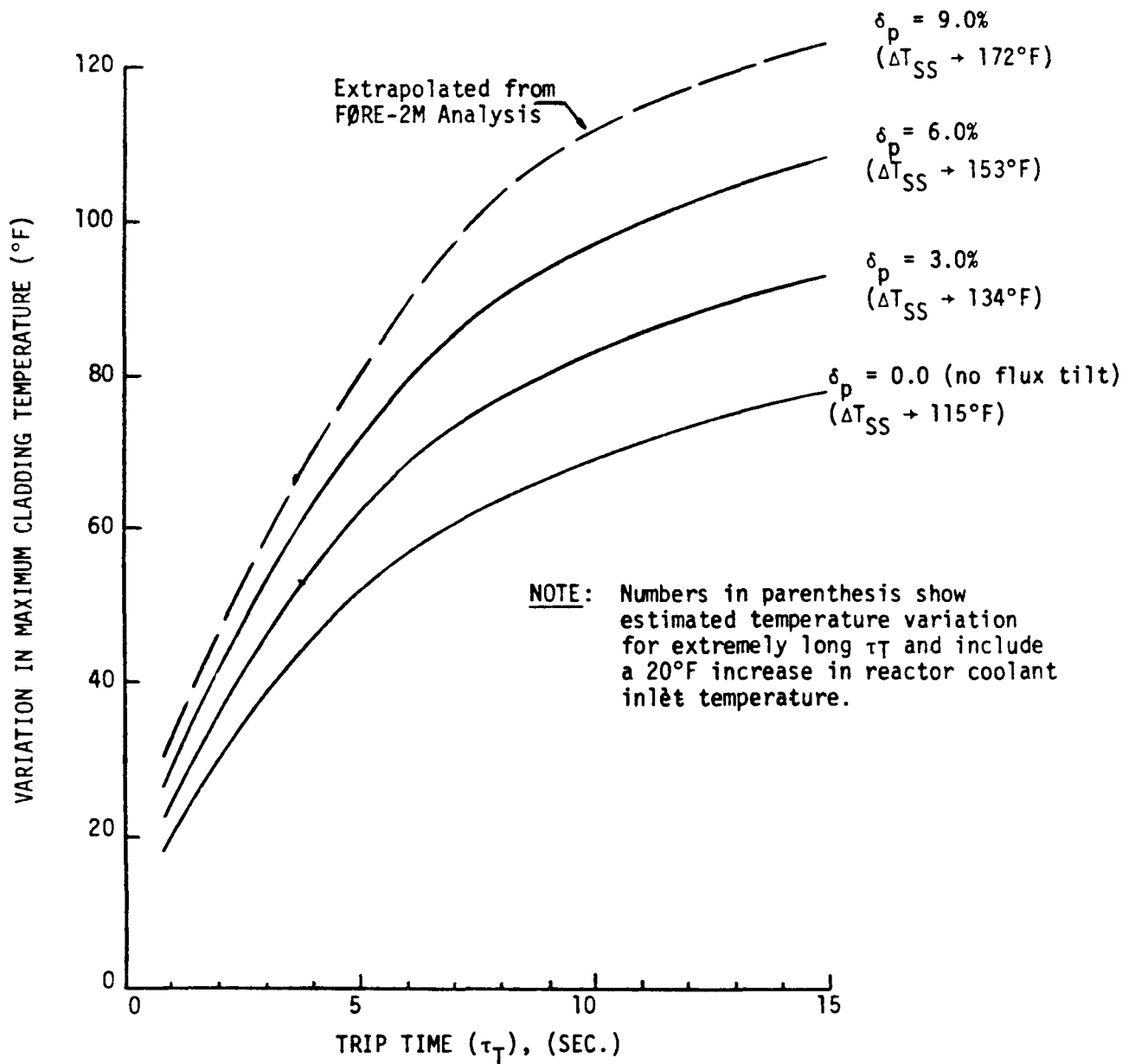


Figure 3-27 EFFECT ON FUEL HOT ROD MAXIMUM CLADDING TEMPERATURE OF POWER RISE RATE WITH AND WITHOUT FLUX TILT EFFECT

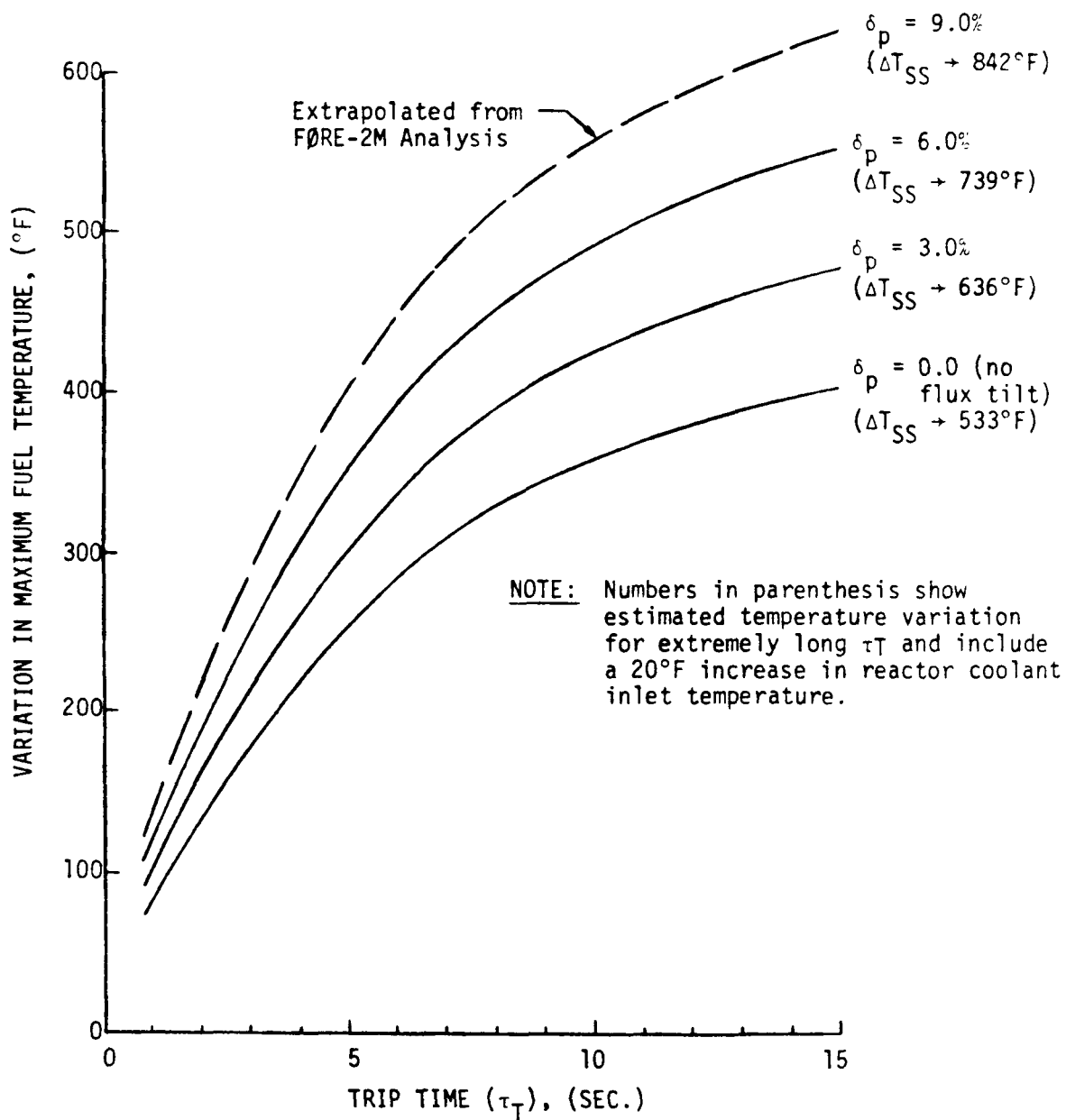


Figure 3-28 EFFECT ON FUEL HOT ROD MAXIMUM FUEL TEMPERATURE OF POWER RISE RATE WITH AND WITHOUT FLUX TILT EFFECT

TABLE 3-38
HOT ROD PARAMETERS FOR UNCOMPENSATED SINGLE
ROD WITHDRAWAL AT 15% OVERPOWER
WITH LONG TRIP LINE

<u>Assembly</u>	<u>δp (%)</u>	<u>Hot Rod Maximum Cladding Temperature (°F)</u>	<u>Hot Rod Molten Fuel Fraction (a) (%)</u>
FA #101	5.4	1510	8
FA #52 (b)	8.6	1561	0
IB #99	6.0	1461 (c)	16
RB #201	7.2	1400 (c)	14

- (a) At center of fuel pellet.
- (b) Data from Figures 3-27 and 3-28 used in CDF evaluations. This is slightly less conservative since Figure 3-24 data is multiplicative rather than additive (i.e., 9% effect instead of 10% perturbation was considered).
- (c) Temperature prior to scram; after scram maximum cladding temperature would be significantly higher (by ~200°F).

described above, the estimated CDF value would exceed 1.0 in the worst fuel rods. This does not necessarily mean that rod failure is guaranteed to occur, rather it indicates rod failure can not be precluded on a design basis. A conservative estimate of the total number of rods with a CDF value exceeding 1.0 is approximately 3200. (This estimate does not include effects of molten fuel on fuel-clad differential expansion, therefore the above number of rods with a CDF exceeding 1.0 could be somewhat under-estimated.) A histogram of these calculated CDF values (number of fuel rods vs. % CDF over 1.0) is shown in Figure 3-29. The corresponding CDF cumulative distribution is shown in Figure 3-30.

3.6.2.2 Assessment of Single Control Assembly Withdrawal at Power - Compensated Case

Nuclear calculations were performed to determine the beginning-of-life power perturbation caused by a row seven corner control rod withdrawal with the consequent reactivity insertion being compensated by the moving-in of the other five row seven corner rods in the bank. Analysis was performed for a worst-case configuration with a rod runout of approximately 22 inches from a position near the maximum, equilibrium insertion to fully withdrawn, and reactivity compensation by insertion of the remaining five row seven corner rods from the approximately 22 inches-inserted position to fully inserted (a net movement of ~14 inches). The final tilted configuration has one row seven corner rod fully withdrawn and five row seven corner rods fully inserted, compared to the reference symmetric configuration with the six row seven corner rods symmetrically inserted at 22 inches (from the top of the core). The local power in the sector of the core containing the runout rod is severely increased, whereas the other core sectors where the remainder of the control bank is inserted are decreased, the combination of which results in a very substantial power skew. Local power increases exceed a factor of 2.7 (ratio of perturbed power density to reference power density) and corresponding power decreases on the opposite side of the core are of the order of 0.4.

To consider the power tilt effect for the compensated control rod runout case, a parametric study of molten fuel fraction (at mid plane of active core) versus overpower level was performed using the FORE-2M code. The results are

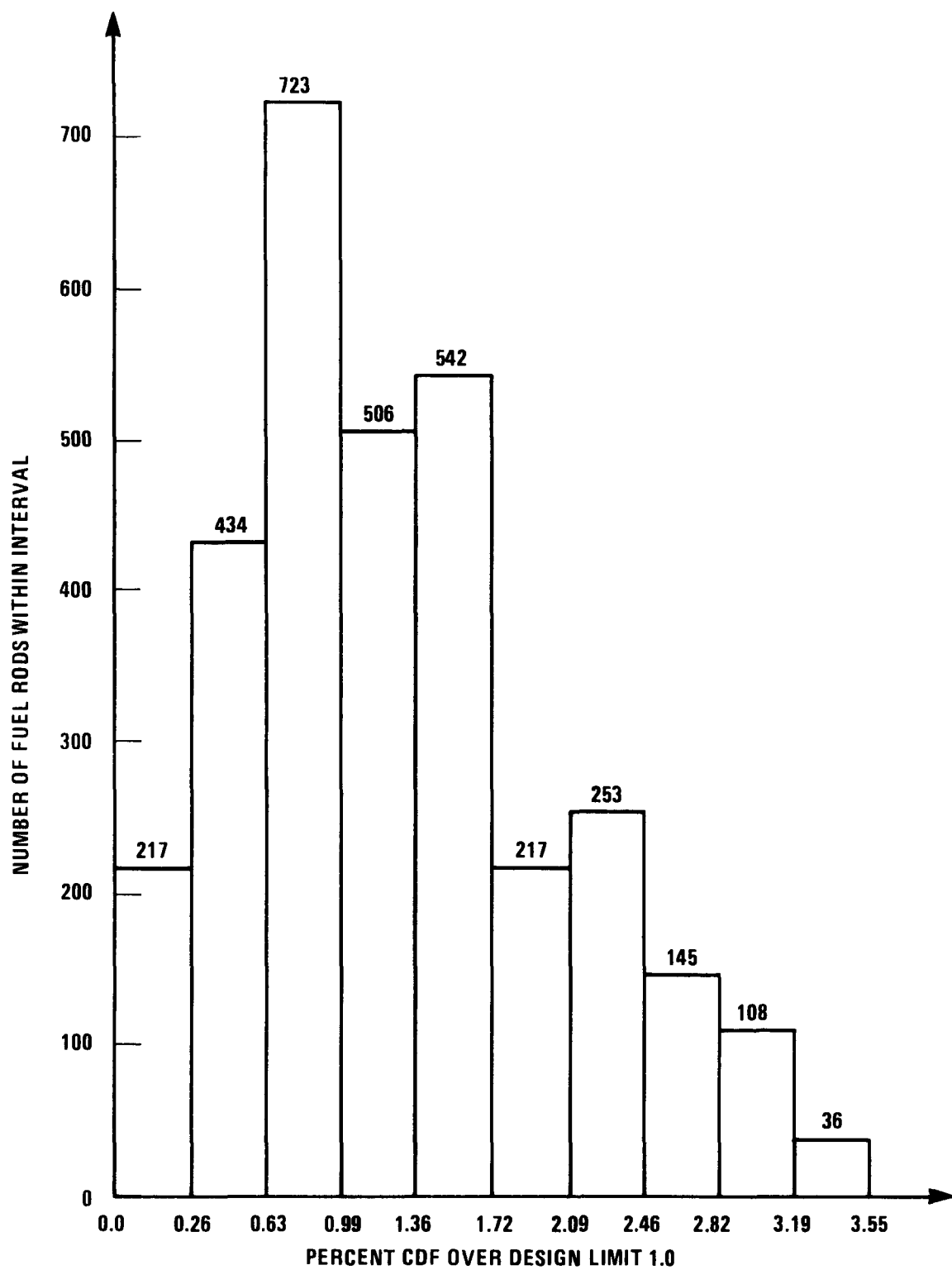


Figure 3-29 CDF Histogram for a Single Control Assembly Withdrawal Event

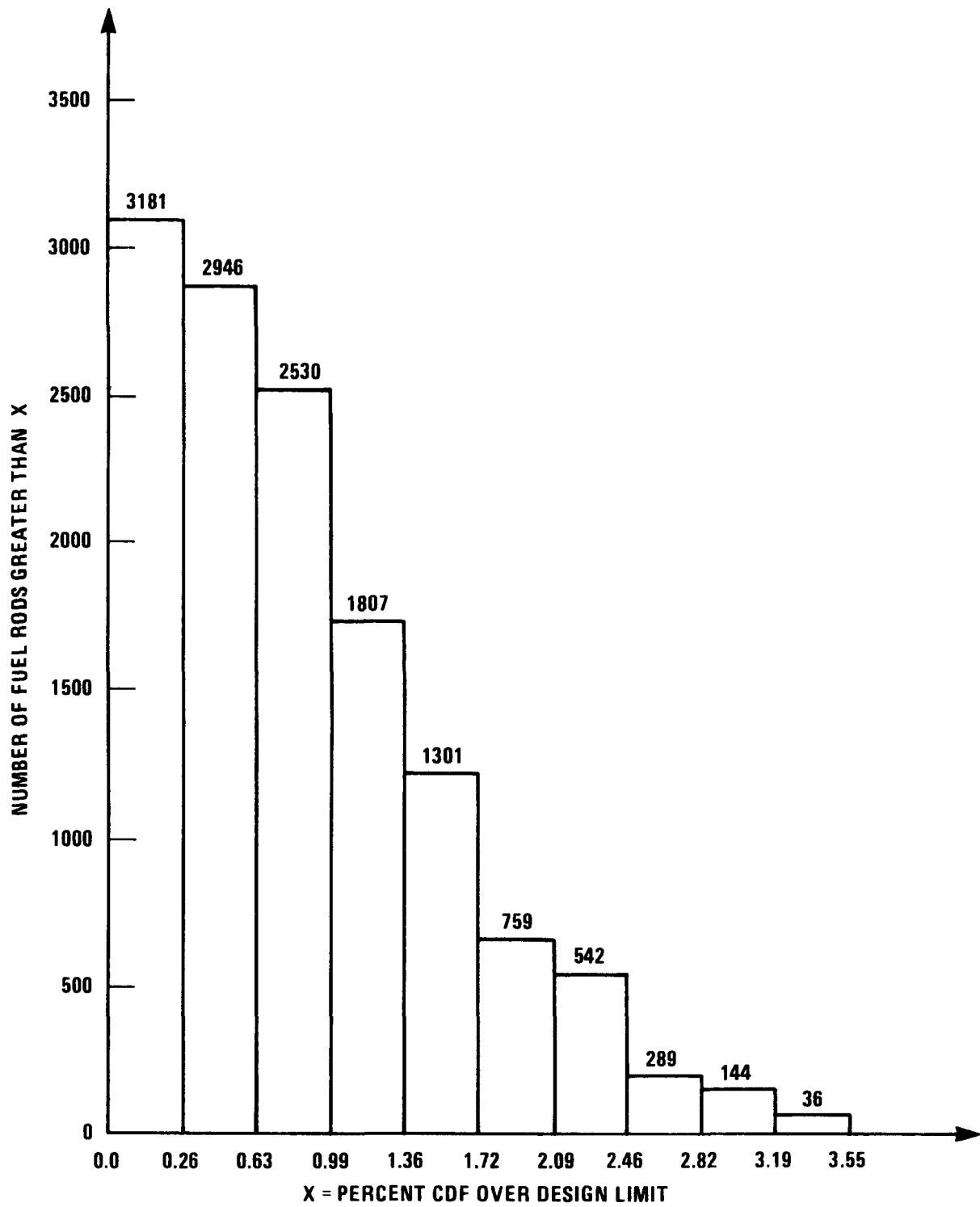


Figure 3-30 CDF Cumulative Distribution for a Single Control Assembly Withdrawal Event

shown in Figure 3-31 for the hot fuel rod in FA #101. Using the power tilt values as described above, the maximum power increase for this assembly would be approximately 187%. From Figure 3-31, it can be seen that this would result in approximately 55% molten fuel. Many other rods would experience this amount of molten fuel or more. An estimate indicates that approximately 6000 rods in the core would contain molten fuel. Of these rods, approximately 4000 would have cladding temperatures in excess of 1700°F. At these temperatures, rod failure has a high probability of occurring and it is anticipated that extended core damage (even beyond the ~4000 fuel rods mentioned above) could result.

3.6.3 Design and Safety Implications

Justifications for the recommendation on the inclusion of events in the design evaluation and the classification of events are largely based on LWR precedents. The following paragraphs provide a brief summary of a review of the LWR practice, industry standards, NRC requirements, and the CRBRP design and status.

LWR experience forms the primary basis for event classification. Boiling Water Reactor (BWR) designs include the consideration of an Uncontrolled Withdrawal of a Single Control Rod Assembly as a "Plant Condition II" event (the LWR equivalent to the "Anticipated Event" or "Upset Event"). Pressurized Water Reactors (PWRs) consider the postulated Uncontrolled Control Rod Bank Withdrawal as a "Plant Condition II" event and the Single Control Rod Assembly Withdrawal as a "Plant Condition III" event (the LWR equivalent to the "Unlikely Event" or "Emergency Event"). The reason for this difference between the BWR and PWR is that, in the case of PWRs, no single malfunction can result in the withdrawal of a single control rod assembly. It should also be noted that withdrawal of a single control rod assembly results in both a positive reactivity insertion and an increase in local power density in the area of the withdrawn control rod assembly. The total effect is considered in the analysis of the event. Classification of the events is largely based on engineering judgment. No probabilistic assessment is made and used as the basis.

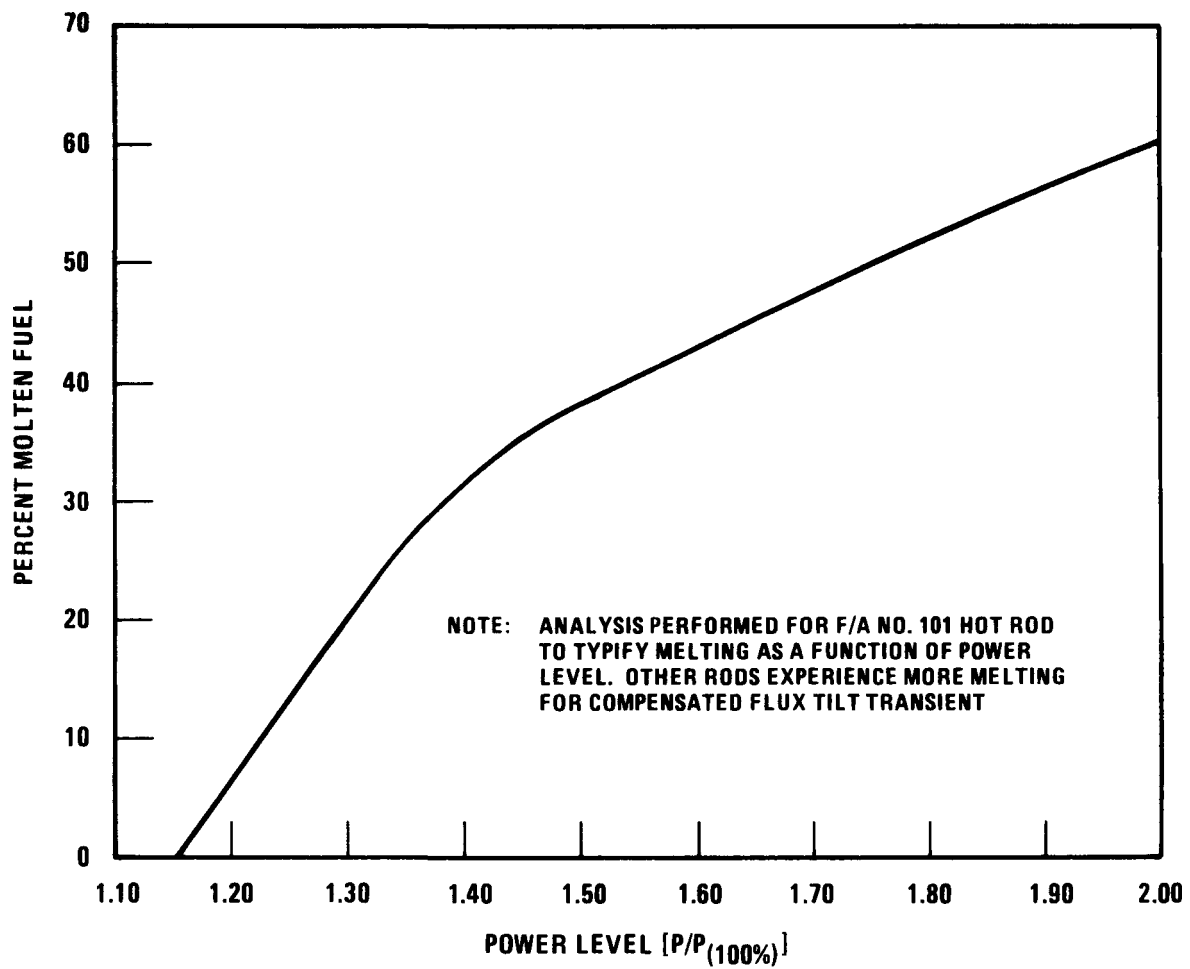


Figure 3-31 Percent Molten Fuel Versus Power Level to Typify Molten Fuel Increase With Increasing Power

Current ANS standards are basically consistent with the LWR practice as described above. This includes ANSI-N18.2 (for PWRs) and ANSI/ANS-52.1 (for BWRs). Although some standards development work has been underway for some time to develop a basis for relating events classification to probability numbers, insofar as formal ANS standards are concerned, no such correlations exist. In other words, events classification is largely based on engineering judgment. The only exception is that 10^{-7} is considered as the (annual) probability dividing the 'design base' and 'beyond design base' in the design consideration of safety systems (ANSI/ANS-4.1, 1978).

The NRC Standard Review Plan (SRP) requires consideration of uncontrolled single control rod withdrawal for BWRs. For PWRs, rod bank withdrawal and single rod withdrawal, respectively, are required to be considered. SRP is silent on events classification. Implicitly, however, SRP requires that an uncontrolled or erroneous withdrawal of a single control rod under any conditions of reactor operation should not result in exceeding the "specified acceptable fuel design limits". Otherwise, a Failure Modes and Effects Analysis (FMEA) must be provided to demonstrate that a single failure in the control system, or an operator error, will not cause an uncontrolled or erroneous withdrawal of a single control rod.

As currently defined in OPDD-10, the uncontrolled withdrawal of a single control rod at 100% power is an "Upset Event" or "Anticipated Event" designated as U-2b. The specified fuel design limit for this type of event requires that there be no reduction of effective fuel lifetime or fuel power capability. As shown in Section 3.6.2, this fuel design limit is exceeded when flux tilt considerations are applied. An examination of the likelihood of a single rod withdrawal event disclosed that with the original Plant Control System design a single component failure could indeed cause accidental movement of a single rod. Therefore the Plant Control System design did not meet the NRC requirements described above, and a design change was necessary. This design change, the Rod Misalignment Block System (described in Section 3.6.4), was proposed in ECP-W1663 [3] and was subsequently approved [4] and incorporated into the design.

3.6.4 Design Change for Resolution of Problem

As described in Section 3.6.3, the fact that a single component failure could lead to a single rod withdrawal event necessitated a change in the Plant Control System design to preclude this. The possible changes can be characterized as falling within two categories; prevention and mitigation. Prevention is defined as upgrading the present control rod system design to eliminate potential failure modes which could cause rod withdrawal type events. This upgrading could involve adding equipment redundancy to the design, provision of test features, and the use of higher reliability parts. Mitigation is defined as detecting that a failure has occurred and blocking the movement of the malfunctioning rod and/or rod control system. Detection could be based on rod position indicators or comparison of control rod response to input commands.

At the time of the proposed design change, the control rod system was in an advanced stage of fabrication. Consequently, any potential change had to be evaluated in terms of its impact on the existing fabrication contract. Apart from a requirement for periodic testing of control rod error detection and equipment blocking, implementing the prevention option would have significantly impacted the vendor contract. The fabrication would have to be stopped and an extensive reevaluation of the design made. Retrofitting or redesign would have been required. The mitigation option, however, could be pursued with minimal impact on the existing contract. Detection and blocking logic could be built into a system relatively independent of the existing equipment. Because of the lesser impact, this was chosen as the preferred option.

A schematic diagram of the Rod Misalignment Rod Block System is shown in Figure 3-32. This system stops all motion of the 6 rods comprising the operating bank when a single operating bank rod is misaligned more than 0.4 inches above the average position of all 6 operating bank rods. Two trains of rod block logic is provided. Train I uses position information provided by the Relative Rod Position Indication (RRPI) System. Train II uses position information provided by the Absolute Rod Position Indication (ARPI) System.

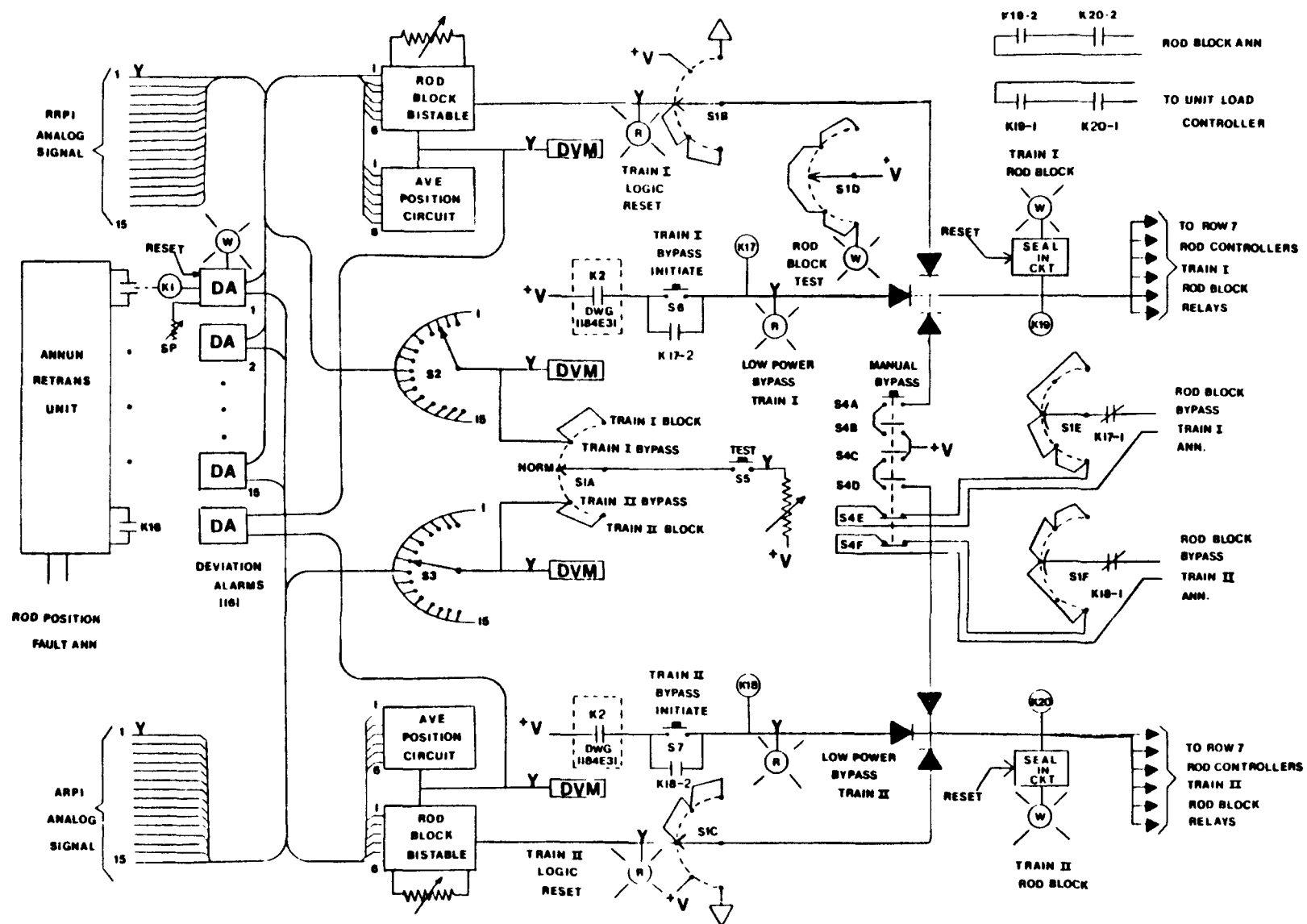


Figure 3-32 ROD BLOCK SYSTEM

Each train provides a Rod Block input to the 6 controller drawers of the operating bank. To allow plant operation with either Relative Rod Position Indication System or Absolute Rod Position Indication System failure, provisions have been made to allow an operating bypass of one of the two logic trains.

This system change was proposed in ECP-W1663 [3]. Because the Control Rod Drive Mechanism Controllers were scheduled for imminent shipment from the vendor, and the proposed modifications needed to be incorporated at the vendor's location, an expedited change was submitted and subsequently approved [4]. The hardware design changes were then made accordingly.

With the incorporation of this system, a single component failure can no longer lead to a single rod withdrawal event. For either type of rod withdrawal event (compensated or uncompensated), the Rod Misalignment Rod Block System must be failed such that the resultant misalignment condition is not automatically detected and acted upon. In most cases this requires at least two failures within the rod block system. However under certain conditions where a single rod block logic train has been bypassed, a single failure in the unbypassed logic train may disable the system. It should be noted however that a separate rod misalignment alarm is provided. This alarm will alert the operator of a misalignment condition even if the rod block system does not respond.

The occurrence of a compensated rod withdrawal would require the failure of the Rod Misalignment Rod Block system combined with failures of a controller drawer. At a minimum, four failures would be required. In addition it is necessary to assume that the flux controller maintains average core power below 101.7% during the rod withdrawal and that one of the failures of the controller drawer disabled both the command error detection and overspeed detection circuits. A detailed probabilistic evaluation indicated that a compensated rod withdrawal has a probability of occurrence equal to 1.2×10^{-11} .

The occurrence of an uncompensated rod withdrawal requires the failure of both the Rod Misalignment Rod Block System and the rod withdrawal system signal. In addition to these two failures, additional failures are required to initiate the event. It is possible that a few specific failure points exist within the auto interface board which could permit the withdrawal of a single control rod. Under these conditions the withdrawal speed would depend upon the Speed Demand Signal provided by the flux controller, however withdrawal speeds between 0.06 and 6.0 inches/minute are possible. The most likely occurrence of this event would be through multiple (two or more) failures of the auto interface board and/or the controller drawer. According to a probabilistic evaluation, the probability of occurrence for an uncompensated withdrawal is 4.6×10^{-7} .

3.6.5 Final Status and Recommendations

The design change described in Section 3.6.4 reduces the probability of inadvertent single rod withdrawals to the point where the uncompensated case could reasonably be considered as an "Extremely Unlikely Event" or "Faulted Event". The occurrence of a single rod withdrawal with compensation from the other rods in the bank can now be considered as beyond the design base. These reclassifications were recommended, but were not officially carried out before CRBRP termination.

In addition, a recommendation was made to redefine the initiating accident for the U-2b event scenario in terms of a control rod bank withdrawal rather than a single rod withdrawal. This redefinition is justified in light of the rod block system which precludes a single failure from causing a single rod withdrawal. This change would also bring the requirements more into line with LWR precedents as discussed in Section 3.6.3. The bank withdrawal U-2b scenario is consistent with the analysis already performed (i.e., with flux tilt not considered) and no further analysis is necessary.

3.6.6 References

1. J. V. Miller, R. D. Coffield, K. D. Daschke, J. S. Killimayer, H. C. Anderson III, T. R. Reid, "Supplementary Manual for the FORE-2M Computer Program," CRBRP-ARD-0257, Westinghouse Electric Corporation, September 1982.
2. R. A. Shober, T. A. Daly, and D. R. Ferguson, "FX2-TH: A Two-Dimensional Nuclear Reactor Kinetics Code with Thermal-Hydraulic Feedback," ANL-78-97, Argonne National Laboratory, October 1978.
3. ECP-W1663, "Block Row 7 PCRDm for Rod Out of Alignment," May 5, 1981.
4. Letter LW810307, W. H. Hamilton to W. W. Dewald, "CRBRP; Design Change to Primary Rod Control Equipment, Addition of Rod Block Circuitry," dated July 28, 1981.

3.7 Control Rod Systems

Two control rod systems are used for reactor control and shutdown. Each system utilizes movable boron carbide neutron absorber pin bundles or control rods which are a part of the Control Assemblies. The Control Assemblies' outside dimensions are identical to those of the Fuel Assemblies. The control rods are positioned in or above the core by Control Rod Drive Mechanisms located on top of the intermediate rotating plug of the reactor vessel closure head. A Control Rod Driveline provides a connection between each control rod and its associated drive mechanism.

The Primary Control Rod System provides reactivity control for load follow, burnup compensation and startup and shutdown. The Secondary Control Rod System is a redundant system used only for shutdown. Reactor shutdown is achieved by inserting either the primary or secondary system control rods fully into the core. The Primary Control Rod System has sufficient worth at any time in the reactor cycle, assuming the failure of any single active component (i.e., the highest worth rod stuck), to shut down the reactor from any operating condition to the hot shutdown temperature of the coolant. The Secondary Control Rod System has sufficient worth at any time in the reactor cycle, assuming failure of any single active component (i.e., a stuck rod), to shut down the reactor from any operating condition and to maintain subcriticality at the refueling temperature. Allowance is made in each of the two systems for the maximum reactivity fault associated with any anticipated occurrence.

The Primary and Secondary Control Rod Systems operate independently such that the capability of either system to fulfill its safety function is not dependent on the operation (or failure) of the other system. Design diversity and separation are provided to protect against common-mode failures.

3.7.1 Primary Control Rod System

The Primary Control Rod System (PCRS) provides safe, reliable shutdown and reactivity control capability for the CRBRP by moving and positioning neutron absorbing rods (known as control rods) in the core. Operational control of

the PCRS is provided by the Reactor Instrumentation and Control System, and signals for emergency plant shutdown (known as the scram condition) are provided by the Plant Protection System. The system consists of nine mechanical subsystems, each of which is made up of a Primary Control Rod Drive Mechanism (PCRDM), a Primary Control Rod Driveline (PCRD) and a Primary Control Assembly (PCA). The PCRS has the capability for scram insertion, with sufficient reactivity worth, to shut down the reactor to hot shutdown conditions with the highest worth control rod stuck in any operating condition, and to maintain shutdown in this condition.

The nine PCAs are located in Row 4 and Row 7 of the core as shown in Figure 3-33. The PCRDMs are located on the closure head intermediate rotating plug (IRP) of the Reactor Enclosure System in the same pattern as the PCAs. The PCRDs connect the primary control rods of the PCAs to their corresponding PCRDM leadscrews.

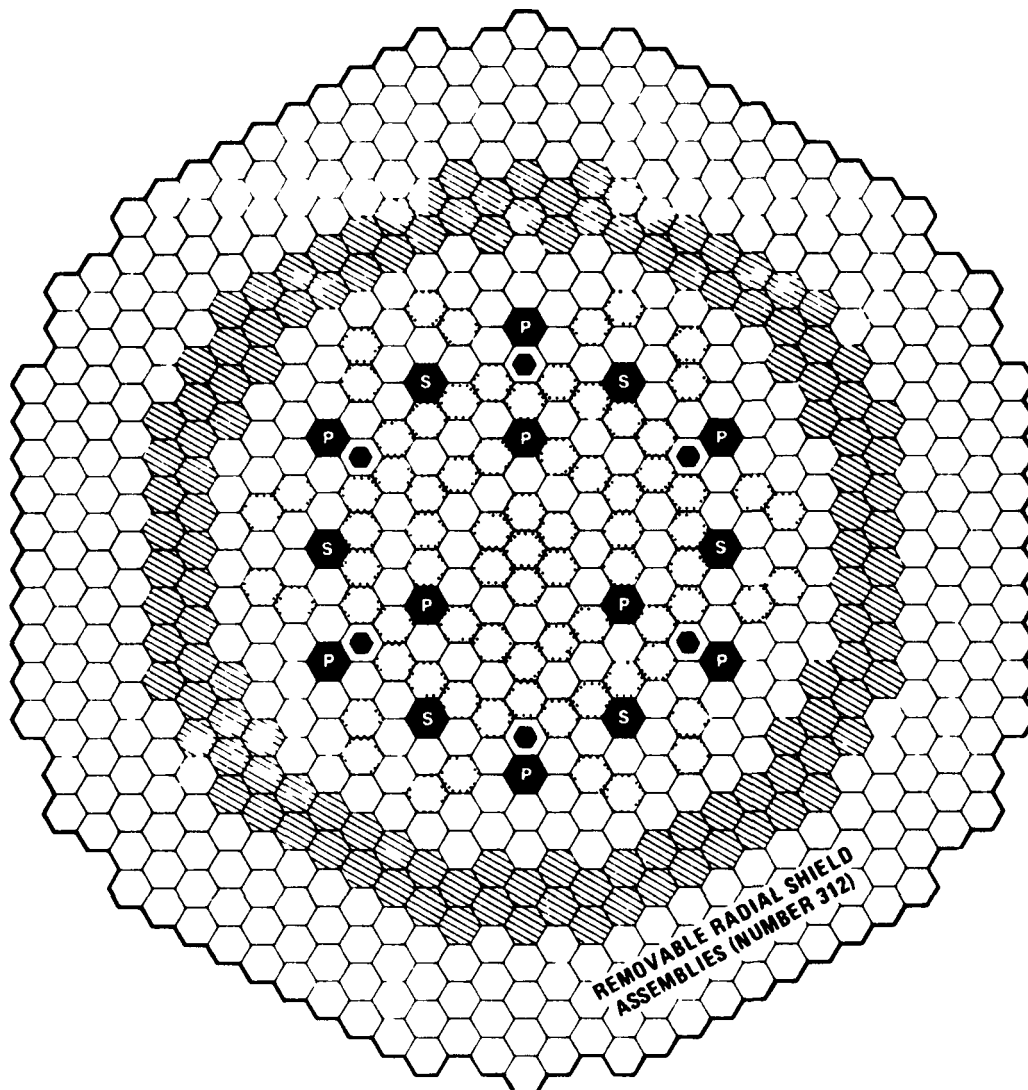
A Shield and Seismic Support (SSS) is provided on the IRP. This structure surrounds the CRDMs and provides lateral restraint, thermal insulation, and radiation shielding for the head access area.

The PCRS also includes a set of maintenance tools and equipment.

3.7.1.1 Summary of Functions and Descriptions

3.7.1.1.1 Primary Control Rod Drive Mechanism

The PCRDMs are a part of the reactor primary pressure boundary. As such, those components of the PCRDMs which are a part of this boundary (extension nozzle, motor tube, position indicator housing, seals, fasteners), are designated as Class 1 in accordance with Section III, Subsection NB, of the ASME Boiler and Pressure Vessel Code. Each PCRDM is divided into two major sections: Upper and Lower.



SYMBOL	COMPONENT NAME	NUMBER
	FUEL ASSEMBLY	156
	INNER BLANKET ASSEMBLY	76
	RADIAL BLANKET ASSEMBLY	126
	ALTERNATE FUEL/BLANKET ASSEMBLY	6
	PRIMARY CONTROL ASSEMBLY	9
	SECONDARY CONTROL ASSEMBLY	6

Figure 3-33 Reactor Cross Section

A. Upper Primary Control Rod Drive Mechanism

Each upper PCRDM (Figure 3-34) is an electro-mechanical actuating device which consists of a stator mounted on the outside of the motor tube pressure boundary, and a collapsible rotor and roller nut assembly mounted inside the motor tube. The rotor assembly consists of a bearing-mounted rotor tube and two segment arms pivoted near the bottom of the rotor tube. Near the bottom of each segment arm there are two windows which form mounting points for the rollers. When the stator is energized, the upper ends of the arms are pulled outward by the magnetic field and the lower arms are pivoted inward against springs so that they engage the rollers with the threads of the leadscrews.

Axial motion of each driveline and control rod is achieved by rotating the magnetic field around the stator.

To produce a scram (rapid control rod insertion), the electrical power to the stator is removed causing the magnetic force field to collapse. Loss of the magnetic field releases the rotor arms causing the springs to separate the lower ends of the arms. A linkage (synchronizer bearing) is provided which assures that the segment arms separate simultaneously. This action disengages the roller nuts from the leadscrew-PCRD assembly causing the control rod to drop by gravity into the reactor core. A scram assist spring in the lower PCRDM provides initial supplemental accelerating action.

Anti-ejection pawls in the segment arms engage the leadscrew to prevent unplanned upward motion of the control rod. These pawls are spring loaded allowing the leadscrew to ratchet downward, together with the driveline and control rod, until the control rod is fully inserted into the core.

Two independent position indicator systems and signals are provided for each control rod to sense and indicate the position of the leadscrew. The first system, the Relative Rod Position Indicator

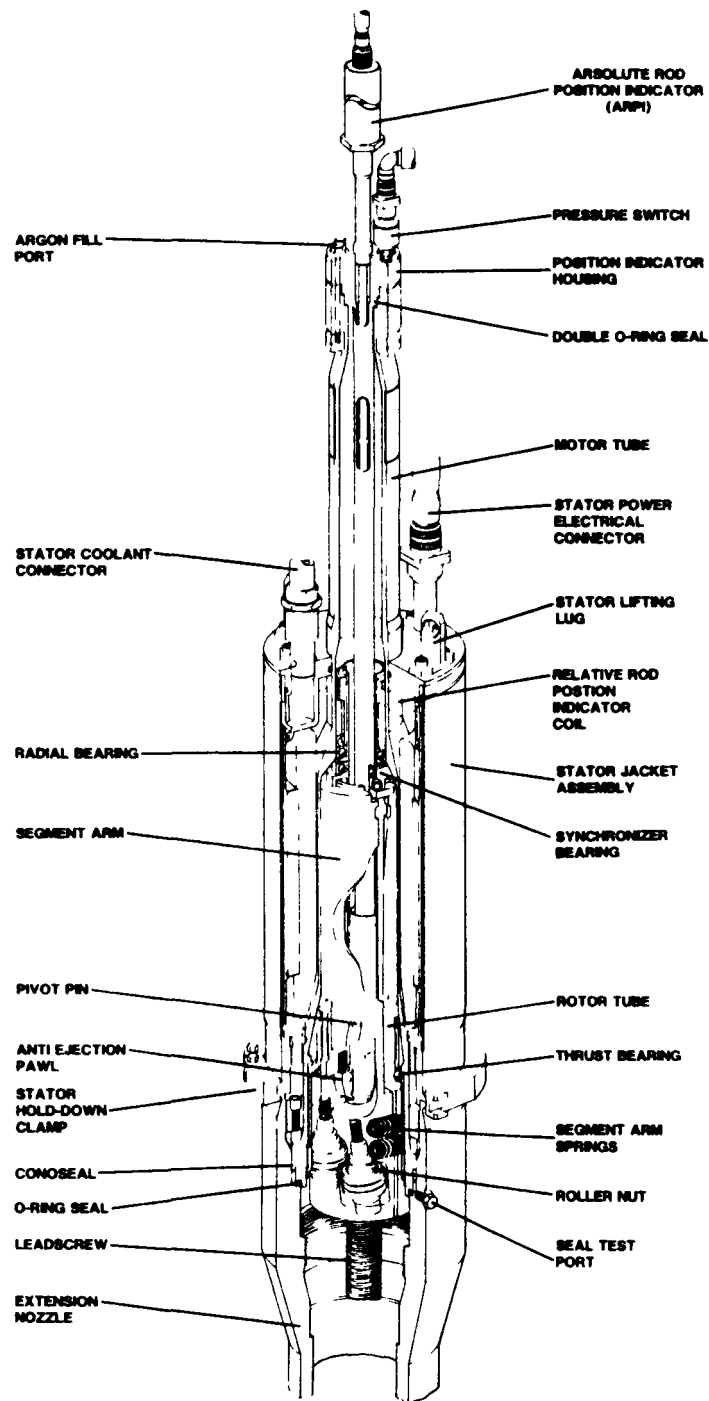


Figure 3-34 PRIMARY CONTROL ROD DRIVE MECHANISM (PCRD)
-- UPPER ASSEMBLY

system (RRPI), consists of an electromagnetic sensor which counts the passes of the lobes of an indicating disc attached to the drive mechanism rotor. The number and fraction of revolutions, multiplied by the pitch of the leadscrew, provides the amount of rod withdrawal from the fully inserted position.

The second system, the Absolute Rod Position Indicator system (ARPI), provides a direct measurement of rod position at any time and, unlike the RRPI, does not require re-zeroing after a scram or temporary loss of power. The system is solid state, utilizing ultrasonics and magnetics to provide a D.C. output indicative of rod position.

The sensor for this system consists of a tube extending down from the top of the motor tube and into the inside diameter of the PCRDM lead screw. A nickle-cadmium wire is stretched axially through the tube. As the lead screw translates, the flux from a torroidal magnet mounted on top of the lead screw intersects the wire at a point indicative of the rod position. Electrical pulses sent down the wire generate magnetic fields which, when they intersect the flux of the lead screw magnet, causes a torsional strain creating a sonic pulse which travels from the point of flux intersection upward. The sonic pulse is detected at the top of the wire, and the time of propogation is measured electronically. This propogation time is converted to a D.C. signal which is analagous to rod position.

B. Lower Primary Control Rod Drive Mechanism

The lower PCRDM components (Figure 3-35) include the extension nozzle, torque taker, torque tube, shield plug, internal seal systems, scram spring and leadscrew. Provisions are made on the leadscrew for attachment to the PCRD. The internal seal system utilizes three metallic bellows as well as a metallic expansion seal (Conoseal) to separate the rotor assembly and leadscrew from the reactor cover gas environment, thereby precluding the possible buildup of sodium frost on those components and the leakage of radioactive cover gas. The main bellows seals the leadscrew to the

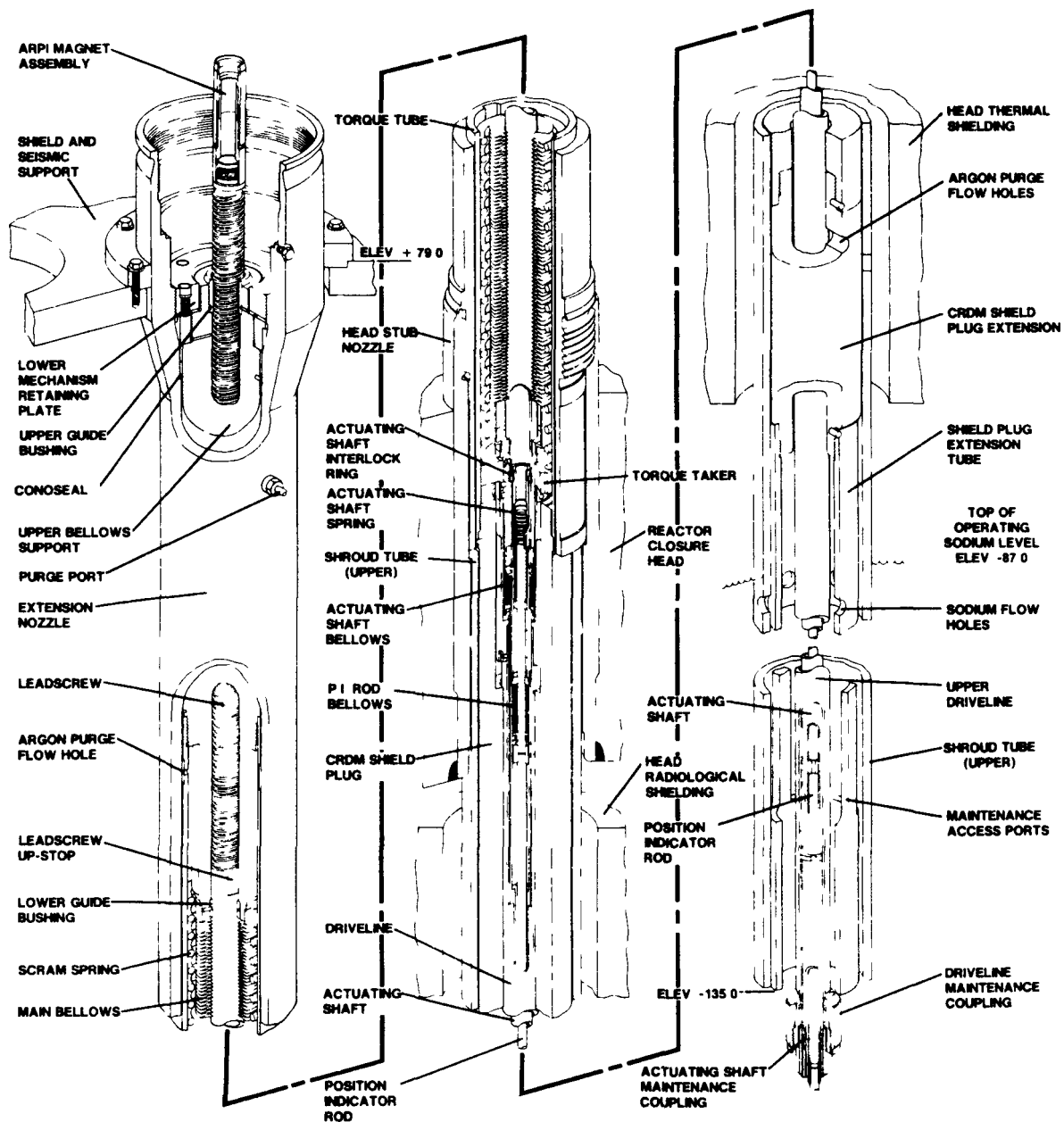


Figure 3-35

PRIMARY CONTROL ROD DRIVE MECHANISM (PCRD)
-- LOWER ASSEMBLY

extension nozzle and operates over the full stroke with a small pressure differential across the wall of the bellows. Two smaller bellows, located inside the driveline, are flexed only during coupling or uncoupling of the driveline from the control rod.

A shield plug and a cover gas purge system are provided for limiting radiation to the PCRDs and the Head Access Area (HAA) to safe levels.

3.7.1.1.2 Primary Control Rod Driveline

Each PCRD (Figure 3-36) consists of three concentric shafts: (1) the driveline shaft, (2) the disconnect actuating shaft, and (3) the position indicator shaft (rod). The driveline shaft is the load carrying member of the group and is the outer shaft. Each driveline shaft connects each PCRD leadscrew with the primary control rod of each PCA. The disconnect actuating shaft, which is the middle shaft acts as an extension for the Disconnect Actuating Tool (DAT) which is used to disconnect the driveline from the control rod for maintenance or refueling. The inner shaft is the position indicator rod which is used to verify that the control rod remains fully inserted during refueling uncoupling and withdrawal of the driveline.

A dashpot is included in the PCRD to decelerate the translating driveline and control rod over the last nine inches of travel during a scram. As the driveline is scrambled, the dashpot absorbs the energy at a deceleration rate that limits the impact velocity of the control rod at the PCA scram arrest flange.

3.7.1.1.3 Primary Control Assembly

The nine PCAs are the nuclear components of the PCRS and are located in the reactor core. Each PCA contains boron carbide (B_4C) as the neutron absorbing material. The available reactivity worth of each assembly depends upon the B-10 content of the B_4C . Sufficient B-10 is provided in the nine PCAs to give adequate worth for shutting the reactor down to hot shutdown

BEST AVAILABLE COPY

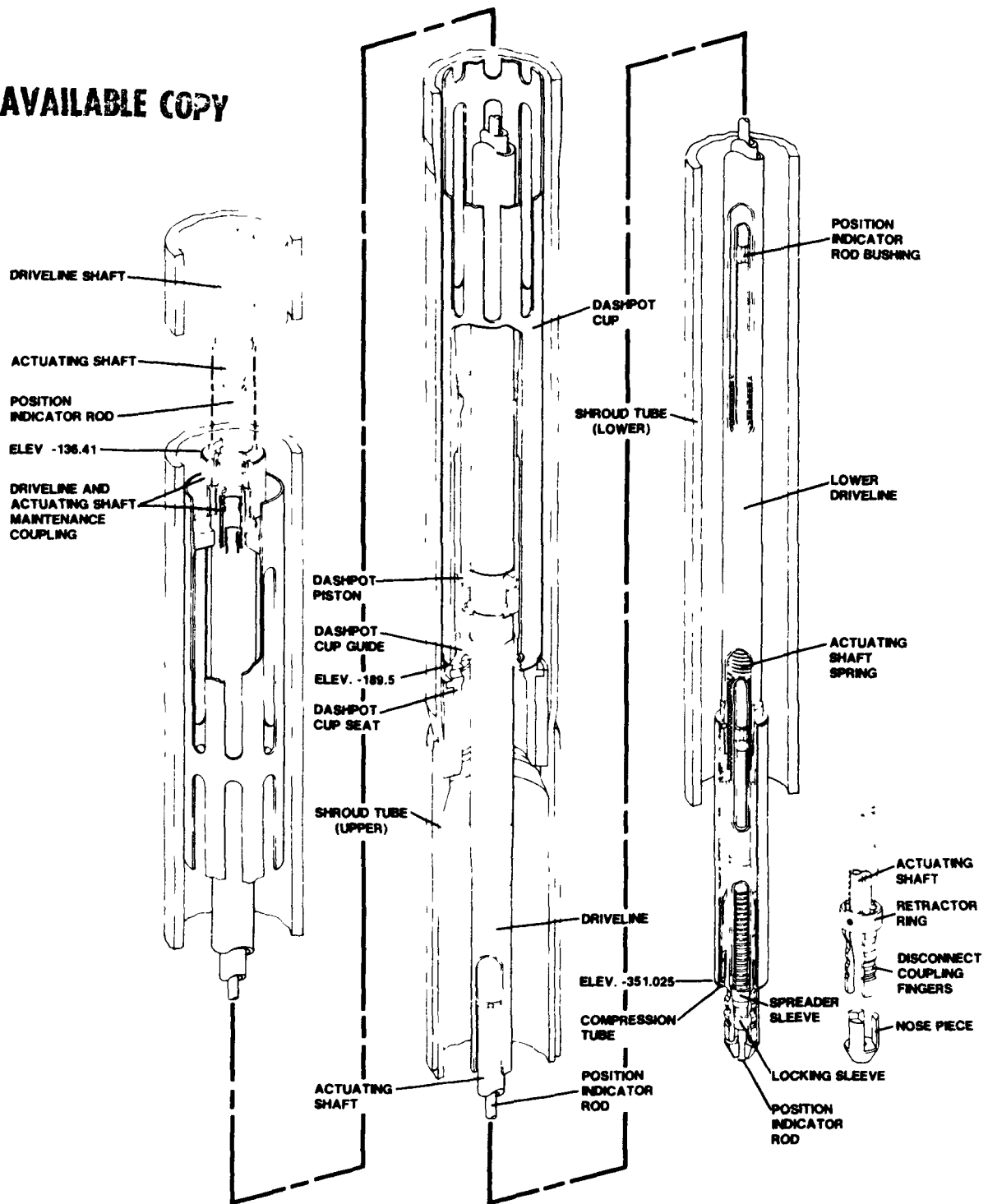


Figure 3-36 PRIMARY CONTROL ROD DRIVELINE (PCRD)

condition from any operating condition, with allowances for a reactivity fault, uncertainties, and the highest worth rod stuck in any operating condition.

A PCA (Figure 3-37) consists of two major subassemblies: (1) the outer duct assembly and (2) the pin bundle/shaft assembly, referred to as the control rod. The outer duct assembly configuration is hexagonal having external dimensions identical to those of the Fuel and Blanket Assemblies. Above core and top load pads, located on the outside of each duct assembly, interface with adjacent Fuel Assemblies to provide positioning and seismic load carrying capabilities.

A shield block, including flow orifice plates, is welded to the outer duct at its lower end to provide neutron shielding consistent with interfacing reactor structure requirements and coolant flow control.

Each control rod includes nuclear and structural components. The nuclear components are the neutron absorber pins, which are sealed stainless steel tubes containing the neutron absorbing material (B_4C). Each pin contains a 36-inch high stack of right circular cylindrical pellets which, when the rod is fully inserted, are nominally aligned with the 36-inch high fuel region of the core. Inside the pin are gas plena, above and below the B_4C stack, which collect the helium released in the neutron absorption process, and a holddown spring which maintains the axial position of the pellets. Each pin is sealed at the top and bottom by welded end caps which also provide axial and lateral end constraint of the pin. Externally, each pin is helically wrapped with a continuous wire that maintains the pin spacing in the rod and promotes reactor sodium coolant mixing within the rod.

Thirty-seven pins, spaced on an equilateral triangular pitch, are contained in each control rod to form a pin bundle.

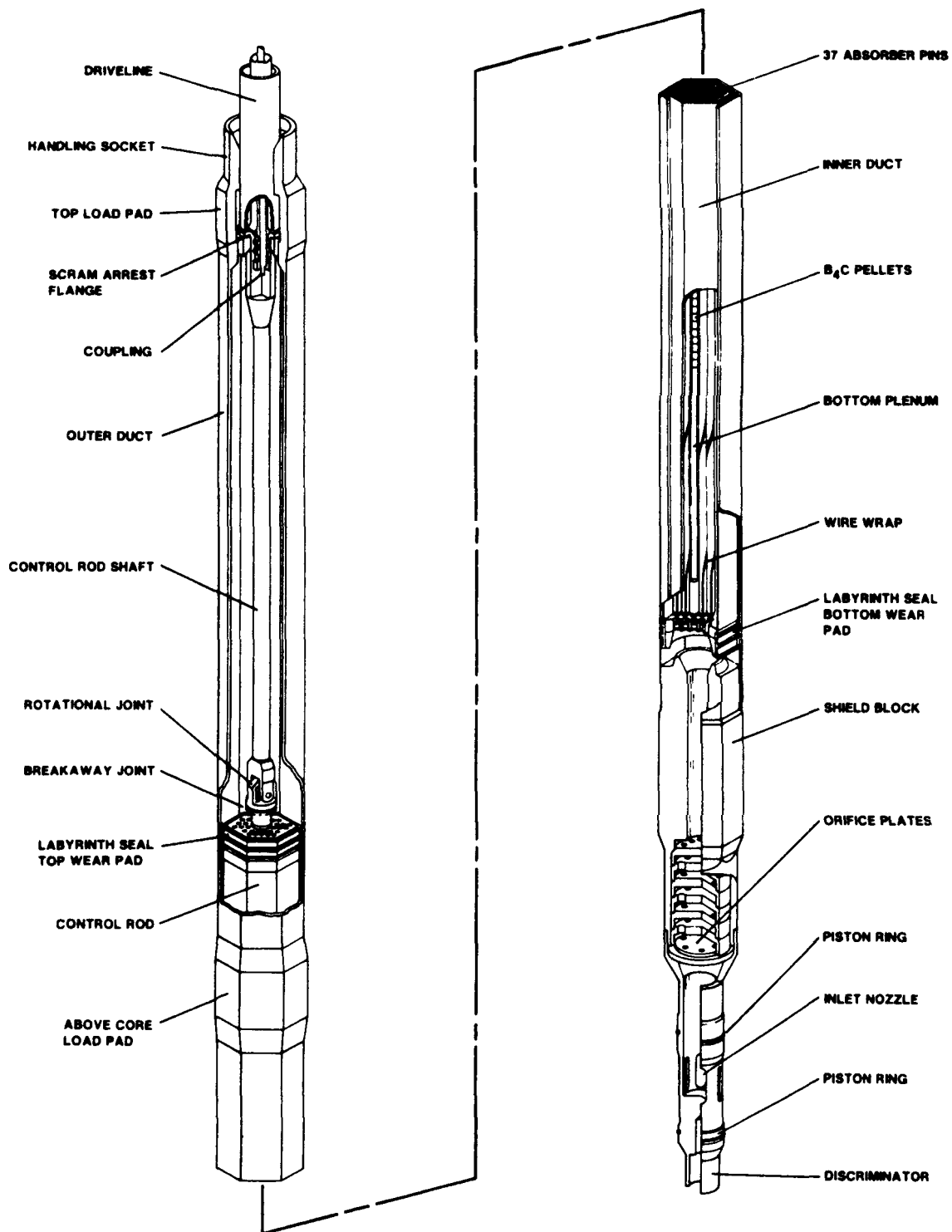


Figure 3-37 PRIMARY CONTROL ASSEMBLY (PCA)

The control rod can be moved axially within the outer duct assembly. Sufficient clearance is provided between the control rod duct and outer duct to limit contact drag forces to values consistent with adequate scram performance when integrated with the other components of the PCRS.

An adapter plate is welded to the top plate of the control rod and is connected to the control rod shaft. In addition, a labyrinth-type wear pad is mounted on the periphery of the adapter plate. A similar wear pad is mounted on the bottom plate. These wear pads serve to align the control rod within the outer duct. They also provide sufficient clearance between inner and outer ducts for limiting lateral loads due to bowing and misalignment and serve to restrict bypass flow for adequate cooling of the absorber pins. The wear pads are made of hard material to limit the wear between inner and outer ducts.

3.7.1.1.4 Shield and Seismic Support

The Shield and Seismic Support (SSS), shown in Figure 3-38, is a hexagonal enclosure around the PCRDs. This enclosure is fastened to the intermediate rotating plug (IRP) of the reactor vessel closure head and provides radiation shielding and thermal insulation for protecting the head access area, and structural support of the PCRDs and secondary control rod drive mechanisms (SCRDMs) under Seismic Events and Structural Margin beyond the Design Base loadings.

The vertical hexagonal portion of the SSS is covered by a horizontal top plate which has holes through which the nine PCRDs and six SCRDMs protrude. Support rings at the holes provide lateral restraint to the PCRDs as needed. Holddown studs pass through the two-inch thick top plate and attach to the IRP. The wall of the hexagonal enclosure has cutouts at the base which engage the six key extensions of the IRP. The top plate and the hexagonal enclosure are made of carbon steel (ASME-SA-516, Grade 60).

3.7.1.1.5 Maintenance Equipment

The PCRS maintenance and refueling equipment includes the major specialized tools, equipment, and procedures required to support PCRS refueling and maintenance operations as well as initial system installation. The Disconnect Actuating Tool (DAT) is the primary PCRS maintenance tool associated with reactor refueling (Figure 3-39). The tool is utilized to disconnect the PCA from the PCRD prior to rotating the closure head during refueling or prior to system maintenance operations. The DAT is pneumatically powered and remotely operated with design features to limit the potential for operator error.

Other specialized equipment for hoisting, tightening, and leak checking is used for PCRDM and PCRD installation and removal and for PCRD changeout operations. The tools provide for removing the PCRDM and PCRD from the closure head in an inert atmosphere, replacing the spent driveline, and reinstalling the PCRDM and PCRD while maintaining allowable dose limits and manual lift limits. Contingency equipment is provided to recover from malfunctions such as failure of the driveline to disconnect from the control assembly, a stuck control rod, or failure of the PCRD maintenance coupling to unthread. PCRS extension nozzle installation and automatic seal weld equipment is used for initial reactor installation.

3.7.1.2 Summary of Test Programs and Results

Five major system level test series were performed and full-scale prototypic control rod systems were used to determine the PCRS's capability to perform simulated reactor power control and emergency shutdown operations reliably. The test series were:

- o Accelerated unlatching life tests
- o Prototype accelerated life tests
- o Failed bellows tests

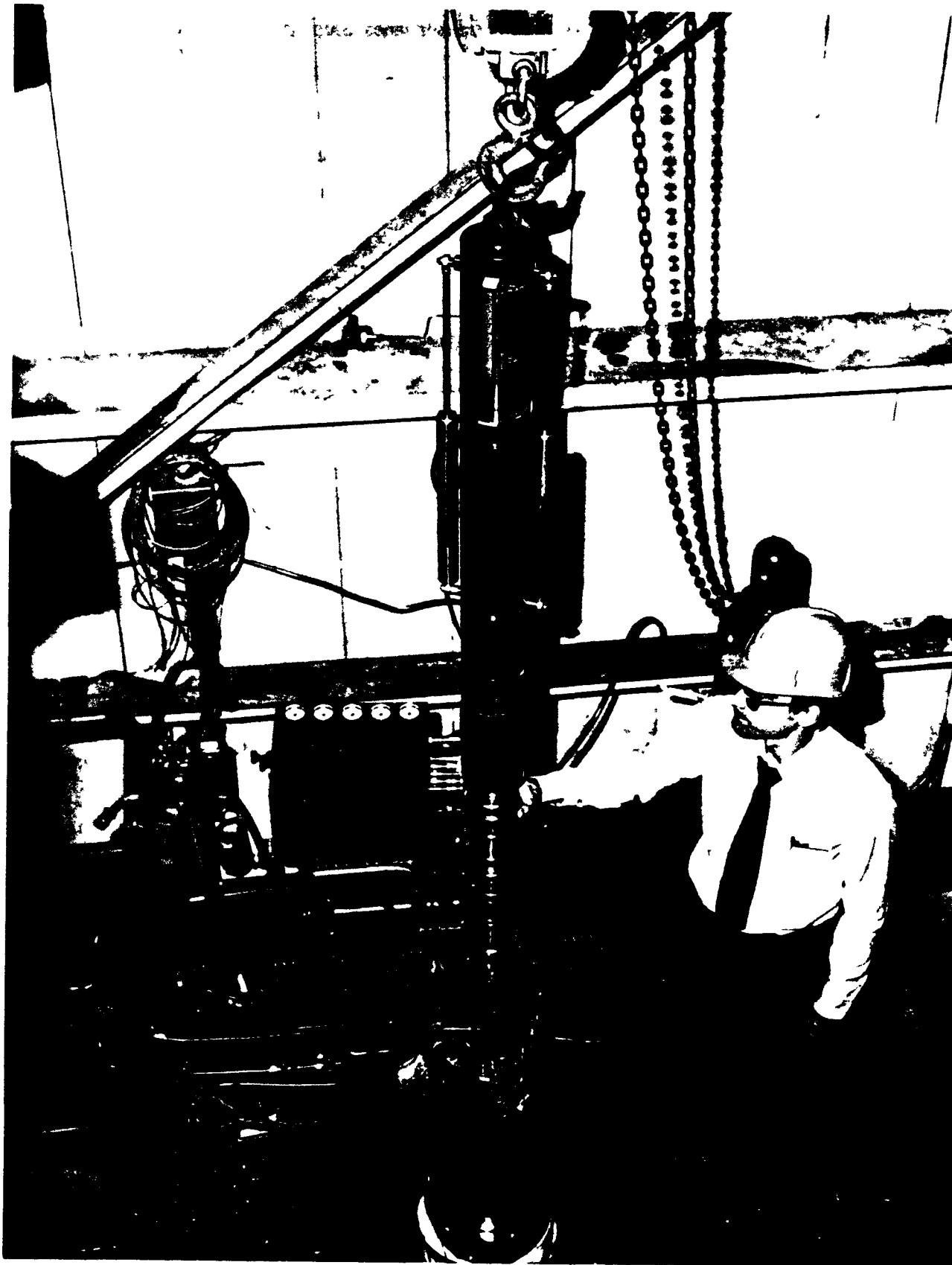


Figure 3-39 Disconnect Actuating Tool for the Primary Control Rod System.

- o Real time tests
- o Flow induced vibration tests

The test objectives were grouped into several major categories. For the system level tests, these included the evaluation of:

- o Scram performance
- o Control rod position indicating systems performance
- o Friction drag
- o Control rod disconnect/reconnect performance
- o Consequences resulting from PCRD stator overstress
- o Consequences of flow induced vibrations
- o Consequences resulting from the PCRD to PCA coupling lockup
- o Maintenance equipment performance

Additional component level tests were performed to provide design verification of the PCRS components. These tests and their objectives were:

- o Dynamic seismic friction test to evaluate the coefficient of friction between a rod and its guide bushings during impact loading.
- o PCA hydraulic and flow induced vibration tests to obtain hydraulic characteristics of the PCA design under prototypic flow conditions.
- o PCA duct bowing test to determine scram retarding forces.
- o PCA pin rupture test to assess the effects of the pressure release pulse from an absorber pin rupture on inner duct deformation and pin-to-pin propagation.

- o PCA duct impact test to confirm the acceptability of scram impact loads on an irradiated outer duct at elevated temperature.
- o PCA rotational joint test to establish performance parameters for that design feature.
- o PCA pin compaction test to determine duct and inner pin loads.
- o PCA irradiation test in the FFTF to confirm lifetime performance under operating conditions.

3.7.1.2.1 Scram Performance

Scram performance was determined by evaluating PCRDM unlatch time, control rod insertion time and velocity profile, and scram arrest impact velocity.

A. PCRDM Unlatching Time

From the accelerated unlatching life test, data was obtained that showed no degradation of the unlatch function over two design lifetimes and under overstress conditions. From the accelerated life test, results indicated that unlatch times measured prior to and immediately following 65 days of control rod hold confirmed that long-term hold periods did not adversely affect unlatch performance. Additionally, elevated stator temperatures did not have a detrimental effect on unlatch function. The failed bellows and real time tests also showed no measurable degradation in unlatch performance.

B. Control Rod Insertion Time and Velocity Profile

Control rod insertion times were recorded during a combination of test conditions. These conditions included the following:

- o Sodium flows from 4.5 klb/hr (10% of design flow) to 53 klb/hr (120% of design flow)

- o Sodium temperatures from 400°F to 1100°F
- o Bellows argon fill pressures to produce internal to external pressure differentials of -21 psig to +20 psig
- o Six cases of PCRS misalignment

Test results indicated that the test condition variables produce the following effects on control rod scram insertion:

Results that were generic to all system level tests indicated that control rod insertion times became longer at higher flows, lower temperatures, lower bellows pressure, and at severe misalignments. During the failed bellows test program, insertion times were longer with failed bellows than with bellows properly functioning. However, in all cases tested, control rod insertion times were less than the specification maximum of 1.2 seconds and 3.0 seconds, respectively, for dashpot entry and full insertion. The drop versus time profile remained well within the specification envelope for scram success.

C. Scram Arrest Impact Velocity

Specified acceptable dashpot performance requires that the control rod velocity be slowed to less than 14 in/sec prior to scram arrest impact.

During the PCRDM unlatch tests the impact velocities in non-flowing water were acceptably low and there was no degradation of performance after completing two design lifetimes of scram and cyclic travel operations at PCRDM overstress conditions beyond the second lifetime.

During the accelerated life, failed bellows, and real time tests, it was determined that peak velocities during scram insertion from any drop height occurred during the initial one-third of the drop. The velocity at scram arrest was acceptably low.

3.7.1.2.2 Control Rod Position Indicating Systems Performance

During the PCRDM unlatch tests, an optic probe device was installed to count leadscrew threads during rod travel and was used in conjunction with the ARPI electrical output signal to obtain rod position during rod drop scrams. The optic device output was compared to stator pulse counts and determined to be highly accurate. The ARPI signal trace was then compared to the optic device output and it was determined that it could locate the rod position within an acceptable uncertainty range during a scram insertion.

During the accelerated life tests, the RRPI was determined to provide acceptably accurate data up to stator winding heatup to 670°F. The ARPI readouts were accurate to stator winding temperatures to 500°F. About 500°F the ARPI signal was erratic, but there was no permanent damage to the system.

All the test results indicated that the ARPI and RRPI performances met design specifications.

3.7.1.2.3 Friction Drag Measurements

Friction drag loads were measured at each of two leadscrew translating speeds for the pre-test and post-test benchmark tests. The speed at which the rod was moved during the tests did not appear to have any definable effect on the measured drag loads. Test results indicated negligible drag forces compared with the 400-pound weight of the translating assembly and they produced no measurable differences in scram times between beginning and end of test.

Maximum loads due to worst case design conditions were encountered during prototype accelerated life tests. The post-test drag load was approximately twice the pre-test load. During an out-of-specification misalignment test, a drag load which was about thrice the post-test drag load was encountered; however, even this drag load had no measurable change in scram insertion performance.

3.7.1.2.4 Control Rod Disconnect/Reconnect Performance

A total of 180 refueling mode disconnect reconnect cycles, six removal and installation (R&I) mode connects and five R&I mode disconnects were successfully completed during the test period. The Disconnect Actuating Tool (DAT) performed well in all tests without any design modification. Initial use of the tool required 24 minutes to perform a refueling mode disconnect and 14 minutes to perform a refueling mode connect. With operator experience, this time was reduced to about 7 minutes for completing either of the two operations in the refueling mode.

3.7.1.2.5 Consequences of PCRD Stator Overstress

During the accelerated life test, an evaluation that was made to determine the effects of a loss of argon purge flow showed that the extension nozzle temperature profile was not affected for periods up to five hours following argon flow cutoff. Additional evaluations were made to assess the effects on PCRS performance of reduced stator coolant flow including a complete coolant shutoff. Also, the ability to maintain a hold position to insert/withdraw and scram the rod was evaluated. The results were as follows:

- o Following a complete shutoff of stator coolant flow, the winding temperatures rose and stabilized at 670°F in four hours with the control rod in a 3-phase hold position and at 570°F in five hours with the control rod in a 2-phase hold position, respectively. A low coolant flow of 10 scfm was sufficient to maintain the stator winding hot spot to <500°F with the control rod in a 3-phase hold.
- o Hold, travel, and scram tests performed at the stabilized temperature for each mode of reduced cooling (including shutoff) confirmed that control rod hold can be maintained even at the maximum temperature reached following a loss-of-stator-coolant, the rod could be moved without a roll-out scram occurring, the scram unlatch function remained intact, and the scram unlatch time remained within

specification limits. However, relatch following a scram could not be achieved with stator winding temperatures greater than 500°F. The latch function was restored when the temperature dropped below 500°F.

Scram unlatch times became faster at higher temperatures decreasing from 48 msec at normal conditions to 21 msec at 670°F.

- o Elevated stator temperatures had no effect on scram insertion time except as it reflects the faster unlatch time.
- o The ARPI system performance remained within specification at stator temperatures up to and including 535°F. The position readout was erratic above this temperature but returned to normal following cooldown.
- o The data provided a good correlation of outlet air temperature with stator winding temperatures. The relationship appears to be linear.
- o Post-test functional and operational checkout of the overstressed stator confirmed that temperatures as high as 670°F for a few hours produced no degradation of PCRD/stator operation.

3.7.1.2.6 Consequences of Flow Induced Vibration

It was found, as expected, that there was low-level, flow-induced vibration in the PCA with a broad frequency spectrum. The energy magnitude was so low that quantitative analysis was neither accurate nor useful. The only possible damage that could result from this vibration level would be wear between adjacent helical wires on the control rods. While not observed at the conclusion of life testing, such wear over the life of the reactor could only be ruled out by life testing. It was demonstrated that the vibration level rose as the square of the velocity and displayed neither resonant peaks nor instabilities. This behavior showed no great sensitivity to rod height or misalignment.

There was also low-level impacting in the dashpot area. Both the intensity and rate of impacting increased approximately as the square of the flow rate with no indication of instabilities or resonances. The impact level did not appear to be high enough to cause appreciable damage or affect performance. The points of impact could not be determined exactly, but in the dashpot area there appeared to be lateral impact between the upper guide and the driveline and possibly between the lower guide and the driveline as well. Neither the impact rate nor the magnitude was appreciably influenced by either rod position or system misalignment.

3.7.1.2.7 Consequences of PCRD to PCA Coupling Lockup

The CRBRP PCRD-to-PCA coupling was designed to lockup at temperatures above the 400°F plant refueling condition. Tests were performed to measure the range of ambient temperature and associated physical gap between the two component parts and to determine the temperature at which lockup would occur. At ambient temperature, the gaps ranged from 0.006 to 0.013 inches. In these tests, thermal lockup was determined to occur between 500°F and 700°F with the higher temperature occurring after thermal cycling. The results support a conclusion that the coupling would meet the design objective of not being locked-up at refueling temperature (400°F), but would lockup at operating temperature (780°F).

3.7.1.2.8 Maintenance Equipment Performance

Successful tests performed utilizing Lower PCRD/PCRD installation and removal tools included nine installations and six removals over sodium at 400°F or with sodium drained from components previously sodium-wetted and at 400°F.

Leak rates from a lower PCRD Conoseal loaded only by the weight of the assembly were low and acceptable to permit the performance of maintenance activities without bagging for expected reactor cover gas radioactivity levels.

Seven disassemblies (and reassemblies) of the control rod driveline were completed to demonstrate driveline changeout capability. These were completed under dry and sodium-wetted conditions and included both torquing disassembly

and cutting of the driveline maintenance coupling. The prototype tools used during the test performed their intended design function and no tooling problems were identified. Heating of the sodium-wetted (but drained) driveline was necessary to complete the required disassembly operations within the allowable torque ranges.

Six changeouts of the Lower PCRDM Conoseal were completed during the test program. All were successfully performed on sodium-wetted components.

The prototype tooling for Upper PCRDM removal and installation became available late in the test program and were used for two installations and three removals. The activities performed followed procedures to install the motor tube, position indicator housing, stator and clamp, and position indicator. No significant problems were identified.

Helium and argon leak tests were performed using prototype tooling to check the leak tightness of the Lower and Upper PCRDMs and the Position Indicator Housing. Two special tests were conducted: (1) a comparative test of helium versus argon leak detection, and (2) a leak measurement time comparison for the two available Lower PCRDM leak test fixtures. No problems were experienced with the use of the leak-test tooling or with the test procedure.

The prototype vent and purge tooling was applied four times during installation and removal of prototype PCRDM test articles. A special test was performed to determine leak rate decay time and maximum measurable leak rate for the tool and available argon source. No problems were experienced with the prototype tooling or with the procedure.

A full-size wooden mockup of the CRBRP Reactor Head Access Area was available for demonstrating tooling applications within the space constraints which would be experienced by plant operators. The other tests described in this report were performed on isolated prototype PCRDMs without the head access area space constraints. The test included the installation and removal of prototype upper PCRDM components and the lower PCRDM component parts and tooling which interface the upper end of the extension nozzle. Two minor problems were identified with the use of the tooling in the head access area.

One of these related to delay caused by a galled nut on the stator clamp and the other concerned the limited space available to swing a mallet required to seat the stator clamp. Neither problem prevented completion of the test. The galled clamp nut resulted from repeated reuse of the self-locking clincher nut throughout the test program. This would probably not be a problem in plant use due to the relatively few times the nut would be loosened or tightened. The limited space for use of a mallet to set the stator clamp is not a serious limitation. The clamp can be seated (with some inconvenience) within the physical constraints of the adjacent PCRDMs.

The PCA breakaway test was performed using prototype tools in a water-filled test vessel. The breakaway joint, which is located just below the rotational joint, failed as predicted at ~19,000 pounds tension.

3.7.1.2.9 Dynamic Seismic Friction Test

The Dynamic Seismic Friction Test was completed in air, argon, water, and sodium environments. The testing consisted of dropping a cylindrical rod in three bushings and a hexagonal rod in a hexagonal duct under lateral vibration conditions typical of seismic events. Material couple tests were Alloy 718 to itself and to Type 316 stainless steel. Rod drop times and impact loads on instrumented bushings were measured. Approximately 500 rod drops were completed. Measured friction coefficients were between 0.27 and 0.5, and the 3-sigma design value was 0.45. The friction coefficients were approximately the same for 130°F water and 800°F sodium.

Analyses using design methods to predict the rod-to-bushing impact loads together with the measured friction coefficients showed good agreement with the test drop times. This agreement provided test support for the design methods used to predict PCRS seismic scram speeds.

3.7.1.2.10 PCA Hydraulic and Flow Induced Vibration Tests

Evaluation of the PCA Flow Test results showed general agreement with hydraulic predictions. The test results included orifice, pin bundle, and bypass channel pressure drops; control rod flotation flow rates; flow split

between the pin bundle and bypass regions; orifice cavitation; and sensitivity of pressure drops to control rod position and alignment. These test data were used to update the methods used for PCA thermal/hydraulic analyses and to reduce associated uncertainty factors.

Flow Induced Vibrations (FIV) due to the particular design of the PCA test ADVAB-2 were identified as the cause for power oscillations in FFTF. A simple model of the control rod was designed and tested to simulate the control rod vibration and then to test alternative designs capable of suppressing it. Test results showed that FIV could be suppressed with a control rod design which controls the flow in the gap between the inner and the outer duct and locates the pressure drop in the gap at the top of the control rod, a solution which worked in FFTF. This finding resulted in a recommendation of a PCA design with one long labyrinth seal at the top wear pad and a low pressure drop wear pad at the bottom of the CRBRP primary control rod.

3.7.1.2.11 PCA Duct Bowing Test

A duct bowing test confirmed that significant margin to the scram limiting drag force existed under both three-point and two-point duct-to-duct contact.

3.7.1.2.12 PCA Pin Rupture Test

A pin rupture test indicated that no observable PCA inner duct damage occurred due to absorber pin rupture at internal pressures of 2000 to 3000 psi which are predicted for the absorber pin at end-of-life. Small permanent duct deformation was observed at 5000 psi rupture pressure; however, the deformations were small compared with available clearances.

3.7.1.2.13 PCA Duct Impact Test

A duct impact test, utilizing an irradiated Type 304 stainless steel duct from EBR-II, showed no potential for brittle fracture when axially impacted in a manner similar to scram arrest loading at loads far in excess of the predicted PCA scram arrest loads.

3.7.1.2.14 PCA Rotational Joint Test

The rotational joint test verified the integrity and provided the necessary performance data for the rotational joint.

3.7.1.2.15 PCA Pin Compaction Test

The pin compaction test was completed. It demonstrated that the worst-case predicted absorber pin bowing would not result in significant interaction forces between the pins and the inner duct. The deflections resulting from these forces were small compared to the available clearances: thus, they would have no impact on scram performance. Analyses completed for this test adequately predicted the forces required at the absorber pin end caps to compact the bundle.

3.7.1.2.16 PCA Irradiation Test

This test was ongoing at the time of CRBRP termination.

The objective of the PCA Irradiation Test (PIT) was to demonstrate lifetime operating capability of the PCA in a near-prototypic environment. To achieve the objective, the maximum B-10 loading permitted for Fast Test Reactor (FTR) was included in the design to simulate the enriched CRBRP PCA. The PIT assembly was to be installed as a replacement to an FTR secondary control assembly and operated as a functioning component of the Plant Protection System. Plans called for a residence time in the reactor of six FTR cycles.

3.7.1.3 Evolution of Design

3.7.1.3.1 Primary Control Rod Drive Mechanism and Driveline

The PCRDM and PCRD design was based on the FFTF concept. Design evolutions included the following:

- o The PCRDM motor was redesigned to accommodate the increased loads required for CRBRP versus FFTF.
- o The scram spring was redesigned to provide the same peak scram assist force as in FFTF but over a longer stroke. Resultantly the system is less sensitive to scram retarding forces.
- o The lower PCRDM Conoseal installation was redesigned to facilitate maintenance and reduce shutdown time.
- o Prototype testing indicated that the PCRDM leadscrew bellows did not meet the requirements for leakage. The heat treatment process for the bellows material (Alloy 718) was changed to eliminate annealing and this resulted in a design which met the leakage requirements.
- o The stator holddown clamp used during installation was redesigned as a result of an increase in the Structural Margin Beyond Design Basis (SMBDB) loads.
- o The shield tube and upper elbows support used during lower PCRDM installation were modified to increase the reactor shroud tube clearance to reduce the side loads during installation.
- o The sodium flow holes in the shield tube were relocated and sized to achieve the desired sodium level and flow within the PCRDM and reactor shroud tube.
- o The Conoseal backup ring was made integral with the upper bellows support to eliminate loose parts and facilitate installation.
- o The actuating shaft spline was shortened and the clearance was increased to eliminate binding during the control rod disconnect operation.

- o The lock-cup to motor tube holddown ring interface was modified to facilitate installation.
- o The upper bellows support was redesigned to accommodate seismic events.

3.7.1.3.2 Primary Control Assembly

The PCA design was based on the FFTF concept. Design evolutions included the following:

- o The stacked orifice/shield inlet nozzle of the conceptual design was changed to a single piece shield and up to seven thin orifice plates welded to the inlet nozzle. This facilitated machining and assembly welding.
- o A labyrinth seal wear pad design was incorporated for the top and bottom wear pads to improve the flow split between the pin bundle and bypass channel.
- o The inner duct size was decreased. This improved the potential for two cycle operation of the PCA from a bowing and swelling standpoint.
- o A rotational joint was included in the design since it reduced PCA sensitivity to misalignment.
- o The PCA absorber pin design was optimized to accommodate expected B_4C absorber material swelling and helium release.
- o The core was reorificed to provide assurance that thermal striping of the PCA handling socket would not be a life limiting problem.

3.7.1.3.3 Shield and Seismic Support

The SSS design was based on the FFTF concept. Design evolutions included the following:

- o Head mounted keys were used to restrain movement of the SSS base.
- o External flanges used to holddown the SSS to the head were replaced by studs to accommodate third-level structural margin load. This design facilitated reassembly of a cold SSS to the heated head.
- o An undercut on the base of the SSS wall was added to reduce heat loss from the closure head by reducing the contact area.
- o Insulation was added to the SSS north wall to reduce heat transfer from the SSS to the inflatable seal on the small rotating plug riser assembly.
- o Removable plates were incorporated in the SSS north wall design to improve maintenance access on the small rotating plug riser.
- o The material for the top support plate, north wall plates, plate fasteners, and holddown studs was changed to increase stress margins and to prevent web tearout in the plates at the fastener hole locations.

3.7.1.3.4 Maintenance Equipment

A number of tool modifications were made following some minor redesigns resulting from tests. Both the PCRDM extension nozzle thread protector and the breakaway tool nozzle extension were redesigned to increase the inner diameter to eliminate Conoseal scraping, and the seating surface was changed to eliminate a galling problem. The breakaway tool split nut was redesigned to better accommodate slightly damaged threads on the leadscrew. Also, the retaining plate removal and installation tool was modified to incorporate better lockup staker operation and positioning as well as improved position indicator readability.

3.7.1.4 Status of Final Design

3.7.1.4.1 Primary Control Rod Drive Mechanism and Driveline

The final design was released in March 1979. Fabrication of plant unit components was completed. Final acceptance testing of all plant units was completed and no problems were found. The 18 units were shipped to storage.

No unresolved technical problems were encountered.

3.7.1.4.2 Primary Control Assembly

Final design activities for the PCA were continuing at the time of CRBRP termination.

Unresolved problems that existed at the time of project termination included the following:

During licensing hearings, the Nuclear Regulatory Commission (NRC) identified extremely unlikely conditions during refueling, when the PCA flotation margin might be reduced to zero. An engineering study was completed which identified two possible solutions to this problem. The first solution was to increase the diameter of the PCA coupling shaft from 1.0 inch to 1.625 inches, thereby adding 10 pounds to the weight of the translating assembly and increasing the flotation margin by 8 percent. An additional 7 pounds of weight could be added by boring out the large diameter shaft and filling the cavity with high density material (e.g., tungsten). This increases the flotation margin to 13.5 percent. Analysis showed that the increase in the shaft diameter and weight would not produce any significant change in the control rod scram performance. The second solution was to decrease the expulsion pressure acting on the PCA during refueling by venting the core. In the vented core scheme, fuel assemblies (up to five) in each of three Lower Inlet Modules (LIMs) containing PCAs would be replaced by special non-fueled assemblies to provide very low flow impedance paths in parallel with the PCAs. Venting one assembly in each of three LIMs produces a PCA flotation margin increase of 6 percent. The flotation margin is increased to 29.5 percent if five assemblies

are vented in each of three LIMs. No change in PCA design is required if venting alone is adopted. The recommended solution was the increased shaft weight with the core venting option to be implemented if additional flotation margin is required.

The second problem in PCA design occurred as a power oscillation in FFTF during the initial ascent to power after installation of the PIT assembly. Subsequent experiments revealed that power oscillations greater than ± 8 percent were caused by the radial oscillation of the PIT assembly within its outer duct. Analytical studies indicated that the random motion of six PCAs in CRBRP would create power oscillations greater than the design limit of ± 1 percent. A possible cause of these oscillations was the design of the PCA wear pads between the inner and outer ducts. New wear pads were designed, which reduced the pressure drop across the lower wear pad and increased the pressure drop across the upper wear pads. This design change was intended to be tested to confirm the expected flow split and reduction in radial oscillation.

The power oscillations in FFTF, due to the PIT, did not exceed the FFTF operational requirement of ± 1 percent and so the irradiation test assembly was to remain in FFTF with no modifications to the operational procedures or withdrawal profile.

3.7.1.4.3 Shield and Seismic Support

The final design release, including drawings and the equipment specification, was completed. Additional required analyses were updated and issued following the final design review.

At the time of CRBRP termination, no unresolved technical problems were encountered.

3.7.1.4.4 Maintenance Equipment

The final design review of all PCRS maintenance tools was held in December 1982.

At the time of CRBRP termination, no unresolved technical problems were encountered.

3.7.2 Secondary Control Rod System

In this section the design changes and analysis performed for extending the Secondary Control Assembly in-core life to 603 effective full power days (EFPD) from 328 EFPD are described. The design life extension was achieved by adjusting the diameter of the absorber (B_4C) pellets and cross-section of the pin bundle filler rods to accommodate added material swelling and by modification to the clad tube structural design criteria. The longer in-core life resulted in a substantially higher end-of-life clad tube internal helium pressure loading. Analysis determined stress levels satisfied the clad tube structural criteria limits.

The design modification was approved by the CRBRP Project Office and implemented by the General Electric Company. The design change was incorporated into affected secondary control assembly engineering drawings and specifications.

3.7.2.1 Summary of Functions and Description

3.7.2.1.1 Functions

The primary functions of the secondary control rod assembly as stated in Reference 1 are as follows:

- A. Provide an alternative shutdown capability even in the extremely unlikely event of failure of the primary control rod system to shut down the reactor upon demand for scram.
- B. Shall be capable of shutting down the reactor to the refueling temperature from full power conditions with allowance for the unlikely event of a stuck rod and maximum anticipated fault in the reactor.

3.7.2.1.2 Description

A. Secondary Control Rod System (SCRS)

The principal features of the SCRS components, shown on Figure 3-40, consist of: (1) a twin-screw drive mechanism--the secondary control rod drive mechanism (SCRDM)--providing vertical translation of a carriage supporting a pneumatic scram actuation device and including solenoid valves, (2) the secondary control rod driveline (SCRD), with a control rod scram latch at the bottom end located within the secondary control assembly (SCA) during reactor operation, (3) the SCA with the movable control rod (B_4C pin bundle) supported by the SCRDM latch above the core during normal operation, (4) a hydraulic assist to speed scram motion, and (5) a hydraulic spring damper to decelerate the control rod over the last few inches of travel.

To achieve a reactor scram, pressure to the pneumatic piston in the SCRDM is relieved. A flexible rod, coupling the pneumatic piston and the SCRDM latch, drops approximately 0.25 inch to open the latch. The hydraulic assist feature consists of the high pressure sodium at the top of the control rod piston with the lower side of the piston vented to the low pressure plenum acting to drive the absorber section of the SCA into the core region to shut down the reactor.

In order to ensure that the Secondary Control Rod System would meet requirements under normal and extreme conditions, comprehensive programs of testing were conducted on critical components. These programs demonstrated the reliability and life of the hardware. In addition, four complete prototype assemblies were tested over extended periods of time as the designs evolved to demonstrate the principles and performance of the assemblies. The assemblies were proven to meet the requirements imposed upon them.

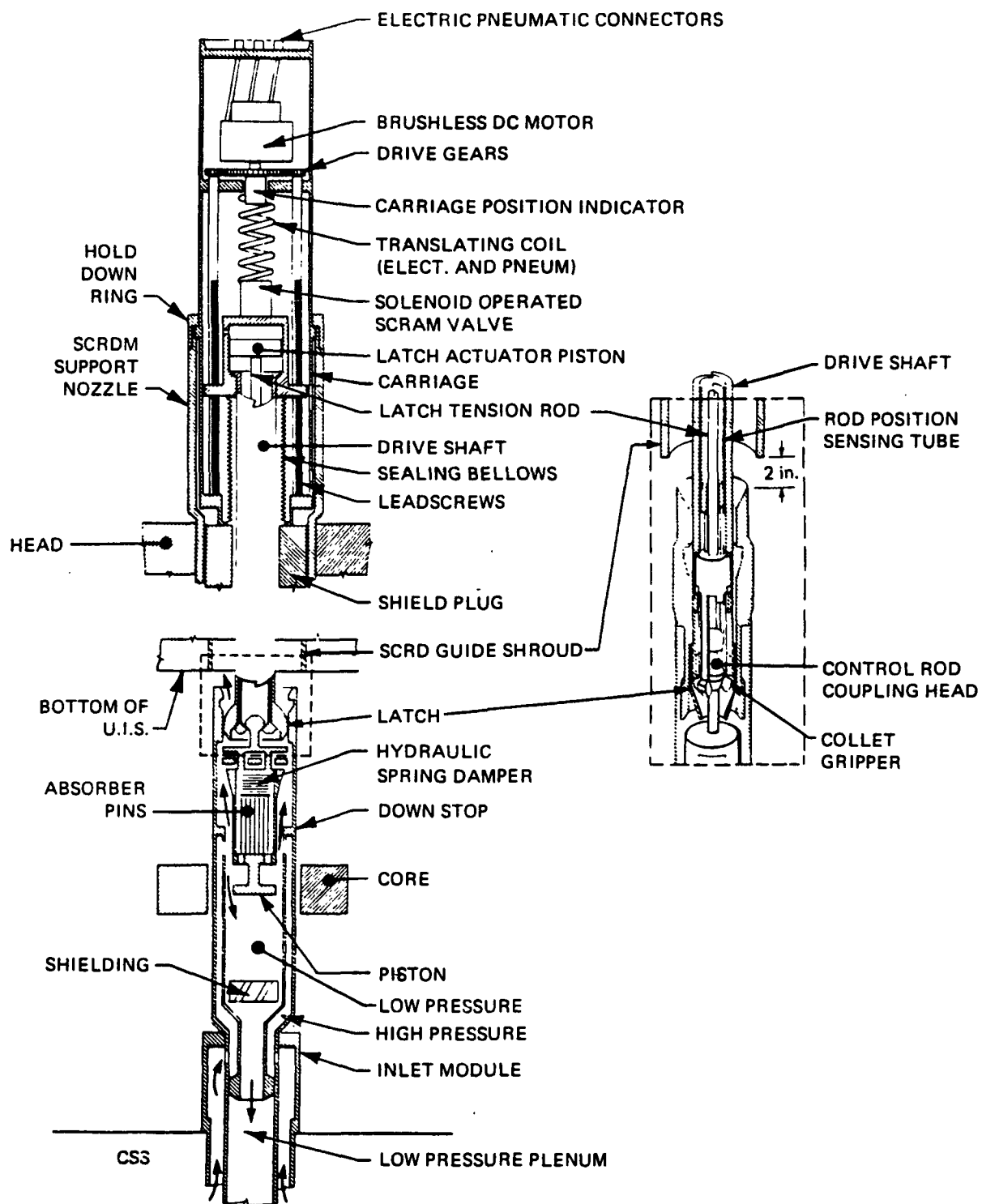


Figure 3-40 SECONDARY CONTROL ROD SCHEMATIC

B. Secondary Control Assembly

The six SCAs provide reactor shutdown capabilities that are independent of the primary control assemblies (PCA). For reactivity control, the control rod of each SCA contains boron carbide enriched in B-10. The SCAs can be replaced in the core at refueling times in a manner similar to that used for the fuel and primary control assemblies.

Each SCA (see Figure 3-41), consists of a control rod, an outer hexagonal duct assembly (similar to the fuel assembly ducts), an inlet nozzle, lower axial shield, guide tube and handling socket. Each control rod is composed of absorber pins surrounded by a control rod duct. A piston assembly is included at the bottom and a damper assembly at the top. Each absorber pin contains a 36-inch column of boron carbide pellets with gas plena provided above and below the column. A spacer tube in each pin maintains the vertical position of the pellet column. The upper gas plenum contains a pellet holddown spring.

External features of the SCA are the same as the PCA so that interfaces with other reactor components are essentially the same for both types of assemblies. Identification and discrimination features are provided, however, that assure proper loading of the SCAs in the reactor.

Since the scram release of the control rod is located within the SCA, alignment of the SCA relative to the SCRDM within anticipated upper bounds, is not critical to scram performance. Consequently, the principal potential resistance to scram insertion of the control rod arises from duct bowing. The assembly is designed so that there is no forced three-point contact during the scram which would lead to large drag loads. Since the control rod has circular wear pads and travels in a circular guide tube, there is no need to maintain azimuthal alignment of the control rod with respect to the guide tube.

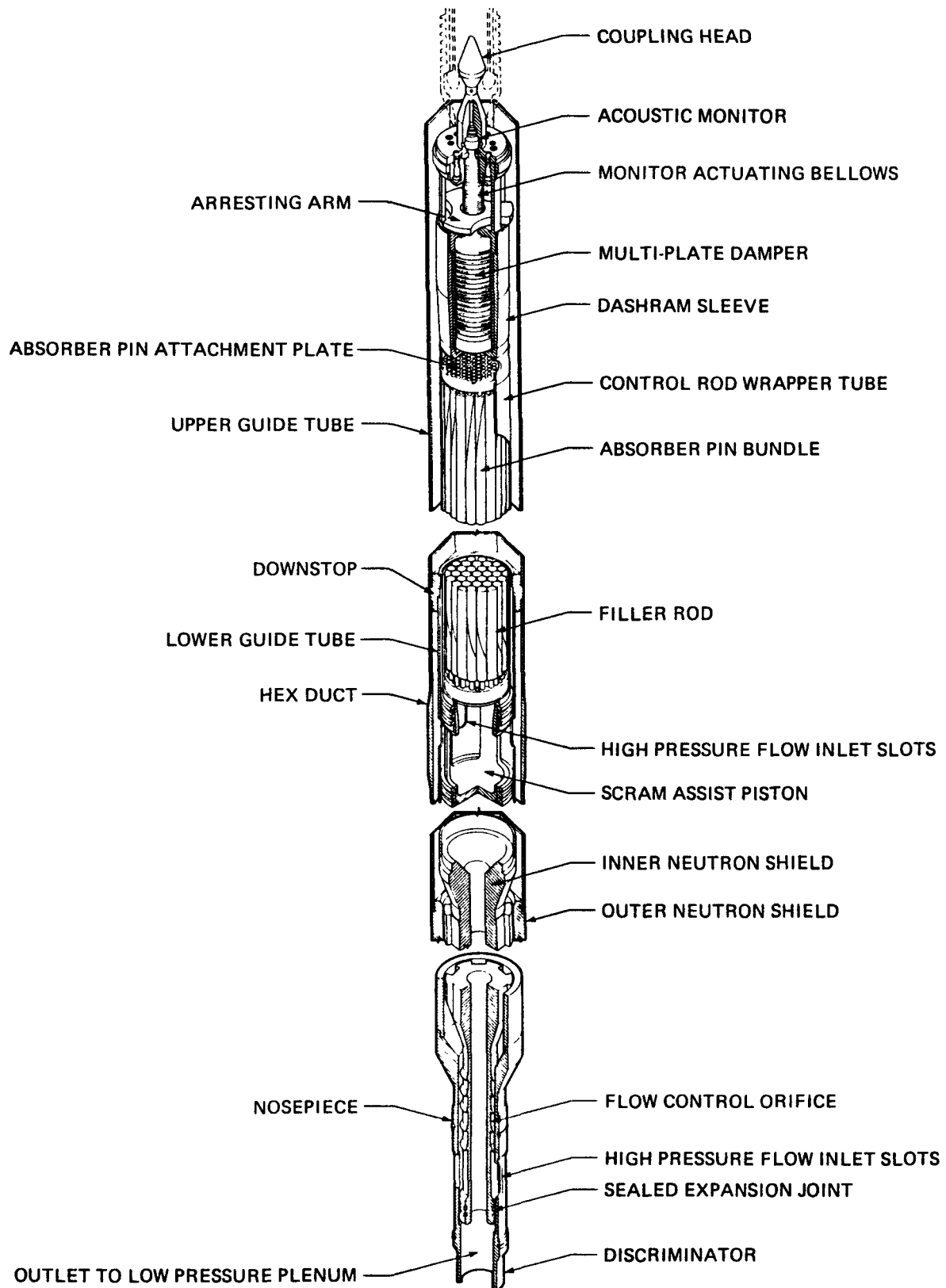


Figure 3-41 SECONDARY CONTROL ASSEMBLY

3.7.2.2 Evolution of Design

The design life of the secondary control rod assembly was extended to 603 EFPDs from the original design of 328 EFPDs. This additional in-core life permitted use of the initial set of secondary control assemblies for the first three fuel cycles ($128 + 200 + 275 = 603$) and follow-on sets of assemblies for two fuel cycles ($275 + 275 = 550$). A significant cost reduction for CRBRP was achieved by reducing the secondary control assembly total quantity requirements.

The secondary control assembly design changes to achieve the added in-core time are described in detail in Engineering Change Proposals G1371, G1408, and G1507. In summary, the design change was accomplished revising the diameter of the absorber pellets, revising the cross-section of the pin bundle filler rods, modifying the absorber clad tube structural criteria and use of a detailed elastic-plastic analysis of the clad tube upper end cap region. The dimensional changes to the absorber pellets diameter and filler rod section were made to accommodate the material swelling from the extended in-core time to prevent mechanical interferences. The higher helium pressure loading was handled by a modification to the clad tube steady state structural criteria which allows application of a function of minimum yield strength instead of the proportional elastic limit. The critical region at the clad tube top end cap weld annealed zone was shown to satisfy the criteria by a detailed elastic-plastic analysis.

3.7.2.3 Key Problems and Resolution

The secondary control assembly life limiting conditions, design criteria and analysis results are discussed in the following paragraphs.

Key factors limiting the design of the secondary control assembly are material swelling and clad stress from helium pressure loading. The absorber pellets (B_4C), Type 316 stainless steel (SS) 20% cold worked (CW) clad tube, and Type 316 stainless steel annealed pin bundle filler rods experience varying volume growth due to different operating temperatures, neutron fluence levels

and material swelling characteristics. The design requirement [1] limits maximum allowable swelling so that positive clearances between adjacent components are maintained. This requirement was satisfied by dimensional changes to the absorber pellet diameter and pin bundle filler rod section.

Dimensional changes made for the absorber pellets to achieve the design life extension while satisfying the positive clearance criteria are illustrated in Figure 3-42. Shown are the pellet stack dimensions for the initial 328 EFPDs design and the 603 EFPDs design. In the parked position the lower end of the pellet stack is in the highest fluence region. To accommodate the swelling at longer life time, the diameter of the pellet stack for the lower 10 inch portion was reduced by 0.030 inches. The diameter of the remaining pellets was increased proportionately to maintain total boron 10 minimum loading. Dimensional changes were made to the pin plenum spacers and the plenum spring to accommodate added stack growth.

Another dimension affected is the clearance between the filler rods and the absorber pins of the pin bundle assembly. The filler rods are made of annealed Type 316 SS which exhibits volume swelling an order of magnitude higher than the 20% CW Type 316 SS, the absorber clad tube material. In the parked position the lower end of the filler rods and clad tube extend below the B_4C column and are subjected to a higher fluence. This end is 95 inches above the core midplane when at the ready to scram position during reactor power operation. The additional swelling of the filler rods from the added in-core time was accommodated by slight reduction of the filler rod dimensions to increase the rod-to-pin spacing. The dimensional changes made are summarized in Figure 3-43. Thermal-hydraulic analysis [3] determined that the initial higher filler rod-to-pin spacing had no significant influence on the flow and temperature distribution within the pin bundle.

The extension of design life to 603 days resulted in a substantially higher end of life (EOL) internal helium pressure. The pin tube is sealed by welding at the end plugs. All material is 20% CW Type 316 SS. The cladding itself was of sufficient strength to contain the pressure but the end plug weld area posed a problem [4]. With conventional welding the fused area and some of the parent material on either side, called the heated affected zone, will have

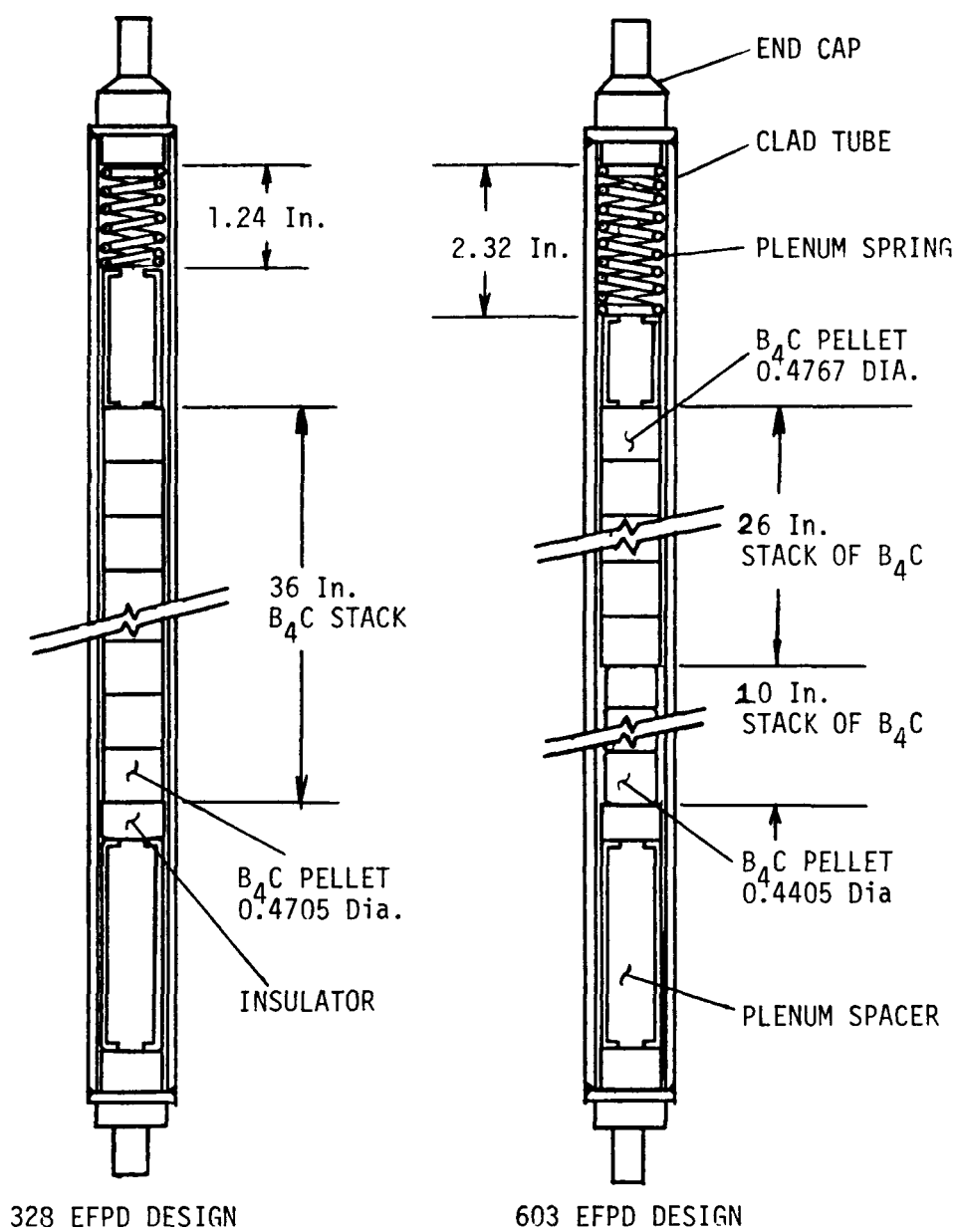


Figure 3-42 COMPARISON OF SYSTEM 31 SCRS ABSORBER PIN DESIGNS

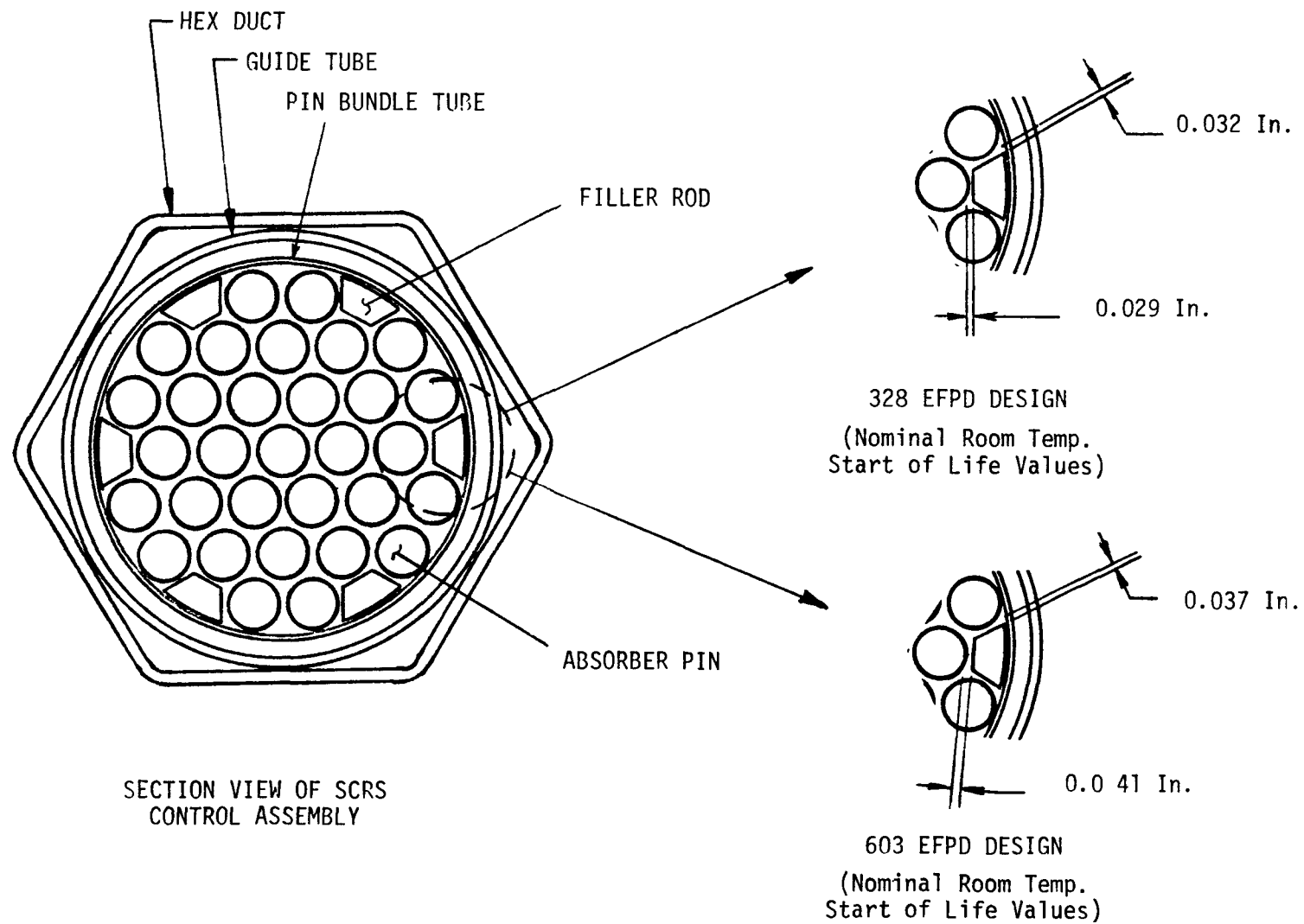


Figure 3-43

COMPARISON OF SYSTEM 31 SCRS CONTROL ASSEMBLY FILLER ROD TO ABSORBER PIN SPACING

their structural properties altered (20% cold worked to annealed). Consequently to be conservative in the analysis, the material properties of these regions were considered to be those of fully annealed Type 316 SS subject to environment condition changes. The design criteria of the specification document [1] at the time required all primary membrane stress intensities to be lower than the proportional elastic limit (PEL) for the material as a function of environmental exposure. A search for the origin of this limit revealed it to be an arbitrary choice that would be very conservative and likely to escape licensing criticism. As such, a more sophisticated approach, based on earlier efforts to quantify design limits for breeder core components, would safely extend the design space to allow the longer lifetime and be defensible. This new limit required assessing the ductility of the material (expressed as a ratio of yield to ultimate stresses) and imposing either a fraction of the ultimate stress or the PEL as the limit for materials of high and low ductility.

The design criteria change solved the primary stress intensity problem, but one problem remained: the upper end plug, which experienced little radiation embrittlement of the weld, could still fail from progressive plastic deformation (ratchetting) as calculated by elastic analysis methods. A detailed elastic-plastic finite element analysis of this region indicated plastic flow and subsequent stress redistribution would alleviate the progressive cycling and failure would not occur.

The primary design tool used for the absorber pin analysis was CONROD-II [2]. The use of the code required assembling a design data base and generating appropriate modeling inputs. The code determines helium generation rates, clad tube pressure, clad tube and pellet swelling, and clad tube stresses. Worst case design cases are determined from parametric studies of various parameters.

3.7.2.4 Final System Status

3.7.2.4.1 Status of Design/Analysis

The secondary control assembly 603 EFPDs design and analysis has been completed. The analysis report [4] was issued. Dimensional and requirements changes have been incorporated into affected engineering drawings and procurement and fabrication requirements specifications.

3.7.2.4.2 Outstanding Activities to Complete Design

Outstanding secondary control assembly design activities consisted of two items--completion of the SCA structural evaluation report and completion of the SCA design report. A preliminary structural evaluation report had been completed. Incorporation of the 603 EFPD analysis and other updates needed to be made. The SCA design report which discusses each SCA equipment design requirement [1] and how the requirement is satisfied had not been initiated.

3.7.2.5 References

1. Equipment Specification E-953013, (22A3952), "Secondary Control Assembly."
2. General Electric Co. Specification 23A3087, "Verification for Computer Code CONROD-II," January 1984.
3. J. P. Wei, "Thermal and Hydraulic Analysis of CRBRP Secondary Control Rod System," CRBRP-GEFR-00542 (DR), dated January 1984.
4. D. Heisser, "CRBRP-SCA 603 FPD Life Absorber Pin Analysis," CRBRP-GEFR-00710 (DR), dated April 1984.

3.7.2.6 List of Engineering Change Proposals (ECPS)

1. ECP G1371, Secondary Control Assembly Design Life.
2. ECP G1468, Secondary Control Assembly Plenum Spring.
3. ECP G1507, SCRS - Control Rod Assembly Filler Rod.

3.8 Core Transient Design Considerations

3.8.1 Introduction

Current design practice is that LMFBR components must meet the required conditions of ASME Code Section III[1] and RDT Standard C-16-1T[2]. Transient reactor design events are divided into categories of normal, upset, emergency and faulted according to their likelihood of occurrence. Table 3-39 presents: a) the definitions for the various incidents; b) the probability of occurrence of events in each category[3]; and c) the allowable severity with respect to structural consequences. Note that the RDT Standard terminology for the events are, respectively: normal operation, anticipated fault, unlikely fault and extremely unlikely fault.

Table 3-40 presents a summary of preliminary design limits for emergency and faulted events to assure that the core operates safely over its design lifetime and meets the requirements of the ASME code and RDT Standard. The frequency of occurrence and classification of events is established by the designer based on industrial and nuclear experience and also the special characteristics and differences in LMFBR design (as compared with an LWR, for example).

Under normal steady state operating conditions, the cladding is loaded due to the internal gas pressure. Fission gases are released from the fuel with burnup, and thus, the internal pressure continually increases over the rods' lifetime. This pressure produces a very slow and small creep deformation of the cladding. In addition to this long-term creep effect associated with steady state operations, there are two basic types of transients that could cause dynamic loading of the cladding. All the upset, emergency and faulted events which result in a cladding temperature increase can be categorized into one of these types.

The first type transient results from undercooling or slow overpower events which can cause an increase in the cladding temperature. If the transient is of sufficient duration, the fill gas (helium) and fission gas in the plenum of the rod may have time to heat up resulting in an increase of the internal rod

Table 3-39

EVENT CLASSIFICATION AND CLADDING DAMAGE SEVERITY LIMIT

<u>Definitions and Probability of Occurrence</u>	<u>Allowable Severity</u>
Normal: $(1.0)^*$ Any condition of system startup, design range operations, hot standby, or shutdown other than an upset, emergency, faulted or testing conditions.	No Damage: No damage is defined as: 1) no significant loss of effective fuel lifetime; 2) accommodations within the fuel and plant operating margins without requiring automatic or manual protective action; and 3) no planned release of radioactivity.
Upset: $(1-3 \times 10^{-2})^*$ Any abnormal incident not causing a forced outage or causing a forced outage for which the corrective action does not include any repair of mechanical damage.	Operational Incident: An operational incident is defined as an occurrence which results in: 1) no reduction of effective fuel lifetime below the design values; 2) accommodation with, at most, a reactor trip that assures the plant will be capable of returning to operation after corrective action to clear the trip cause; and/or 3) plant radioactivity releases that may approach the 10CFR20 guidelines.
Emergency: $(3 \times 10^{-2} - 10^{-4})^*$ Infrequent incident requiring shutdown for correction of the condition or repair of damage in the system. No loss of structural integrity.	Minor Incident: A minor incident is defined as an occurrence which results in: 1) a general reduction in the fuel burnup capability and, at most, a small fraction of fuel rod cladding failures; 2) sufficient plant or fuel rod damage that could preclude resumption of operation for a considerable time; and/or 3) plant radioactivity releases that may exceed 10CFR20 guidelines, but does not result in interruption or restriction of public use of areas beyond the exclusion boundary.
Faulted: $(10^{-4} - 10^{-7})^*$ Postulated event and consequences where integrity and operability may be impaired to the extent that considerations of public health and safety are involved.	Major Incident: A major incident is defined as an occurrence which results in: 1) substantial fuel and/or cladding melting or distortion in individual fuel rods, but the configuration remains coolable; 2) plant damage that may preclude resumption plant operations, but no loss of safety functions necessary to cope with the occurrence; and/or 3) radioactivity release that may exceed the 10CFR20 guidelines but are well within the 10CFR100 guidelines.
Normal Operation: Normal operation includes steady power operations and those departures from steady operation which are expected frequently or regularly in the course of power operations, refueling, maintenance or maneuvering of the plant.	
Anticipated Faulted: An off-normal condition which individually may be expected to occur once or more during the plant lifetime.	
Unlikely Faulted: An off-normal condition which individually is not expected to occur during the plant lifetime; however, when integrated over all plant components, events in this category may be expected to occur a number of times.	
Extremely Unlikely Faulted: An off-normal condition of such extremely low probability that no events in this category are expected to occur during the plant lifetime, but which nevertheless represents extreme or limiting cases of failures which are identified as design bases.	

*Probability of occurrence of events in this category (per reactor year).

TABLE 3-40
SUMMARY OF PRELIMINARY DESIGN LIMITS**

<u>Event Classification</u>	<u>Severity Level</u>	<u>Limit</u>
Emergency (Unlikely Faults)	Minor Incident	The total cumulative damage function is to be less than 1.0. The accumulated plastic and thermal creep strain is to be less than 0.3%.
Faulted (Extremely Unlikely Faults)	Major Incident	No cladding melting (temperature less than 2475°F) No sodium boiling (temperature less than saturation temperature at the existing pressure)

* Sodium boiling temperature is quoted as a limit in order to establish that no cladding melting can occur.

**See Table 3.8.1-1 for Upset Events requirements.

pressure and a resultant increase of the cladding loading. Simultaneously with the load buildup, the cladding becomes weaker due to its increased temperature. The cladding deformation, due to these effects, must be factored into structural evaluations of the cladding.

The second type of transient involves fast overpower events. Worst case transients of this type are extremely rapid such that the fuel temperature increases at a much faster rate than the cladding and coolant temperatures. The differential thermal expansion between fuel and cladding results in increased fuel-cladding interaction pressures. The gas in the rod plenum does not have time to heat up significantly with resultant pressure buildup as with the first type.

The evaluation of rod damage due to the various design transients over the rod lifetime is based on in-depth structural evaluation of the temperature effects in the fuel and cladding. In addition to the thermal response, (including expansion), effects such as cladding wastage, radiation effects on the cladding material properties, chemical interaction effects between the fuel and cladding and the sequence and/or combination of occurrence of the transients are also considered to demonstrate design adequacy of the rods.

In addition to the rods, the assembly duct and associated hardware are affected by thermal transients. The mechanism of damage in this case is thermal stressing in thicker portions (e.g., inlet shield block/orifice and outlet nozzle regions) with reactor coolant temperature changes.

Limiting design transients and their associated thermal conditions are presented herein for both the rods and duct hardware.

3.8.2 Limiting Design Event Selections

The design basis events for the CRBR assemblies are provided in OPDD-10[4]. Over the 30 year plant life, 850 shutdowns are conservatively designed for, of which 580 are due to upset, emergency and faulted events which cause scram of the control systems.

Considering all OPDD-10 events, less severe transients can be umbrellaed under more severe events to limit the number of structural evaluations required in the design analyses. Table 3-41 shows a summary of the limiting core assembly umbrella transients for the upset, emergency and faulted design categories. As can be noted, the assembly has been divided into three regions, each having its own design basis events, i.e., inlet nozzle/shield block region, rod region and duct/handling socket region. OPDD-10 provides sufficient description for the various design events that engineering judgment can be used in deciding which events are limiting. However, if any doubt exists for a particular transient, it is listed as one of the design umbrella events.

In Table 3-39 the U-2b event for the rod and duct/handling socket regions has been taken to envelop the following OPDD-10 transients:

- o U-2b @ 10 per 30 year plant life;
- o U-18 @ 16 per 30 year plant life;
- o U-3a @ 6 per 30 year plant life;
- o U-3b @ 15 per 30 year plant life;

Thus, the U-2b with a 30 year plant lifetime frequency of 47 occurrences, will envelop itself and the other three events indicated above. Considering the U-2b, c, d, e and OBE each explicitly as described by OPDD-10 means that all other upset transients can be enveloped under the normal scram event (U-1) with a 30 year lifetime frequency of 485 occurrences.

Implicit in this umbrellaing procedure is an assumed guideline that events which cause less than a 2°F core temperature increase before trip can be considered as a normal scram transient. Many of the OPDD-10 events are initiated by perturbations that occur outside the primary heat transport system, and thus, have little or no effect on the core except that they initiate a reactor scram (e.g., an inadvertent isolation and blowdown of the steam generator).

Table 3-41

ASSEMBLY UMBRELLA DESIGN
DUTY CYCLE EVENTS AND FREQUENCY

Inlet Nozzle/Shield Block Region (Seismic Category I)		Rod Region (Seismic Category II)		Duct/Outlet Nozzle Region (Seismic Category I)	
Event	Number for 30 Years(1)	Event	Number for 30 Years(1)	Event	Number for 30 Years(1)
<u>Normal</u>		<u>Normal</u>		<u>Normal</u>	
N-2	850	N-2	850	N-2	850
N-3	270	N-3	270	N-3	270
N-4(2)		N-4(2)		N-4(2)	
N-5	750 (each)	N-5	750 (each)	N-5	750 (each)
N-6	30×10^6	N-6	30×10^6	N-6	30×10^6
N-7	10^{10}	N-7	10^{10}	N-7	10^{10}
<u>Upset</u>		<u>Upset</u>		<u>Upset</u>	
U-2b	562 of worst	U-1	485	U-1	485
U-11a		U-2b	47	U-2b	47
U-18		U-2c,d	20	U-2c,d	20
U-21b		U-2e	10	U-2e	10
OBE(4)	4	OBE(4)	5	OBE(4)	4
<u>Emergency</u>		<u>Emergency</u>		<u>Emergency</u>	
E-7	} 1 of worst	U-18S(3)	} 1 of worst	U-18S(3)	} 1 of worst
E-15		E-7		E-7	
OBE(5)		60¢ step		60¢ step	
		E-16		E-16	
				OBE(5)	
<u>Faulted</u>		<u>Faulted</u>		<u>Faulted</u>	
SSE	} 1 of worst	60¢ step	} 1 of worst	SSE	} 1 of worst
F-2		with seismic		2\$/sec ramp	
F-4a		shutdown		F-2	
		2\$/sec Ramp			

NOTES: See next page

Table 3-41 (Continued)

FUEL/BLANKET ASSEMBLY UMBRELLA DESIGN
DUTY CYCLE EVENTS AND FREQUENCY

NOTES:

- (1) For first core fuel assemblies residing in reactor through cycle 1 only (128 full power days), the number of normal and upset events is 1/30 of the number specified above rounded up to the next largest whole number.

For first core fuel assemblies residing in reactor through cycles 1 and 2 (328 full power days), the number of normal and upset events is 1/15 of the number specified above rounded up to the next largest whole number.

For the blanket assembly duty cycles, the number of transients will be $n/30$ (rounded to the next largest number) where "n" is the design lifetime in reactor cycles. Note that this conservatively envelopes the first and second reactor cycles, which are 128 and 200 full power days, respectively, as equilibrium reactor cycles, which are 275 full power days.

- (2) Blanket rods for initial core will not be designed for load following.
- (3) U-18 event with shutdown from secondary control rod system.
- (4) OBE acting alone.
- (5) OBE in combination with U-2b (worst upset event) per load combination described in Reference 13.

Table 3-42 provides a brief word description for most of the Table 3-41 events. A more in-depth description for each can be found in Reference 4. Several transients are included in Table 3-41 which are not in Reference 4. These additional events can have a significant thermal effect on the core. Outside the core, the thermal transient appears as a normal scram event (under which they are enveloped for the plant). These extremely quick acting events have the following definitions with regard to the fuel design:

A. OBE (Operating Basis Earthquake)

As an upset event, the OBE is evaluated for the more damaging of the following two conditions: 1) a reactivity insertion is assumed due to the OBE, which causes a step increase in reactor power to 115%. A plant trip is assumed to occur five minutes after initiation of the OBE. It is expected, however, that this trip will not be automatic and that the plant will be designed to continue operation subsequent to the OBE; and 2) loss of pumping power concurrent with 30% step reactivity insertion. A decreased insertion speed of the primary shutdown system is also assumed to occur during the OBE.

For an emergency event for Seismic Category I structures, one OBE is evaluated as condition (1) up to reactor trip followed immediately by condition (2). For Seismic Category I structures, the maximum thermally induced load produced in the structure by this event is used in combination with the maximum seismic loads.

B. 60% Step Reactivity Insertion

A large step reactivity insertion of 60% is considered to demonstrate the adequacy of the Plant Protection System (PPS) to shut down the reactor within the allowable fuel damage limits. There are no realistic means of obtaining this size reactivity insertion to the core. Although hot rod temperatures will increase significantly very quickly before the core is shut down due to control rod scram, the average rod bundle coolant temperature change would be substantially less. The hot slug of coolant exiting the assemblies due to the

TABLE 3-42
NAMES OF UMBRELLA TRANSIENTS DESCRIBED IN OPDD-10

<u>Designation</u>	<u>Name</u>
<u>Normal Events</u>	
N-2	Normal Startup
N-3	Normal Shutdown
N-4	Load Following
N-5	Step Load Changes of $\pm 10\%$ of Full Load
N-6	Steady State Temperature Fluctuations
N-7	Steady State Flow Induced Vibrations
<u>Upset Events</u>	
U-1	Reactor Trip from Full Power
U-2b	Uncontrolled Rod Withdrawal from 100% Power with Delayed Manual Trip
U-2c	Uncontrolled Rod Withdrawal from Startup with Automatic Trip
U-2d	Uncontrolled Rod Withdrawal from Startup to Trip Point with Delayed Manual Trip
U-2e	Plant Loadings at Maximum Rod Withdrawal Rate
U-11a	Water Side Isolation and the Dump of Both Evaporators and the Superheater
U-18	Loss of Offsite Power
U-21b	Inadvertent Opening of Superheater Outlet Safety/Power Relief Valves
<u>Emergency Events</u>	
E-7	One Loop Natural Circulation Heat Rejection from Initial Two Loop Operation
E-15	Direct Heat Removal System (DHRS) Activation 24 Hours After Scram
E-16	Three Loop Natural Circulation
<u>Faulted Events</u>	
F-2	DHRS Activation without Steam Generator System Cooldown
F-4a	Saturated Steam Line Rupture

transient would have a small effect on hardware above the top of the core assemblies once it mixes with the relatively cooler outlet plenum fluid.

C. SSE (Safe Shutdown Earthquake)

A 60¢ step reactivity insertion, loss of offsite power and decreased insertion speed of the primary shutdown system is considered to occur during the SSE transient. The maximum thermally induced load produced in the structure by this event is used in combination with the maximum seismic loads.

D. 2\$/sec Ramp

A large ramp reactivity insertion of 2\$/sec is considered to demonstrate the adequacy of the Plant Protection System (PPS) to shut down the reactor within the allowable fuel damage limits. There are no identifiable means of obtaining this size reactivity insertion to the core. Although the hot rod temperatures will increase significantly very quickly before the core is shut down due to control rod scram, the average rod bundle coolant temperature change would be substantially less. The hot slug of coolant (exiting the assemblies) due to the transient would have a small effect on hardware above the top of the core assemblies once it mixes with the relatively cooler outlet plenum fluid.

Table 3-43 shows the shift in allowable damage as a function of shutdown system used (i.e., primary or secondary). OPDD-10 requires that the core be designed for either/or both shutdown systems functioning for a particular event. If primary scram is precluded, the designer is allowed to use a one category shift in damage severity. For example, the U-18 would be terminated by primary control rod action from a loss of electrical power trip (LOEP) in the Plant Protection System (PPS); however, if this trip is arbitrarily precluded, the event moves from upset to emergency and secondary control system action is taken (from a secondary system under-voltage

Table 3-43

SPECIFIC DESIGN REQUIREMENTS FOR THE REACTOR SHUTDOWN SYSTEMS(6)

<u>PLANT CONDITION</u>	<u>DAMAGE SEVERITY LIMITS</u>			
	<u>PRIMARY SYSTEM ACTION ONLY</u>		<u>SECONDARY SYSTEM ACTION ONLY</u>	
	<u>WITHOUT STUCK ROD</u>	<u>WITH STUCK ROD</u>	<u>WITHOUT STUCK ROD</u>	<u>WITH STUCK ROD</u>
Normal Operation	No Damage	No Damage	No Damage	Operational Incident
Anticipated Faults	Operational Incident	Operational Incident	Minor Incident	Minor Incident
Unlikely Faults	Minor Incident	Minor Incident	Major Incident	Major Incident
Extremely Unlikely Faults	Major Incident(1)	Major Incident(1)	Not a Design Basis(2)	Not a Design Basis(2)

(1) Combined probability of Extremely Unlikely Fault and complete failure of either of the shutdown systems is exceedingly low.

(2) Combined probability of two independent failures (extremely unlikely fault and failure of Primary Control Rod System) is exceedingly low and not appropriate as a design basis. However, as an exception, upon concurrent loss of off-site power, a Safe Shutdown Earthquake with a consequent step in reactivity insertion (60¢) and failure of the Primary Control Rod System, the Secondary Shutdown System shall be capable of shutting down the reactor without exceeding Major Incident limits.

PPS function). Thus, the emergency event listed in Table 3-41 as U-18S stands for the U-18 with secondary shutdown, and has been used as an emergency umbrella event. This effect of considering either or both control systems functioning for a particular shutdown has been used throughout the Table 3-41 logic, except for faulted events where only primary action needs to be considered since the combined probability of the event and failure of the primary system is so low that it is not part of the design basis.[4]

3.8.3 Modeling Approach for Overpower and Undercooling Transients

3.8.3.1 Typical Assumptions for Transient Core Analyses

The following conservative assumptions typify those applied for performing analyses on the various overpower and undercooling transients considered in the CRBR core design:

- A. Worst case Doppler coefficient including uncertainties[5] (maximum value is worst case for undercooling events and minimum value is worst case for overpower transients);
- B. Maximum time delay between scram signal and start of rod motion consistent with design requirements[6];
- C. Maximum decay heat with uncertainties included[7];
- D. Rods analyzed for worst time in life of initial four cycles (for most cases this is the condition resulting in maximum cladding and fuel temperatures);
- E. Uncertainties are applied in the form of hot channel factors to provide the desired level of confidence on the study.[12]

For the control system worth as a function of time after scram, the design required values given by Section 4.0 of the CRBRP PSAR are conservatively used. The actual scram worth will be substantially larger since these values

arbitrarily assume that the highest worth rod of either system is stuck and includes uncertainties and other worst case assumptions, for example, the most limiting banking position of the primary rods. Tables 3-44 and 3-45 present the design required values for non-seismic events. Seismic studies are not required for thermal analyses on the blanket rods because of their large inertial effects to be described later. Overpower seismic conditions do not result in a significant thermal transient. Both the OBE and SSE transient can be readily enveloped by the upset event U-2b.

The CRBR primary pumps have specified conditions which limit their range of flow coastdown rate when they are de-energized[10]. Worst case characteristics are incorporated (e.g., minimum pump and drive system inertia, minimum impeller efficiency, maximum pump friction, maximum primary heat transport system pressure drop, etc.). Additionally, the pony motors have a minimum specified flow of 7.5% full flow[10]. Figure 3-44 shows the quickest and slowest rate of core flow coastdown which can occur.

3.8.3.2 Computer Code Description

FORE-2M[8] is used to evaluate the core temperatures. Reactor inlet temperature and flow rate for the transients are input from the DEMO code[9] which analyzed the coupled thermal-hydraulics of the primary, intermediate and steam loops.

FORE-2M is a coupled thermal-hydraulics point-kinetics digital computer code designed to calculate significant reactor core parameters under steady state conditions or as functions of time during transients. Variable inlet coolant flow rate and temperature are considered. The code calculates the reactor power, the individual reactivity feedbacks, and the temperature of coolant, cladding, fuel, structure and additional material for up to seven axial positions in the core. Various Plant Protection System trip functions can be simulated, and the control rod shutdown worth prescribed as a function of time after the trip signal. By specifying appropriate hot channel/spot factors, the transient behavior of an average, peak and hot rod can be analyzed. The heat of fusion accompanying fuel melting and the spatial and time variation of the fuel-cladding gap coefficient (due to changes in gap size) can be

Table 3-44

CORE MAP REV. 4 PRIMARY CONTROL ROD SYSTEM
SCRAM INSERTION REQUIREMENTS

<u>Time After Current Interruption to Sole- noid Valve (Seconds)</u>	<u>Reactivity Insertion - \$</u>	
	<u>Normal Scram</u>	
	<u>100% Power</u>	<u>0-Power</u>
0	0	0
.05	0	0
.10	0	0
.15	.05	.06
.20	.12	.13
.25	.25	.27
.30	.43	.42
.35	.69	.62
.40	.94	.86
.407	.98	
.45	1.23	1.07
.46		1.11
.50	1.48	1.30
.55	1.74	1.48
.59		
.60	2.02	1.67
.65	2.28	1.89
.70	2.53	2.08
.75	2.81	2.28
.80	3.05	2.44
.85	3.28	2.61
.90	3.50	2.74
.95	3.70	2.89
1.00	3.92	3.02
1.05	4.06	3.17
1.10	4.20	3.28
1.15	4.35	3.39
1.20	4.50	3.50
1.25	4.61	3.59
1.30	4.75	3.69
1.35	4.85	3.74
1.40	4.97	3.78
1.50	5.17	3.94
1.60	5.37	4.05
1.70	5.51	4.14
1.80	5.63	4.22
1.90	5.71	4.30
2.00	5.79	4.39
2.05		4.41

Table 3-45

CORE MAP REV. 4 SECONDARY CONTROL ROD SYSTEM
SCRAM INSERTION REQUIREMENTS

<u>Time After Current Interruption to Sole- noid Valve (Seconds)</u>	<u>Reactivity Insertion - β</u>	
	<u>Normal Scram Event Full Flow</u>	<u>Normal Scram Event 40% Flow</u>
0.0	0	0
0.1	0	0
0.2	0	0
0.3	.09	.03
0.4	.26	.11
.5	.60	.30
0.595	1.08	.57
0.695	1.85	1.08
0.75	2.29	1.42
0.795	2.62	1.72
.9	3.29	2.39
1.0	3.84	3.05
1.1	4.34	3.59
1.2	4.65	4.02
1.3	4.87	4.45
1.4	5.08	4.72
1.5	5.21	4.95
1.6	5.32	5.20
1.7	5.38	5.36
1.8	5.45	5.45
1.85	5.47	5.47

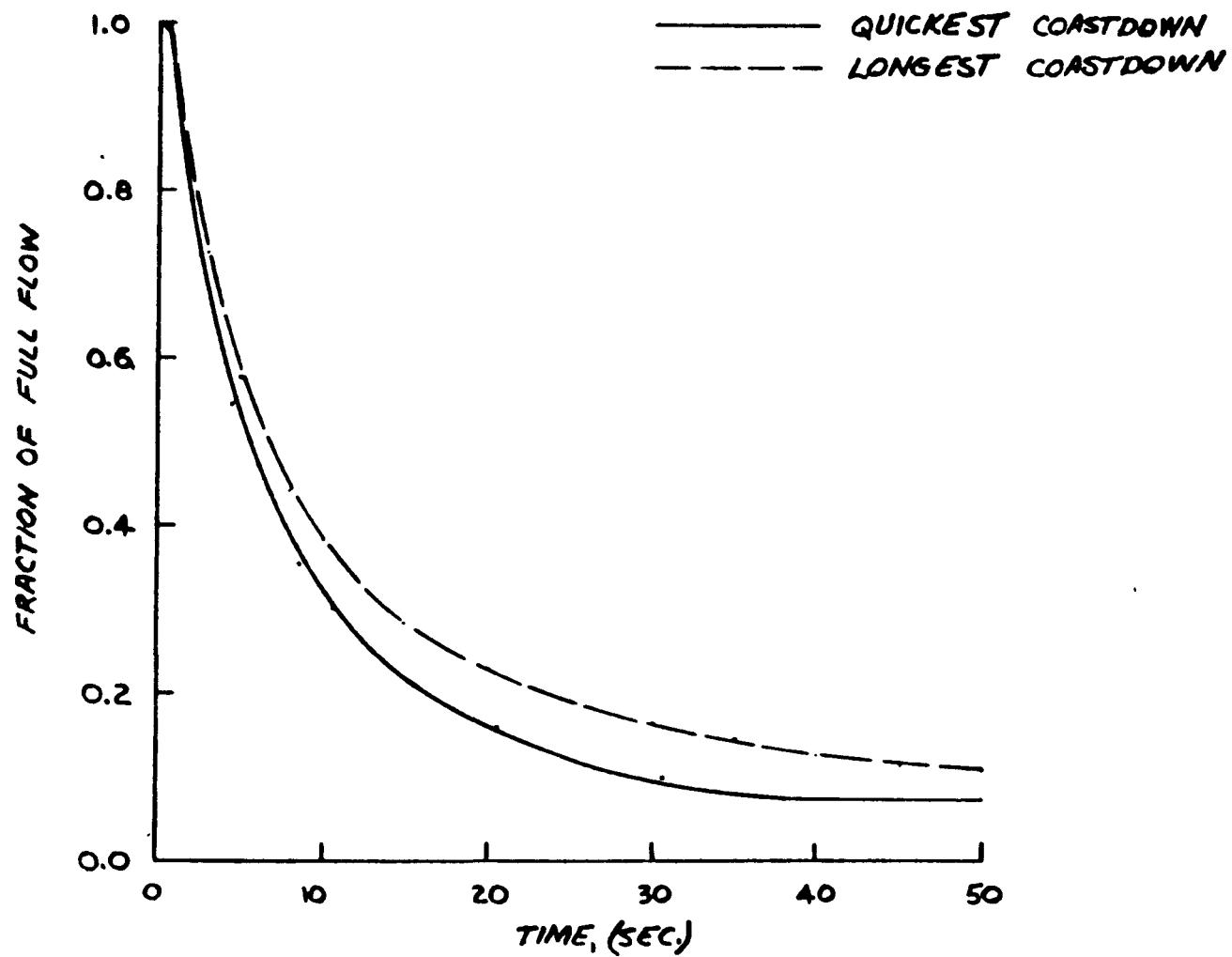


Figure 3-44

LONGEST AND QUICKEST FLOW COASTDOWN RATES
FOR CRBR PRIMARY SYSTEM

considered. The feedback reactivity includes contributions due to the Doppler effect, coolant density changes and dimensional changes (including bowing and radial expansion). FORE-2M is valid only while the core retains its initial geometry.

The FORE-2M thermal-hydraulic model for the CRBR fuel and blanket rods used seven axial nodes. As many as ten radial nodes can be used in the fuel; however, seven have been typically used. Average cladding temperature plus inner and outer cladding temperatures are calculated for each of the seven axial sections. Likewise, a coolant temperature is calculated for the average and outlet of each axial section. An under-the-wire-cladding temperature calculation is made for the hot rod at the maximum temperature position. Figure 3-45 shows schematically the relative axial positions of thermal analysis on a rod.

3.8.3.3 Hot Channel Uncertainties

The rod hot channel analyses use what is termed the "semi-statistical" approach of combining uncertainty factors. Table 3-46 and Table 3-47 present the nuclear uncertainties for the fuel and blanket assemblies, respectively and Table 3-48 presents the engineering uncertainties which were utilized in the transient studies presented in this report. These tables are consistent with those used in Reference 12. As can be noted, each consists of direct (non-statistical) and statistical type factors. Figure 3-46 shows graphically the application of the semi-statistical method. The nominal temperature difference $\Delta T_{j,nom}$ of component j is increased by the nominal nuclear radial and axial peaking factor to give the peak temperature difference $\Delta T_{j,peak}$. Next, by applying the direct uncertainty factors, one arrives at the temperature difference indicated as $\Delta T_{j,dir}$. It is about this temperature that the statistical uncertainties are applied. A 3σ analysis is exemplified by the temperature indicated as $T_{j,3\sigma}$.

This semi-statistical approach has been accepted as a conservative approach to core design since it was originally formulated by Chelemer and Tong in 1962[11] for PWRs.

NOTE: Typical maximum fuel temperature position is at core midplane and typical maximum cladding position is at top of active core.

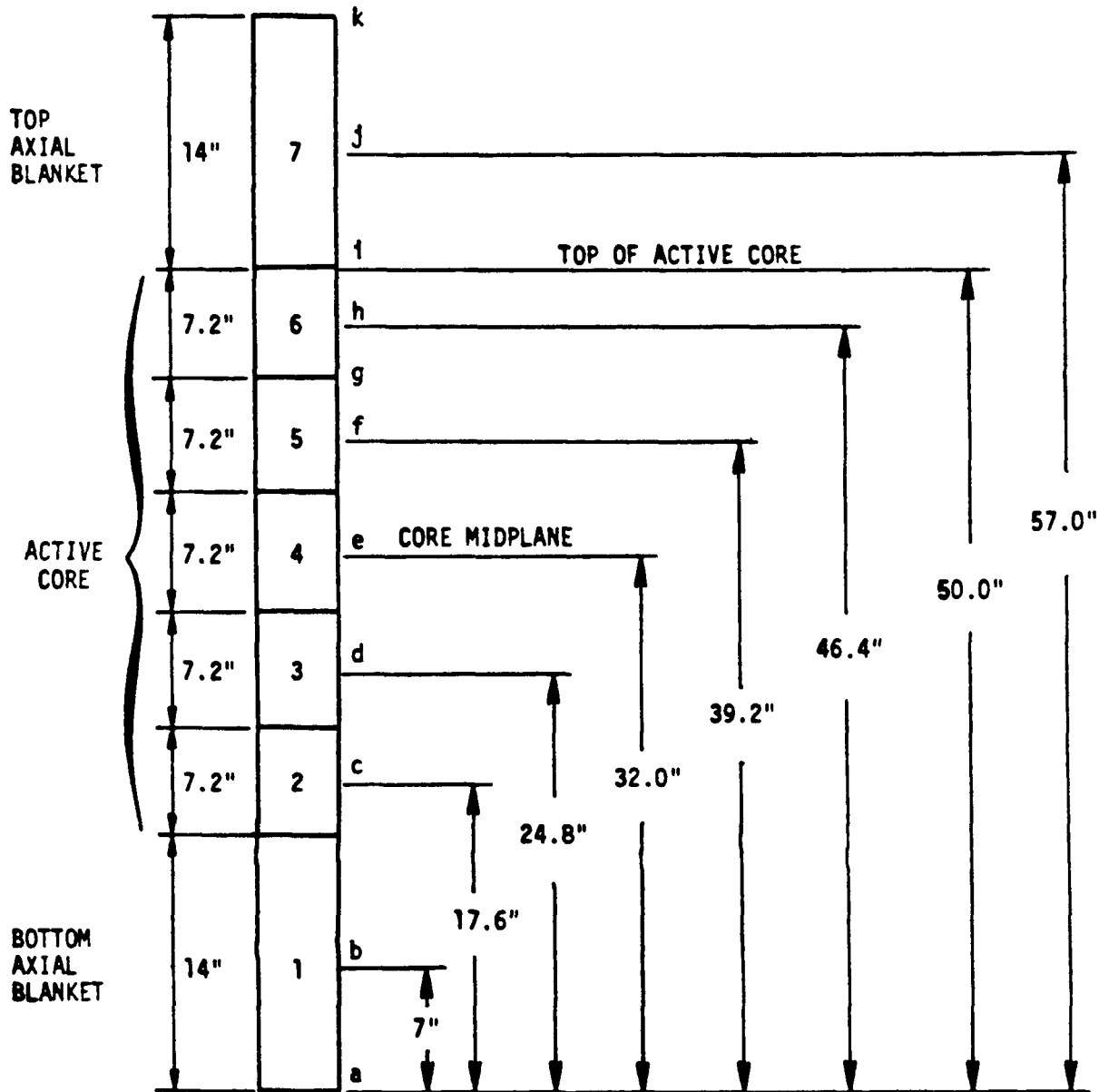


Figure 3-45 RELATIVE AXIAL LOCATIONS FOR HOT ROD TEMPERATURES

Table 3-46

CRBR FUEL ASSEMBLIES ROD TEMPERATURE NUCLEAR UNCERTAINTY FACTORS
WITH AND WITHOUT CONTROL ASSEMBLY INFLUENCE

<u>DIRECT^(o)</u>	<u>COOLANT</u>	<u>HEAT FLUX</u>
Physics Modeling	1.02 ⁽¹⁾	(*)1.02(1.10) ⁽¹⁾
Control Rod Banking	1.02 ⁽²⁾	1.02 ⁽²⁾
ZPPR-7 Flux Tilt	1.0 ⁽⁴⁾	1.0 ⁽⁴⁾
 <u>STATISTICAL (3σ)^(o)</u>		
Nuclear Data	1.07	1.07
Criticality	1.01 ⁽³⁾	1.01 ⁽³⁾
Fissile Fuel Maldistribution	1.03	1.03

If assembly is influenced by adjacent control rod, replace with:

		<u>COOLANT</u>		<u>HEAT FLUX</u>			
		<u>BOL</u>	<u>EOL</u>	<u>"Peak Power Position"</u>		<u>"Top of Core"</u>	
				<u>BOL</u>	<u>EOL</u>	<u>BOL</u>	<u>EOL</u>
1) Physics Modeling	Adjacent	1.04	1.02	1.03	1.02	1.15	1.15
	Far Side	1.01	1.02	.95	1.02	1.30	1.15
2) Control Rod Banking	Adjacent	1.04	1.02	1.04	1.02	1.01	1.02
	Far Side	1.02	1.02	1.02	1.02	1.01	1.02
3) Criticality	Adjacent	1.04	1.04	1.04	1.04	1.0	1.01
	Far Side	1.01	1.01	1.01	1.01	1.03	1.01

4) ZPPR-7 Flux Tilt - Assy's. 9, 10, 13, 14, 15, 16, 17, 23, 25, 37, 38, 41, 42, 43, 44, 45, 51, 53 (0.97 @ BOL, 1.0 @ EOL). Assy's. 8, 11, 19, 36, 39, 47, 65, 68, 101, 104 (0.99 @ BOL, 1.0 @ EOL). Assys. 62, 98 (0.99 @ BOL, 1.0 @ EOL).

(*)Non-parenthesized value applies at the peak power position (i.e., core midplane). Parenthesized value applies at the core lower/upper axial blanket interface except as superseded by note (1).

(o)Engineering Uncertainty Factors are given on Table 3-48

Table 3-47

CRBR INNER/RADIAL BLANKET ASSEMBLIES ROD TEMPERATURE NUCLEAR UNCERTAINTY FACTORS (14)

	<u>INNER BLANKET</u>		<u>RADIAL BLANKET</u>			
	<u>COOLANT</u>	<u>HEAT FLUX</u>	<u>COOLANT</u>	<u>Row 1</u>	<u>COOLANT</u>	<u>Row 2</u>
				<u>HEAT FLUX</u>		<u>HEAT FLUX</u>
<u>DIRECT (BOL)(o)</u>						
Physics Modeling	1.06	(*)1.07(1.11)	1.07	(*)1.07(1.02)	1.03	(*)1.07(0.99)
Control Rod Banking	1.02	1.02	1.02	1.02	1.02	1.02
Nuclear Data	1.12	(*)1.10(1.17)	1.13	(*)1.11(1.18)	1.27	(*)1.24(1.32)
Criticality	1.02	1.02	----	----	----	----
Heavy Metal	1.01	1.01	1.01	1.01	1.01	1.01
U-235	1.01	1.01	1.01	1.01	1.01	1.01
<u>DIRECT (EOL)(o)</u>						
Physics Modeling	1.02	(*)1.04(1.12)	1.01	(*)1.07(1.05)	1.01	(*)1.07(1.02)
Control Rod Banking	1.02	1.02	1.02	1.02	1.02	1.02
Nuclear Data	1.03	(*)1.00(1.07)	1.05	(*)1.03(1.10)	1.15	(*)1.13(1.21)
Criticality	1.01	1.01	----	----	----	----
Heavy Metal	1.01	1.01	1.01	1.01	1.01	1.01
U-235	1.0	----	----	----	----	----

(*)Non-parenthesized values apply at the peak power position (i.e., near core midplane). Parenthesized values apply at core upper/lower axial extension interface.

(o)Engineering Uncertainty Factors are given on Table 3-48

Table 3-48

CRBR FUEL/BLANKET ASSEMBLIES ROD TEMPERATURES ENGINEERING UNCERTAINTY FACTORS (12)

	<u>COOLANT</u>	<u>FILM</u>	<u>CLADDING</u>	<u>GAP</u>	<u>FUEL</u>	<u>HEAT FLUX</u>
<u>DIRECT</u> (o)						
Power Level Measurement and Control System Dead Band	1.03(1.0)					1.03
Inlet Flow Maldistribution	1.07					
Flow Distribution Computational Uncertainty (Simulation Bias)	1.03	-----1.03				
Cladding Circumferential Temperature Variation		1.0(+)	2.2(*)	1.0(+)		
<u>STATISTICAL</u> (3o)(o)						
Reactor ΔT Variation	1.0(1.144)					
Wire Wrap Orientation	1.01					
Subchannel Flow Area	1.035	1.0				
Film Heat Transfer Coefficient		1.21				
Pellet-Cladding Eccentricity		1.15	1.15			
Cladding Thickness and Conductivity			1.12			
Gap Conductance				1.48(ϕ)		
Fuel Conductivity					1.10	
Coolant Properties	1.01					
Flow Distribution Computational Uncertainty (Calibration)	1.199(1) 1.10(2)	-----1.056				

(*) For cladding midwall temperature calculations. Applies to the nominal temperature drop between cladding midwall and bulk coolant.

(+) For fuel temperature calculations

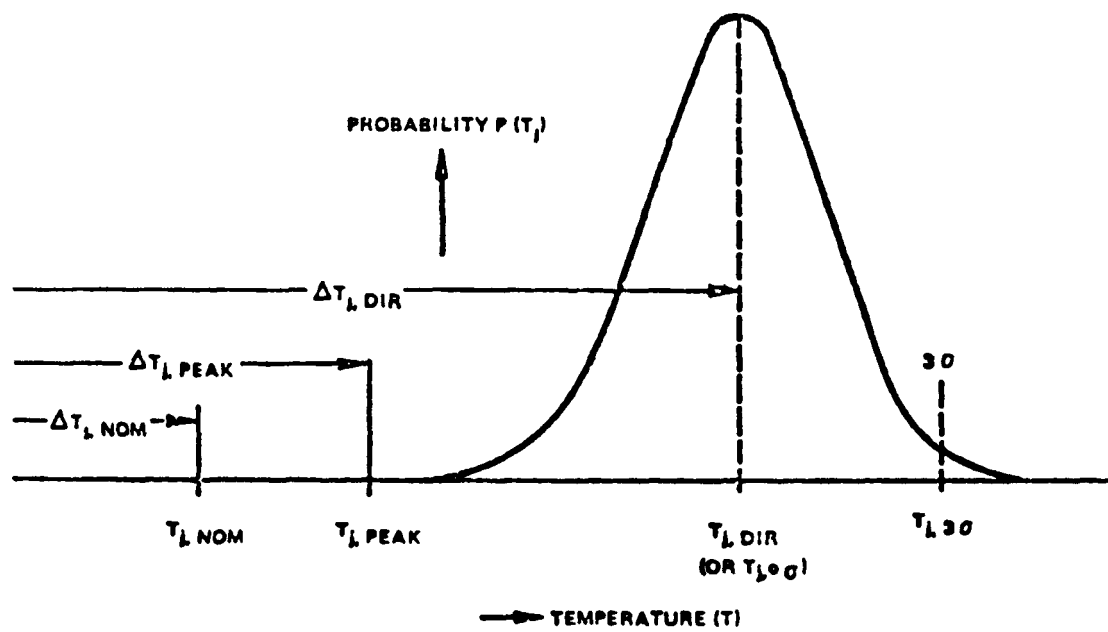
(ϕ) Applies to BOL conditions

(o) Nuclear Uncertainty Factors are given on Tables 3-46 and 3-47

(1) Inner Blanket Assembly

(2) Radial Blanket Assembly

NOTE: Same values of subfactors apply to both Plant T&H and Expected Operating conditions except when two values are given; in this case, the parenthesized values apply to Plant Expected Operating conditions while the non-parenthesized values apply to T&H Operating conditions.



GRAPHICAL ILLUSTRATION OF SEMI-STATISTICAL METHOD

Figure 3-46

3.8.3.4 Sensitivity to Key Parameters

As indicated in the earlier discussions of this section, the thermal transient analyses are performed by the "worst case method". Conditions for the studies are set at conservative values that, for example, result in maximum power and minimum flow for cases where maximum temperatures are of concern.

The analyses consider both the highest power hot rod and maximum cladding temperature hot rod with 3σ uncertainties applied. The gap conductance uncertainty is applied in the direction of using the minimum value. Past sensitivity studies have indicated that this is a conservative assumption for predicting fuel, cladding and coolant temperature changes during undercooling type events. The minimum gap conductance tends to decrease the rate of stored heat release from the rods such that larger amounts of energy are released to the coolant at lower levels of flow (later in the pump flow coastdown). With regard to overpower events, severe transient studies indicate that the cladding temperature will be underpredicted by several degrees Fahrenheit for minimum gap conductance; however, the most severe fuel temperatures are obtained. If a much larger gap conductance were used (factor of two), the fuel temperature would be decreased by several hundred degrees as compared to only an insignificant increase in cladding temperature. Since important parameters for severe overpower events are fuel melting and fuel cladding interaction (which is more severe for the higher fuel temperatures), the minimum gap conductance also formulates the worst case basis for overpower transients.

The FORE-2M calculations which were performed for the CRBR core temperature predictions during natural circulation did not incorporate transient flow redistribution effects. That is, the relative flow fraction established by orificing for each assembly is maintained at its full power/flow operating value throughout the transient. Also, the flow distribution for the hot channel with an assembly is maintained at its full flow value. In reality, for design events involving low flow and high core temperature such as three-loop natural circulation, significant flow redistribution will occur which moderates maximum temperatures, i.e., mechanisms of flow redistribution exist between the various core orificing zones and types of assemblies (fuel,

radial blanket and control). First, flow redistribution can occur between assemblies, i.e., inter-assembly flow redistribution where the higher temperature assemblies tend to receive enhanced flow due to buoyancy effects. Second, flow redistribution can occur within the assemblies, i.e., intra-assembly flow redistribution where hotter subchannels tend to receive more flow due to buoyancy effects at low flows. In addition to the buoyancy effect, the long transport time for the coolant through the core at low flow conditions results in assemblies operating with rather flat temperature profiles due to the increased time for heat transfer by conduction between subchannels.

The FORE-2M analyses also neglect inter-assembly heat transfer. At low velocities, the significant transport time for the coolant would allow cooling of hotter assemblies by heat conduction to surrounding cooler assemblies. Although neglecting the inter-assembly heat transfer is known to be a conservatism, quantifying the phenomena is difficult due to the strong coupling of inter- and intra-assembly transient flow effects with the inter-assembly transient heat transfer effects between the fuel assemblies, radial blanket assemblies, control assemblies, and other core regions, all of which act as parallel flow channels. The net effect of neglecting inter- and intra-assembly heat and flow redistribution for natural circulation analysis is an over-prediction of the hot rod temperatures by perhaps as much as several hundred degrees Fahrenheit.

CRBRP had an in-depth Natural Circulation Verification Program (NCVP), which improved the inter and intra-assembly flow and heat redistribution calculational capability by development of a system of verified computer codes. One of these codes is FORE-2M; another is COBRA-WC (dynamic whole-core version). This latter code was used to perform the dynamic prediction of coupled inter and intra-assembly flow and heat redistribution when increases in design margins were desired [15].

3.8.4 Summary of Core Transient Technical Papers

Early in the conceptual design phase, the ability to evaluate a reactor design for transient response, and take appropriate corrective action would minimize analysis costs. The effect of rod diameter, fuel type, and peak power rating on the transient response is studied in Reference 14, "Generalized Transient Characteristics and Capabilities of Liquid Metal Fast Breeder Reactor Fuel and Blanket Rods." Methods to incorporate this information into an optimized core thermal-hydraulic design are also provided.

During a terminated accident, detection by the Plant Protection System and subsequent control rod scram limits the effect of the transient. Such events are either undercooling or overpower transients. Guidelines to evaluate their severity based on fuel melting and coolant boiling and the treatment of uncertainties are discussed in Reference 15, "Typical Nuclear Kinetic/Thermal-Hydraulic Characteristics of a Heterogeneous LMFBR Core Under Terminated Transient Conditions."

FORE-2M is a coupled thermal-hydraulic/nuclear kinetic computer code used for core transient performance predictions. Extensive verification of the FORE-2M code was performed for any type of design transient considered in order to gain confidence to predict or reasonably envelope the true thermal response. The verification of FORE-2M is described in Reference 16, "Verification Study of the FORE-2M Nuclear/ Thermal-Hydraulic Analysis Computer Code."

There are several special phenomena that must be considered in the development of transient core analysis computer codes. The dynamic effects of core assembly bowing, localized power perturbations, and pellet-to-cladding gap conductance are analytically characterized in Reference 17, "Special Topics of LMFBR Core Transient Performance".

3.8.5 References

1. ASME Boiler and Pressure Vessel Code, Section III - Division 1, Subsection NB, Class 1 Components, 1974 edition, July 1974.

2. RDT Standard, RDT-C-16-1T, "Supplemental Criteria and Requirements for RDT Reactor Plant Protection Systems," December 1969.
3. J. Graham, L. E. Strawbridge and P. Bradbury, "Safety of the Clinch River Plant," Nucl. Eng. Int., 19, No. 221, pp. 854-857, October 1974.
4. OPDD-10, "CRBRP; Overall Plant Design Description," Revision 23, June 15, 1977.
5. WARD-D-0198, "CRBRP Nuclear Design Methodology", to be issued.
6. SDD-99, Rev. 16, "Plant Protection System," October 20, 1977.
7. WARD-D-0194, "Decay Power Predictions of the CRBRP AFMS Design," to be issued.
8. J. V. Miller and R. D. Coffield, "FORE-2M: A Modified Version of the FORE-II Computer Program for the Analysis of LMFBR Transients," CRBRP-ARD-0142, May 1976.
9. "Clinch River Breeder Reactor Plant Nuclear Island Simulation Model DEMO," CRBRP-ARD-0005, February 1978.
10. SDD-51A, Rev. 12, "Primary Heat Transport Systems," August 25, 1977.
11. R. Chelemer and L. S. Tong, "Engineering Hot Channel Factors for Open-Lattice Cores," Nucleonics 20, No. 9, p. 68 (1962).
12. WARD-D-0210, Rev. 1, "Predicted Steady State Thermal-Hydraulic Performance of the Fuel and Blanket Assemblies in the CRBRP Heterogeneous Core," to be issued.
13. WARD-D-0037 Rev. 1, "CRBRP; Seismic Design Criteria for the Clinch River Breeder Reactor," May 1977.
14. R. D. Coffield, Jr., R. A. Markley, P. W. Dickson, and H. C. Anderson III, "Generalized Transient Characteristics and Capabilities of Liquid Metal Fast Breeder Reactor Fuel and Blanket Rods," NUREG/CP-0014 Vol. 3, 1980, NRC, Public Document Room, 1718 H Street, Washington, DC.
15. R. D. Coffield, Jr., R. A. Markley, K. D. Daschke, and R. K. Sievers, "Typical Nuclear Kinetic/Thermal-Hydraulic Characteristics of a Heterogeneous LMFBR Core Under Terminated Transient Conditions," ASME 82-NE-25, 1982.
16. R. D. Coffield, Y. S. Tang, and R. A. Markley. "Verification Study of the FORE-2M Nuclear/Thermal-Hydraulic Analysis Computer Code," Nucl. Engrg. Des. 68 (1981) 323-336.
17. R. D. Coffield, J. V. Miller, and R. A. Markley, "Special Topics on LMFBR Core Transient Performance," Nucl. Engrg. Des. 68 (1981) 337-345.

3.9 Upper Internals Structure Design

3.9.1 Functional Requirements and Design Conditions

The Clinch River Breeder Reactor Plant, Upper Internals Structure (UIS) is located directly above the reactor core as shown schematically in Figure 3-47. The UIS is a welded Type 316 stainless steel structure supported by jacking mechanisms located on the head. The welded structure is protected by Alloy 718 in regions subject to direct core effluent impingement. The primary functions provided by the UIS are: provide support for routing all in-vessel instrumentation to core assemblies; maintain alignment between the core assemblies, the UIS and the closure head; provide guidance and cross-flow protection for the control rod drivelines; and mix/duct flow to the upper region of the vessel outlet plenum to minimize rapid temperature changes in components during a reactor trip.

The UIS was initially conceived as a Type 316SS structure of low thermal inertia protected by Alloy 718 on surfaces exposed to high frequency temperature variations (high cycle fatigue). The low thermal inertia structure minimized self restraint loads induced as a result of thermal transients in the flowing sodium. The stiffness required for control of flow-induced vibration response was also designed into the low thermal inertia structure. The structural concept has not changed during the design process. However, significant changes have been made in the manner of directing flow through the UIS.

3.9.1.1 Functional Requirements

The UIS is located in the reactor outlet plenum above the core. This region experiences a severe thermal environment, high temperature and rapid changes in temperature. The UIS must be designed to perform satisfactorily in this environment for a life of 30 years. The UIS must also be designed to meet the following functional requirements. Figure 3-47 provides identification of the parts and components used in delineating these functional requirements.

BEST AVAILABLE COPY

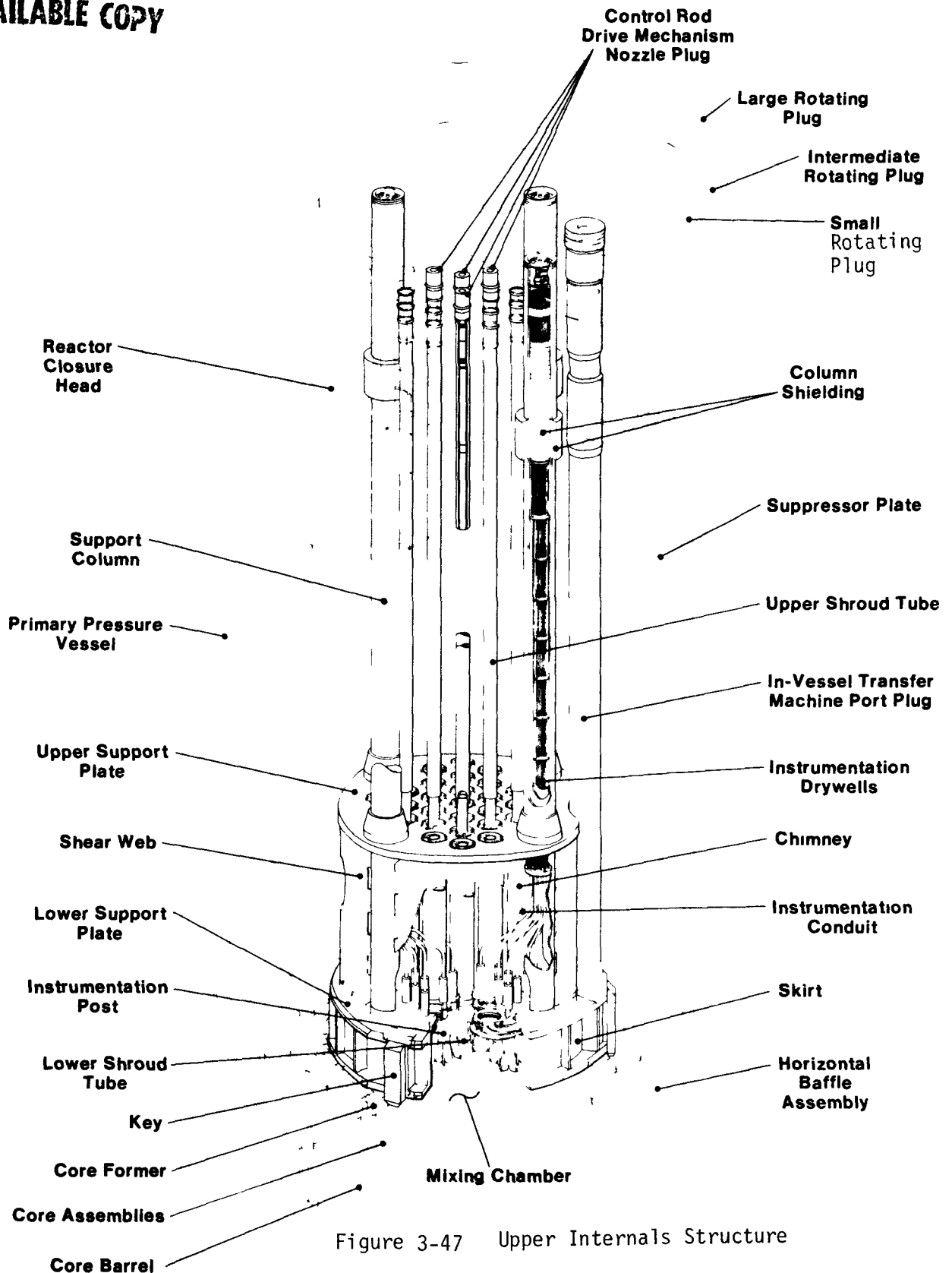


Figure 3-47 Upper Internals Structure

- A. Provide backup mechanical holddown to limit the motion of the reactor core assemblies to 1.0 inch during reactor operation. A conservative design basis resulted in postulating that core assemblies could move up during reactor operation. The allowable motion is limited by reactivity insertion produced if the fuel and blanket assemblies drop downward. A seismic event could induce downward motion of the assemblies. The allowable reactivity insertion (10¢) was established by the fuel assembly damage limit.
- B. Provide capability of positioning and supporting replaceable thermocouples over the core assemblies. A small number (30) of above core thermocouples are required for reactor control. A distribution of exit temperatures at fuel and blanket assemblies is desired to provide diagnostic information; all fuel assemblies and most of the blanket assemblies are monitored.
- C. Provide routing and support of diagnostic instrumentation for the UIS and the outlet plenum. Thermocouples are located on the UIS to provide temperature data on the structure. Accelerometers are provided to measure loads during initial full flow tests.
- D. Assure alignment of the UIS shroud tubes to within 0.120 inch with the core control rod ducts. The control rod driveline is enclosed in the UIS shroud tubes, and the UIS is keyed to the core barrel. Excessive lateral displacement of the UIS relative to the control rod ducts could cause retardation of the control rods during a reactor trip. The allowable displacement was determined from the total reactor system alignment analysis. This analysis included the control rod driveline interactions resulting from all reactor vessel and internals fabrication tolerances and installation gaps.
- E. Provide ducting of the core effluent to the upper region of the outlet plenum. Sodium exiting from the core could result in stratification or incomplete mixing. This condition must be controlled to insure an acceptable cumulative damage on the vessel

and primary system hot leg components. The UIS mixing chamber and chimneys duct the core effluent to the upper region of the outlet plenum insuring proper flow mixing prior to reaching the reactor vessel or hot leg components.

- F. Protect the control rod drivelines from cross flow in the outlet plenum. Core effluent flows upward until turned radially outward by the suppressor plate. Radial flow could induce vibration of the control rod drivelines if they were not protected from this flow. The UIS shroud tubes provide this protection by isolating the control rod drivelines from cross flow.
- G. Provide scram arrest dashpots for the primary control rod drivelines. Retardation of the downward velocity of the control rods after a scram is provided by sodium filled dashpots. These dashpots must be in the sodium pool where they are always primed and must not be positioned close to the core, where the ductility loss would be unacceptable. These constraints, plus the need for a structure to react the loads, position the dashpots in the upper shroud tubes of the UIS.
- H. Provide access for refueling and for the replacement of the lower inlet modules. Geometric constraints on the rotating plugs dictate that the in-vessel transfer machine (IVTM, refueling machine) be positioned to penetrate the outer region of the UIS. The size of the opening was established by the envelope diameter required to remove the lower inlet modules. This capability permits major modifications to the core by replacement of the lower inlet modules. Proper flow control requires that the IVTM hole through the UIS be plugged with the IVTM Port Plug during reactor operation.
- I. Provide lift capability to the UIS to allow rotation of a portion of the closure head for refueling. The UIS has keys at its lower elevation which engage with slots in the core former rings. Refueling requires rotation of the UIS and thus disengagement of the

keys. The jacking mechanism raises the UIS 9.50 inch to disengage the keys and provide a working clearance between the UIS and reactor components.

- J. Provide shielding at the UIS column penetrations in the intermediate rotating plug (IRP) of the closure head. Radiological requirements limit the dose rate to 25 mRem per hour at the top of the head. Meeting this dose rate requires elimination of streaming paths around the four columns, which in turn requires a step shield (collar) surrounding the columns just below the vessel head.
- K. Provide seals at the column penetrations in the IRP, to be compatible with the head access area dose rate limit. Purge gas flow into the jacking mechanism housing is at a higher pressure than the cover gas. Dual elastomer seals at the top of the jacking mechanism housing and piston ring seals adjacent to the cover gas combine to form an effective seal/purge system.

3.9.1.2 Design Conditions

A. Codes and Standards

The UIS contains two general regions for purposes of American Society of Mechanical Engineers (ASME) Boiler and Pressure Vessel (B&PV) Code Classification, the region which forms a part of the primary pressure boundary and the region within the primary pressure boundary. The support columns above and in the zone passing through the cover gas seal as well as the IVTM port plug cap are classified as ASME Class 1 appurtenances. The UIS in the region inside of the primary pressure boundary performs a core support function. However, subsection NG of the Code does not now apply for temperatures in excess of 800°F. Accordingly, until the ASME B&PV code covers this region, the UIS was designed and will be constructed as a Class 1 component per subsection NG and NB of Section III of the Code. As the operating temperature is above 800°F, the design is in compliance with Code Cases 1592, 1593, 1594 and 1595 [1] supplemented by more restrictive

portions of subsection NG and by Reactor Development and Technology Standards (RDT) E15-2NB, RDT F9-4 and RDT F9-5 [2-4]. This approach, on the portion of the UIS not covered by the Code, assures the maximum likelihood of minimizing changes in the event that an applicable ASME Document is issued prior to placement of the fabrication contract.

B. Design Conditions

The UIS design conditions include the following:

- o Mechanical loads which include the dead weight load of 100,000 pounds and applied loads induced during fabrication, handling, shipping and installation. In addition to the normal column support of the UIS, the UIS keys must support the dead weight load during installation and the frictional forces induced as a result of deflecting the UIS while lowering it into the keyed positions.
- o The design pressure for the Class 1 appurtenances is an internal pressure of 15 psig and a minimum internal pressure of -14.7 psig. The design pressure of the unclassified portion is 5 psi from the inlet to the outlet of the chimneys.
- o The design flow rate through the UIS is 43.6×10^6 lb/hr.
- o The design deflection of the UIS while engaging the keyways is 0.75 inch.

C. UIS Design Basis Events

The operating conditions are specified in a 30-year histogram and are characterized as normal, upset, emergency and faulted conditions. Some of the significant operating conditions include the following: Normal conditions, mechanical loads include the friction loads

resulting from raising the UIS with the design deflection. A coefficient of friction equal to 0.7 produces a total load of 157,000 lb at the three keys.

The normal conditions of flow produce thermal striping of components in the mixing chamber. This thermal striping is a result of mixing the discharge of fuel, blanket and control assemblies whose fluid temperature differences can be as high as 345°F. This mixing produces temperature oscillations on the surfaces of the components. The bottom of the instrumentation post sees a peak to peak (metal to metal) temperature of 220°F at a maximum frequency of 1.2 Hz.

Upset mechanical loads include the operating basis earthquake (OBE). The response spectra loading applicable to the UIS was derived from a model of the plant system with a horizontal ground acceleration of 0.125g. This ground acceleration is approximately 1.0g at the IRP for the UIS natural frequency. In addition to the response spectra loading, a horizontal relative displacement between the IRP (attachment point of the UIS) and the core barrel (location of keyways for the UIS keys) of 0.37 in. was a required consideration in evaluating the UIS. The UIS required designing to 5 OBE events with 4 events occurring during the most adverse normal operating conditions and 1 event occurring during the most adverse upset event. The most adverse conditions to be combined with the OBE events were established during the structural evaluation.

The reactor system upset duty cycle temperature transients are based on umbrellaed plant duty cycles, which in turn are umbrellaed into nine transient events to define the UIS duty cycle.

The grouping of events was according to a characteristic behavior such as up or down transient, initial and terminal state conditions, and ramp rates. Structural evaluation provided further definition of the significant transients as:

- (1) Uncontrolled rod withdrawal from full power (UIS-2U). This event encompasses reactivity insertions in which the plant protection system trip point is not exceeded. This event assumed a power increase from 100% to just under 115% (just under high flux trip point) with a manual trip after 5 minutes. The UIS-2U event is the most severe of the reactor trip types due to the elevated core exit temperature, 1140°F. The frequency of the UIS-2U umbrella event is 16 and results in an average fuel exit down ramp of 42°F/second.
- (2) Loss of preferred and alternate preferred power (UIS-8U). This event occurs during the loss of main sodium pump power. Reactor and turbine trips occur and the auxiliary feedwater supply of the steam generator auxiliary heat removal system is activated. The effect of this transient is a rapid and extended down transient. Two other upset events which activate the auxiliary heat removal system are umbrellaed under the UIS-8U. The frequency of the event is 30 and results in an average fuel exit down ramp of 32°F/second from 1055°F.
- (3) Three-loop natural circulation (UIS-2E). This event is described under emergency conditions.

Emergency conditions were analyzed as upset events as inspection of the UIS after operation is not planned.

The mechanical loads induced due to the loss of hydraulic balance of the core assemblies occurs 5 times and was analyzed as an upset event. The load application is a gradual process in which random assemblies move upward and contact the UIS. The maximum upward load is 167,000 lb. The transient emergency event UIS-2E, three loop natural circulation, assumes a complete loss of forced sodium circulation in all loops. A reactor and turbine trip is initiated by primary pump under-voltage and/or under-frequency relays. Sodium pumps coast down and natural circulation is established in all

loops. The reactor trip produces a down transient of the average fuel exit temperature of 27.5°F/second from 1085°F to 810°F in 10 seconds, and is followed by an up transient to 1180°F at 100 seconds.

Faulted conditions mechanical loads include a safe shutdown earthquake (SSE). However, to insure safe shutdown, the UIS was designed to the ASME B&PV Code criteria for emergency conditions, when in the operating position and exposed to the SSE. The IRP sees a horizontal acceleration of about 6g at the natural frequency for a SSE.

Third level margin loading is not a faulted condition per the ASME B&PV Code. However, the UIS was analyzed in accordance with the intent of the Code faulted limits to ensure that there would be no significant ejection of sodium and that no missiles would be generated as a result of the instantaneous peak integral pressure of 2960 psi, decaying to 300 psi at 100 milliseconds.

D. Thermal and Hydraulic Environment

An accurate assessment of the thermal environment, during steady-state and transient operation, was necessary to adequately design the UIS. Effects of flow stratification during the transient and steady-state thermal striping must be quantified and the vibration behavior must be characterized. Experimental programs were carried out to aid in quantifying the thermal environment and to characterize the vibration behavior of the overall structure. The experimental data was scaled to the CRBRP conditions and used to qualify thermal and hydraulic computer codes. These codes were then used to predict fluid transient temperatures. These temperature distributions were in turn used to determine metal temperatures, which became the input for the structural evaluation of the UIS. See Section 3.9.5 for resolution of reactor transients, thermal striping, and vibration loads.

The maximum steady state temperature at the critical location of the main structure is 1105°F for the 6 in. lower plate design and 17°F lower for the 4 in. lower plate design. This corresponds to a reactor outlet temperature of 1000°F. The temperatures at other locations vary between these values. The UIS is subjected to a total of 577 reactor transients during its 30 year lifetime. A duty cycle evaluation based upon scoping calculations for the creep-fatigue failure mode has condensed the many types of reactor transients down to the U-2b, uncontrolled rod withdrawal from 100% power, and the U-18, loss of preferred and alternate preferred power. The analysis is based upon 577 cycles of the critical transient of these two with a plant availability of 85%. This is distributed among operation at 1000°F, 980°F, and 915°F outlet nozzle temperatures. Based upon a plant capacity factor of 75%, the equivalent full power operating time at maximum outlet nozzle temperature is 22.5 years or 197,100 hours.

The dominant fluid boundary condition for the lower plate ligament is the chimney effluent. Shown in Figure 3-48 are the temperature time histories of a central chimney for the U-2b and U-18 transients which illustrates the difference in the type of response for these two transients.

3.9.2 Evolution of Design

3.9.2.1 Initial Requirements

At a May 11, 1973, meeting the Westinghouse Advanced Reactor Division (WARD) agreed to provide the Reactor Research and Development Division (RRD) of the Energy Research and Development Administration a detailed assessment of core-related instrumentation requirements for Contract AT(11-1)-3045 Task 5 (04140) Core Restraint Development, Item 21. The status of these requirements was reviewed with Mr. J. Jichca of RRD at the Westinghouse Waltz Mill Site on June 20, 1973. The meeting agenda included UIS Functions of Structure, Design Requirements, Installation, T/H Conditions, Shielding, Materials and Safety.

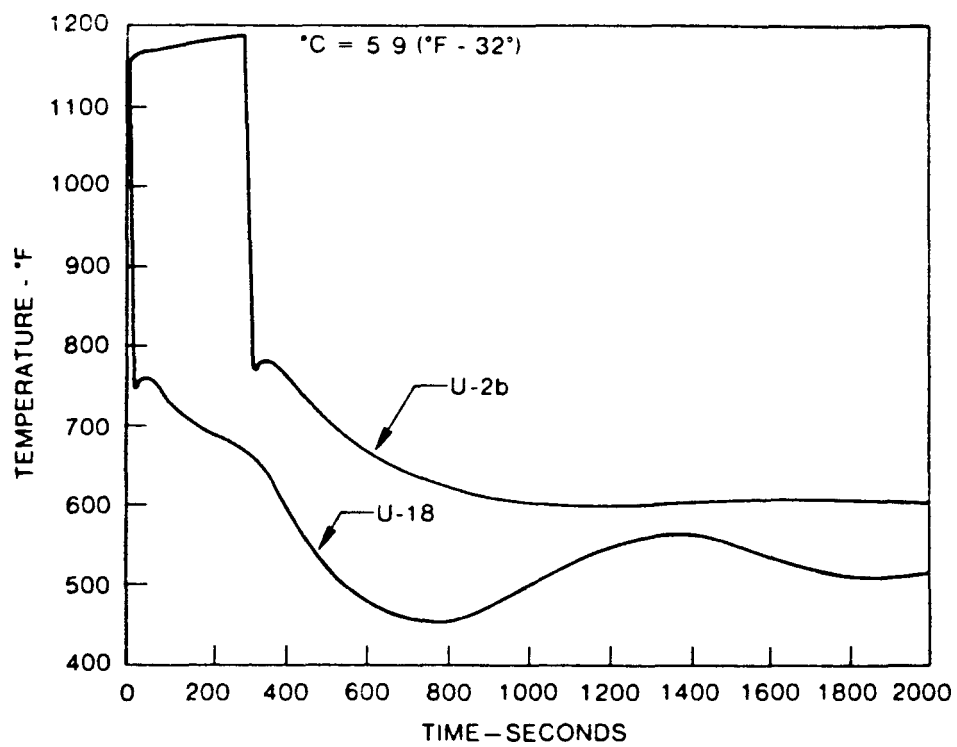


Figure 3-43 Chimney Effluent Temperature Time History

It was agreed that the UIS shall be supported from the IRP, having its own mechanisms located above the head for raising and lowering the UIS. The UIS was to perform the following functions:

- A. Provide secondary holddown for the fuel, blanket, reflector, restraint, and control rod absorber duct assemblies.
- B. Direct the coolant flow from the in-core assemblies and radial blankets, to the outlet plena to ensure good mixing and prevent flow stratification.
- C. Provide support, location, and translation to the control rod driveline disconnect assemblies.
- D. Provide space and support for reactor core, and blanket instrumentation assemblies.
- E. Provide the capability to withstand the loading conditions of shipping, operation, OBE, maintenance and design basis events.
- F. Operate without interference with the functioning or the components of other systems.

3.9.2.2 Conceptual Design

Type 316SS was selected as the structural material for the UIS and Alloy 718 was to be used in areas of high striping and large thermal transients. Thirty-five welded Alloy 718 outlet flow modules collected the effluent from the core assemblies and ducted the flow through chimneys to the outlet plenum. The chimneys were supported by four horizontal plates attached to the four columns. The columns were supported from the reactor vessel closure head.

In June 1974 the UIS design was revised to improve its structural stiffness and sodium coolant mixing. Also, protective shrouds and conduits were added on the instrumentation lines to reduce damage from flow induced vibration.

In August 1974 a forged outlet module concept was proposed which eliminated the need to weld Alloy 718.

The formal Conceptual Design Review was held on September 4, 1974 with RRD, Project Management Corporation (PMC) and ARD personnel in attendance. The forged outlet flow module was presented along with a stiffened Type 316SS structure. The basic structure now exhibited additional stiffness by using a double portal frame concept. See Figures 3-49, 3-50, and 3-51. This design eliminated welded Alloy 718.

Type 316SS was selected as the material for the basic load-carrying weldment on the basis of (a) its demonstrated suitability for service in liquid sodium (b) its well developed fabrication technology and (c) its superior creep rupture strength (when compared with Type 304SS).

3.9.2.3 Preliminary Design

Due to vibration and mixing problems with the outlet flow modules, meetings were held on April 7-9, 1975, to propose a new design concept in the area of the outlet flow modules and the basic structure. The proposed design consisted of 19 10-inch ID chimneys containing 4.50 inch ID shroud tubes for control rods, and 10 8-inch ID chimneys at the outer periphery. The 10 outer chimneys were necked down to 4.50 ID. The shroud tubes and the 10 outer chimneys provided holddown capabilities. In addition, 104 forged Alloy 718 posts, 24 inches long, provided holddown and instrumentation support. The basic structure was changed to an upper and lower plate with shear webs, similar to the final design concept. The 6 inch thick lower plate and skirt were protected with Alloy 718. The UIS (called the instrumentation post concept) now had a mixing chamber directly above the core. All parts on the inside of the mixing chamber, including the chimneys were to be made from Alloy 718, and the Alloy 718 outlet modules were deleted. At this time, the 10- and 8-inch diameter chimneys and the 4.50 inch diameter shroud tubes were planned to be tre-panned from an Alloy 718 forging. This design reduced the cost and was similar to the final design configuration.

UPPER INTERNALS STRUCTURE ELEVATION

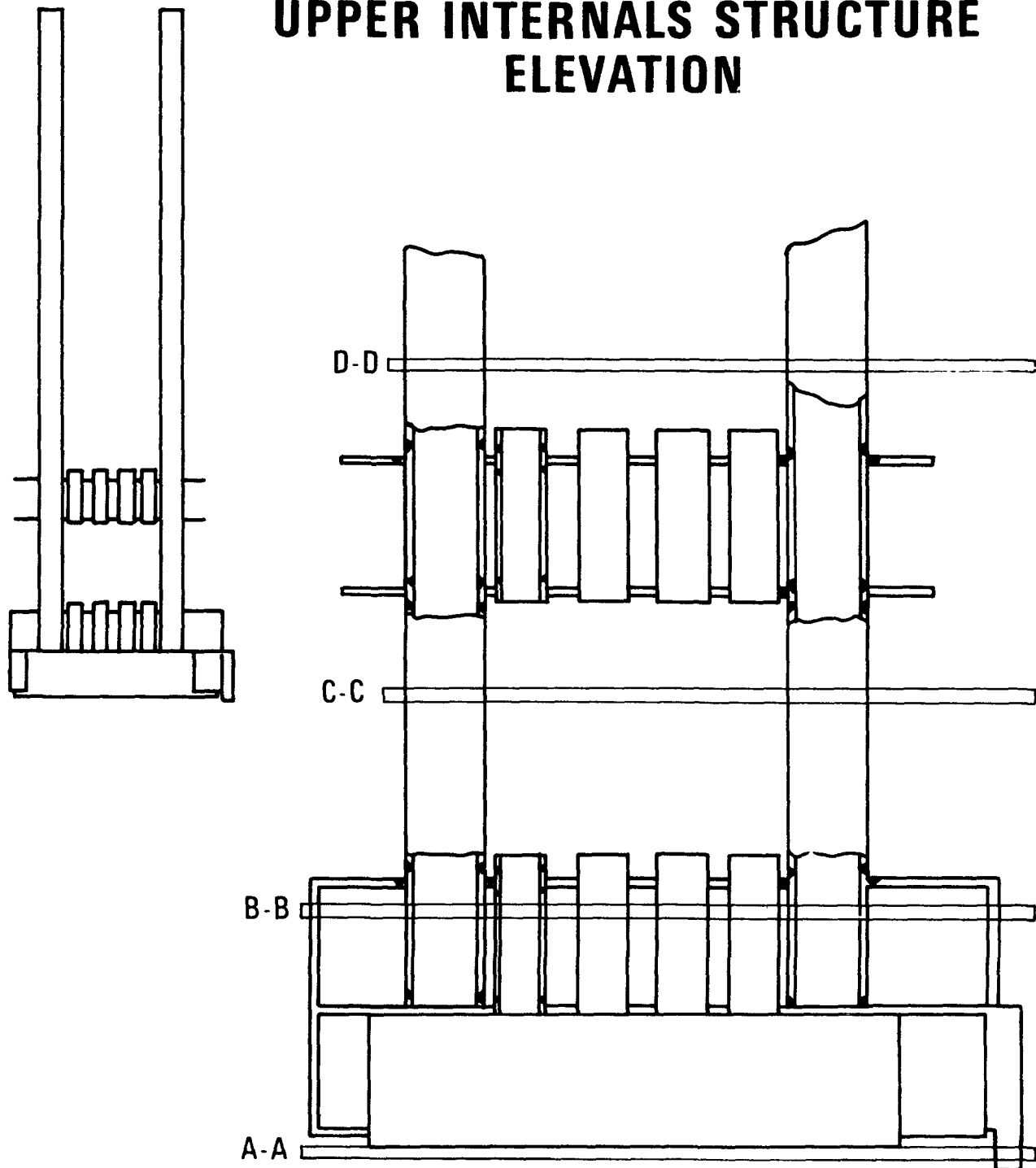


Figure 3-49

UPPER INTERNALS STRUCTURE PLAN VIEW

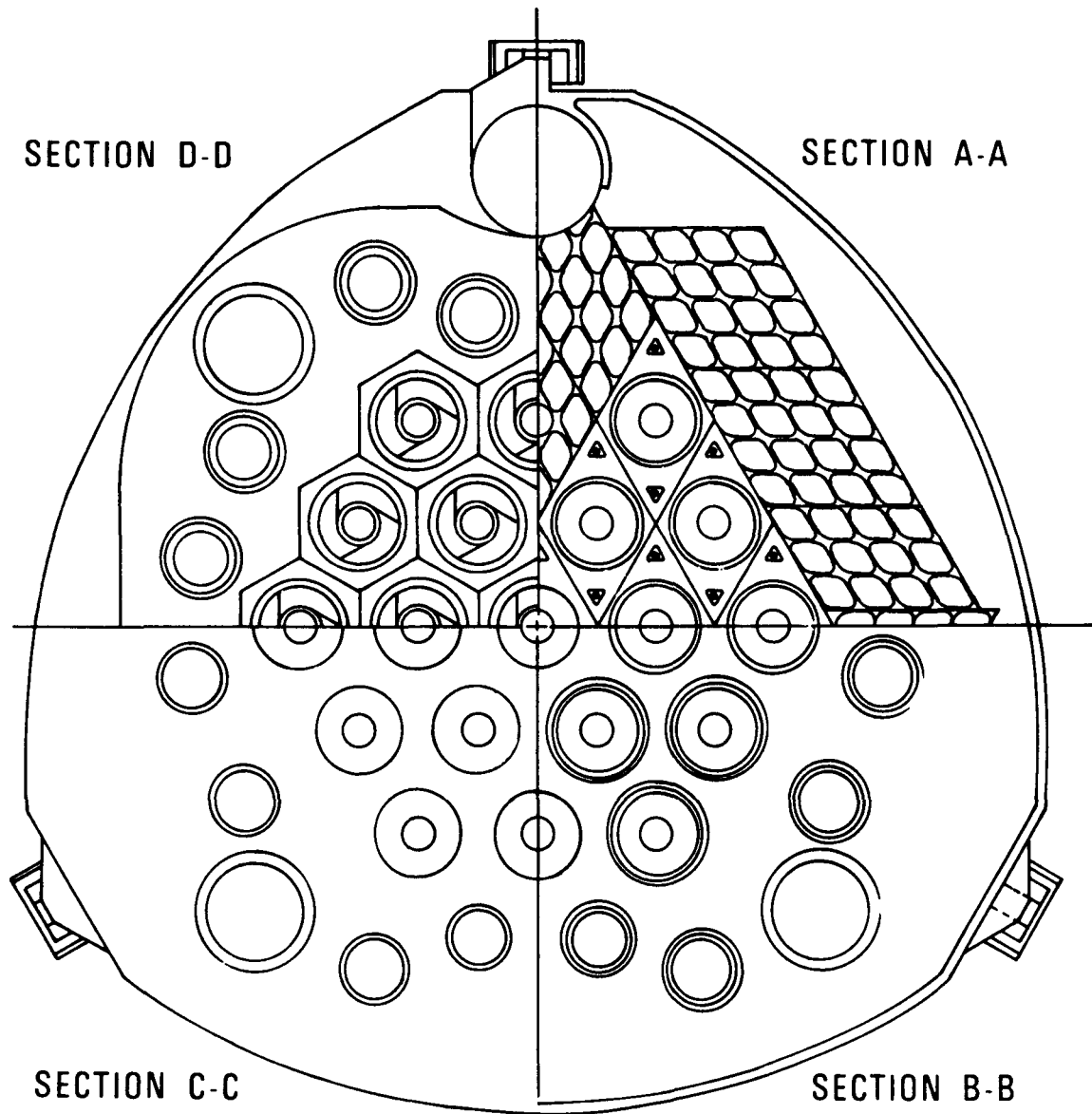


Figure 3-50

BEST AVAILABLE COPY

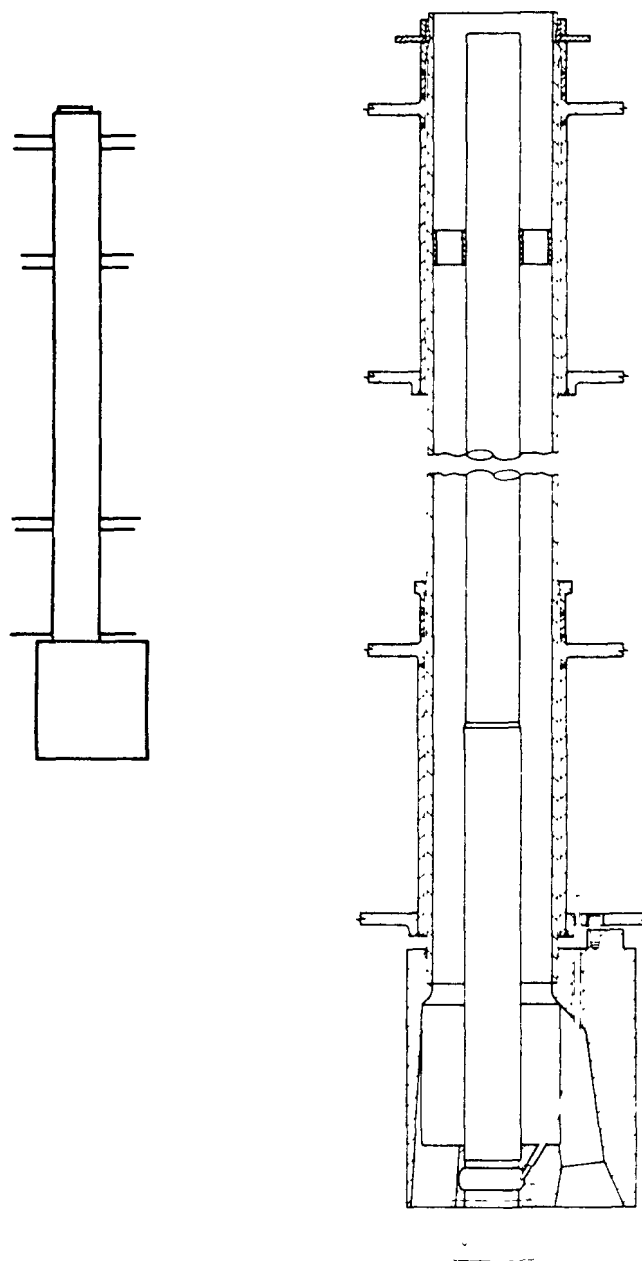


Figure 3-51 UIS Outlet Flow Module and Chimney

On August 28, 1975, it was agreed by the Project Office that the April 7, 8 and 9 meetings constituted an adequate Preliminary Design Review.

3.9.2.4 Final Design

In March 1976 the tre-panned Alloy 718 8- and 10-inch ID chimneys were replaced with 29 10.75-inch ID built-up Alloy 718 chimneys made from flat plate.

In November 1976 the 10.75-inch ID built-up Alloy 718 chimneys were replaced by 10.75-inch ID Alloy 718 roll extruded tubes which were later manufactured by Rollmet of California.

The Final Design Review was held on December 15-16, 1976, at WARD with personnel from LRM, ARD and MPR Associates, Inc. in attendance.

The basic structure was essentially the same as that which was presented at the preliminary design review. It was a box structure with an upper and lower plate and shear webs, supported by four columns from the head. The lower plate was 6 inches thick protected by one Alloy 718 liner which was 1/2-inch thick. The upper plate was protected by two Alloy 718 liners. The IVTM Port Plug was also protected by Alloy 718.

During the June 8-9, 1977, meetings at the Project Office (Review of UIS Design), a 4-inch thick lower plate was presented. It was protected by five liners, two 1-inch thick stainless steel liners, two 3/8-inch thick stainless steel liners and one 1/2-inch thick Alloy 718 liner.

In July 1979 the UIS drawings were updated for the AFMS core Engineering Change Proposals (ECP) W-0491. Four upper shroud tubes were removed and replaced by CRDM nozzle plugs. Seventy-five thermocouples, 24 conduits, and 4 instrumentation posts were added. Alloy 718 was added to protect the keys.

A review of the UIS status was held on July 16-17, 1980. The consensus of the attendees was that the UIS was ready for procurement.

The UIS fabrication purchase order 54-7CCN-260918 was placed with Combustion Engineering, Chattanooga, TN on August 26, 1980. Fabrication was approximately 3/4 complete when the CRBRP Project was terminated in November 1983.

The evolution of the UIS design is shown in table form in Table 3-49. Table 3-50 shows a tabular listing of the major ECPs that have been incorporated into the design. The final design description appears in the following section.

3.9.3 Design Description

3.9.3.1 Mechanical

The UIS is a composite structure (Figure 3-47) consisting of a basic support structure with mixing chamber, instrumentation posts, flow chimneys, upper control rod shroud tubes, and a port plug. The latter component is used in place of the IVTM of the Reactor Refueling System during reactor operation and is referred to as the IVTM Port Plug.

The UIS is a low thermal inertia, Type 316SS welded structure protected by Alloy 718 in the regions exposed to high frequency temperature variations. These high frequency temperature variations (thermal striping) are the result of unmixed flow from the fuel, blanket, and control assemblies and result in high cycle fatigue. Alloy 718, which has a higher fatigue strength than Type 316SS, is used to line the mixing chamber and the peripheral shroud. The parts located in the mixing chamber, instrumentation post and lower shroud tube, and the chimney, are also Alloy 718. Figure 3-52 is an elevation schematic of the UIS which illustrates the sodium flow paths and defines the parts and components of the UIS.

3.9.3.1.1 Basic Structure

The basic structure of the UIS is a Type 316SS weldment to which all components of the UIS are attached except the upper control rod shroud tubes and the IVTM Port Plug. It is through this main structural member that all

TABLE 3-49
EVOLUTION OF UIS DESIGN

<u>Date</u>	<u>Type of Review</u>	<u>Remarks</u>
9/4/74	Conceptual - UIS Formal Design Review	Presented the double portal frame concept with forged Alloy 718 outlet modules and Alloy 718 chimneys. This design eliminated welded Alloy 718.
4/7, 8/75	Critical Review of the CRBR Outlet Plenum T&H Condition at the Design of Outlet Plenum Components "Slamber Committee"	"The present design seems to adequately satisfy its functional requirements."
8/28/75	Letter, WL50036, Agreement between Purvis of ERDA and Pennell - April 7 and 8 and April 8 and 9 Meetings constituted an adequate Preliminary Design Review.	Presented the Alloy 718 instrumentation post concept with posts for holddown and instrumentation. The basic structure was changed to a box structure with an upper and lower plate and shear webs. The lower plate and skirt were protected with Alloy 718. This design reduced the cost.
12/15, 16/76	Final Design Review	The lower plate is 6 inches thick protected by one liner of 1/2-inch thick Alloy 718. The upper plate is protected by 2 liners. The IVTM Port Plug bottom is protected by Alloy 718 plate.
3/23/77	Senior Management Review of UIS	There is nothing fundamentally wrong with the UIS design.
6/8, 9/77	Project Office Review of UIS	The lower support plate is protected by two one inch stainless steel liners, two 3/8 inch stainless steel liners and one 1/2 inch Alloy 718 liner (5 total).
7/79	UIS Drawings are updated for AFMS	
11/29/79	Reactor Internals Design Status Review	Presented AFMS design. Removed four upper shroud tubes, added 75 T/Cs, 24 conduits and 4 instrument posts, Alloy 718 liners on lower shroud tubes, Alloy 718 protection on keys.
7/16, 11/80	UIS Status Review	The UIS was found ready for procurement.
8/26/80		UIS purchase order placed with Combustion Engineering for fabrication to print.

TABLE 3-50
MAJOR CHANGES TO UIS DESIGN

<u>ECP</u>	<u>Date</u>	<u>Description of Change</u>
L-10-011	9/15/76	CRBRP load combinations ECP
W-0147	4/14/76	Increase thickness of lower chimney forging
W-0155	5/5/76	Revise IVTM port plug to include a purge port
W-0194	7/21/76	Increase number of T/Cs Fuel assembly 63 to 198 Blanket assembly 54 to 65
W-0313	11/18/76	Change lower shroud tube from 4.37" to 4.50"
W-0337	12/6/76	Reduce design verification T/Cs to 8
W-0414	3/1/77	Increase secondary core holddown to include all radial blanket assemblies
W-0436	2/28/77	Add thermal liners to UIS in region of gap between UIS and core
W-0445	4/7/77	Change bolts on IVTM port plug to Alloy 718
W-0453	3/11/77	Change keyway vertical gap from 2" to 1.25" and radial gap to 0.040"
W-0467	3/21/77	Reduce lower plate thickness to 4", add 2-1" liners and add 3" diameter holes
W-0491	7/19/77	Alternate Fuel Management Scheme (AFMS)
W-0521	6/2/77	Incorporate items from tolerance study
W-0523	5/3/77	Modify upper and lower shroud tubes to eliminate creep damage
W-0555	6/2/77	Relax fabrication tolerances
W-0580	6/16/77	Add hardness and chemistry requirements for Haynes-273
W-0599	6/29/77	Change weldment assembly
W-0996	5/1/78	Change flow holes in the upper shroud tube to accommodate higher flow rates

TABLE 3-50 (Continued)
MAJOR CHANGES TO UIS DESIGN

<u>ECP</u>	<u>Date</u>	<u>Description of Change</u>
W-1066	11/29/78	"Simplification of UIS Jacking Mechanism." The height of the UIS columns were increased approximately 40 inches.
W-1626	9/23/80	Change lower shroud tube and CRDM Nozzle Plug to insure acceptable creep/fatigue damage
W-1692	4/24/81	Add liner to structure in area of IVTM port plug

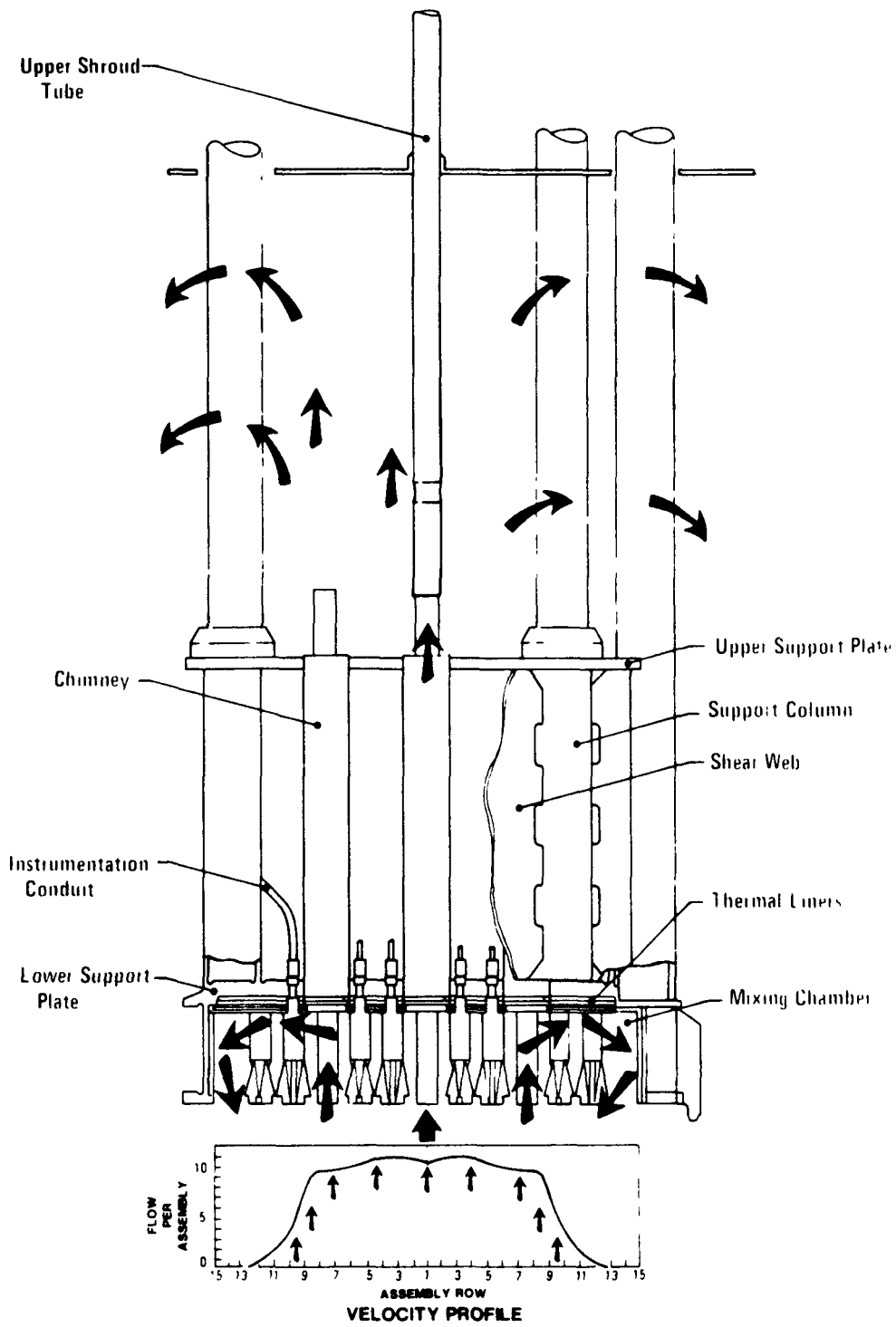


Figure 3-52 Elevation Schematic of the Upper Internals Structure

loads applied to the UIS are transmitted with the exception of those from the control rods. The basic structure, shown on Figure 3-47, is supported by and built around four columns attached to jacking mechanisms, which in turn are supported by nozzles extending upward from the IRP. The weldment is a box structure consisting of upper and lower plates, circumferential shear webs, the lower portions of the columns, and the peripheral shroud. The box-column structure thus formed provides the stiffness required for adequate vibration control without the use of heavy sections which could be susceptible to excessive thermal strains under transient conditions.

The sodium flow from the mixing chamber, Figure 3-52, is directed through the thin wall chimneys into the outlet plenum, where a strong vortex provides good mixing during a transient (such as a reactor trip). Thus, the large transient ΔT is seen by the chimneys but the reactor vessel sees a mild transient, mitigated by mixing in the mixing chamber, chimneys and outlet plenum.

A. Support Columns

Each of the four support columns is a 14 inch diameter tube with a wall thickness of 1 inch. Between the upper and lower support plates, holes in the walls of the support columns provide passage for instrumentation conduits. An integral collar on the upper part of each support column provides radiation shielding for the head access area; it also would prevent the UIS from moving through the head in the event of a Third Level Design Margin Loading Event.

The outer surface of the support column interfaces with an eccentric ring seal in the Jacking Mechanism nozzles of the IRP. A primary pressure boundary seal surface is provided at the upper end of each column and at the IVTM Port Plug. The seal is provided by each Jacking Mechanism at the column. Attachments to the Jacking Mechanism lead screws are provided at the upper ends of the columns and the lower ends of the columns are welded to transition forgings incorporated in the upper and lower support plates. Type 316SS collar type thermal shields are placed around the column forging welds to protect them from thermal loads.

B. Upper Support Plate, Lower Support Plate and Shear Web

The upper 3.25 inch support plate forms the top of the box structure. Holes are provided to position the chimneys. The lower 4 inch thick support plate has the same penetrations for chimneys as the upper support plate except that the penetrations for the chimneys have mechanical joints which secure the lower ends of the chimneys. To complete the box structure, a 1 inch thick shear web is welded between the support columns, the upper support plate and the lower support plate. The shear web attachment to the upper and lower support plates is made by a non-continuous weld with cutouts in the shear web between attachment welds. This configuration minimizes the thermal stresses resulting from the thickness mismatch (thermal mass mismatch) between the support plates and shear web.

C. Peripheral Shroud

A Peripheral Shroud, conforming to the configuration of and welded to the lower support plate, is provided to form the outer enclosure of the UIS mixing chamber.

Three keys are attached to the outside of the Peripheral Shroud to provide positive location of the lower end of the UIS with respect to the Core Support Structure. These keys engage keyways in the Core Former Structure when the UIS is lowered. The keys are hard faced with Haynes 273 to reduce friction and prevent galling.

Type 316SS was selected as the material for the basic load-carrying weldment on the basis of (1) its demonstrated compatibility with liquid sodium, (2) its well-developed fabrication technology, and (3) its superior creep rupture strength relative to that of Type 304SS.

3.9.3.1.2 Mixing Chamber

The UIS mixing chamber (Figure 3-52) is bounded on the bottom by the outlet nozzles of the core assemblies, on the sides by the peripheral shroud, and on

the top by the lower support plate. The static pressure in the mixing chamber decreases from the periphery to the center due to the higher velocities of the sodium exiting from the centrally located fuel assemblies and the lower velocities of those exiting from the peripheral Radial Blanket Assemblies. This pressure distribution creates a toroidal flow pattern which in turn causes mixing of the core assembly effluent as shown in Figure 3-52. About 12% of the flow, at steady state operation, leaks under the peripheral shroud of the mixing chamber.

Located within the mixing chamber are the instrumentation posts, thermal liners for the support plate, thermal liners for the peripheral shroud, the ends of the lower control rod shroud tubes, and the inlets for the chimneys. Stream-to-stream temperature differences of up to approximately 250°F with a cyclic frequency of one Hz (approximately 7×10^8 cycles in the life of the plant) impinge on the exposed surfaces in the mixing chamber. This thermal striping requires that a liner material having a very good high cycle fatigue strength be placed on the surfaces of the structure. Alloy 718 can withstand a 350° to 425°F thermal striping amplitude whereas Type 316SS can withstand only about 50°F striping amplitude for the UIS environment. Therefore, all material directly exposed to the sodium flow in the mixing chamber is Alloy 718.

3.9.3.1.3 Instrumentation Posts

One unique feature of the UIS for the CRBRP is the instrumentation post shown in Figure 3-53. The instrumentation post provides secondary mechanical holddown and thermocouple instrumentation for core assemblies located beneath the UIS.

An instrumentation post can contain four thermocouple conduits, each containing one thermocouple in an inert helium environment. Helium gas is used to provide a response time of less than five seconds.

As shown in Figure 3-54, the outer portion of the instrumentation post is Alloy 718, with a Type 316SS support core mechanically attached to the lower support plate. The mechanical connection consists of two half rings mating

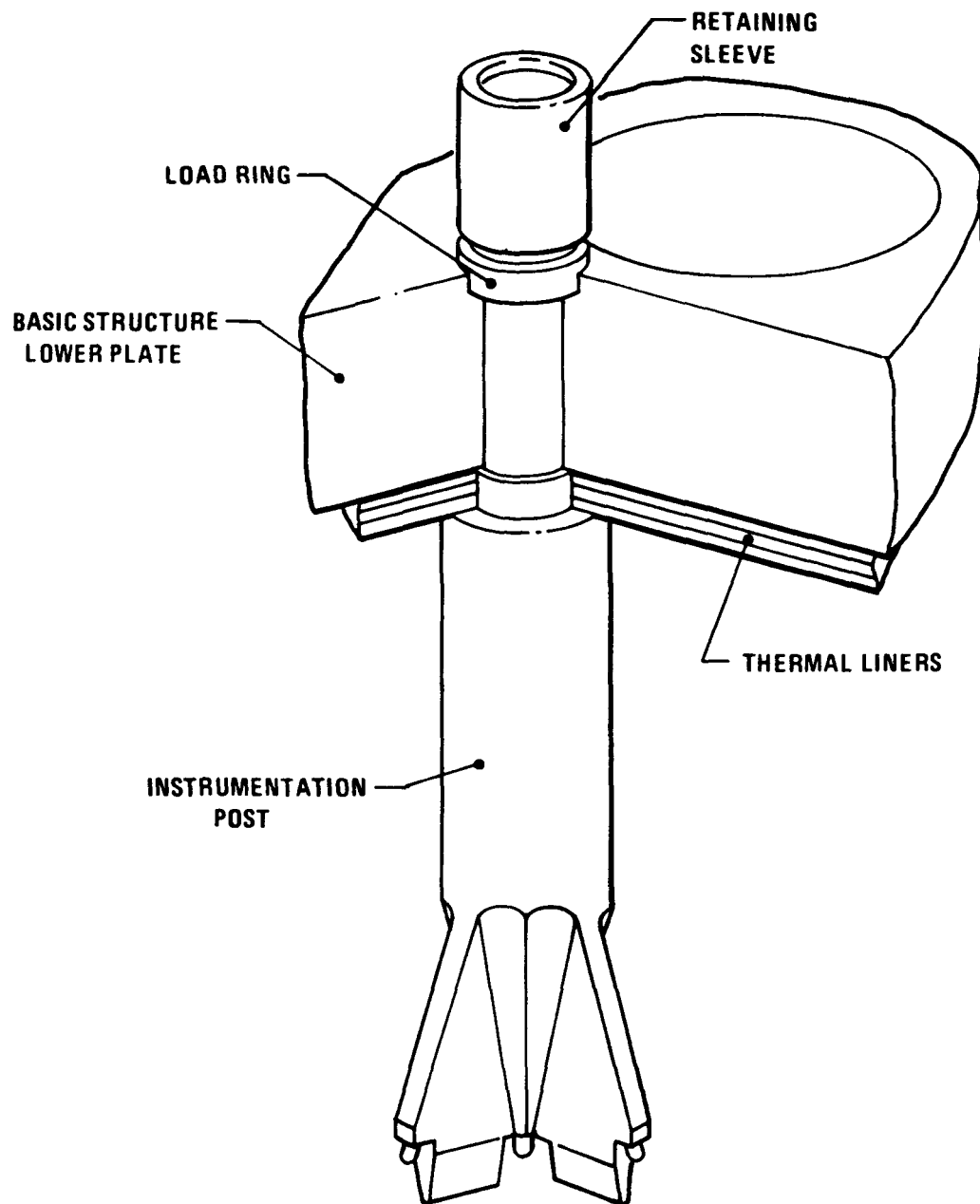


Figure 3-53 Instrumentation Post Installation

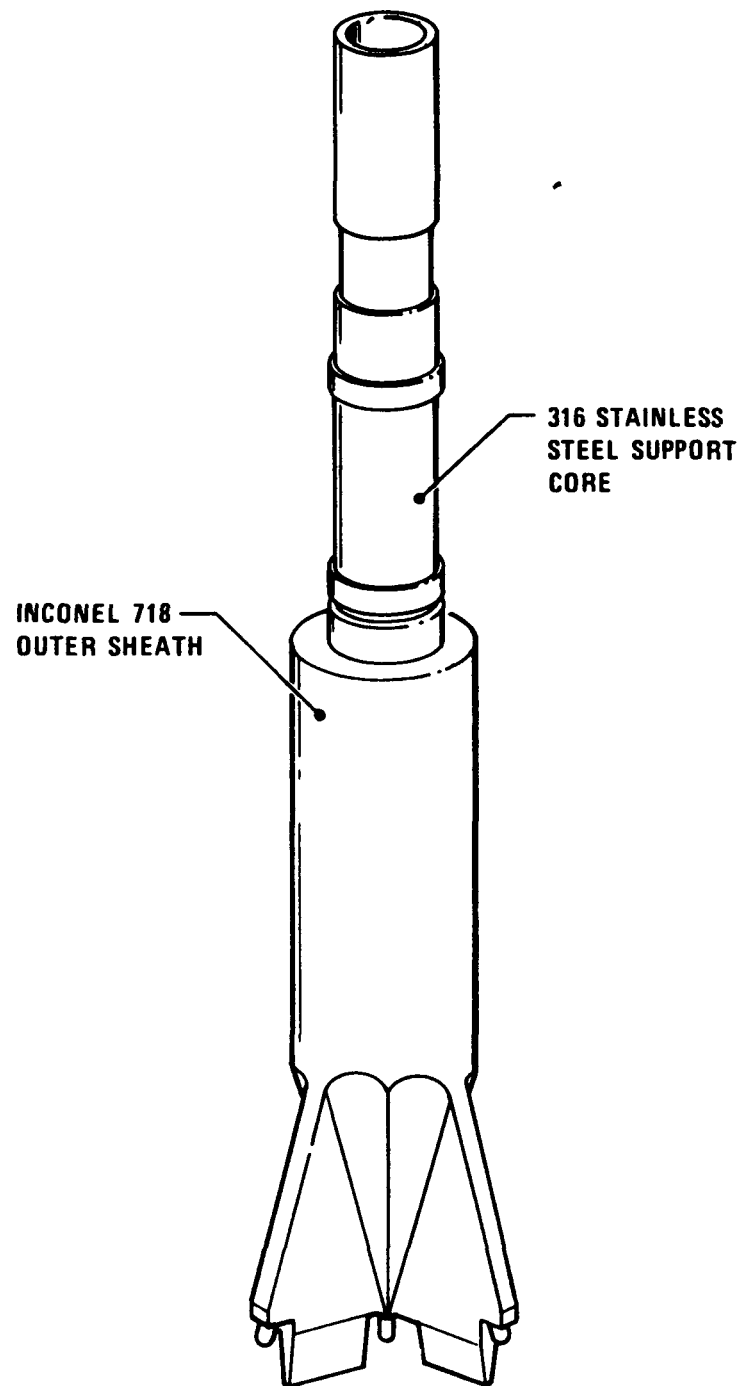


Figure 3-54 Instrumentation Post (Bench Subassembly)

with a groove in the stainless support core reacted by a load ring and trapped by a retaining sleeve. The Alloy 718 is mechanically attached to the Type 316SS inner core by three radial pins.

There are ninety-eight instrumentation posts in the UIS arranged in a pattern to provide instrumentation and secondary core holddown at the required assemblies. All fuel assemblies and most of the blanket assemblies are instrumented. Additional secondary holddown is provided to all remaining blanket assemblies by installing instrumentation post assemblies without thermocouples.

In the cold condition, with unirradiated core assemblies in place in the reactor lattice, a 2-1/2 inch clearance exists between the bottom edges of the posts and each core assembly outlet nozzle. The cold gap between the peripheral shroud and the Removable Radial Shield Assemblies is 2.0 inches. In the event of a loss of hydraulic balance (primary core holddown), it is possible that the fuel and control assemblies would be moved upward until they become restrained by the UIS.

3.9.3.1.4 Chimneys

Twenty-nine Alloy 718 chimneys 10.75 inch diameter with a 0.250 inch wall are provided to conduct the partially mixed sodium from the mixing chamber to the upper plenum. The inlets of the chimneys receive sodium from the mixing chamber. The outlets, located above the upper support plate, discharge sodium into the upper region of the outlet plenum. The chimneys are mechanically attached to the lower support plate but also receive lateral support from the upper plate. The mechanical attachment consists of an eccentric flange machined on the outside of an Alloy 718 chimney support forging which mates with a groove in the lower plate thermal liners. Vertical chimney loads are carried by the lower support plate while the chimneys are free to expand through the upper support plate.

3.9.3.1.5 Lower Control Rod Shroud Tubes

The 19 Alloy 718 lower shroud tubes are attached to the inner chimneys. These shroud tubes are centered in the chimneys as shown in Figure 3-55 and extend upward through the UIS and into the upper region of the outlet plenum. The lower control rod shroud tubes provide secondary holddown and alignment of control rod system components.

3.9.3.1.6 Chimney Support

Forgings of Alloy 718 in the upper and lower support plates hold the chimneys and lower control rod shroud tubes. Tangential spokes connecting the inner and outer rings of these support forgings (Figure 3-55) alleviate thermal stresses due to radial temperature differences by inducing relative twist between the rings.

The lower shroud tubes and chimney supports are fabricated from Alloy 718 so that they will withstand the thermal striping fatigue stresses imposed by sodium streams of widely differing temperatures.

3.9.3.1.7 Instrumentation Conduits

The 0.312 inch ID drywells for the instrumentation leads from each instrumentation post are directed inside 1.05 inch ID conduits as shown in Figure 3-56. Routing the thermocouple drywells inside the larger conduits insure that the drywells are isolated from flow induced vibration. The instrumentation conduits are collected and directed into the support columns as shown in Figure 3-57, where they terminate. The individual drywells continue up through the radiation shielding and gas seals located inside the upper ends of the UIS support columns and terminate in the UIS instrumentation housings above the reactor vessel closure head.

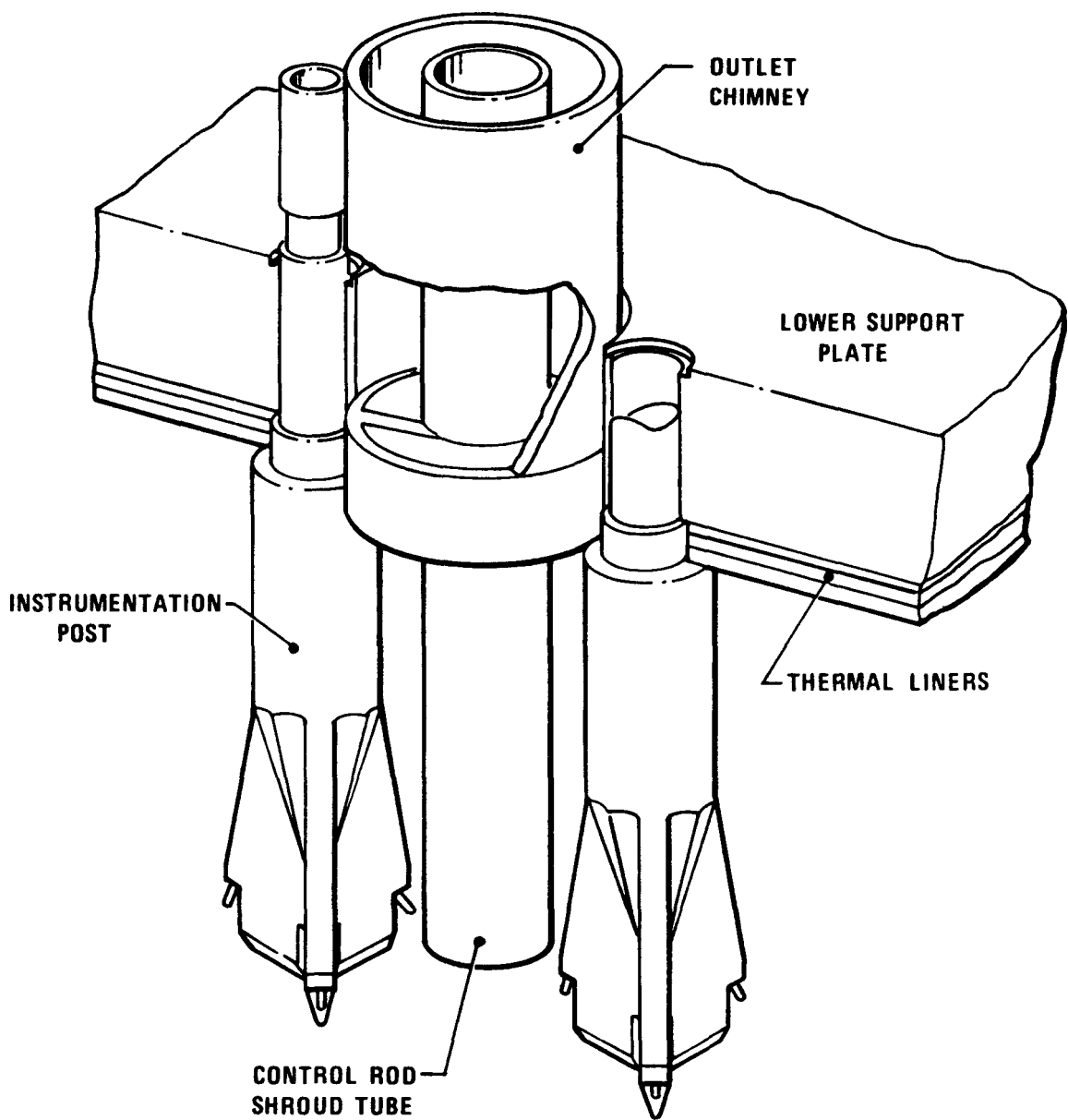


Figure 3-55 Method of Positioning Control Rod Shroud Tube

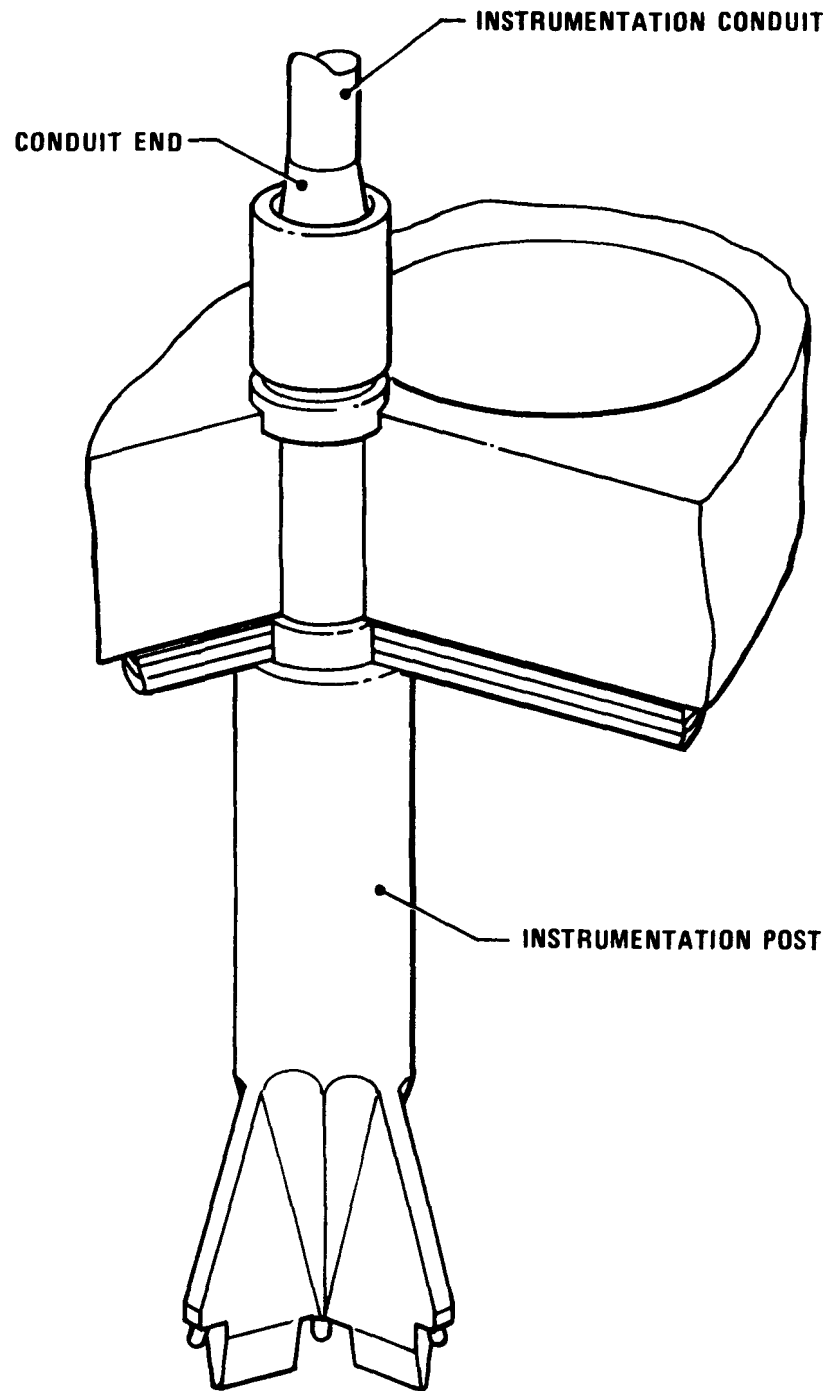


Figure 3-56 Instrumentation Conduit Installation

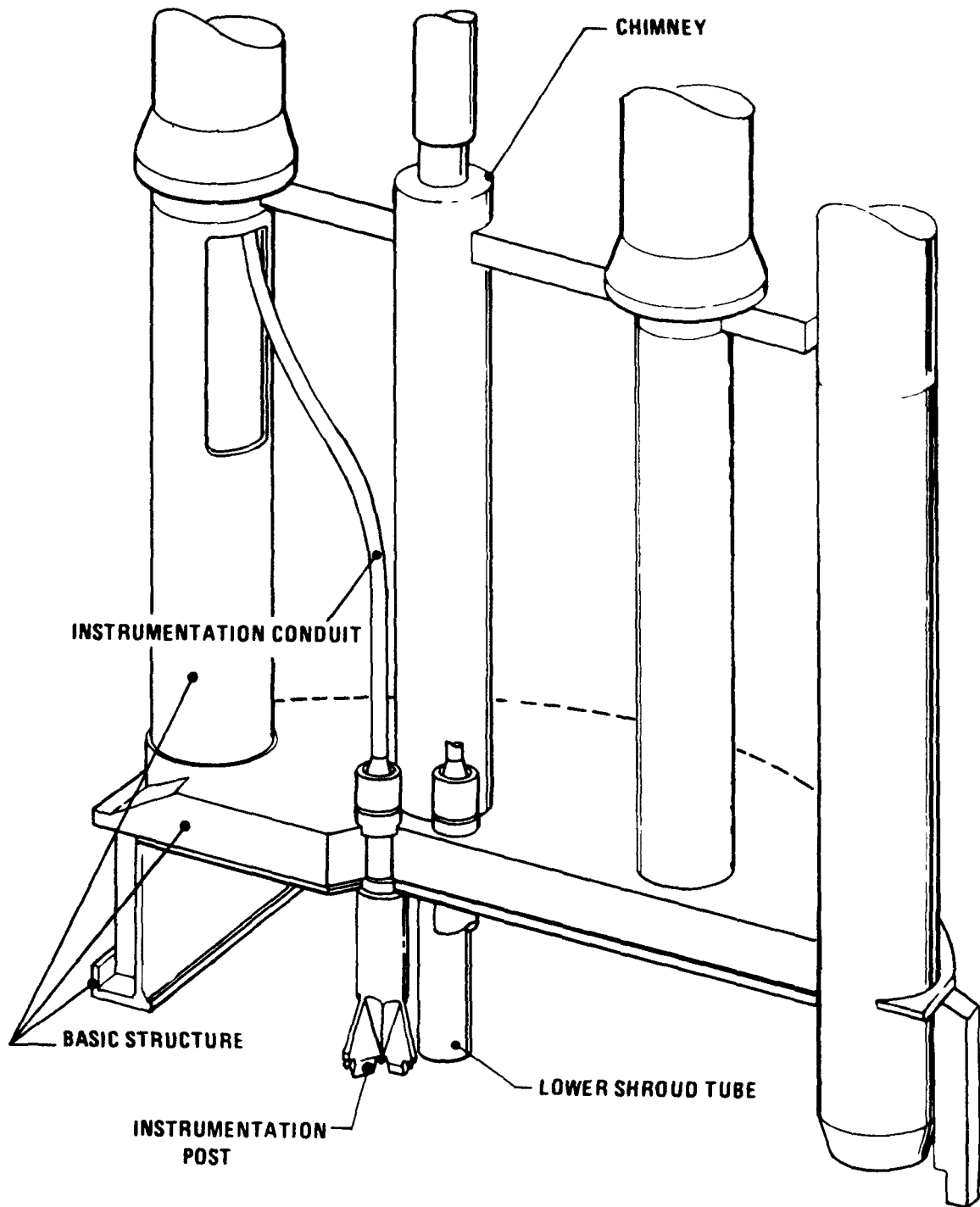


Figure 3-57 Upper Internals Structure

The drywells that contain thermocouples are filled with helium. The relatively high thermal conductivity of this gas results in response times for temperature measurements that are less than 10 seconds as has been demonstrated by test.

3.9.3.1.8 Upper Shroud Tubes

The upper shroud tubes are attached to the intermediate plug of the reactor closure head assembly of the Reactor Enclosure System and have a slip fit of 0.001 to 0.006 inch with the lower shroud tubes. Fabricated of Alloy 718 the upper shroud tubes provide alignment and support of the Control Rod Drivelines (CRDs). The control rod scram arrest loads are reacted by the upper shroud tubes. Finally, the upper shroud tubes provide sodium cross-flow protection for the CRDs in the outlet plenum. Although the upper shroud tubes are in a total fluence of 1.02×10^{21} n/cm², which is less than the loss of ductility threshold, one or more upper shroud tubes could be removed for surveillance during the 30-year plant lifetime.

3.9.3.1.9 In-Vessel Transfer Machine Port Plug

The IVTM Port Plug is included as a component of the UIS. The Port Plug is inserted into the IVTM nozzle of the Small Rotating Plug (SRP) which is part of the reactor closure head assembly of the Reactor Enclosure System. The IVTM Port Plug, when removed, leaves an access hole 15.5 inch diameter extending through the SRP, the upper plenum, and the UIS through which the Auxiliary Handling Machine of the Reactor Refueling System can penetrate and remove items as large in cross section as the Lower Inlet Modules, thus providing the flexibility of core design changes such as advanced fuel assembly materials. When installed, the Port Plug blocks flow through the UIS, preventing aberrant sodium flow and leakage, and limits radiation streaming through the reactor vessel closure head assembly/IVTM port plug interface to safe levels.

3.9.3.1.10 Component Joints

The UIS and its parts are subject to the potential for flow-induced vibration. Designing for tight joints, one way to suppress flow vibrations, is complicated by parts having different thermal responses, functional requirements for relative motion, and assembly limitations of fitting together. Some of the significant regions in which a balance was obtained between these conflicting requirements include the following:

- o Upper-to-lower shroud tube joints.
- o UIS keys to core former keyway.
- o Chimney to top and bottom plates.
- o Instrumentation post attachment and sheath.

The upper shroud tube has a telescope fit over the lower shroud tube. This fit is a clearance of 0.001 to 0.006 inches when it is assumed that the upper shroud tube ends are coincident with the upper end of the lower shroud tube and that no bow (or bending) of the upper shroud tube occurs.

The telescope fit is required to allow upward motion of the lower shroud tube when the UIS is raised for refueling. The design reflects the real world conditions of a spring load, cantilever deflection of the upper shroud tube, applied to one side of the telescope joint. The spring load results from assembly tolerances of shroud tube centerline locations, bow of upper shroud tube and thermal growth mismatch between the closure head and the UIS.

The UIS keys engage the core former keyslot with a 0.04 inch nominal gap on each side of the key. The keys disengage the key slot when the UIS is raised so that rotation of the IRP and UIS can be utilized during refueling. The tangential gap can be established as zero by biasing the IRP (rotation) to obtain a desired preload on the UIS.

The joint between the chimneys and the top and bottom plates includes Type 316SS sleeves that engage the Type 316SS plates. This insures a minimum change in the assembly conditions as both parts have the same coefficient of thermal expansion. The sleeves have a spigot fit engaging the Alloy 718

support forging which mates with the Alloy 718 chimney. Provision for the differential expansion is thus provided by elastic stresses in the sleeve rather than an increased gap at the operating condition.

The instrumentation post attachment is a shrink fit of the stainless support core to the stainless lower plate. The Alloy 718 instrumentation post sheath is attached to the stainless support core by three radial pins seating in a groove on the inside of the sheath. The pins are seated by a preloaded taper pin.

3.9.3.2 Full Scale MockUp [20]

A mockup of the UIS was built to assist in design, reveal any design problems, assure the adequacy of the space envelopes, aid in fabrication of the actual UIS, and establish the sequence and techniques for the installation.

The mockup was fabricated with wood, plastic, and aluminum at the Genuine Tool Co. in Irwin, PA, in 1976, and assembled at the Westinghouse Advanced Energy Systems Division Waltz Mill, PA Site where it served as a design tool. In 1981, it was sent to Combustion Engineering in Chattanooga, TN for use during the construction of the actual UIS.

Accuracy of dimension and contour of the mockup components was emphasized to assure the ability to verify feasibility of assembly, adequacy of space envelopes, and instrument replacement techniques. Accuracy was ± 0.01 in. for dimensions of 1 ft and under, and ranged up to ± 0.250 in. for dimensions over 10 ft.

Figure 3-58 shows the lower part of the UIS. Space envelopes and installation sequences were checked out on the mockup as it was assembled. Both the space and the sequences had been pre-determined, but certainty was assured only when the workmen were able to install the conduits, post assemblies, and chimneys sequentially.

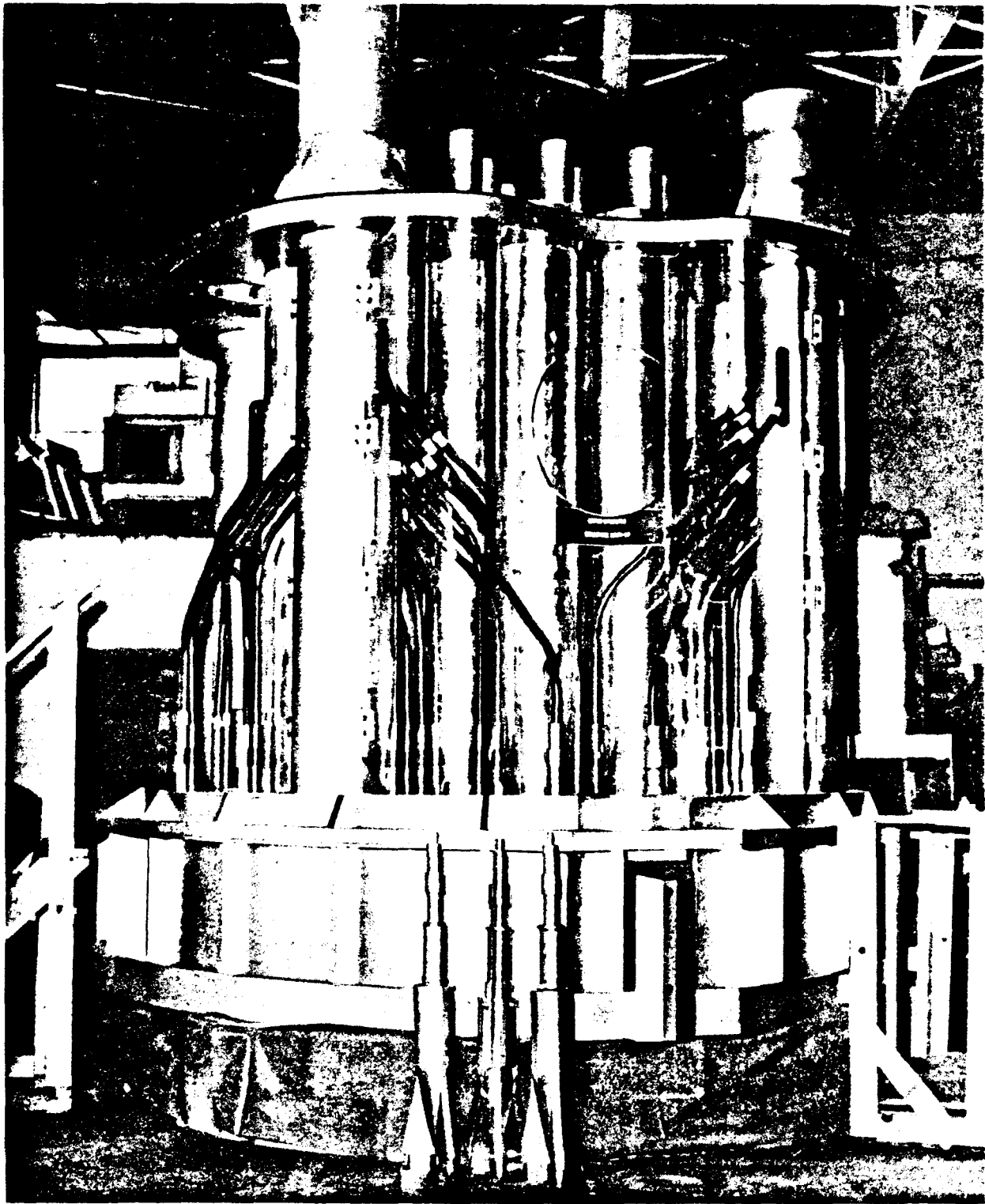


Figure 3-58
Photograph of Full Size Upper
Internals Structure Mock-up

One of the design challenges is to route 98 instrumentation conduits from the posts in the lower support plate to the instrumentation chambers at the top of each UIS support column. The conduits have many bends in three planes and must be routed between the closely spaced chimneys. The chimney support structure is about 106 in. in diameter and 77-1/2 in. high, resulting in a tight space envelope in which the designers had to route the conduits.

Conduit routing first was worked out using descriptive geometry, and then verified on the mockup. Without the mockup, clearances and routings would have to be checked out during assembly of the actual UIS. Resolving problems found at that time would be very costly.

Some problems were identified during assembly of the mockup. For example, the instrumentation posts are fastened into the lower support plate with a shrink fit. The close spacing of the posts was appreciated fully after the mockup was built, so special assembly requirements were established along with a requirement for qualification of the assembly technique for the actual UIS.

The mockup also showed that 338 thermocouple drywell welds could be eliminated. Originally, the drywells were designed with a circumferential weld in the vicinity of the lower support plate. This introduced the possibility of drywell and thermocouple hangup at this point during insertion of the thermocouples. The drywells are now continuous (no welds) from the bottoms of the instrumentation posts to the chambers at the tops of the support columns.

During construction of the mockup, it was shown that an instrumentation post with full-length drywells attached could be installed as a subassembly, thereby saving assembly time. Several prototypic drywells were installed and thermocouple insertion and withdrawal were carried out. Thus, the drywell welds were eliminated and the benefits of using subassemblies were gained as well.

The value of a full-size mockup of the reactor upper internals structure was demonstrated. The designers found the mockup particularly useful in developing design improvements, and for verifying assembly techniques and space envelopes.

3.9.3.3 Instrumentation

The UIS Instrumentation consists of thermocouples suspended in the exit flow of fuel and blanket assemblies, thermocouples to monitor the outlet plenum and UIS structure temperatures and accelerometers to measure UIS accelerations. All thermocouples are of the drywell type, with a response time of less than 10 seconds.

3.9.3.3.1 Core Exit Thermocouples

There are 338 thermocouples provided above the core assemblies; 148 are above fuel assemblies, 184 are above blanket assemblies and 6 are above locations occupied alternately by fuel or blanket assemblies. The functions for thermocouples located above the core assemblies' exits are:

- o Reactor control.
- o Surveillance (primarily assessment of cladding cumulative damage function).
- o Design verification (verification of design margins, power distribution, symmetry, operational disturbances, assessment of uncertainty factors).

Core exit instrumentation will not be required to perform any PPS function, nor will it be used for detection of blockages or local failures.

The thermocouples' coverage will satisfy the above functions as follows:

- o Thirty fuel assemblies have one thermocouple each for reactor control purposes.

- o Full instrumentation of fuel assemblies (one per assembly) for surveillance and design verification purposes (148 or 154, including the 30 thermocouples for control).
- o Extensive coverage of the radial blanket assemblies for surveillance and design verification purposes (190 or 184 total).

A. Reactor Control

The core exit thermocouples employed for reactor control purposes will provide measurements used to: 1) maintain the steady state primary outlet temperature at the design value within a fixed band; 2) minimize number of scrams due to both reactor and balance of plant disturbances; and 3) reduce the temperature overshoot due to the above disturbances.

B. Fuel Surveillance and Design Verification

Thermocouples located at the exit of the fuel assemblies will provide measurements satisfying surveillance and design verification functions. Analyses have indicated that: 1) the maximum rod cladding temperature can be correlated to the assembly mixed mean temperature and consequently, cladding cumulative damage function (CDF) can be monitored; 2) design uncertainties on assembly mixed mean temperatures can be significantly reduced with consequent reduction in exit temperature predictions of up to 50°F; 3) the corresponding reduction in maximum cladding temperature predictions can be of the order of 100°F.

C. Blanket Surveillance and Design Verification

The objective of thermocouples instrumentation at the exit of the radial blanket assemblies is the same as discussed for the fuel assemblies, i.e., surveillance and design verification.

The maximum cladding temperature can be correlated to the assembly exit temperature similarly to the fuel assemblies. However, CDF in the radial blanket assemblies occurs almost entirely at end-of-life, rather than continuously as in the fuel assemblies case; thus requiring accurate measurements in those locations where the assemblies reside for the final part of their lifetime. Uncertainty factors can be reduced, especially for first row assemblies, even though not to the same extent as for the fuel assemblies. For the radial blanket assemblies, the benefits of monitoring the CDF and reducing the uncertainty factors are: a) verification of the adequacy of physics and thermal-hydraulic design; b) confirmation of the capability to attain lifetime objective; and c) subsequent improvements of the flow management and allocation (e.g., diverting excess flow to fuel assemblies, thus increasing burnup).

3.9.3.3.2 UIS Diagnostic Instrumentation

Eight thermocouples monitor outlet plenum and structure temperatures and two triaxial accelerometers measure UIS accelerations. The thermocouples are located as follows:

- A. Two on the lower skirt ring 180° apart to measure the temperature of bypass flow in the gap between the UIS and core former.
- B. Two on the outside of the lower column between the upper and lower plate to measure the temperature of sodium on the outside of the UIS.
- C. One on the top side and near the middle of the lower support plate to measure the lower support plate temperature.
- D. One inside of the box weldment, approximately in the center of the box, to measure the temperature of the sodium inside the weldment.
- E. One on the bottom and one on the top of the upper support plate to measure the plate temperature.

One triaxial accelerometer is located on the top surface of the lower support plate at the 135° column and the other is located on the 315° lower column just below the upper support plate. Horizontal and vertical accelerations are measured at both locations.

All of the thermocouples are replaceable, however the accelerometers are not. The accelerometers will be used during Pre-Operations and Start-Up testing, but they are not expected to provide data during operation because the operating temperature is expected to damage them.

3.9.3.4 Stratification Control

In the CRBRP reactor, sodium coolant enters the pressure vessel and flows upward through the reactor fuel assemblies and other components into an outlet plenum. In the large outlet plenum, the coolant streams mix and flow from the plenum through three outlet nozzles into the primary hot leg piping. During a normal reactor scram (trip transient), a large decrease in fuel assembly outlet sodium temperature occurs. The design of the outlet plenum and hot leg piping components is highly dependent on the expected thermal transients caused by this abrupt change in coolant temperature. The large volume of the outlet plenum provides a region in which the cooler fuel assembly coolant can mix with the fluid already in the outlet plenum before entering the reactor outlets and hot leg piping.

The determination of the flow patterns and the degree of mixing or, conversely, the stratification in the outlet plenum during a reactor trip accompanied by a flow coast down is a complex problem which cannot be adequately resolved with currently available analytical techniques. Therefore, large uncertainties exist in predicting the transient thermal environment for components in the outlet plenum. During a reactor trip transient the core exit temperature drops approximately 300°F and the flow rate drops to the pony motor condition which is approximately 10 percent of the full flow rate. The resulting fluid velocity and density changes may lead to earlier flow stratification in the outlet plenum characterized by retention

of hot sodium above the outlet nozzles and the filling of the lower portion of the plenum with colder fluid followed by streaming of the cold fluid directly toward the outlet nozzles. The consequences of flow stratification include:

- A. An increase in severity of thermal transients on components in the lower region of the plenum as well as in the downstream piping
- B. The establishment of a hot-cold interface in the plenum with resultant thermal discontinuity stresses and possible temperature oscillations on the affected components.

To insure that the proposed design of the CRBRP outlet plenum and piping is adequate, Westinghouse concluded that a quantitative experimental evaluation of the effects of fluid density and flow rate changes during a scram were necessary. Battelle Columbus Laboratories was selected by Westinghouse to conduct this quantitative flow evaluation.

Reference 5 describes the flow modeling studies conducted by Battelle in a 0.55 scale 1/3 sector model to obtain quantitative data for evaluating the effect of fluid flow rate changes on the mixing characteristics of the CRBRP outlet plenum during a normal reactor scram.

This report documents the model and facility configuration with descriptions, photographs, and drawings, along with test procedures and instrumentation descriptions and locations. Supplemental experimental studies verifying model/facility performance are described and the experimental test run parameters detailed.

Complete data for each of the experimental runs in hard copy form and on magnetic tape were transmitted to Westinghouse for analysis following each run. These data and their detailed analyses for each run are not covered in the report. Data presentation is limited to a few selected specific examples to illustrate data format and present typical time-temperature profiles at several model locations.

3.9.3.5 Thermal Striping Distribution and Control

The UIS is designed to accommodate thermal striping. Thermal striping is the condition which exists when sodium at different temperatures mix and impinge upon the exposed structural surfaces, alternately heating and cooling the surface. Sodium leaving the core assemblies could result in the most severe striping in the UIS. The UIS surfaces directly exposed to several thermal striping from these assemblies are the instrumentation posts, control rod shroud tubes, the internal surfaces of the chimneys and the UIS mixing chamber liners. In addition, sodium exiting from the chimneys and beneath the skirt flange could produce thermal striping of a lesser magnitude on the support columns and the external surfaces of the UIS.

Striping damage is caused by local temperature fluctuations in the sodium adjacent to structural surfaces, which results in high cycle thermal fatigue. Excessive striping fatigue damage can produce surface cracks which may progress into the material.

Fatigue damage due to thermal striping is prevented by meeting striping design criteria. The criteria are based on high cycle uniaxial mechanically cycles tests performed in an air environment. The criteria for permanent components include the same appropriate margins which are applied to fatigue failure data by the ASME Code.

In order to apply the striping design criteria, it is first necessary to determine conservative boundaries of striping for the actual operating environments, during all modes of reactor operation. The analysis to determine such bounds is based on the following prototypic tests:

- A. Prototypic Full Scale 7-Assembly Thermal Striping Tests.
- B. Prototypic CRBRP Upper Internals Structure Striping Tests.

- C. Prototypic Fluid Mixing and Thermal Striping in Fuel Transfer and Storage Assemblies on Control Rod Shrouds and Inside Removable Radial Shield Outlets as Measured in the Integral Reactor Flow Model of CRBRP.
- D. Prototypic UIS Full Scale Striping Tests of the Instrumentation Posts and Lower Shroud Tube.

In order to minimize the effort expended in calculations, engineers first use very conservative simplified methods to evaluate safety margins. If the simple methods do not demonstrate positive margins, more exact and complex methods are used. Four levels of complexity were required to demonstrate that all critical Upper Internals components have positive margins. The methods used in increasing order of complexity are:

Method 1. It was determined from analysis of the tests that for the worst possible striping environment, Type 316SS can withstand a ΔT_m (metal surface cyclic temperature) of at least 80°F and that Alloy 718 can withstand a ΔT_m of at least 220°F without exceeding the endurance limit. Since the ΔT_f (fluid cyclic temperature) is larger than the ΔT_m , the simplest way to qualify the component was to calculate the worst ΔT_f for all possible conditions and compare it to endurance limit ΔT_m .

Method 2. This method is similar to Method 1 except the worst possible metal surface T_m is calculated and compared to the endurance limit ΔT_m .

Method 3. This method is an extension of Method 2, in which the fatigue damage caused by all ΔT_m 's, which exceed the endurance limit ΔT_m , is conservatively calculated. This damage is combined with the creep-fatigue damage of other transients and compared to the allowable damage envelope.

Method 4. Method 4 is an extension of Method 3 in which time history finite element analysis is used to determine a more accurate ΔT_m and creep-fatigue for the component based on the actual frequency of the temperature cycles and the material geometry.

Note that margins determined by these analysts are minimum margins and when methods 3 or 4 are used, analysis proceeds only until margins are 0.0. The actual margins are larger.

UC evaluations have determined that the following components are the critical components for thermal striping. The margins for other components are greater or equal to those of critical components. Analysis of critical components has determined that the minimum positive margins are as given in Table 3-51.

New core orificing has resulted in a less severe striping environment. Therefore, the actual margins are larger than the values shown.

3.9.4 Material Selection and Fabrication Technology

3.9.4.1 Material Selection

3.9.4.1 Material Selection

Service conditions in the region immediately above the CRBRP core are demanding of the materials used to construct the above-core structure. High operating temperatures coupled with severe thermal transients produce low cycle creep-fatigue damage while thermal striping produces high cycle fatigue damage. A reduction in the severity of the above-core environment would have had an unacceptable impact on the overall reactor system performance. The UIS designers' task was therefore to select materials and design configurations which would perform satisfactorily in the above-core environment for the design life of the plant.

A number of candidate structural materials were considered with the objective of producing a simple homogeneous all-welded structure using well proven manufacturing techniques. Of the materials considered, the three considered were American Iron and Steel Institute (AISI) Type 316 SS, Alloy 718 and a modified 9Cr-1Mo ferritic steel. No one of the materials considered was found to have the combination of well proven welded fabrication technology together with high resistance to damage accumulation required to support the adoption of a single material UIS design. The design adopted therefore made use of two

TABLE 3-51
MINIMUM POSITIVE MARGINS FOR UIS COMPONENTS

<u>Part</u>	<u>Material</u>	<u>Basis for Acceptability</u>	<u>Margin = $\frac{\text{Allow}}{\text{Actual}} - 1$</u>
Instrumentation Post	Alloy 718	Method #2	0.00+
		Method #1	0.12
Lower Shroud Tube	Alloy 718	Method #4	0.0+
Upper Shroud Tube	Alloy 718	Method #1	0.31
Lower Support Plate Liner	Alloy 718	Method #1	0.16
Skirt Liner	Alloy 718	Method #1	0.16
Skirt Ring Horizontal Liner	Alloy 718	Method #2	0.41
Chimney Inlet	Alloy 718	Method #1	0.06
Chimney Outlet	Alloy 718	Method #1	0.77
IVTM Port Plug Liner	Alloy 718	Method #2	0.54
IVTM Port Plug Lower End	Alloy 718	Method #2	1.07
IVTM Port Plug Above I-718	316SS	Method #1	0.40
Seismic Keys Liner	316SS	Method #1	0.45
Alloy 718		Method #1	1.43
Shear Web	316SS	Method #3	0.0+
Upper Support Plate	316SS	Method #1	0.65

materials to obtain the required combination of properties. Alloy 718 thermal shields were used to protect a conventional Type 316 SS load bearing weldment. The modified 9Cr-1Mo material was found to be unsuitable for either the load bearing structure or the thermal shield application.

Selection of the UIS materials and design configuration was backed by an extensive development testing program. Hydraulic tests provided a detailed mapping of the thermal fluid environment. Materials development tests provided essential mechanical properties and defined the effect of the service environment on those properties. Further tests provided validation of design methods and fabrication techniques. The net result of this extensive design, development and validation program is a UIS design based on proven technology which will operate satisfactorily in the CRBRP above-core environment for the design life of the plant.

3.9.4.2 Fabrication Technology

The basic load-carrying structure of the UIS is a Type 316SS weldment. Fabrication of this structure can be accomplished using only well-proven state-of-the-art fabrication techniques. Where Alloy 718 is used, it is used in product forms and sizes which have been established as being within the current production capability of material suppliers. The design is arranged such that attachment and retention of Alloy 718 components is by means of mechanical fastening. No structural welding of Alloy 718 is required.

3.9.4.2.1 The UIS Alloy 718 chimney and shroud tubes were manufactured at Rollmet, Inc., Irvine, California using their patented external roll extrusion process. External roll extrusion utilizes a mandrel over which the blank is slipped and secured. Two annular die rings are brought into position at one end of the blank, and moved apart to the reduction in outside diameter that is required. Mandrel and blank assembly are rotated while the die rings progress along the blank, simultaneously reducing wall thickness and outside diameter while lengthening the part.

Inside diameter remains constant while the outside diameter decreases in direct proportion to the reduction in wall thickness.

The design requirement that lead to the selection of Alloy 718 for use in thermal striping applications, i.e., high fatigue strength, also dictated that manufacturing processes did not cause any significant reduction in this strength level. Of particular importance was the avoidance or removal of high residual stresses which could reduce fatigue strength. Also of importance was the avoidance or removal of any surface or matrix imperfections that could reduce fatigue strength.

The chimney test sample material evaluated was supplied by Rollmet, Inc.⁽¹⁾ as a 10-3/4 inch ID pipe section of Alloy 718 having a 1/4 inch wall. This pipe geometry was roll formed from a 34 ± 2 inch long, 12 inch OD and 2 inch wall forging supplied by Carlton Forge Works⁽²⁾ which had been made from a billet of Alloy 718 supplied by Universal Cyclops.⁽³⁾

The nature of the roll extrusion forming process used in fabricating the UIS chimneys is such that high residual stresses are induced in the material. However, these stresses are readily reduced (and essentially eliminated) by subsequent in-process heat treatments that are required to develop the grain structure and properties of Alloy 718.

(1) Rollmet, Inc., Division of Wyman Gordan, 1822 Deere Avenue, Irvine, California 92714.

(2) Carlton Forge Works, 7743 East Adams Street, Paramount, California 90727.

(3) Universal Cyclops Speciality Steel Division, Cyclops Corporation, Bridgeville, Pennsylvania.

The forming process could conceivably also lead to laps or seams in the material, particularly at the surface. This may result since the roll extrusion process entails such heavy working of material at room temperature.

3.9.4.2.2 Development of Process Spec (2) - Non-Destructive Examination

Sample Alloy 718 pipe sections were visually, liquid penetrant and ultrasonically examined.

A. Visual Examination Results

A visual inspection of the roll extruded pipe section surfaces revealed that the ID surface was free of defects that could be attributed to the roll extrusion process. There were a few instances where it appeared that some post roll extrusion ID fixture arrangement had left marks approximately 1/4 inch in diameter.

The OD surface exhibited a rippled appearance caused by high and low bands occurring in the circumferential direction. The spacing of these bands are of the order of 1/8 inch and are believed due to the tool action on the surface. The general appearance was that of material being rolled over and smeared by the forming tool as the pipe was rotated.

B. Liquid Penetrant Inspection Results

The ID and OD surfaces of both pipe sections were examined using red dye liquid penetrant. No acceptance criteria were imposed and all indications were considered relevant and significant. No obvious differences were noted between the LP results of the two sections.

The OD surface had no LP indications. The ID surfaces did not have any specific indications but there was a general faint bleedout thought to be similar to that caused by general porosity. Subsequent examination showed that this faint bleedout was probably due to the overall condition of the ID surface.

C. Ultrasonic Examination Results

The ultrasonic examination was done in a water tank using an angle beam with a 3.5 MHz crystal. A notch was made in both the ID and OD surface in accordance with the ASME Code and RDT M2-18 [21] requirements. The notches were approximately 1 inch long, 0.0075 inch deep and 0.050 inch wide. The indications from these notches were set to be about 60 percent of full scale height (FSH).

Numerous UT indications were noted during the testing. Some were directly related to the ID surface marks noted during visual examination that were attributed to some fixture arrangement. All apparent indications were less than the 35 percent FSH, i.e., all indications, if real, were less than the size of the standard notch which was put in the material.

By comparing the above UT results with those from a heat treated pipe section from the same extrusion as the above material, it was concluded that the majority of the indications observed on the above sections may have been due to residual stresses. The heat treated section was free of UT indications.

3.9.4.2.3 Destructive Testing

The destructive testing on this material consisted of metallography and scanning electron microscopy (SEM). Suitable sections were taken from the GE pipe section by parting off a one-inch ring, making an axial cut through the

ring and removing metallographic specimen material from the cut end. It is interesting to note that when the axial cut was made through the ring, the as-rolled ring opened up an inch or more due to residual stresses.

A. Scanning Electron Microscopy (SEM) Results

Sections of the GE designated material were examined at the OD and ID surfaces using the scanning electron microscope at magnifications to 1000X. The ID surface had the appearance of being stretched to the point where fracture occurred. This left islands of flat, worked surface essentially surrounded by shallow fracture. This structure probably accounts for the general bleed out noted during LP testing. The shall nature of the fracture or tears was confined during the cross-section metallography as noted below.

The OD surface examination revealed a relatively smooth surface with indications of material being smeared over creating laps. There are also what appear to be hole-like indications.

B. Metallographic Examination Results

Metallographic sections through the thickness of the material were prepared using standard metallographic techniques. The sections were nickel plated to preserve edges during the metallographic preparations. Examinations were performed at magnifications to 500X.

The general grain structure of this material was typical of the structure of material in the roll-extruded condition. The grains in the axial (roll extrusion) direction were elongated. The grains when viewed in the transverse direction were more nearly equiaxial.

The OD surface was characterized by a generally smooth surface containing numerous small laps. A few pit-like defects were also observed. Examination of an area containing one of the ripples noted during the visual examination revealed about a 0.0015 inch height

difference from the high-to-low level. There were no indications that the ripple effect was caused by a ploughing over of material. There were no laps evident in this area.

The most significant defect was on the OD surface. This defect, which appears to have originated under the surface, was at least 0.002 inch deep. The defect did branch to the surface but it is possible that the branching to the surface did occur after the ring was sectioned. There were no LP indications on the full ring section. Examination at successive levels indicates the defect occurs at 30-60° to the surface and is about 0.004 inch in length. The maximum width was 0.00009 inch.

The examination of the ID surface did not reveal any of the internal defects noted above. The surface can be characterized as containing many shallow surface imperfections with a depth of less than 0.00034 inch. This is consistent with the SEM results.

3.9.4.2.4 Conclusions and Recommendations

The conclusions that were drawn from this review of the Alloy 718 in the roll-extruded condition are as follows:

- A. The material in the roll-extruded condition has high residual stresses.
- B. The material is basically sound with no matrix defects.
- C. The OD surface contains numerous laps, pit-like defects, and some crack-like, near surface defects. The crack-like defects are at least 0.002 inch deep as measured from the OD surface.
- D. The ID surface contains numerous shallow tears or fractures to a depth of less than 0.0004 inch. The tears or fractures are not crack-like.

The following recommendations were made and implemented as follows:

- A. (1) Recommendation - The OD surface should be conditioned to remove at least 0.002 inch of material to eliminate surface or near surface defects since they are crack like and could possibly reduce fatigue strength.
- (2) Implementation - The OD of the pipes were belt sanded per Rollmet's "Belt Sanding Procedures" (RPS 5028)^[3], which removes at least 0.007 inch from the thickness on the OD.
- B. (1) Recommendation - The ID surface should be conditioned to remove at least 0.0004 inch of material to eliminate surface defects. This recommendation is made in lieu of adequate information on the effect of the ID surface type defect on fatigue strength. If it can be shown that such imperfections do not decrease fatigue strength, then surface conditioning would be not recommended.
- (2) Implementation - (a) The ID of the pipes were belt sanded per Rollmet's "Belt Sanding Procedure (RPS 5028)^[3], which removes at least 0.001 inch from the thickness on the ID; (b) A 2,000 cycle thermal fatigue test^[4] was performed in high temperature sodium to experimentally qualify Alloy 718 and the fabrication processes. Samples were cut from a chimney manufactured in accordance with WARD approved process specs.
- C. (1) Recommendations - Any surface conditioning operations selected should not in themselves introduce high residual stresses or defects that could reduce fatigue strength.
- (2) Implementation - See B(2)(b) above.

3.9.5 Key Problems and Resolutions

3.9.5.1 Reactor Transients

A reactor trip results in a rapid reduction in temperature (approximately 300°F in 20 seconds) of the sodium exiting from the fuel assemblies. The radial blanket exit sodium temperature also decreases, but at a slower rate. A reduction in total reactor flow rate (100% to 10% in 35 seconds) occurs simultaneously in order to minimize the rate of temperature change in the outlet plenum and the reactor vessel outlet nozzle; this is done to minimize the thermal shock on components outside the vessel. The fuel and blanket temperature profiles and the flow coast down curve are shown in Figure 3-59. During the initial portion of the reactor trip transient, the colder, more dense sodium, does not have sufficient kinetic energy to overcome negative buoyant forces and flow stratification occurs. Since this phenomenon was not well understood in the early design phases of the CRBRP, and the resulting transient temperatures have an impact on the design of the UIS, as well as additional reactor vessel and plant components, it was considered essential to perform experimental studies in order to assess this effect. A series of water tests were performed, in which the Froude number (ratio of buoyancy to inertial forces) was simulated.

Water tests were performed, in which the density differences were simulated, using water and brine, at Battelle Columbus Laboratories in a 1/3 sector, 0.55 scale model^[27] and at Argonne National Laboratory in a 1/15th scale model.^[28] Additionally, Argonne National Laboratory simulated the reactor trip using sodium in a 1/15th scale model.^[29] The sodium experiments were performed to assess the differences between water and sodium. The conclusions from these early tests included:

- o sodium and water data gives similar results and water tests conservatively predict the transient.
- o the 1/3 sector and full 360° model testing give similar results.

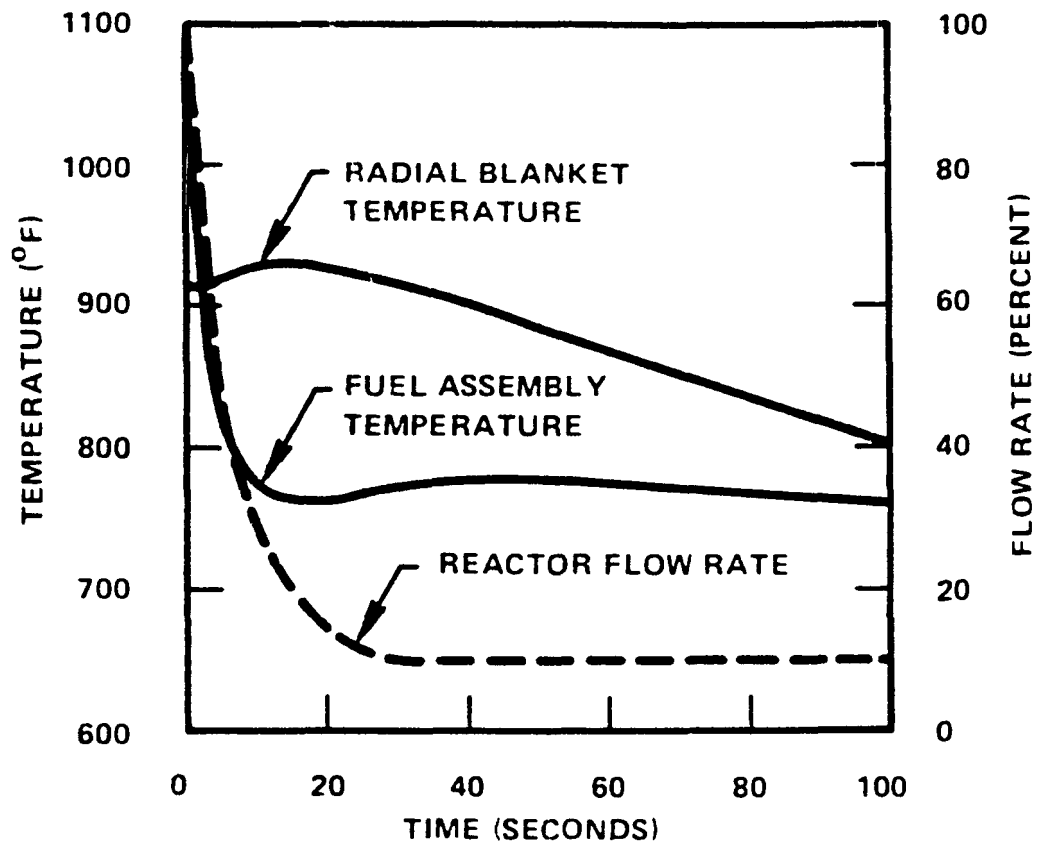


Figure 3-59

Prototypic Fuel and Blanket Exit Temperatures and Reactor Flow Rate Following a Reactor Trip

- o flow stratification, and a resulting hot/cold interface in the outlet plenum, exists following a reactor trip.
- o transients at outlet nozzle are more severe when the chimneys are reduced in height

Due to design evolution, the UIS simulation in the 0.55 scale model and the 1/15 scale model did not fully simulate the UIS. Additional information was obtained which simulated the revised UIS design. This additional testing was performed at ANL in a 1/10th scale model.^[29] This model included a detail hydraulic representation of the upper internals structure, including simulation of the mixing chamber and instrumentation posts. Additionally, data acquisition techniques were improved, based on experience with previous tests. Transient data was obtained, following the reactor trip for a period of several reactor hours. The temperature measurements from this test provided the basis for thermal boundary conditions, required for calculating the thermal stresses of the UIS.

3.9.5.2 Thermal Striping

Temperature oscillations of the sodium occur during normal operation and are caused by the mixing of different temperatures of coolant exiting from hot fuel assemblies which are located next to colder fuel, blanket and/or control assemblies. This phenomenon is called "thermal striping." These temperature oscillations cause metal surface temperature oscillations, which can lead to fatigue of materials. For example, the differences in coolant temperature between a control assembly and a fuel assembly can be as high as 345°F. This causes high fluid temperature oscillations of the coolant flowing in the lower shroud tube. Additionally, temperature differentials between the coolant exiting a fuel assembly adjacent to a blanket assembly can be as large as 270°F. Thermal striping occurs on the instrumentation posts in the vicinity of these assemblies, which have large temperature differentials.

Measurements of the magnitude of thermal striping were made in the ANL 1/10 scale model.^[29] In these tests, all fuel assemblies were simulated with identical temperatures and all blanket assemblies had the same temperatures.

The 1/10 scale model tests simulated "gross" striping caused by temperature differences between fuel and blanket assemblies and provided useful data away from the mixing chamber region. However, these tests could not assess the effects of striping caused by individual assemblies.

Thermal striping measurements, performed in the one quarter scale water model at the Hanford Engineering Development Laboratory (HEDL), were able to assess individual assembly effects. Figure 3-60 schematically illustrates the model and a photograph of the UIS. Data from both the 1/10 and 1/4 scale models were used to assess the magnitude of thermal striping.

3.9.5.3 Vibration

The complex flow patterns and velocities of up to 20 ft/sec (6 m/sec) in the reactor vessel outlet plenum required an experimental characterization of the vibration behavior of the UIS. The experimental data was obtained in two phases.^[30] Early data was obtained from 1/3 scale bench model tests of the instrumentation posts and chimney/shroud tube. Significant results of these ANL tests include:

- o No significant vibration of instrumentation post (less than 0.0004 inch at 165 Hz).
- o At normal flow rates testing indicated very small response levels in the control rod shroud and chimney.
- o Rattling existed in the lower control rod shroud and chimney support region at levels below one g (peak to peak).

The second phase of vibration data was obtained from the 1/4 scale integral reactor flow model. This model included detail mechanical structural simulation of the UIS. Conclusions from the initial testing include the following:

- o Measured response was generally small, about .001 to .003 inches, for flow rates between 10 and 15%, for three loop flow.

CRBR INTEGRAL REACTOR FLOW MODEL

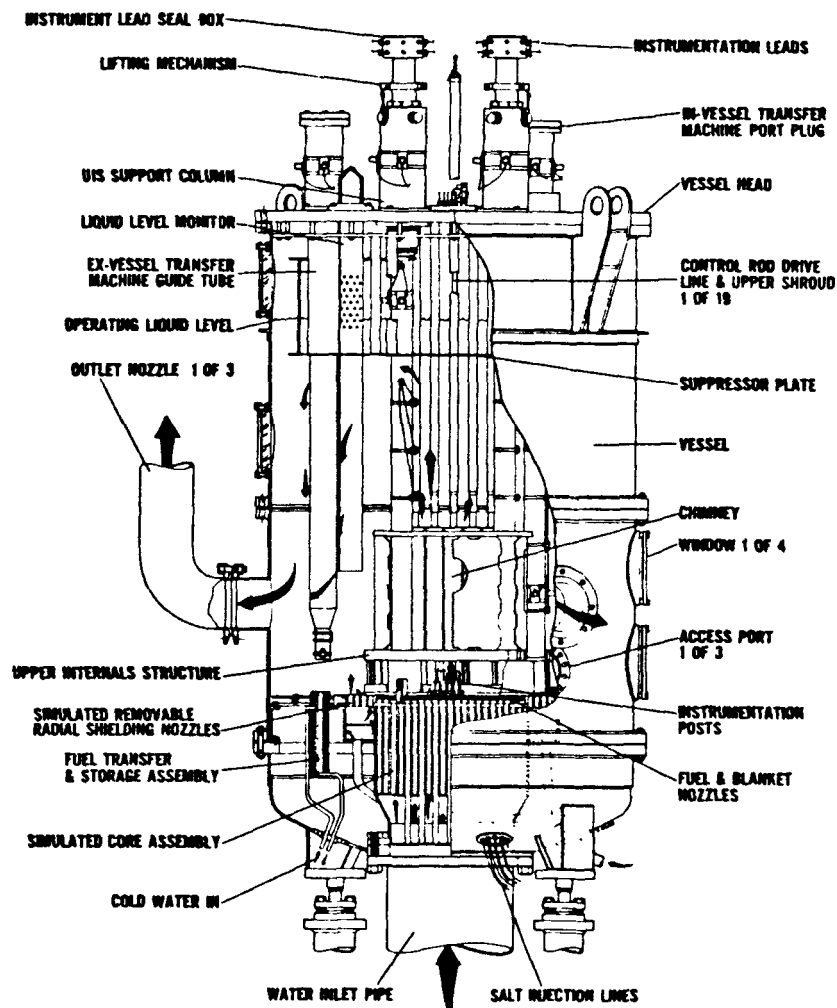


Figure 3-60 SCHEMATIC OF INTEGRAL REACTOR FLOW MODEL

- o Measured response was similar with two loop flow.
- o Measured resonant responses were close to calculated natural frequencies.
- o Impacting up to 1g (peak to peak) was observed on the following components:
 - UIS Keys/Core Former Ring
 - UIS Column/Head
 - UIS/IVTM Port Plug
 - UIS Upper Shroud Tube/Lower Shroud Tube
 - Dashpot/Control Rod Driveline

Based on these tests, some gaps were reduced to prevent rattling. The model simulation of the final design was to be used to confirm the adequacy of these changes.

3.9.6 Final System Status

The Upper Internals Structure Design was completed on July 17, 1980 and the fabrication purchase order was placed with Combustion Engineering, Chattanooga, Tennessee on August 26, 1980. The fabrication was approximately 3/4 complete when the CRBRP project was terminated in November, 1983.

3.9.6.1 Outstanding Changes

This section shows some of the outstanding changes that have been incorporated into the design since the conceptual design review.

- A. Flow through the UIS was initially contained in separate flow tubes for each reactor assembly. Each flow tube penetrated the main structural plates. The resulting low structural efficiency led to flow collectors directing the flow through a smaller number of flow tubes. Alloy 718 forging was selected as the flow collector material over the core where maximum temperature differences occurred in the

core effluent. Cost improvement and design simplification studies produced the final design of a mixing chamber above the core with instrumentation posts extending from the top of the chamber to position thermocouples over the core assemblies, along with an upper and lower plate weldment to replace the double portal frame structure.

- B. The stress analysis developed for the final design review, December 1976, indicated some redesign was required in the lower plate of the welded box structure and in the instrumentation post. Subsequent data from the thermal hydraulic test program also indicated a design change had to be made to the shroud tubes. These changes have been incorporated into the final configuration. Subsequent analyses indicates the cumulative creep-fatigue damage is within the allowables.

The lower plate thickness was changed from 6.0 inch to 4.0 inch.^[49] The 2.0 inch plate thickness removed was retained as thermal insulation washers, which were non-structural. The chimney support forging thickness was increased from .75 inches to 1.11 inch to increase the thermal insulation of the ligament. The outer ring of the lower plate was reduced in stiffness by incorporating a number of 3.0 inch diameter holes. These changes produced three favorable results: 1) The radial temperature variations at steady-state and transient conditions are closer together, 2) A closer match of the thermal inertia between the plate central region and that of the rim, and 3) The reduction of local gradients while simultaneously reducing the temperature in the critical region.

The critical region is near the bottom corner in the minimum section of the ligament between the first row of chimneys. The cumulative creep-fatigue damage, based on the final design review temperatures, was calculated to be 0.96 compared to an allowable of 0.98.

The lower plate ligament supports the instrumentation post by an interference fit. Instrumentation posts are positioned at the center of the tri-cusp regions of each ligament. Stress analysis has specified a 0.001 to 0.004 inch interference fit for an acceptable cumulative creep-fatigue damage.

- C. The outer Alloy 718 forging of the instrumentation post is supported by three radial pins near the top of the Alloy 718 forging. The central Type 316SS post has three radial holes for the support pins. The central post is exposed to high temperature sodium, due to the gap between the Alloy 718 forging and stainless as a provision for differential thermal expansion. The initial cumulative damage analysis indicated that the allowable usage factor was not met. A subsequent design change increased the thickness of the Alloy 718 from 0.75 to 1.25 inch and provided an acceptable transient temperature gradient reduction in the Type 316SS post.

Additionally, data was available that indicated notched specimens had a greater creep rupture life than unnotched specimens based on nominal stress. Thus, this indicated that the design values of Code Case 1592 are conservative. Accordingly, a request was submitted to modify the elevated temperature design rule for creep damage evaluation of notched specimens. This request for modification was backed up by an experimental test program to determine the stress rupture and creep-fatigue strengths of notched specimens with sodium exposure. Test results justified the requested modification to the higher temperature design rule. The cumulative damage analysis usage factor of 0.24 meets the allowable 0.85 when using the modified design rule.

- D. Integral reactor flow model test data evaluation indicated that the upper shroud tube had increased radial temperature differences and that the maximum thermal gradients through the radial thickness were increased on the lower shroud tubes. These two effects resulted in changing the upper shroud tube from 316 stainless to Alloy 718 and in changing the single thickness of the lower Alloy 718 shroud tube from

0.375 to 0.1 inch with a double wall.[58] The design changes were found to be structurally adequate having a creep-fatigue damage of 0.09 which is considerably less than the allowable of 1.0.

- E. When ECP-0491[50] was approved, the reference fuel management scheme (RFMS) was replaced by the alternate fuel management scheme (AFMS), the homogeneous core was replaced with the heterogeneous core. Fuel assemblies were interspersed with blanket assemblies and vice-versa. Fifteen control rod assemblies replaced the original 19. Temperature distributions were completely changed.

This change required major modifications of the UIS. Following is a list of tasks that were required for implementation:

<u>Task No.</u>	<u>Task</u>
1	UIS overall thermal load analysis, both thermal and stress
2	Lower plate analysis, both thermal and stress
3	Lower plate ligament analysis, both thermal and stress
4	Instrumentation post analysis, both thermal and stress
5	Lower shroud analysis, both thermal and stress
6	Chimney analysis, both thermal and stress
7	Chimney, spider and attachment to lower plate analysis, thermal and stress
8	Mixing chamber shield plate interaction analysis, thermal and stress
9	Striping evaluation, both thermal and stress
10	Updating E-Specification, thermal, stress and design
11	Define and analyze four plugs to suppressor plate
12	Implementation of holddown requirements
13	Develop thermal design environment

<u>Task No.</u>	<u>Task</u>
14	Modifications to shroud tubes, chimney, spiders, and IVTM port plug, design and drafting
15	Change holddown posts (25) to instrumentation posts, design and drafting
17	Add six holddown posts, design and drafting
18	Add conduits, dry-wells and thermocouples, design and drafting
19	Prepare and issue AFMS layout drawings

F. ECP W-1066[57] was submitted for approval in November, 1978 to reduce the UIS jacking mechanism seal temperatures and to simplify the UISJM. The UISJM was raised and the UIS columns were increased in height by approximately 40 inches. These changes resulted in a reanalysis of the upper part of the UIS by the thermal-hydraulics and stress groups.

3.9.6.2 Outstanding Activities to Complete Design

The design was essentially completed on July 17, 1980 after a status review meeting of the UIS.

The fabrication purchase order was placed with Combustion Engineering, Chattanooga, Tennessee on August 26, 1980.

Fabrication was approximately 3/4 complete when the CRBRP project was terminated in November, 1983.

Additional testing is required to verify the UIS design (see Paragraph 3.9.6.3).

3.9.6.3 Outstanding Problems and Description of Actions Needed

The Upper Internals Structure was being fabricated when the CRBRP program was terminated. Several activities were being planned to verify the UIS design. One activity (DRS 31.14.07 ADDENDUM 7) was flow induced vibration testing in the Integral Reactor Flow Model. The object of this test was to evaluate the flow induced vibration characteristics of the UIS final design and to characterize the vibro-impacting observed in several regions during the IRFM Phase I Tests.

Approval was requested to complete the flow induced vibration portion of the program. Based on the assessments of the results of this program, should the wear test be considered necessary, funding for this portion of the program would be requested.

Following are the objectives and justification from the DRS:

OBJECTIVES

The objectives of this test are to evaluate the flow induced vibration characteristics of the UIS final design under simulated full power and pre-operational test conditions and to characterize the vibro-impacting observed in several regions during the IRFM Phase I tests. In addition, accelerometers will be located at identical locations in the IRFM model to that proposed for CRBRP pre-operational testing with the objective of providing a correlation of the pre-operational tests to the model tests. These objectives are consistent with the intent of Item C.2.2 of the NRC Regulatory Guide 1.20 in which, in the case of the Upper Internals Structure, the Integral Reactor Flow Model is considered a prototype[38].

JUSTIFICATION

As indicated in the ARD response to NRC Questions 110.67 & 110.68 and further amplified in PSAR Section 3.9.1.3, the CRBRP approach to assure that the reactor can satisfactorily operate without impairment from flow induced vibrations is different than the Light Water Reactor (LWR) approach. This

inherent difference is largely due to the fact that the sodium in CRBRP has significantly different characteristics than water that makes disassembly of an instrumented reactor for inspection and removal of instrumentation after pre-operational testing very difficult and possibly detrimental to the continued integrity of the structure because of adhering sodium/oxidation products at re-installation. Some of these differences are 1) sodium is opaque and therefore precludes in-reactor viewing, 2) sodium wetted components require inert environments for handling and cleaning with subsequent requalification for insertion into the reactor and 3) high thermal stresses occur in CRBRP components during transients. In addition, the high operating temperatures ($>600^{\circ}\text{F}$) are beyond which instruments such as accelerometers, displacement transducers, etc. can function reliably.

For these reasons the comprehensive program to meet the intent of Item C.2.2 of NRC Regulatory Guide 1.20 which the CRBRP project has developed, differs from the LWR practice recognizing the difference between LMFBRs and LWRs. This program is one of testing to assure that excessive flow induced vibrations will not occur. This extensive model test program serves the function of a prototype, in the case of the UIS [36], which eliminates the need for extensive plant vibration testing.

At a meeting in March 1977 at the CRBRP/PO, attended by Plunkett (U.C. of Minn.), Connors (W-R&D), Wambsganss (ANL), Dodge (SRI) & Ryan (HEDL) the overall CRBRP flow induced vibration program was reviewed.

It was generally agreed by these well known experts in the field of flow-induced vibration, that the CRBRP vibration program would be sufficiently comprehensive to verify the structural integrity of the prototype reactor internals for flow-induced vibration prior to power operation. These experts recommended that in addition to the 1/4 scale IRFM tests selected full scale tests on portions of the UIS be conducted. While the DRS primarily describes the final testing to be performed in IRFM, the full scale testing is briefly discussed below.

At the start of the IRFM Phase 1 design and testing at HEDL, it was recognized that the final design of certain components would not be available and would have to be added later and that changes would evolve that would require updating and re-testing. These Phase 2E tests, therefore, will simulate the UIS final design and will provide confirmation of the structural adequacy of the UIS from the flow-induced vibration standpoint.

IRFM Phase 1 [37] and ANL 1/3 scale [38] flow-induced vibration tests detected impacting of certain UIS components. The areas of impacting were in regions where assembly clearances are incorporated in the CRBR and scaled in the models. Minimum clearances in the model were limited to 0.008 inches to avoid squeeze film damping. Should a problem exist it would be one of wear and/or fretting and to be able to predict wear and/or fretting or to conduct wear tests, it is necessary to characterize the vibro-impact at the component interface, i.e., determine impact frequency, etc. With the present state-of-the-art, it is not possible to predict the parameters necessary for wear evaluation, therefore model testing is required to verify that the design is free from possible damage due to vibro-impacting.

To supplement the IRFM model program, several component full scale tests were assigned to the Argonne National Laboratory. These tests require full scale testing to satisfy similarity criteria which was not possible in the 1/4 scale IRFM model. They include the gap modulation flow-induced vibration test of the control rod upper to lower shroud tube slip joint, the UIS chimney vibro-impact test, and the IVTM gap modulation flow induced vibration test. The upper to lower shroud tube slip joint test has been successfully completed, the chimney vibro impact test is currently underway and the IVTM gap modulation test was planned for early calendar 1982. (The chimney vibro-impact test and the IVTM gap modulation test were successfully completed.)

3.9.7 Conclusions and Recommendations

As a result of the experience gained in designing and analyzing the CRBRP Upper Internals Structure, several conclusions and recommendations can be made that can serve as guidance or as a frame of reference for others involved in the design and analysis of elevated temperature components.

- A. A coordinated effort with designer, thermal-hydraulics analyst, stress analyst, and manufacturing engineer must be maintained throughout all design phases of the component.
- B. Model building and model testing have been demonstrated to be effective techniques to develop an assembly sequence and to reduce thermal uncertainties in the structural input parameters and to characterize the vibration behavior of a structure in a complex flow field.
- C. Design changes based on both analyses and experimental evaluation of data have been demonstrated to be a rational approach in developing a structurally adequate component for use in a high temperature environment.
- D. Every effort should be made to get accurate fluid temperatures by analysis or test at the earliest possible time.
- E. Improved creep-fatigue criteria should be developed for evaluations where the creep damage is predominant and ratchetting does not occur. The use of the present criteria and rules results in the hold time effect on fatigue life being estimated by way of a creep damage integral which is sensitive to the assumed stress/strain curve, the accuracy in its representation, and the choice of the thermal creep equation. It would appear more realistic to determine the cyclic life from the fatigue strength based upon data with hold times at the peak compressive strain. This would permit a more universal

consideration of the stress/strain relationships, manufacturing stresses induced in the structure, and cyclic and creep hardening behaviors.

- F. Simplified methods of thermal stress analysis of complex structures need to be developed to provide more accurate predictions prior to rigorous inelastic analysis.

3.9.8 REFERENCES

1. J. A. Carr and L. J. Flanigan, "CRBRP Outlet Plenum Flow Stratification Study," prepared by Battelle Columbus Laboratories under Subcontract 54-WH-184775-B with Westinghouse, ERDA AI(11-1)-2395, submitted December 9, 1975 [BCL-75-1].
2. Deleted
3. Deleted
4. Ji Young Chang, "Alloy 718 - Chimney Tube - 2000 Cycle Thermal Fatigue Test," (ARD/LMT/JYC/80-06).
5. ASME Boiler and Pressure Vessel Code, Section III, Case N-47 (1592-10), "Class 1 Component in Elevated Temperature Service," ASME, New York, 1977.
6. Department of Energy Division of Nuclear Power Development, RDT Standard F9-4T, "Requirements for Construction of Nuclear System Components at Elevated Temperatures (Supplement to ASME Code Case 1592, 1593, 1594, 1595 and 1596)," Oak Ridge National Laboratory, April 1977.
7. Department of Energy Division of Nuclear Power Development, RDT F9-5T, "Guidelines and Procedures for Design of Nuclear System Components at Elevated Temperature," Oak Ridge National Laboratory, September 1974.
8. S. S. Manson, G. R. Hanford, and M. H. Hirschberg, "Creep-Fatigue Analysis by Strain-Range Partitioning," NASA TM X-67838, National Aeronautics and Space Administration, 1971.
9. Nuclear Systems Materials Handbook, TID-26666, Hanford Engineering Development Laboratory, 1977.
10. R. B. Snyder, K. Natasan, and T. F. Kassner, "Kinetics of the Carburization - Decarburization Process of Austenitic Stainless Steels in Sodium," J. Nucl. Mater. Vol. 50, 1974, pp. 259-274.
11. P. D. Goodell, T. M. Cullan, J. W. Freeman, "The Influence of Nitrogen and Certain Other Elements on the Creep-Rupture Properties of Wholly Austenitic Type 304 Steel," Trans. ASME, J. Basic Eng., 1967, pp. 517-524.
12. R. L. Miller, S. L. Schrock, G. A. Whitlow, "Metallography of the 300 Series Stainless Steels Exposed to High Temperature Sodium," Proceedings of the Third Annual Meeting - International Metallographic Society, November 1970, pp. 105-114.
13. P. N. Flagella, J. R. Kahrs, "Low Cycle Fatigue Testing in Flowing Sodium at Elevated Temperatures," International Conference on Liquid Metal Technology in Energy Production, 1976.

14. J. L. Houtman, "Inelastic Strains from Thermal Shock," Machine Design, Vol. 46, pp. 190-194, March, 1974.
15. J. L. Houtman, "Elastic-Plastic Strains from Thermal Shock at a Discontinuity-A Simplified Approach," Simplified Methods in Pressure Vessel Analysis, PVP-PD-029, June, 1978.
16. Proprietary
17. J. L. Cook, P. I. Nakayama, "VARR-II: A Computer Program for Calculating Time Dependent Turbulent Fluid Flows with Slight Density Variation," CRBRP-ARD-0106, November 1976.
18. "Nuclear Systems Materials Handbook," TID 26666, Hanford Engineering Development Laboratory.
19. S. S. Manson, "Fatigue: A Complex Subject - Some Simple Approximations Approximations," Experimental Mechanics, 5, No. 7, pp. 193-226, July 1965.
20. C. E. Jaske, and J. O'Donnell, "Fatigue Design Criterion for Pressure Vessel Alloys," ASME Journal of Pressure Vessel Technology, Vol. 99, No. 4, p. 584-592, November 1977.
21. E. P. Esztergar, "Creep-Fatigue Interaction and Cumulative Damage Evaluations for Type 304 Stainless Steel," ORNL-4757, June 1972 Oak Ridge National Laboratory.
22. D. C. Lord, and L. F. Coffin, Jr., "Low Cycle Fatigue Hold Time Behavior of Cast Rene 80," Metal. Trans. 4, No. 7 pp. 1647-1654 (1973).
23. J. L. Houtman, "Inelastic Strains from Thermal Shock," Machine Design, Vol. 46, No. 7, pp. 190-194, March 21, 1974.
24. J. L. Houtman, "Elastic-Plastic Strains from Thermal Shock at a Discontinuity - A Simplified Approach," in: Simplified Methods in Pressure Vessel Analysis, PVP-PB-029, pp. 1-14, June 1978.
25. F. Garofalo, "Creep Rupture Behavior of Notched and Unnotched Specimens of Type 304, 316 and 321 Austenitic Stainless Steels," ASTM Proceedings, Vol. 59, pp. 957-972, 1959.
26. "Mechanical Properties Test Data for Structural Materials Quarterly Progress Report for Period Ending January 31, 1978," ORNL-5380, Oak Ridge National Laboratory, March 1978. [Availability: USDOE, Technical Information Center].
27. E. H. Novendstern, M. D. Woods, T. S. Andreychek, L. J. Flanigan and J. A. Carr, "Outlet Plenum Flow Stratification Studies for the Clinch River Breeder Reactor Plant," ASME Paper No. 76-WA/HT-84, 1976.

28. J. J. Lorenz, R. D. Carlson, and P. A. Howard, "The Influence of Scale Size and Fluid Thermal Properties in Simulating LMFBR Outlet Plenum Behavior," Nuclear, Solar, and Process Heat Transfer - St. Louis 1976, American Institute of Chemical Engineers Symposium Series, No. 164, Volume 73, 1977.
29. P. A. Howard, J. J. Lorenz, W. L. Howarth, and E. H. Novendstern, "CRBRP Outlet Plenum Behavior During Steady State and Transient Conditions," ASME Paper No. 77-HT-32, 1977.
30. E. H. Novendstern, F. A. Grochowski, T. M. Yang, J. A. Ryan and T. M. Mulcahy, "CRBRP Flow Induced Vibration Program," prepared for International Atomic Energy Agency, International Working Group on Fast Reactors Specialists Meeting On LMFBR Flow Induced Vibrations At Argonne National Laboratory on September 20-23, 1977, IAEA Paper Vienna, Austria.
31. ECP W-0491.
32. P. T. Falk, M. Kosanchich, Sr., "Inelastic Analysis of the Upper Internals Structure for the Clinch River Breeder Reactor Plant," ASME Paper No. 79-PVP-25, 1979.
33. J. L. Houtman, "Structural Analysis of the Upper Internals Structure for the Clinch River Breeder Reactor Plant," ASME Paper No. 79-PVP-68, 1979.
34. E. W. Ference, W. Hill, J. R. Cavanaugh, "CRBRP Mockups Identify Design Problems and Facilitate Construction," Power Engineering, PP64-67, June 1983.
35. Letter, D. R. Riley to W. M. Jacobi, "CRBRP: NRC Question 413.1, PSAR CHAPTER 14, First of a Kind Abstracts," EN:R:77-053, dated February 10, 1977.
36. J. A. Ryan and J. J. Mahoney, "Integral Reactor Flow Model Phase 1 Tests: Summary Report," HEDL TC-883, dated June 1977.
37. T. M. Mulcahy, "Flow-Induced Vibration Scale-Model Tests of the CRBR Flow Chimney and Flow Chimney - Lower Shroud: Final Report," ANL-CT-77-12, dated February, 1977.
38. ECP L-10-011, 9/15/76, "Load Combinations ECP."
39. ECP W-0147, 4/14/76, "Revise Lower Chimney Forging."
40. ECP W-0155, 5/5/76, "Revise IVTM Port Plug."
41. ECP W-0194, 7/21/76, "Core Exist Instrumentation."
42. ECP W-0313, 11/18/76, "Change I.D. OF Lower Shroud Tube."
43. ECP W-0337, 12/6/76, "Update System 31."

44. ECP W-0414, 3/1/77, "Increase Secondary Core Holddown."
45. ECP W-0436, 2/28/77, "UIS Thermal Liners."
46. ECP W-0445, 4/7/77, "UIS Design Mods."
47. ECP W-0453, 3/11/77, "UIS Key to Keyway Vertical & Radial Gaps."
48. ECP W-0467, 3/21/77, "UIS Lower Plate Mod."
49. ECP W-0491, 7/19/77, "AFMS ECP."
50. ECP W-0521, 6/2/77, "Incorporate UIS Tolerance Study."
51. ECP W-0523, 5/3/77, "Modify Upper & Lower Shroud Tubes."
52. ECP W-0555, 6/2/77, "Modify Upper & Lower Shroud Tubes."
53. ECP W-0580, 6/16/77, "Revise UIS E-Spec."
54. ECP W-0599, 6/29/77, "Replace Weldment Assy."
55. ECP W-0996, 5/1/78, "UIS Upper Shroud Tube."
56. ECP W-1066, 11/29/78, "Simplification of UIS Jacking Mech."
57. ECP W-1626, 9/23/80, "UIS Drawing Changes."
58. ECP W-1692, 4/24/81, Provide Striping Protection for UIS."

LIST OF ECP'S REFERENCED IN TEXT

1. ECP L-10-011, 9/15/76, "Load Combinations ECP."
2. ECP W-0147, 4/14/76, "Revise Lower Chimney Forging."
3. ECP W-0155, 5/5/76, "Revise IVTM Port Plug."
4. ECP W-0194, 7/21/76, "Core Exist Instrumentation."
5. ECP W-0313, 11/18/76, "Change I.D. of Lower Shroud Tube."
6. ECP W-0337, 12/6/76, "Update System 31."
7. ECP W-0414, 3/1/77, "Increase Secondary Core Holddown."
8. ECP W-0436, 2/28/77, "UIS Thermal Liners."
9. ECP W-0445, 4/7/77, "UIS Design Mods."
10. ECP W-0453, 3/11/77, "UIS Key to Keyway Vertical & Radial Gaps."
11. ECP W-0467, 3/21/77, "UIS Lower Plate Mod."
12. ECP W-0491, 7/19/77, "AFMS ECP."
13. ECP W-0521, 6/2/77, "Incorporate UIS Tolerance Study."
14. ECP W-0523, 5/3/77, "Modify Upper & Lower Shroud Tubes."
15. ECP W-0555, 6/2/77, "Modify Upper & Lower Shroud Tubes."
16. ECP W-0580, 6/16/77, "Revise UIS E-Spec."
17. ECP W-0599, 6/29/77, "Replace Weldment Assy."
18. ECP W-0996, 5/1/78, "UIS Upper Shroud Tube."
19. ECP W-1066, 11/29/78, "Simplification of UIS Jacking Mech."
20. ECP W-1626, 9/23/80, "UIS Drawing Changes."
21. ECP W-1692, 4/24/81, "Provide Striping Protection for UIS."

3.10 Core Restraint Design

3.10.1 Summary of Functions and Description

The core restraint system consists of the mechanical interfaces between the Lower Internals Structure, Fuel Assemblies, Control Assemblies, Blanket Assemblies, Removable Radial Shield Assemblies (RRSAs) and the Upper Internals Structure.

The limited free bow concept is the basis for the CRBRP core restraint system design. This concept is based on passive peripheral restraint of core assemblies while allowing some limited lateral movement of assemblies between the thermal conditions of refueling and normal power operation. The key objectives of this system design concept are: simplicity in design and the utilization of irradiation creep to offset the effects of duct material swelling [1].

3.10.1.1 Functions

The principal functions of the core restraint system are to provide control of core geometry and core motion within the limits imposed by reactivity insertion considerations and to ensure acceptable insertion and withdrawal loads on the reactor assemblies. Other functions and constraints for the core restraint system design are to ensure that operational loads on assemblies and surrounding internals structures are within acceptable levels and that core assembly alignment is within acceptable limits to meet requirements of the control assemblies, the refueling grapple, and the upper internals structure [2].

Assembly-to-assembly power to flow (P/F) variations, particularly between the fuel and blanket regions, result in significant lateral duct thermal gradients within and above the reactor core zone. These gradients cause the assemblies to bow and contact one another. Assembly bowing motions, particularly in the active core zone, affect the reactivity balance of the operating reactor. The synergistic action of the thermal and neutron flux gradients within the reactor core zone results in an additional duct bowing component associated

with material swelling. Irradiation creep affects the residual bowing patterns while reducing duct stresses induced by thermal and swelling gradients.

Additional considerations in the core restraint system are the need to provide a path for the transmission of operational and seismic loads through the core, and provide assembly outlet nozzle alignment for control rod insertion and assembly refueling. Consideration of the above environmental factors and the need to satisfy performance, operational, and safety goals lead to the definition of the functional requirements for the core restraint system [3].

3.10.1.1.1 Reactivity Changes Due to Assembly Motion

In a fast breeder reactor, the reactivity balance is sensitive to small changes in core assembly positions. The presence of load plane interassembly gaps and permissible pivoting motions of the reactor assemblies within the confines of supporting inlet modules allows for possible radially inward assembly displacements. Such motions can be postulated to occur in a continuous fashion, as during power/flow ratio transitions associated with reactor startup and shutdown, or suddenly, as might result from forces which permit the core array to take on a more compact configuration. Consequently, specific reactivity requirements are defined based on sudden or continuous assembly motions.

A. Reactivity Changes During Normal Operation

In the limited free bow core restraint concept, assembly free bowing motions are possible at low power to flow ratios ($P/F \geq 0+$). However, as the power to flow ratio approaches unity thermal gradient induced three point restraints are established on most of the reactor assemblies. When free bowing motions predominate, the net bowing reactivity response can be positive. However, when the three point restraint is imposed the net bowing reactivity response is negative. The core restraint reactivity requirement is specified so as to accommodate net positive bowing reactivity effects at low power to flow ratios. However, at power to flow ratios approaching and equal

to unity a more restrictive approach is taken in that reactivity effects attributable to the core restraint system are required to be negative.

B. Sudden Reactivity Insertions

Sudden lateral assembly motions arise from: flow induced vibrations, the release of static frictional forces at assembly contact surfaces and motion induced by a seismic event. Extensive hydraulic testing has indicated that flow induced duct displacements would be of low amplitude and of a random nature. The net reactivity effect attributable to this motion would be negligible. The latter two sources, however, are considered in establishing the design upper limit on the interassembly gaps relative to the maximum sudden reactivity insertion that can be accommodated during design basis events. For example, if an SSE event occurred while the plant was operating at steady state full power conditions a 60¢ step reactivity insertion could be accommodated without exceeding faulted fuel damage limits. At hot standby, an 85¢ step insertion could be accommodated.

3.10.1.1.2 Reactor Component Loads and Distortions

A. Component Operating Loads and Contract

Loads are developed between reactor assemblies during reactor operation due to the restraint of assembly temperature gradients, differential swelling, and differential expansion between the core and surrounding support structure. Interassembly loads and duct to duct contact also arise at mid-core elevations, excluding control assemblies, due to duct dilation and at other assembly elevations such as the shield block and two point inlet nozzle contact due to assembly bowing. Other sources of assembly and peripheral component loading include seismic loads and thermal loads associated with plant design duty cycle events. Interassembly loads are transmitted to the reactor internals components via the core former and inlet modules.

Loads and contact arising from the above noted sources are considered in assessing functional capability and structural integrity of the core and internals components.

B. Assembly Insertion and Withdrawal Loads

Irradiation induced distortions in the reactor assemblies which develop during on-power operation remain when the reactor is brought to refueling conditions. The restraint of the irradiation induced distortions results in interassembly forces at contacting load pad and inlet nozzle surfaces. Limiting reactor assembly insertion and withdrawal loads are based on the structural integrity of the reactor assembly and degradation of interassembly contact surfaces which could detrimentally affect core restraint performance.

In the case of the former, the specified insertion and withdrawal load limits provide structural margin against tensile failure and buckling of the reactor assembly duct. The latter consideration provides margin against galling at load pad, duct or assembly nozzle-inlet module contact surfaces.

C. Assembly Distortion

When the reactor assembly is withdrawn from the core, the irradiation induced distortions must be accommodated by the refueling and spent fuel transfer equipment. The limit on assembly distortion effects ensures that distorted assemblies can interface with refueling system equipment.

3.10.1.1.3 Assembly Top End Alignment

The refueling system and reactor control system interface with the reactor assembly top ends. Design considerations arising from alignment requirements include maintaining reactor assembly alignment during refueling and control assembly alignment during operation.

Alignment requirements are imposed on the core restraint system by the CRBRP alignment document. The requirements on primary and secondary control assembly top end alignment allow satisfactory operation of the control rod system during reactor operation and satisfactory operation of the control rod and refueling systems during reactor refueling.

3.10.1.1.4 Core Assembly Liftoff

The upward motion of the core assemblies can be postulated due to potential vertical ratchetting mechanism during normal reactor operation and vertical seismic motions of the core support plate. Functional concerns associated with these occurrences are: potential reactivity additions due to sudden assembly repositioning; the degradation in performance of the core restraint system or any interfacing component, specifically, core assembly flow; interassembly load pad contact and control rod insertion; structural integrity of core or interfacing components.

3.10.1.2 Description

In the limited free bow core restraint concept, axial (vertical) support of the core assemblies is provided at the assembly inlet nozzles by the lower inlet modules, bypass flow modules, and core support plate. Vertical holddown of the core assemblies against the upward coolant flow is provided by assembly weight and by hydraulic balance (except for core special assemblies and RRSAs) at the inlet nozzles, with the upper internals structure providing backup or secondary mechanical holddown for the fuel, blanket and control assemblies. Lateral (horizontal) restraint of the core assemblies is provided at three elevations: at the inlet nozzles, at the above core load plane (ACLP), and at the top load plane (TLP). Interassembly loads are transmitted to the lower inlet modules, bypass flow modules and through the load planes to the core formers.

Buildup of duct bowing induced by irradiation swelling is limited by irradiation creep. The core formers restrain bowing of the core assemblies and thereby cause the generation of duct stresses which permit irradiation

creep to offset the effects of swelling. Duct restraint loads are relieved at refueling by allowing the ducts limited freedom of movement as the temperature gradients associated with power production are removed.

Summarizing, the features of the core restraint system design include:

- o Three core assembly support planes--at the inlet nozzles, the above-core load plane (ACLP), and the top load plane (TLP). The load planes maintain assembly top end alignment, limit interassembly and refueling loads to acceptable values, and limit bowing reactivity insertion during reactor operation.
- o Clearances between core assemblies at the mid-core elevation to provide for irradiation creep and swelling dilation of the assemblies while avoiding excess load buildup between assemblies.
- o Hard-surface coatings at the assembly load pads and inlet nozzles and core former surfaces contacting the core to prevent self-welding in sodium and to provide a relatively low friction coefficient for refueling operations.
- o Core former rings at the ACLP and TLP to maintain radial alignment of the core periphery.
- o Hydraulic balance at the assembly inlet nozzles to provide vertical holddown of the assemblies against the upward coolant flow, with the upper internals structure providing secondary holddown in the event of a malfunction of the hydraulic system.

3.10.2 Evolution of Design [4]

The approach to Liquid Metal Fast Breeder Reactor (LMFBR) core restraint system design changed significantly with the discovery of irradiation swelling and creep in core structural materials. Prior to this event, core restraint system designers had been primarily concerned with the control of thermal deformations. The absence of any prior consideration of irradiation swelling and creep in the design of LMFBR core restraint systems had not caused any operational problems because the highly enriched metal fuel used in these cores remained in the reactor a relatively short time to achieve its design burnup. The resulting fluence ($3 \text{ to } 4 \times 10^{22} \text{ n/cm}^2 \text{ E} > 0.1 \text{ Mev}$) was either below the swelling incubation threshold or in the range of very low predicted swelling. With the advent of the mixed oxide fueled core, however, goal fluence levels were raised to the range of $1.5 \text{ to } 2.5 \times 10^{23} \text{ n/cm}^2 \text{ E} > 0.1 \text{ Mev}$. At this fluence level, the available materials irradiation data indicated substantial volumetric swelling. A full consideration of irradiation swelling and creep effects was an essential element of core restraint system design for high fluence mixed oxide cores.

The above events coincided in the U.S. with the initiation of design work on the Fast Flux Test Facility (FFTF) reactor. The FFTF core restraint system design was the first one produced in the U.S. with full consideration given to irradiation swelling and creep effects. Subsequent core restraint system design and development activities have built on the foundation laid in the FFTF program.

Core restraint concepts considered during the early stages of the development of the FFTF system design fall into three broad categories: free bowing, restrained, and hybrid.

The free bowing core restraint concept has the attractive feature of producing a negative contribution to the core power coefficient under both startup and full power operating conditions. It has, however, no features which will prevent the buildup of swelling induced bows. Preliminary analyses showed that in an unrestrained core, the end of life core component lateral deflections resulting from differential duct swelling would amount to several inches at the elevation of the handling sockets. Deflections of this magnitude were incompatible with the operating requirements of both the

control rods and the refueling machine. A further major problem with the free bowing core restraint concept is its response to seismic excitation. The core component ducts in this concept are cantilever supported above the elevation of the core support plate. The combination of a long cantilever overhang, a center of gravity positioned a considerable distance above the cantilever root, and a reduced cantilever stiffness at the root (nozzle) result in the core components having a low natural frequency. Seismic excitation produces large lateral deflections in core components supported in this manner. These deflections were not compatible with the control rod system functional requirements. It was not evident that acceptable near term solutions were available for the performance problems associated with the free standing core. This core restraint concept was therefore eliminated from further consideration.

The restrained core, which has received the most attention in the U.S. program is supported at three points. These are the inlet nozzle, the handling socket elevation and an above core load pad. The location of the above core load pad is selected based upon considerations of (a) the axial distribution of duct transverse swelling and dilation dimensional changes and (b) the requirement to produce a thermal bowing mode shape which results in an outward motion of the fuel in response to an increase in the power to flow ratio.

Bowing behavior of a three point support restrained duct with pin joint supports is governed by: (a) the nature of the axial distribution of duct transverse temperature gradients and (b) the stiffness of the two duct spans between the supports. The transverse temperature gradient builds from zero at the bottom of the core to a maximum at the core top, and remains approximately constant over the remainder of the length of the duct. In terms of structural response, the significance of this axial distribution of transverse temperature gradient lies in the fact that the thermal bow misfit in the duct span above the core is greater than that in the span containing the core. The above core span is also shorter and therefore stiffer than the span containing the core. These conditions combine to produce an S-shaped mode of bowing in

response to the temperature gradients. The duct span containing the core bows outwards away from the core center line, while the upper duct span containing the fission gas plena bows inwards.

Lateral restraint for the core assemblies is provided by core restraint hardware located at the core periphery. During normal power operation, a restrained core will bow into a wheat sheaf mode. The core components react with the core plate and the core restraint features provided at the handling socket elevation. At the above core load plane, there is little or no forced contact with the peripheral restraint hardware. At this elevation, the core components press inwards producing a condition of duct to duct loading across the core. The restrained core concept thus utilizes the thermal bowing behavior of the core to achieve the compaction required for vibration control during steady state operation.

Load pads in the restrained core concept project beyond the envelope of the core component ducts by the amount of the predicted transverse swelling and creep dilation growth in the core region. The load pad height is selected so as to assure that duct to duct contact in the active core region will be confined to small regions within the core.

The principal advantages of the restrained core concept are: (a) its ability to utilize the mechanism of irradiation creep to mitigate the buildup of duct bowing due to differential irradiation swelling, (b) the control which it provides over core component positioning under both steady state and transient operating conditions, and (c) the elimination of cantilever loading and the associated high duct bending stresses and deflections under conditions of seismic excitation.

It was recognized that the restrained core concept had a number of significant performance uncertainties and design problems. An evaluation of these problems and uncertainties, however, indicated that they were of a type which could be resolved by appropriate design measures and development activities. Further, the restrained core concept was considered to have a greater potential for the accommodation of changes resulting from developments in the

area of irradiation swelling and creep behavior than had the other concepts studied. These factors led to the selection of the restrained core concept as the basis for the U.S. LMFBR core restraint development program.

The third core restraint concept is a hybrid containing some of the features of both the free-bowing and the restrained core concepts. Core components in this concept are cantilever supported and free to bow until such time as they make contact at the handling socket elevation with a surrounding array of radial shield assemblies. Once this contact has been made, any further increase in the duct temperature gradients results in the generation of restraint loads between the radial shield assemblies and the core components. These loads produce some outward deflection of the radial shield assemblies and some inward incremental deflection of the core assemblies. Spacer buttons are located in or near the active core region. These buttons limit the amount of any inward deflection of the core components.

The hybrid concept does permit the use of irradiation creep to offset the buildup of duct bows due to irradiation swelling. It does not, however, provide the degree of core component location required by the control rod systems and the refueling machinery. In addition, it provides only a marginal improvement in seismic response over that obtained from the free bowing core. A potential also exists for the hybrid core to produce a positive duct bowing contribution to the core power coefficient during normal power operation. This can result when the combination of irradiation induced bows and thermal bows has resulted in the generation of forced contact with the radial shield assemblies.

The evaluation of the hybrid concept indicated that while it had some of the advantages of both the free bowing and restrained core concepts, it had more of their disadvantages. In addition, it did not appear to offer the flexibility required to respond to changes in the irradiation effects design data. The hybrid concept, therefore, was not emphasized in the U.S. LMFBR core restraint development program.

At the time the FFTF core restraint system was being designed, there was substantial uncertainty associated with the magnitude of both the irradiation swelling and creep effects. Design equations had been developed for both effects, but each of the equations had associated with it upper and lower confidence limits. There were no data available which would permit the designer to eliminate any segment of the design envelope defined by the plus and minus confidence limits associated with both the swelling and creep behavior. The use of this wide a range of swelling and creep behavior, however, resulted in a very wide range of end of life predicted off power bow configurations for the core components. This wide a range of off power core configurations caused particular problems in the design of those features of the core restraint system which were required to release the core for refueling. It was decided that the required flexibility for off power core release could only be guaranteed if the core restraint system design incorporated active core clamps with provision for core release for refueling.

While the active core clamps were adopted as a core restraint feature, it was recognized that there were substantial advantages to be gained if load relief for refueling could be achieved without the use of active core restraint elements. The core restraint feature introduced to achieve this objective was the limited free bow concept. Investigations into the effect of irradiation swelling and creep on duct bowing indicated that the limited free bow core restraint concept would work for off power load relief if swelling-creep superposition behavior is creep dominated. The data used to demonstrate this result were obtained from slit tube tests. Material used in this test is obtained from capsules fabricated from Type 304 stainless steel used in irradiation experiments. This measurement can be made by slitting the irradiated tube sample and measuring the slit width before and after the slitting process is completed. Gap opening corresponds to swelling domination while gap closing indicates creep domination. The data indicated creep domination of the swelling-creep superposition behavior. As a result of this development, it was possible to demonstrate acceptable refueling loads in FFTF without the use of the active core clamps.

For a conventional LMFBR core with the duct temperature gradient vectors centered on the core center line, a peripheral gap would be sufficient to provide the required free motion. The FFTF core, however, had an unusual distribution of temperature gradients due to the cold spots generated within the core by the numerous test loops. Because of this, it was necessary to introduce an additional feature into the FFTF core restraint system design in order to assure limited free bow capability in the core components. The feature adopted for this purpose was the floating collar.

The floating collar is a hexagonal block with a circular bar which fits around the core component outlet nozzles. A radial gap of 0.13" between the collar inside diameter and the nozzle outside diameter provides the limited free motion capability. Additional free motion capability is provided by radial gaps between the core restraint yokes and the peripheral core assemblies.

3.10.2.1 Conceptual Design

The design approach adopted for the CRBRP core restraint system is identical to that evolved in the FFTF program. The system design makes use of irradiation creep to limit the buildup of differential swelling induced bows. Limited free bow is used for off power restraint load relief. The peripheral restraint hardware design, however, is much simpler than that used in FFTF. Structural restraint is provided by forged rings separate from, but located within, the core barrel. Ring forgings were selected for these structural members in order to assure long term dimensional stability. Segmented formers are pinned to the structural rings and can be set at a given distance from the outer profile of a compacted core.

Floating collars on the core components were not considered to be necessary since the homogeneous core temperature gradients are well ordered and highest in the outer core and radial blanket ducts. Off power load relief under these conditions can be achieved using only in core gaps and a radial gap between the outermost core components and the restraint system hardware.

A new feature, however, which was incorporated into the CRBRP core restraint design was the control assembly plug-in feature. Initial conservative determinations of control assembly top end misalignment indicated that this feature was needed in order to preclude excessive drag on the control assembly drivelines in the event of a scram. However, after total reactor system misalignment studies were completed, this feature was eliminated from the design.

3.10.2.2 Preliminary Design [5]

There were new challenges which arose during the CRBRP core restraint design in addition to duct distortion consequences of irradiation creep and swelling which were faced in the design of FFTF. The first significant challenge was dealing with positive bowing reactivity associated with the buildup of radially oriented transverse temperature gradients during reactor startup. Although this effect had been predicted in FFTF core, the magnitude was significantly smaller because of the relatively disordered orientation of transverse duct temperature gradients. However, in CRBRP it was found that the positive bowing reactivity contribution during startup could exceed the net contribution of all other predominately negative reactivity feedbacks resulting in a positive power coefficient over a portion of the startup range. The design solution proposed for this problem was the use of bimetallic spines in the outer RRSAs. Differential thermal expansion between the Inconel and stainless steel spines of these assemblies causes them to bow radially inward as the reactor is heated to hot standby temperature. A clamping action at the above core load plane is produced eliminating the gaps within the core. As a result when the reactor startup above hot standby temperature is initiated, the bowing reactivity effect is negative. Significant cost and developmental penalties were recognized to be associated with the use of bimetallic spine shield assemblies. Consequently this concept was only retained as a backup until a final solution to this problem was determined.

3.10.2.3 Final Design [6,7]

Detailed investigation into the bowing reactivity change with power to flow ratio during reactor startup revealed that the power coefficient was positive only over a limited portion of the power-to-flow ratio range (0.3 to 0.7) and only over a portion of the fuel assembly lifetime (50 to 250 days). Since the power coefficient remained negative in the operating range (above 40% power), it became important to show that adequate reactor control could be assured over the small portion of the range where the positive power coefficient was predicted to occur. Detailed control studies on the hybrid computer showed that control could be maintained manually with operator attention. Further, reactor power could be adequately maintained within the control band if the control system was operated automatically (flux control mode). Consequently the solution selected to resolving the power coefficient problem during reactor startup was to prescribe that reactor startups or operation in the zero to one power-to-flow ratio range were to be performed on automatic control. No further changes to core or peripheral restraint components were judged to be needed. Bimetallic spine RRSAs were dropped from consideration.

During the progression from conceptual to final design irradiation creep and swelling data on cold worked Type 316SS became available. The higher fluence data indicated that the magnitudes of both phenomena appeared to be much greater than previously expected and the uncertainties were also substantially increased. Utilizing correlations evolving from this increased irradiated materials data base it became increasingly apparent that it would become difficult to meet distortion, contact and load limitations for the confined core array. Two events occurred during this design stage which had a substantial effect on the mitigation of the sensitivity of the core restraint design to the noted irradiation materials behavior for cold worked Type 316SS. The first event was the decision to adopt a heterogeneous core configuration for CRBRP. The main thrust of this design change with regard to core distortion, contact and loads was a lowering of the design fast fluence levels. This was due to a reduction in the equilibrium fuel assembly lifetime from three to two years and a reduction in the peak fast flux level. The second event was the consideration of alloy modifications to achieve improved irradiation creep and swelling behavior. The movement to achieve an improved

duct material specification gained impetus when data on Japanese core materials was obtained. This led to a respecification of the fuel and blanket cladding and duct material with higher concentrations of Phosphorus, Boron, and Titanium.

3.10.3 Key Problems and Resolutions

3.10.3.1 Power Coefficient

As described above, the decision was made to accommodate the potential for a positive power coefficient in the startup range. However, in order to enhance controllability in the startup range, automatic flux control was provided in the startup as well as the operating range. It was also necessary to modify project documentation which noted the requirement for the power coefficient to be negative over the entire power range. The above changes were accomplished by means of ECP L10-042[8].

3.10.3.2 Core Restraint Design Uncertainties

During the design of the FFTF core restraint system it became evident that an improved methodology for adequately representing the complex interactions between adjacent core assemblies as well as an improved representation of the core environment and materials behavior was necessary. As a result, steps were taken to initiate a national core restraint development program which included participation by various LMFBR contractors and national laboratories. The areas of investigation and the pace of activities accelerated after the initiation of the CRBRP project. At the peak of activity the following areas were elements of the core restraint development program:

- o 3-D assembly mechanical interaction testing on the 7 and 14 row Core Restraint Test Facility.
- o TRITON code development for the prediction of duct temperatures inboard of the removable radial shielding assemblies.
- o NUBOW-3D code development for the prediction of duct bowing, contact status and loads as well as core reactivity effects.
- o Creep-swelling opposition testing in Experimental Breeder Reactor (EBR)-II on capsules fabricated from candidate duct materials.

- o CORTAC-2D code development for the prediction of transient reactivity and core responses related to duct bowing behavior.
- o Assembly refueling testing limits and criteria.

Other development areas included the qualification of core restraint methodology, duct-to-duct contact methodology, and the PARABOW code development. However, methods qualification and duct-to-duct methodology were initiated late in the CRBRP Project but significant progress was not made before termination.

On the other hand, work on the PARABOW code was discontinued because it was supplanted in use by the NUBOW-3D code which possessed greater capability, accuracy and versatility.

3.10.3.3 Core Restraint Baseline Documentation

During the conceptual and preliminary phases, the core restraint design was organizationally part of the core former structure design. During this time period, consideration was given to incorporating core restraint design requirements into the core former equipment specification, however, this was judged to be inconsistent with format requirements for this document. The problem was ultimately resolved during the final design stage when the core restraint system was identified as a design activity independent of interfacing components. As a result three categories of documents were identified for identifying and demonstrating compliance of the core restraint design requirements. The highest level of specification of the core restraint requirements was in SDD-31[1]. However, the implementation and elaboration of these requirements was specified in WARD-D-0180.[3]. A second tier baseline document was Interface Control Document (ICD) CW50649[9]. The purpose of this document is to specify the requirements, dimensional and surface conditions on reactor system components to satisfy the design requirements of the core restraint system. In addition, design requirements and operational limitations are identified for the Reactor Refueling and Plant Protection Systems in order to assure satisfaction of core restraint system requirements

during related plant operations. Finally WARD-D-0207[10] was identified as the document (topical report) for documenting the status of satisfaction of the core restraint system requirements.

3.10.4 Final System Status

3.10.4.1 Outstanding Changes

At the termination of the CRBRP project, secondary control assembly design duct bowing profiles had been tentatively defined. Final secondary control assembly duct temperatures had been received from General Electric Company and an updated analysis of secondary control assembly bowing was planned.

3.10.4.2 Outstanding Activities to Complete the Design

Two core restraint issues that remained outstanding at CRBRP project termination were: irradiation induced duct-to-duct contact criteria and blanket assembly liftoff induced by a vertical seismic event. Development of methodology to define duct-to-duct contact criteria had been planned into the core restraint development program. However, the implementation of this work was halted with project termination. Had the duct-to-duct contact methodology been developed, it would have been implemented in conjunction with fuel assembly structural analysis methods to establish fuel assembly lifetime limitations or criteria with regard to this mode of loading.

Core assembly liftoff studies had indicated the potential for blanket assembly liftoff during vertical seismic excitation. This determination was based simply on comparing seismic induced accelerations with compensating gravitational and pressure forces. However, once liftoff is initiated, the problem of re-impact and possible contact with the upper internals instrumentation post becomes high nonlinear. Preliminary planning called for modeling core assembly dynamics during seismic excitation in order to bound blanket assembly liftoff displacements and blanket assembly impacting with adjacent structures. It was anticipated that the resulting liftoff displacements and impacting could be structurally accommodated.

3.10.4.3 Outstanding Problems and Descriptions of Actions Needed

There were no core restraint design issues that fell into this category. It was expected that all unresolved issues would be worked out by planned design and development activities.

3.10.5 References

1. CRBRP System Design Description Number 31 "Reactor System."
2. "CRBRP 1982 Technical Progress Report," CRBRP-ARD-0322.
3. "CRBRP; Core Restraint System-Implementation of Design Requirements," WARD-D-0180, Rev. 3, September 23, 1982.
4. W. E. Pennell, "Core Restraint System Design, Selection and Development of the Restrained Core Concept," W-AESD Memo LRC-76-559, December, 1976.
5. W. E. Pennell to A. E. Mravca, "CRBRP; Core Restraint System and Core Former Structure Preliminary Design Review," WR50489, March 27, 1975.
6. R. M. Vijuk to W. J. Purcell, "CRBRP; Review Package for the Core Restraint System Final Design Review," WL800189, March 13, 1980.
7. R. M. Vijuk to W. J. Purcell, "CRBRP; Review Package for the Core Restraint System Final Design Review - Enclosures 4 to 6," WL800294, March 20, 1980.
8. ECP L10-042, Changes to SDD-31, 90; OPDD-10 and PSAR, September, 1977.
9. Interface Control Document CW50649, "CRBRP, Core Restraint System Interface Requirements."
10. J. E. Kalinowski and T. K. Houghtaling, "CRBRP; Core Restraint System Analysis and Performance Evaluations." WARD-D-0207, June, 1982.

3.11 Thermal Striping Mitigations

3.11.1 Introduction

Early in the design phase of the Clinch River Breeder Reactor (CRBR) a phenomenon that has been termed thermal striping was identified as a major design concern. Thermal striping is defined as the fluctuating fluid temperature field that is imposed on a structure when fluid streams at different temperatures mix in the vicinity of the structure. This temperature fluctuation of the structure surface, if of significant magnitude and frequency, results in thermal fatigue of the surface or surface cracks which can gradually propagate leading to failure.

The process for evaluating thermal striping is illustrated on Figure 3-61. Included in the process are the following steps; 1) define the striping potential, 2) determine mixing sodium temperature fluctuations, 3) evaluate the heat transfer film coefficient, 4) calculate structure transient temperatures, and 5) evaluate the thermal fatigue in the structure. Mitigation of thermal striping can be accomplished in any of the first four steps. Detailed analysis can provide relief in the 5th step. Striping potential is reduced by reducing stream-to-stream temperature differences. Improved mixing or mixing away from structures can reduce the magnitude of the coolant temperature fluctuations next to the structures. Structure thermal response to the fluctuating coolant temperature are reduced through lowered heat transfer film coefficients. The surface to mean temperature difference within the structure can be reduced by reducing the thickness of the structure or by adding thermal liners to protect the structure.

There are many sources of potential thermal striping in the CRBR reactor. These sources fall into three categories. A category with great potential is the core coolant stream-to-stream temperature differences. The second category is the mixing of the exit flow from cold low flow assemblies with hot streams from the core. Examples of these are the RRSs and FT&SAs. The third category is cold leakage flow into the hot upper plenum with examples of this being the leakage flow from the horizontal baffle and the vessel nozzle liner.

EVALUATION OF THERMAL STRIPING FATIGUE IS A 5 STEP PROCESS

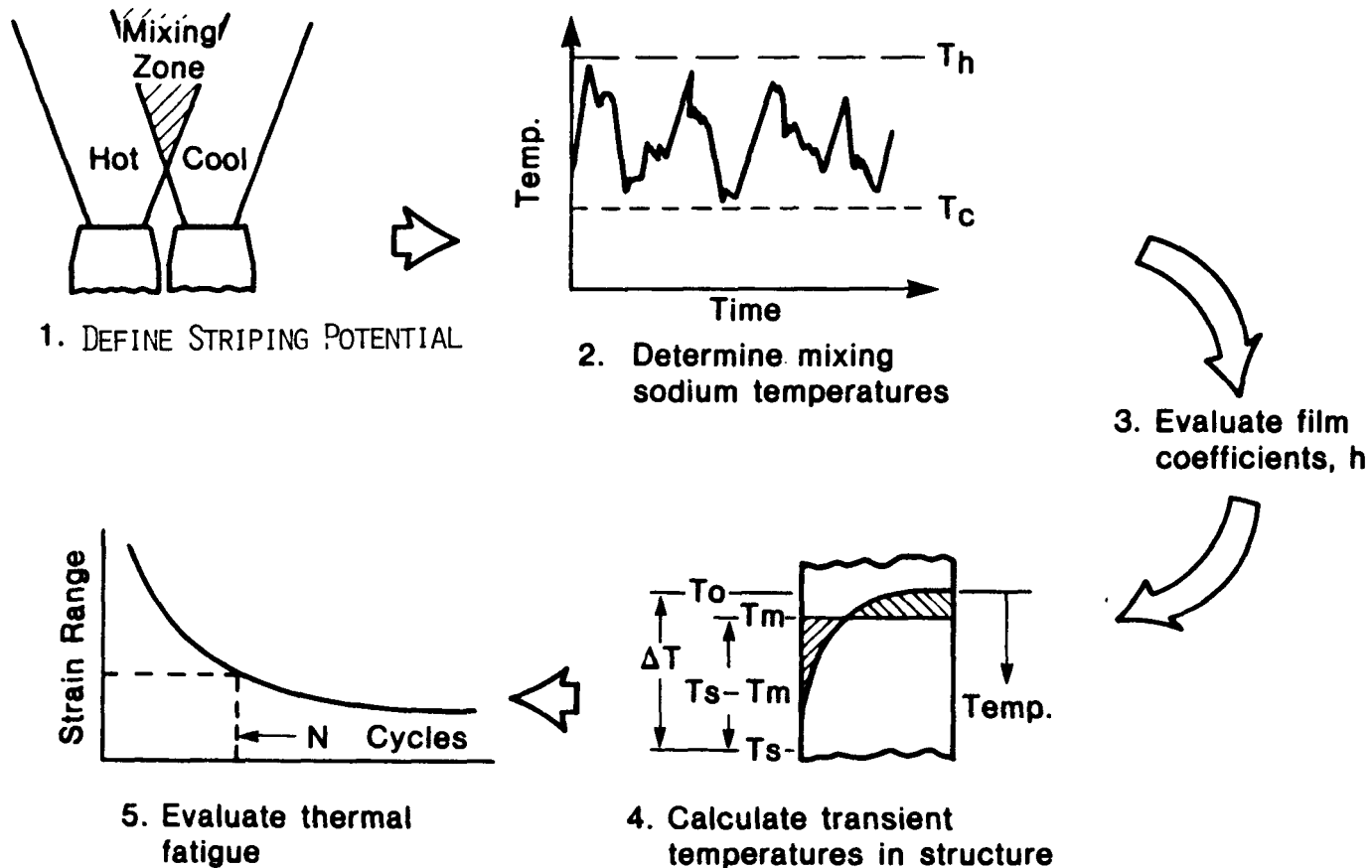


Figure 3-61

While the magnitude of the core coolant stream-to-stream temperature differences defines the maximum potential thermal striping amplitude, this value is not suitable for direct use in design support thermal striping analyses. Thermal striping amplitudes measured adjacent to structural surfaces are found to be significantly less, at many locations, than the thermal striping potential present in the incoming coolant streams. At this time the available analytical tools are not able to make a satisfactory prediction of the thermal striping amplitudes resulting from stream mixing. Thermal striping boundary conditions are therefore based on an extensive series of thermal hydraulic tests.

The temperature fluctuations of the structure surface are less than the adjacent coolant. This difference is due to the film drop which protects the surface. Heat removal from a high power density LMFBR core demands the use of a coolant with high heat transfer capability. Liquid sodium provides the heat transfer characteristics required to produce an efficient core design. Unfortunately, these same heat transfer characteristics serve to increase the severity of thermal strains produced in the structures by both thermal transients and thermal striping temperature fluctuations. Because of the high heat transfer coefficients, the surface temperature of structural components follows closely that of the adjacent coolant. Heat transfer coefficients are particularly high in the highly turbulent region immediately above the core.

As illustrated in Step 4 of Figure 3-61 the transient temperature distribution in the structure is characterized by a fluctuating non-linear temperature profile with the steepest gradient at the surface. The thermal striping strain range is determined by the range of surface to mean temperature differences. Two ways to reduce the surface to mean temperature differences, which mitigates the effects of thermal striping in the structure, is to reduce the thickness of the structure or thermally shield the structure with another structure.

Because thermal striping is a surface phenomena, the mean temperature of the structure responds little to the surface temperature fluctuations. By reducing the structure thickness, the mean temperature will be more responsive to the surface temperature fluctuations thus reducing the surface to mean

temperature differences. Even greater reductions in the surface to mean temperature differences can be obtained by adding a thin thermal shield in front of the main structure. The steep gradient at the surface occurs in the thermal shield mitigating the effects of striping on the main structure. The following sections discuss striping mitigation in the steps illustrated in Figure 3-61.

3.11.2 Source of Potential Thermal Striping

Sources of potential thermal striping within the CRBRP reactor are many and varied. A source with the great potential for thermal striping is the differences that occur between core assembly exit coolant temperatures. Some differences exist between most assemblies but the differences can be quite large between fuel assemblies and inner blanket assemblies, between fuel assemblies and radial blanket assemblies and between fuel assemblies and control assemblies. Maximum temperature differences for these three locations are illustrated on Figure 3-62.

The first step in mitigating thermal striping is to reduce the temperature differences of the source of potential striping. For the core assembly exit flows this is accomplished by orificing the assemblies to reduce temperature differences. In the last reorificing of the CRBR final core design, a constraint was added to restrict temperature differences between adjacent assemblies to mitigate thermal striping on the core assembly nozzles and the "near field" UIS components. This phase of thermal striping mitigation is discussed in Section 3.13, In-Vessel Flow Management. Final core assembly exit temperatures are reported in Reference 1.

A second source of thermal striping potential exists where the exit flow from cold low flow assemblies mixes with hot streams. Two examples of this source are cold flow from the Removable Radial Shield Assemblies mixing in the hot cross flow from the core that leaks through the UIS peripheral gap and cold flow from the Fuel Transfer and Storage Assemblies mixing in the hot upper plenum. The third primary source of thermal striping potential exists where cold leakage mixes with hot coolant. A list of examples of this striping potential would include the following:

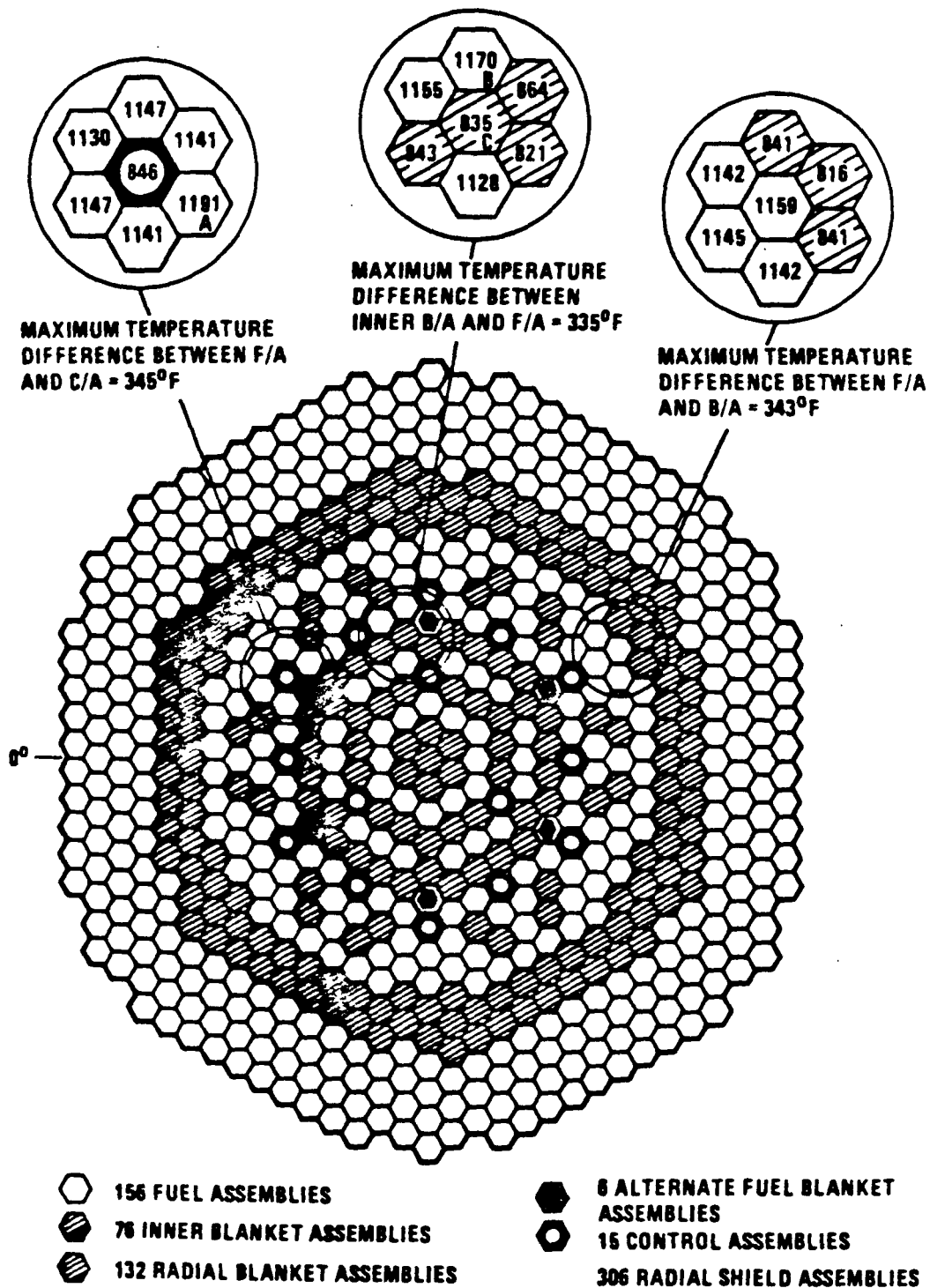


Figure 3-62 Selected CRBRP Temperature Distributions (°F)

- o Cold sodium from the Reactor Vessel/Core Barrel Annulus leaking through the Horizontal Baffle into the upper plenum.
- o Cold sodium from the Reactor Vessel Liner Annulus leaking through the outlet nozzle liner, the makeup sodium nozzle liner and through the vessel liner flow holes.
- o Cold core assembly inlet nozzle leakage that flows upward around periphery of core assemblies and mixes with hot core exit cross flow in the region of the upper core former structure and removable radial shield assemblies.
- o Cold IVTM port plug gap leakage mixing in the hot upper plenum.

3.11.3 Coolant Temperature Fluctuations

The initial CRBR thermal striping evaluations were done using striping magnitudes equal to the temperature difference of the striping source. This approach was dictated by the lack of an adequate analytical method or test data to determine mixed sodium temperatures. Using the full striping potential overestimated the striping at most locations and caused structural design problems. Therefore a comprehensive test program was conducted to mitigate the striping values from the initial evaluation and to better define the overall thermal striping phenomena. Because of the expense of testing with sodium, the striping tests were conducted using water. Japanese and British experience indicate striping in sodium is 20% less than measured in water. The CRBR striping evaluation did not take advantage of this 20% mitigation.

The extensive striping test program for CRBR include tests in the following facilities:

- o WAFSD Water Table
- o ANL 1/10 Scale Outlet Plenum Model
- o HEDL 1/4 Scale IRFM

- o W-AESD Full Scale Seven-Assembly Model
- o W-AESD Thermal Striping Test Facility

Some details of these tests are presented in the following sections.

3.11.3.1 W-AESD Water Table Test

The water table facility at W-AESD was used extensively in the experimental investigation of thermal striping. These tests ranged from simple quantitative tests to assess thermal striping to detailed scale model tests that were used in the final design evaluations.

3.11.3.1.1 UIS Mixing Chamber Test

A two-dimensional water table test of the UIS mixing chamber was performed to provide preliminary striping data for the thermal analysis of the UIS. Results of these preliminary striping data are tabulated on Table 3-52.

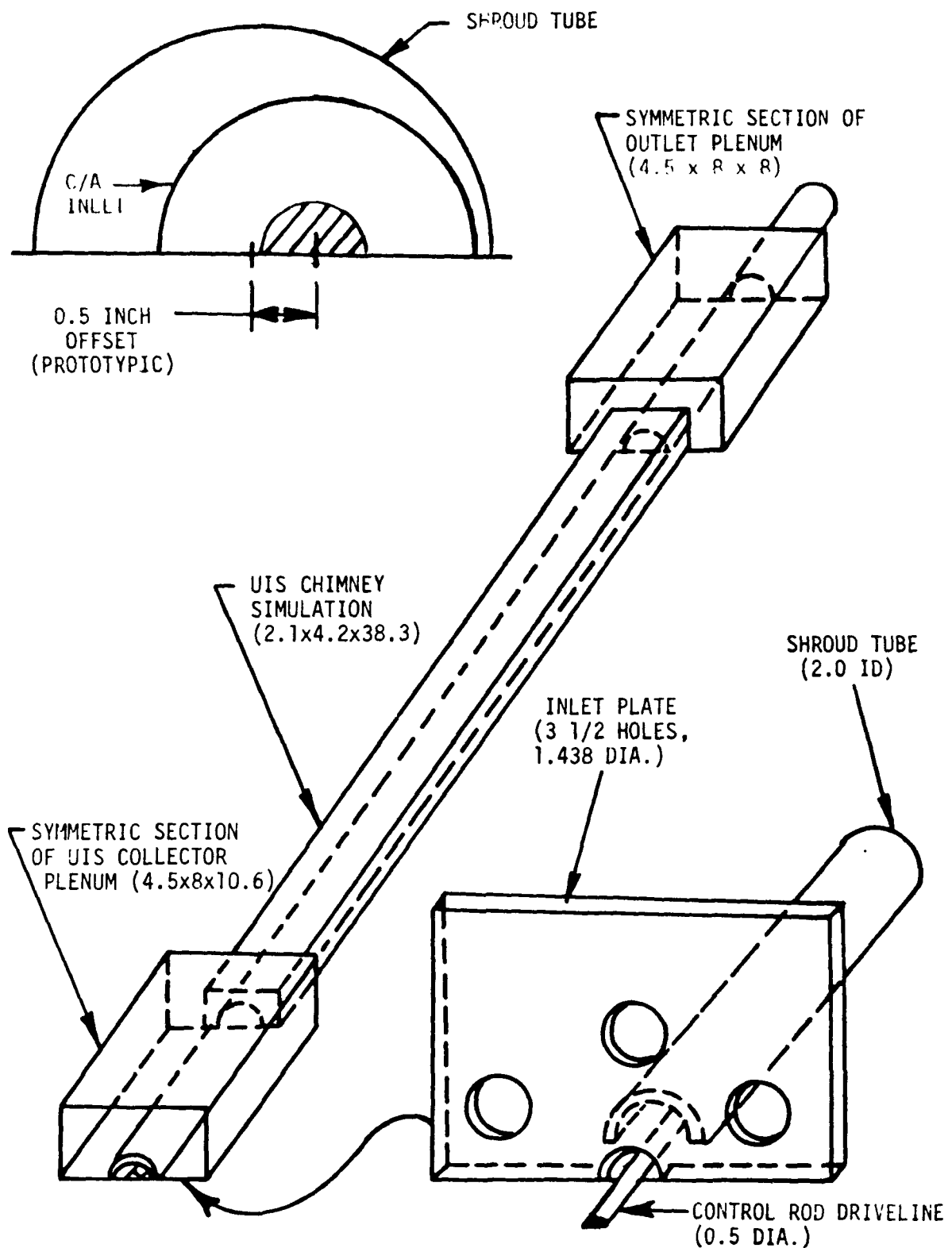
3.11.3.1.2 Lower Shroud Tube Water Table Test

A series of water table tests were conducted at AESD to determine the degree of mixing and thermal striping at the entrance to the lower shroud tubes. These tests were intended to gather design information quickly using readily available materials and AESD water table test facility. The preliminary tests simulated only the lower two feet of the lower shroud tube which extends into the UIS collector plenum. Later tests simulated the entire length of the lower shroud tube including its containment within a UIS chimney. Only the fourth test set is reported in detail here because the data from these tests are most applicable to the UIS lower shroud tube design.

The water table model of the lower shroud tube is shown in Figure 3-63. One half of a lower shroud tube was simulated using 2.0 inch ID plexiglass tubing. One half of a UIS chimney and symmetric sections of the UIS collector plenum and the reactor outlet plenum above the UIS were simulated by rectangular compartments constructed from plexiglass sheets. The control rod driveline was simulated with a 0.5 inch diameter half-round wooden rod. The

TABLE 3-52
UIS MIXING CHAMBER WATER TABLE STRIPING RESULTS

<u>Location</u>	<u>Maximum Peak-to-Peak Striping Amplitude (°F)</u>
Bottom of Instrument Post	104
Bottom of Peripheral Chimney	27
Bottom of Support Plate	27
Skirt	17



NOTE: Model dimensions are given in inches.

Figure 3-63
LOWER SHROUD TUBE WATER TABLE MODEL

model to prototype scale is 0.444 which was set by the ratio of the inner diameter of the available plexiglass tubing to the prototype shroud tube inner diameter (2.0/4.5). It was necessary to make the simulated symmetric sections of the UIS collector plenum and outlet plenum larger than a 0.444 scale to accommodate the positioning of the middle F/A inlet hole. This is the result of modeling the symmetric sections as rectangular compartments instead of hexagonal sections. The 0.444 scale factor was maintained in sizing the remaining principal geometric features such as the chimney flow area, core assembly exit nozzle diameters and component lengths.

The control assembly (C/A) and fuel assembly (F/A) inlets were offset from the centerline of the shroud tube by 0.5 inches prototypic to simulate eccentric positioning of the UIS relative to the core assemblies. This was judged to be the worst condition for radial temperature difference across the wall of the shroud tube because the potential for impingement of the C/A exit jet directly on the inner surface of the shroud tube is increased. The relative position of the shroud tube to the C/A outlet nozzle with 0.5 inch offset is illustrated in Figure 3-63.

A typical trace of striping results is shown in Figure 3-64. The temperature trace shown is from a test with 20,800 lb/hr shroud tube flow for the T/C located at 1.0 inch from the bottom of the shroud tube at an angle of 165 degrees. This is the location of the largest temperature fluctuation. The time span between the two large amplitude peaks is 7 seconds, but there are medium amplitude peaks occurring with a frequency of about 1 - 2 cps.

3.11.3.1.3 Local Interstitial Flow Thermal Striping

Mixing takes place where the hot radial outflow below the upper internals skirt and cold leakage flow between the removable radial shield assembly (RRSA) outlet nozzles and core former structure meet. At this juncture mixing occurs between the hot fluid leaking from the UIS mixing chamber and the cold interstitial flow up through the gaps between the RRSA's and between the RRSA's and core former ring. Depending on the relative flow rates and the flow patterns temperature oscillations may occur at the outlet nozzle and core

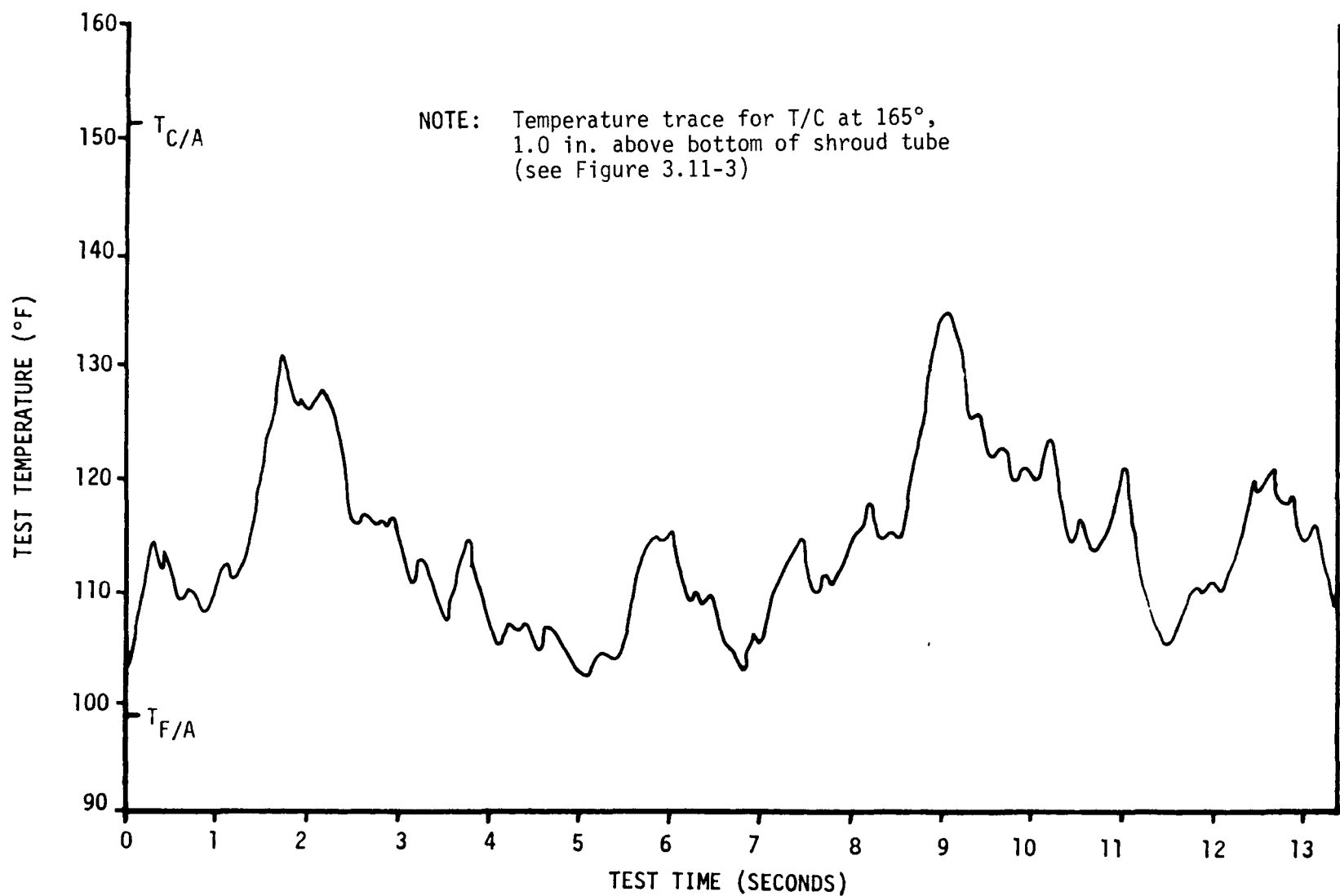


Figure 3-64

TYPICAL TEMPERATURE TRACE FROM LOWER SHROUD TUBE WATER TABLE TESTS

former surfaces. A full scale mockup of the RRSA outlet nozzles, core former structure and UIS skirt region was assembled on the AESD water table. Figure 3-65 is an elevation view and Figure 3-66 a plan view of the test arrangement. Figure 3-66 also shows the locations at which the temperatures were measured by a quick response thermocouple.

The tests were performed with the maximum available warm water flow rate of 55 gpm. The driving heads across the UIS mixing chamber were measured experimentally for each configuration tested, and were used to determine the RRSA/CFS flow rates required to simulate reactor conditions. The corresponding ΔT between warm and cold flow streams for Richardson number simulation was also determined. Tests were also performed at different cold flow rates and ΔT 's to determine the sensitivity to these parameters. The upper internals skirt was placed into locations simulating several differential expansion conditions at different times in life. Most tests were made with cold flow to the gaps next to the core former only. But a few check runs were made with injection into the second row of gaps also.

The temperature measurements were normalized in terms of the temperature difference between warm and cold flow. They were plotted for all runs for each elevation scanned. In summary, there appeared to be little penetration of warm fluid to the level of 3 1/2 inches from the top surface of RRSA nozzles and none below.

It became evident that the most severe striping occurred near the tapered surface of the core former. This region was therefore explored in detail. Maximum striping results from this test series are tabulated in Table 3-53.

3.11.3.1.4 Duct Interstitial Flow Thermal Striping

In the interstitial gaps between the peripheral core assemblies cold bypass flow meets hot downflow. The latter is forced into the gaps by the pressure drop of the radial outflow below the skirt of the upper internals mixing chamber. Studies revealed that at many times in core life relatively wide gaps (~0.30 in.) open between the nozzles of two rows of radial blanket assemblies permitting deep penetration of hot fluid flow from the mixing

Figure 3-65 TEST SET-UP ON WATER TABLE
OF LOCAL INTERSTITIAL FLOW TEST RIG

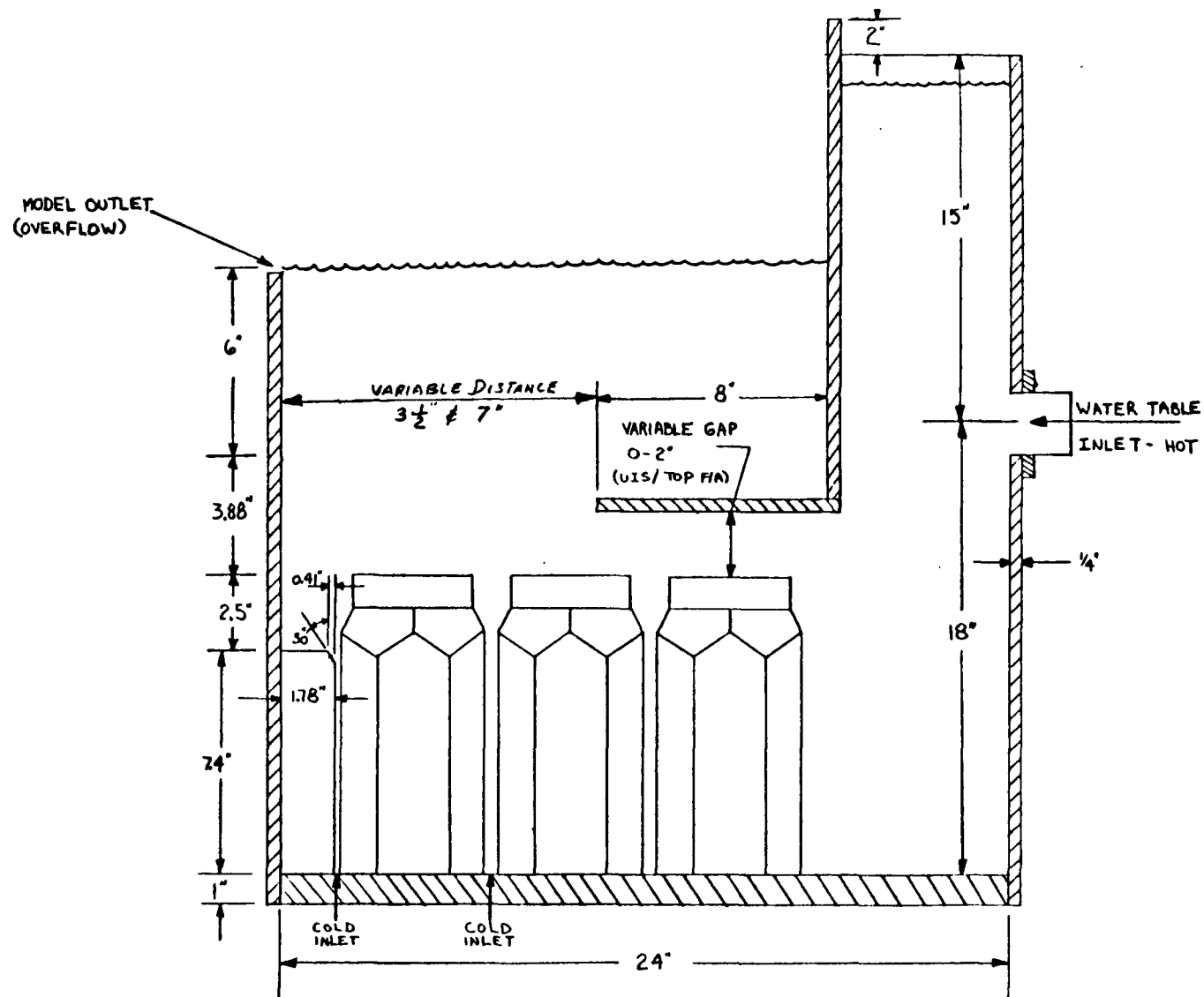


Figure 3-66
PLAN VIEW OF INTERSTITIAL FLOW TEST RIG

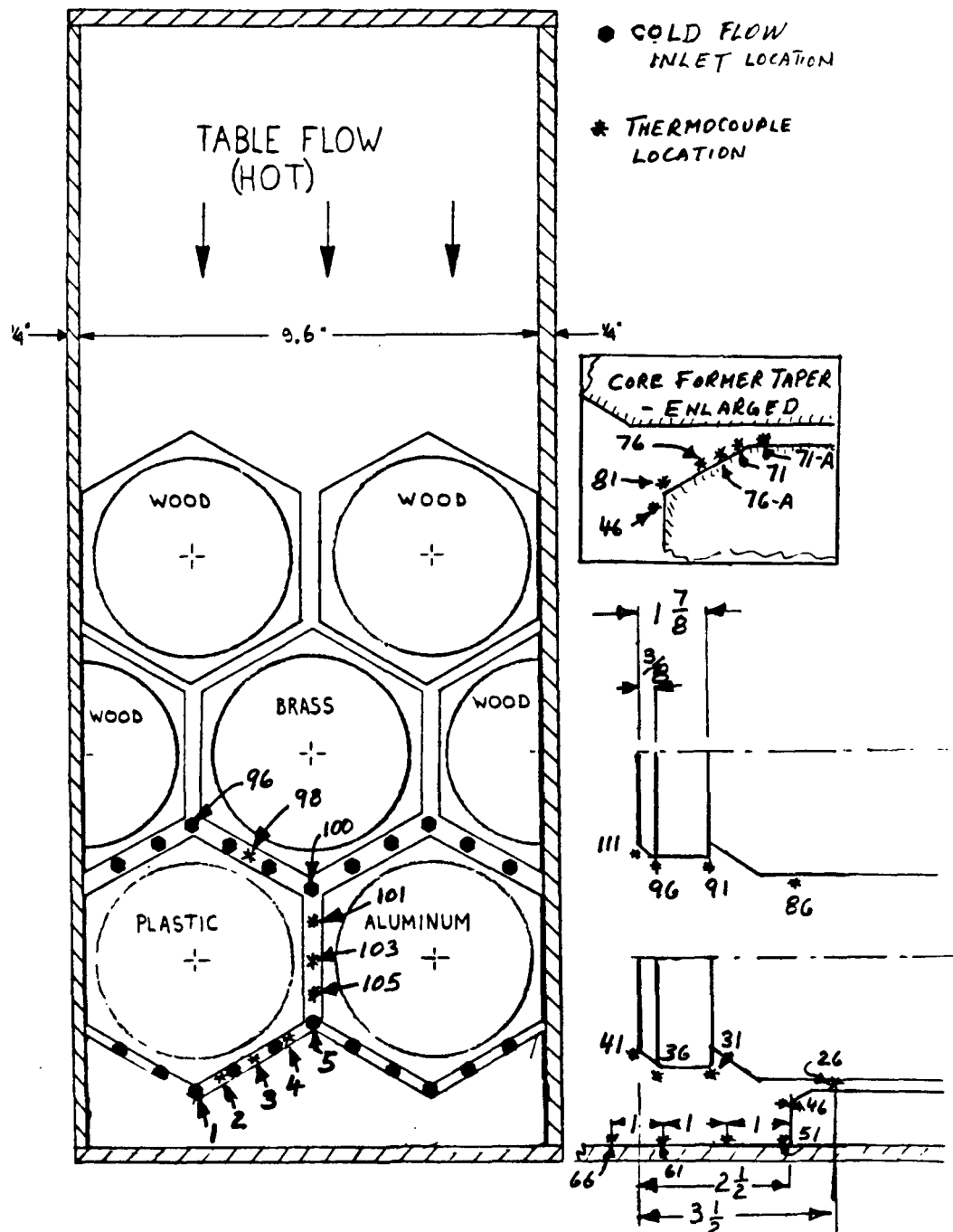


TABLE 3-53
MAXIMUM STRIPING FROM LOCAL INTERSTITIAL FLOW TEST

Run No.	UCFS Insert		UCFS Vertical		RRS Nozzle	
	%	<u>Location</u>	%	<u>Location</u>	%	<u>Location</u>
1	84	78	56	52	65	33A
2	71	83	37	56	72	33A
3	72	78	24	53	49	40
4	67	77A	53	51	71	35,29A
5	75	78A	20	53	54	40
6	69	78	31	55	74	32
7	63	78A	57	55	51	31
8	64	77	72	55	57	38
9	71	83	37	55	41	40
10	64	78A	60	55	58	33A
11	62	75	4	51	50	35
12	38	80A	17	56	56	35
13	63	75A	8	55	29	35
14	64	81	9	51	37	27A

chamber into the interstices between the assemblies. A flow pattern is established in which hot and cold flow meet at various angles and subsequently flow up in parallel and out at the periphery. The interface between hot and cold flows is a region of high mixing and striping potential.

In order to simulate mixing conditions and to measure the magnitude of striping outside of the ducts a plexiglass model was constructed in which two full size, adjacent ducts were surrounded by walls of a housing, simulating other ducts. The model was equipped with several types of inlet connections so that flow directions at different regions of the mixing zone could be simulated on the WAESD water table.

The plexiglass model was designed to facilitate several patterns of flow as shown in Figure 3-67. Cross flow could be supplied by two inlets at the top third of the model and two separate inlets feeding the bottom two thirds. Upflow could be supplied from a bottom manifold through holes in line with the gaps between the walls. Both pairs of feeds are separated by a vertical baffle containing an air gap for insulation. The regions of approach to the hexagonal duct were separated by another horizontal baffle also containing an air gap to avoid premature heat transfer. The gaps throughout the model; i.e., between ducts and walls and filler blocks were 0.30 inches wide. The outside dimension of the hexagonal ducts was 4 3/4 inches across flats. The inlet region was covered at a height of 18 inches. This cover ended at the gap separating the ducts. Downstream the flow could turn upwards to flow over the weir. At high velocities ($Re > 4000$) the cross flow remained parallel to the table along most of the length of one wall of the duct. At lower flows it tended to turn up earlier.

The results of striping tests performed with the 0.020 inch diameter thermocouple with parallel flow upflow and mixed flow operation are summarized in Table 3-54. Striping as high as 98% was observed in midstream. However, it did not exceed 78% and was mostly lower than 75% at the wall. The upflow results may still be slightly high because of slow shifts in the location of the interface with respect to the sensing probe. Peak striping did not occur right at the center plane of the gaps but in the region of the maximum fluid shear and gradients in turbulence.

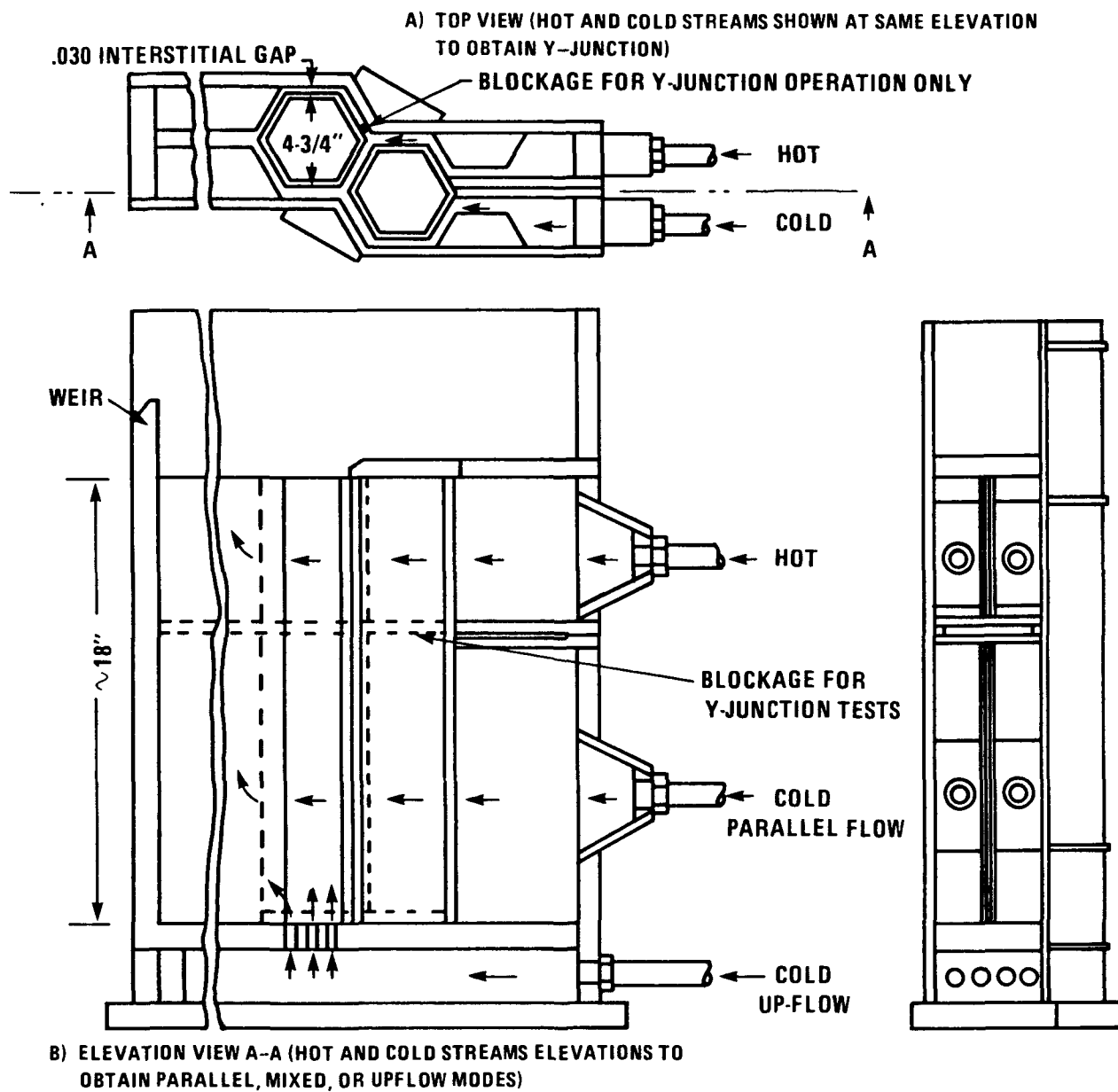


Figure 3-67 Plexiglass Model for Interstitial Flow Testing

TABLE 3-54
SUMMARY OF INTERSTITIAL FLOW STRIPING TESTS

<u>Hot Flow</u>		<u>Cold Flow</u>			<u>Max. Striping - Percent of Potential</u>			<u>Elevation Inches</u>
<u>Approx. gpm</u>	<u>Rein</u>	<u>Approx. gpm</u>	<u>Rein</u>	<u>Type of Inlet</u>	<u>Wall</u>	<u>Centerline</u>	<u>Peak</u>	<u>Wall</u>
5	4750	10	4750	Parallel	50	79	82	+2
6	5700	6	2850	Parallel	59	80	81	+1
9	8550	6	2850	Parallel	66	78	79	+1
10	9500	2	950	Parallel	66	81	81	-6
10	9500	5	800	Up	56	71	72	-1/2
5	4750	2	320	Up	55	81	82	+1
6	5700	2	320	Up	72	88	90	+1/2
8	7600	2	320	Up	73	96	98	-3 1/2
10	9500	2	320	Up	78	94	98	-5 1/2
6	5700	6	1900	Mixed	66	90	90	+1 1/2
9	8550	6	1900	Mixed	66	90	90	+1 1/2
10	9500	5	1590	Mixed	73	92	95	-1/2
10	9500	4	1270	Mixed	67	90	93	-4
10	9500	2	635	Mixed	75	98	98	-5 1/2

3.11.3.1.5 Y-Junction Interstitial Flow Thermal Striping

The pressure differential between the upper mixing chamber and the upper plenum of CRBRP forces some hot fluid from the core outlet flow down into and radially outward through the gaps between the peripheral assemblies. Here it meets cold bypass flow rising in the interstices between the blanket and reflector assembly ducts. Since much of this flow proceeds in the radially outward direction, mixing of the two streams can occur when flow from one gap joins the flow from another in a single downstream gap in a geometry here called a "Y-junction". The extent of striping at the duct walls produced by mixing of the two streams needs to be known to assure that it does not exceed an allowable value. To examine striping at this and related regions between the assemblies, a plexiglass model was constructed and testing on the W-AESD water table.

The model, a full size simulation of the gaps surrounding two hexagonal ducts, was placed on the W-AESD water table. Figure 3-68 shows two water inlets which feed hot or cold fluid to opposite side of the first duct. Approaching the second duct flow splitting and flow joining junctions are formed. The regions downstream of these junctions can be explored for temperature striping. In the tests the flow splitting branch was blocked and only the joining of the hot and cold flows was examined. The flow proceeded horizontally until it reached the junction, guided by a top plate. Downstream of the junction the flow gradually deflected upwards to flow up and out over the weir. At higher flow rates the inertia of the flow caused parallel flow over most the length of the first gap (along the wall before deflecting 30°). At very low rates the flow turned up earlier. The ducts' dimension was 4 3/4 inches across flats. The height of the duct was six inches. The width of all the gaps between ducts and walls was 0.30 inches. 40 mil diameter holes were drilled into the sides of the duct in a pattern covering the region of interest. Through these holes a 20 mil diameter, grounded junction thermocouple was inserted to various depths into the gap between duct and outer wall.

Tests were made with hot to cold flow ratios ranging from equal (1 to 1) to a ratio of 7:1. Table 3-55 gives a summary of the tests performed, as well as

Figure 3-63

Y-JUNCTION MODEL TEST SETUP

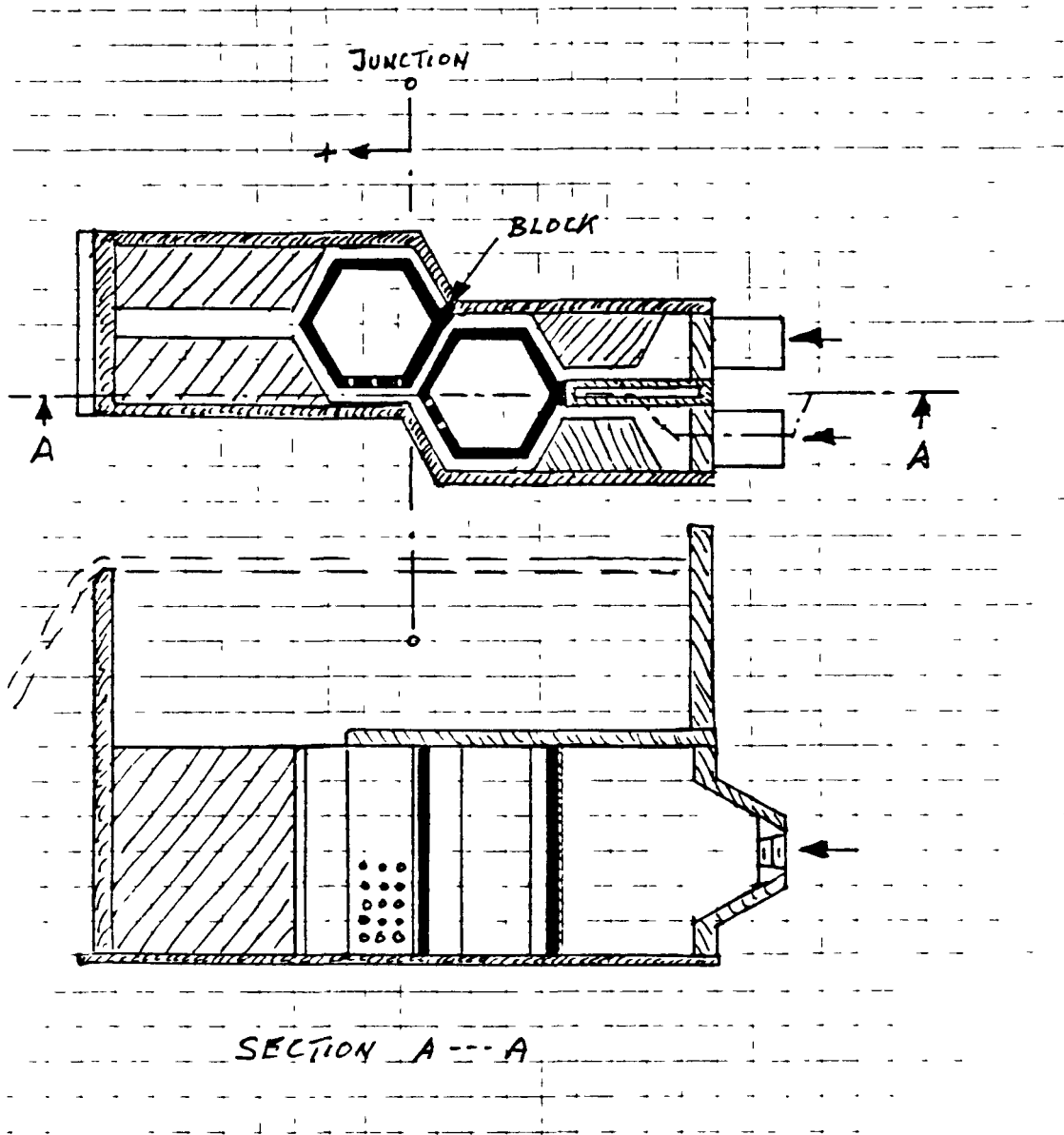


TABLE 3-55
SUMMARY OF TEST RESULTS - Y-JUNCTION STRIPING TESTS

<u>Hot Flow gpm</u>	<u>Cold Flow gpm</u>	<u>Striping - Percent of Potential</u>		
		<u>Wall</u>	<u>Centerline</u>	<u>Peak</u>
0.75	0.75	40	70	--
1.0	1.0	48	92	92
2.0	1.0	68	80	88
3.3	3.3	54	78	89
4.5	4.5	73	88	94
5.5	5.5	55	94	97
1.5	1.0	16	40	58
3.0	2.0	73	82	97
4.5	3.0	52	70	92
5.4	3.6	68	80	95
2.0	1.0	40	80	86
4.0	2.0	71	83	93
6.5	3.25	50	80	92
2.5	1.0	60	81	92
4.0	1.6	60	70	85
5.0	2.0	70	85	94
3.0	1.0	68	80	82
6.0	2.0	50	78	88
4.5	1.0	50	70	83
9.0	2.0	35	57	75
6.0	1.0	50	72	88
9.0	1.5	40	66	80
7.0	1.0	50	54	87

the peak striping values measured. While striping is relatively low at the wall, it is highest close to the wall and somewhat lower at the midplane. Only at very low (laminar) flows does the peak value appear to be located in the midplane.

It was found that the striping decays with distance downstream of the junction. With high hot to cold flow ratios striping occurs also upstream of the junction where hot flow penetrates into and mixes with the cold flow in the low flow gap.

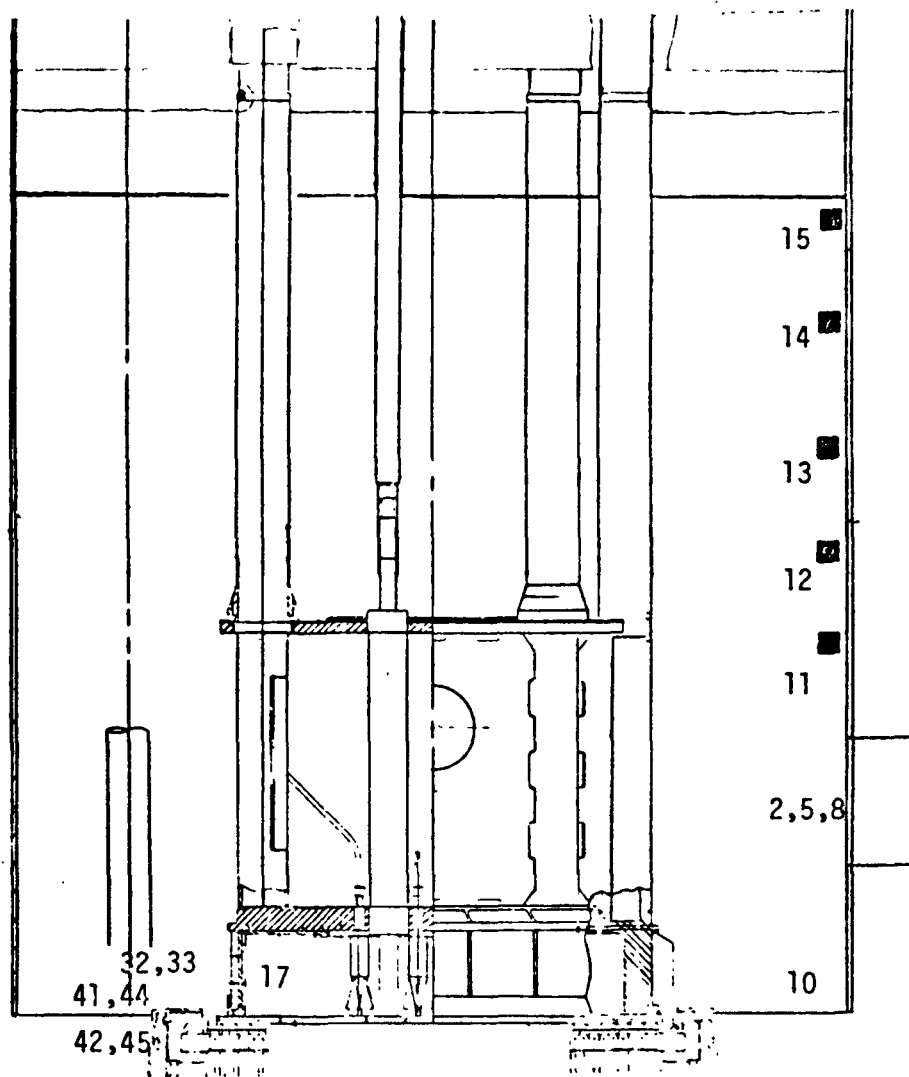
Detailed exploration of peak striping downstream of a Y-junction over a wide range of flow rates (laminar to turbulent) and ratios of hot to cold flows showed peak striping in the gap to approach 98% of the potential. However, at the wall the highest striping observed did not exceed 73% of the potential. The distribution of striping magnitude reflects the distribution of turbulent shear which is low at the wall but highest just outside the boundary layer not far from the wall.

3.11.3.2 ANL Striping Tests

While the water table tests provided preliminary scoping striping data, an ANL 1/10 scale outlet plenum model of CRBR, with the homogeneous core design, provided the initial detailed striping data in the UIS/outlet plenum region.

An experimental investigation of CRBRP outlet plenum temperature oscillations during steady state conditions was conducted. Tests were performed using water with a 1/10 scale model of the CRBRP upper plenum complete with Upper Internal Structure (UIS). The radial blanket/fuel assembly temperature difference was adjusted such that Froude number (ratio of buoyant to inertial forces) similitude of model and prototype was obtained. Temperature-time plots were obtained at 47 critical locations within the plenum. Power spectral density (PSD) analysis at particular locations was implemented to examine the frequency range of the temperature oscillations. Location of the 1/Cs where striping was measured is shown in Figures 3-69, 3-70 and 3-71.

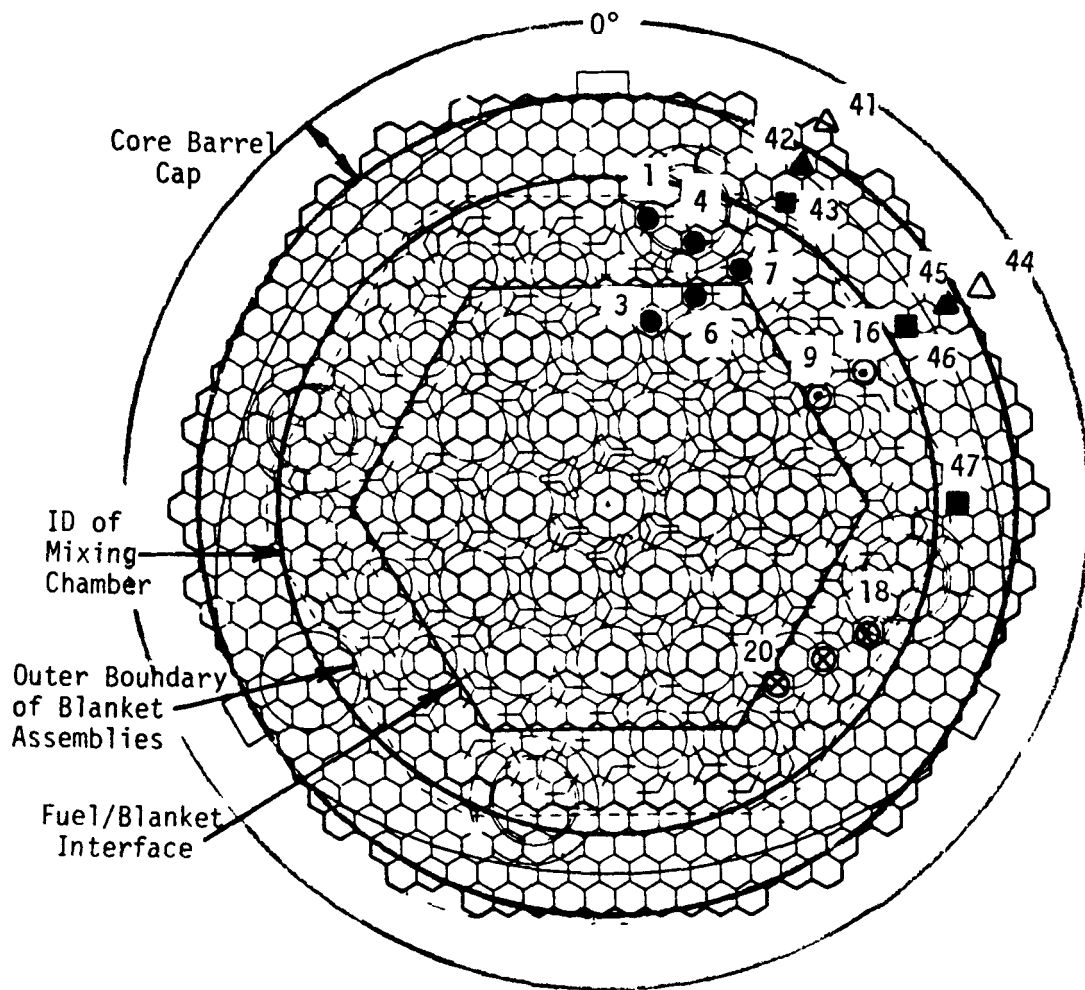
BEST AVAILABLE COPY



■ Vessel Liner

Figure 3-69

ANL 1/10 SCALE OUTLET PLENUM THERMOCOUPLE MAP



- Leakage Gap
- ▲ Inside Vertical Surface of Core Barrel Cap
- △ Center of Core Barrel Cap
- Bottom Center of Instrument Posts
- Top of Mixing Chamber
- ⊗ Instrument Post Top

Figure 3-70

ANL 1/10 SCALE OUTLET PLENUM THERMOCOUPLE MAP

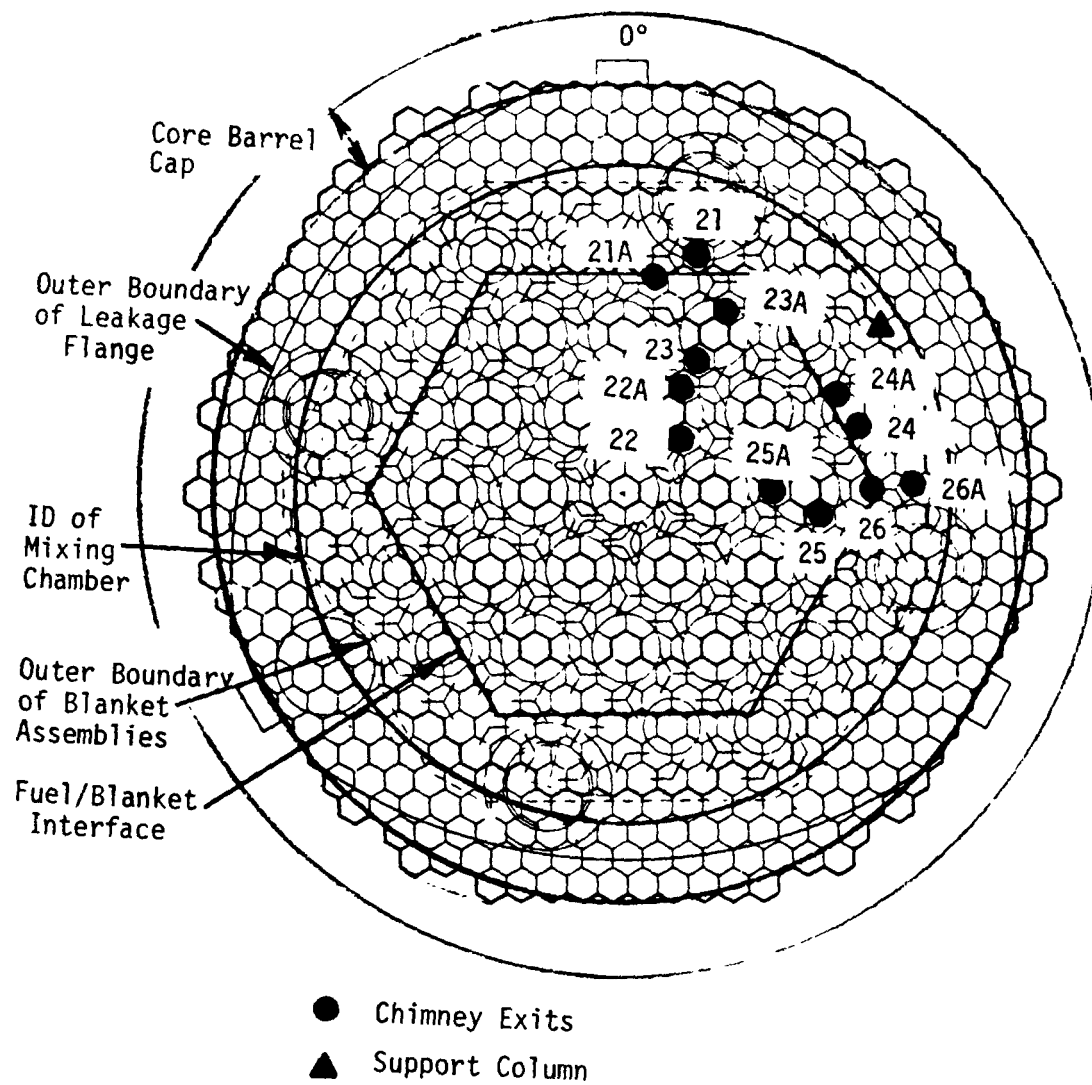


Figure 3-71 ANL 1/10 Scale Outlet Plenum Thermocouple Map

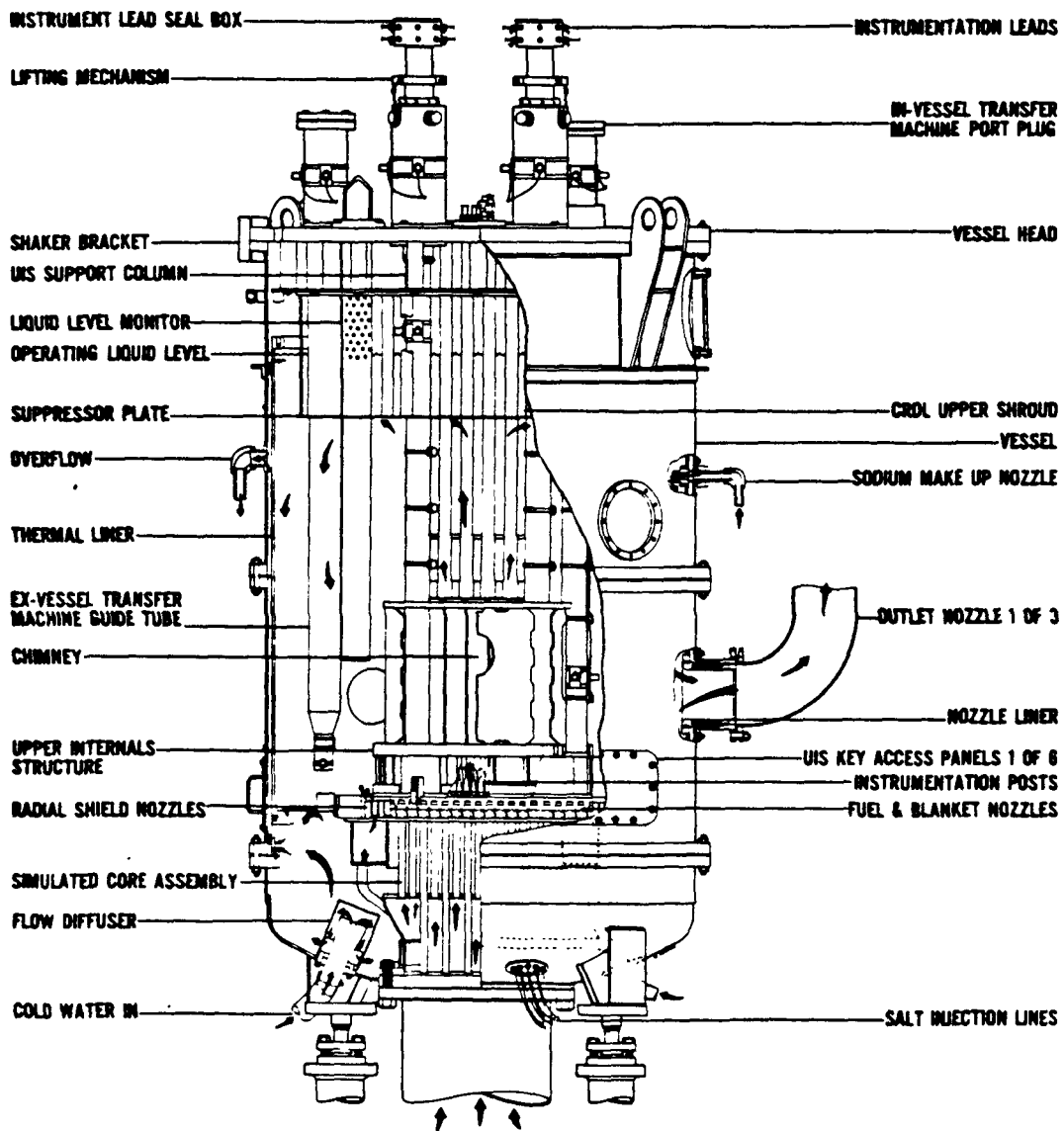
It was found that outlet plenum temperature oscillations in excess of 30% (peak to peak) of the fuel/blanket ΔT occur only within the clearance gap (between lower part of the UIS and top of the core) and within the UIS mixing chamber in the region above the blanket assemblies. Power spectral density analysis revealed that the oscillations were in the frequency range of 0-3 Hz (0-1.2 Hz prototype). Outside the UIS, most components were nearly at the mixed mean plenum temperature and oscillations were generally less than 10% of the fuel/blanket ΔT . Results of these tests are reported in Reference 2.

3.11.3.3 IRFM Striping Tests

The Integral Reactor Flow Model (IRFM) is a 360-degree, 0.248-scale model of the CRBR outlet plenum and its internals which models significant hydraulic and vibrational characteristics of the CRBR outlet plenum. The region of hydraulic modeling extends from just below the top of the core to the liquid surface at the top of the plenum. A cutaway view of the IRFM test model is shown in Figure 3-72. In addition to thermal striping, the objectives of IRFM water hydraulic tests were to provide experimental data from which the following characteristics of the CRBR outlet plenum region can be reliably predicted.

- o Outlet plenum velocity profiles.
- o Pressure drops.
- o Fluid mixing (transient and steady state).
- o Entrainment of cover gas at the pool free surface.
- o Flow-induced vibrations.

The IRFM Program was an extensive test program. The magnitude of the testing program and the specific striping tests that were defined can be judged from the titles of the different parts and addenda of the DRS which are listed below:



CRBRP Integral Reactor Flow Model - Phase II.

Figure 3-72

DRS 31.14.07

Title

Part III	CRBRP: IRFM Reactor Vessel Thermal Liner, Outlet Nozzle Liner and Makeup Nozzle Bridge Liner Thermal/Hydraulic and Flow Induced Vibration Test
Part III, Rev. 1	CRBRP: IRFM Phase IIC, Alternate Outlet Nozzle Thermal Striping Test
Part V	CRBRP: IRFM Phase II, Chimney Streaming and Instrumentation Post Striping
Add. IV	CRBRP: IRFM Phase IID, Horizontal Baffle Thermal Striping Test
Add. V, Rev. 1	CRBRP: IRFM Phase II, UIS Bypass Striping Test
Add VI, Rev. 2	CRBRP: IRFM Phase IIF, Permanent Reactor Structure Thermal Striping Tests

The test performer issued three reports [3-5] documenting the results of some of the tests. Results from the other tests were provided informally to AESD and were used in the CRBR design evaluations.

Some of the more significant striping results from the reports follows:

- o Thermal striping at the discharge of the RV thermal liner was as high as 69% of the difference between the plenum temperature and the vessel wall coolant temperature.
- o Thermal striping as much as 72% of the potential was measured at the makeup nozzle. The striping potential is the temperature difference between the hot and cold fluid.
- o Thermal striping measurements at the outlet nozzle liner were as high as 71% of potential. With the revised outlet nozzle liner the striping was greater than 80%.

- o Thermal striping on the UIS instrumentation posts was measured as high as 43% of the potential fluid temperature difference.

3.11.3.4 Seven Assembly Tests

Thermal striping of interior regions of the Upper Internals Structure (UIS) and on core assembly outlet nozzles of the Clinch River Breeder Reactor was investigated experimentally in a full scale prototypic simulation of a symmetric section of the UIS mixing chamber encompassing the exit flow from seven core assemblies. Hot and cold water was injected into the core assemblies to simulate different locations within the core. The fluid temperature fluctuations near the surface of UIS components and nozzles were measured with fast response thermocouples and the peak-to-peak striping ΔT s were determined as a fraction of the potential temperature difference between the hot and cold inlets. Two basic UIS configurations were tested: one featuring an instrumentation post and another with a lower shroud tube. A schematic of the models for these two configurations is shown on Figures 3-73 and 3-74. Both Primary and Secondary Control Rod Driveline simulations were tested within the shroud tube configuration.

There were three phases of tests conducted in the full scale seven assembly facility. The results of these tests were used in the final design evaluations of the core and UIS components that are in this region where the greatest striping occurs.

The first phase of testing was directed toward the UIS and the results are documented in Reference 6. The second phase was a core assembly outlet nozzle feature test. The last phase focused on detailed striping measurements on the control rod drivelines and the final design of the core assembly outlet nozzles. This last phase of testing was completed just before CRBRP was terminated. Much striping data from these tests was used in the design evaluation of the different components but some of the data was not reduced before termination. Test results used for design evaluation are documented in internal W-AESD memos but most of the test results are not documented.

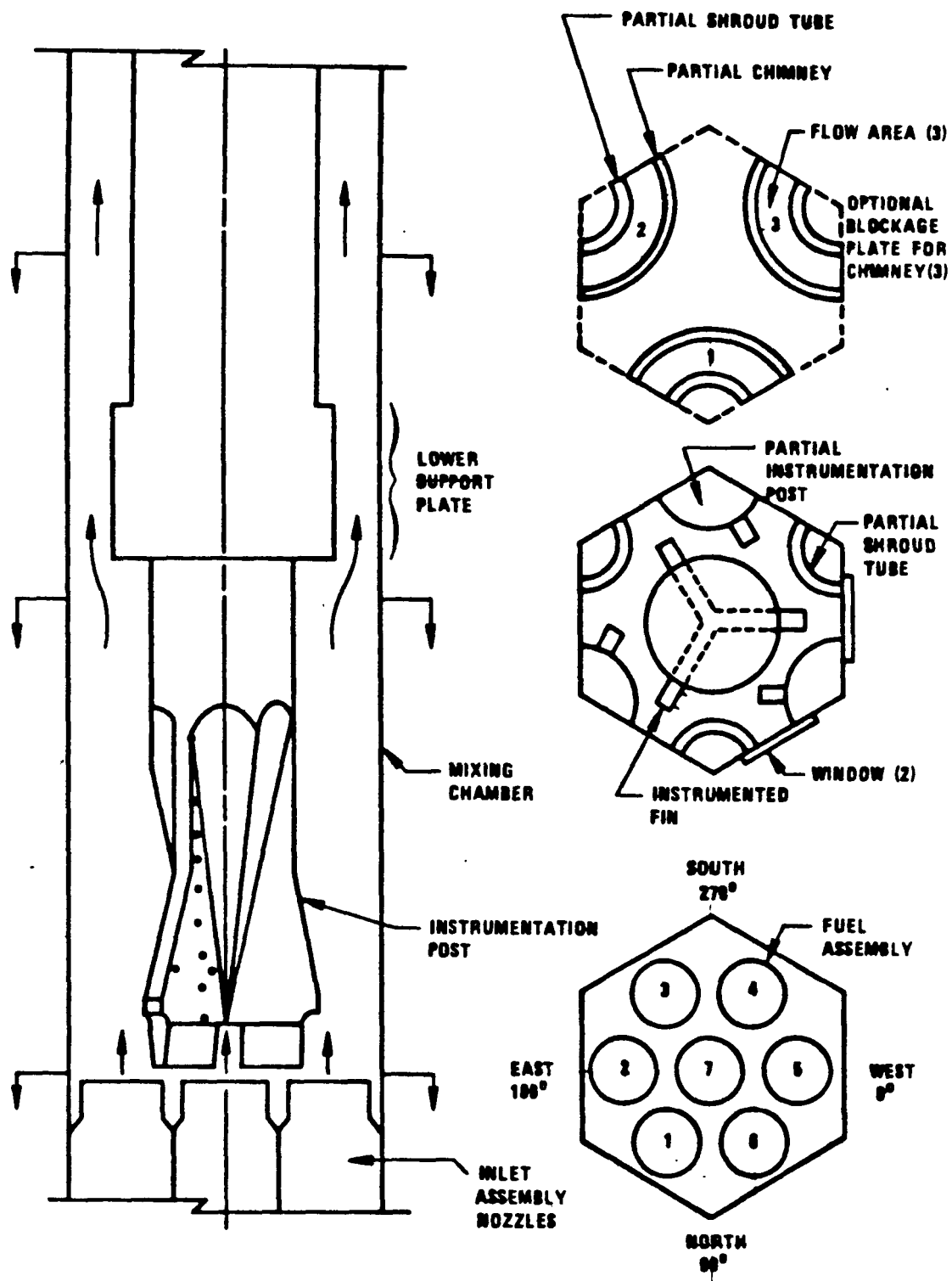


Figure 3-73 Instrumentation Post and Chimney Model

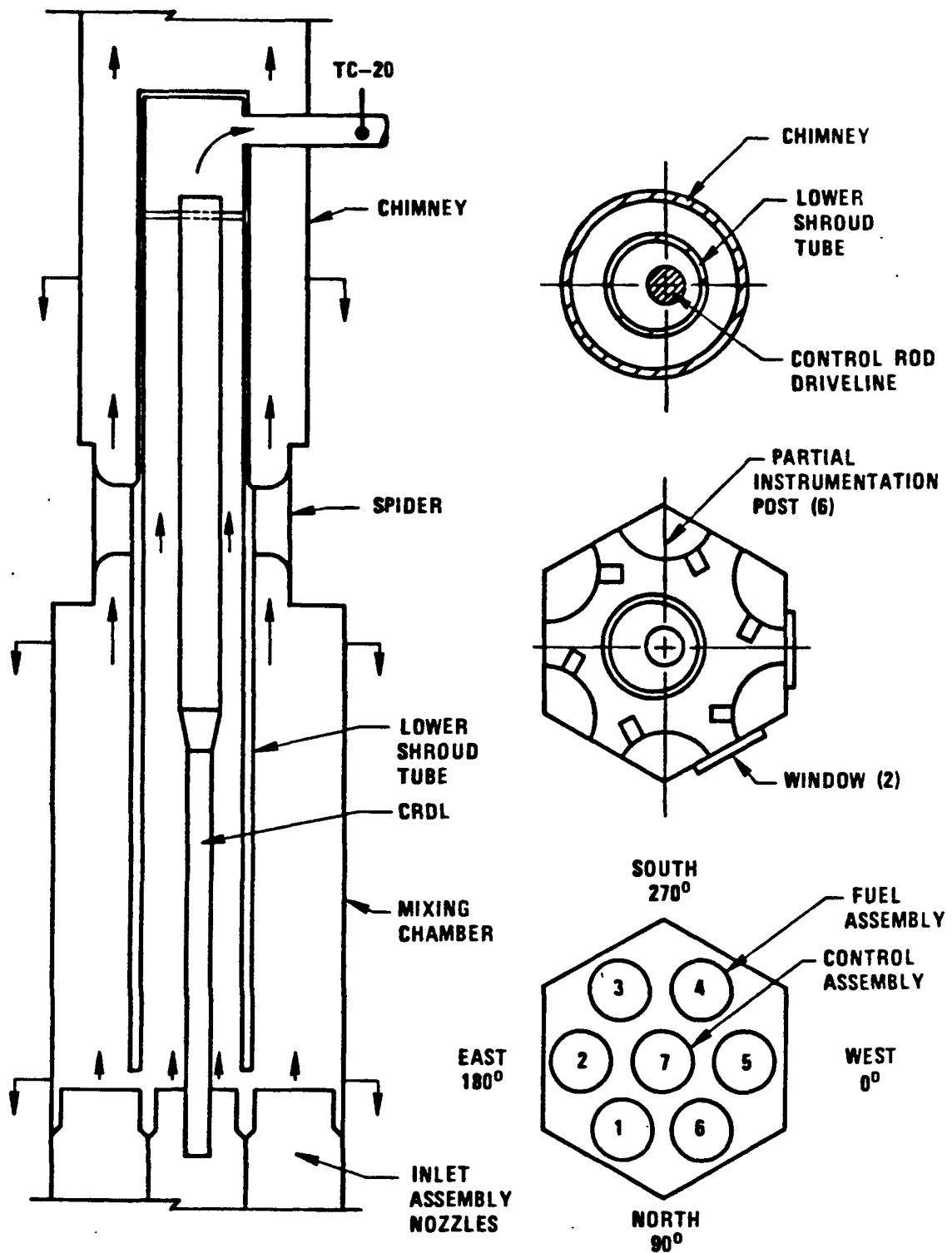


Figure 3-74 Lower Shroud Tube Model

An interim summary of the observed core component thermal striping data is tabulated on Table 3-56. These data are primarily from the seven assembly and IRFM tests but also include water table results. The table is for illustration purposes and does not include the latest phase of the seven assembly test series or the results of the final CRBR core orificing potential ΔT s.

3.11.4 Heat Transfer Film Coefficients

The structure thermal response to the coolant thermal striping boundary conditions is highly dependent on the heat transfer film coefficient. Because of the complexities of structure geometry and the coolant flow fields there is large uncertainty in calculated coefficients. For the striping analysis, the heat transfer coefficients were conservatively calculated from known steady state liquid metal correlations for common geometries (i.e., flat plate, tube, etc.). The heat transfer coefficients cannot be completely defined by these correlations. Therefore, the closest applicable common geometry heat transfer correlation was chosen. To compensate for the possibility of choosing a heat transfer correlation that gives a heat transfer coefficient less than its actual value, a conservative safety factor was imposed on the calculated heat transfer coefficient. A higher heat transfer coefficient increases a component surface striping temperature amplitude which results in a conservative fatigue analysis.

Based on limited test data and some detailed thermal analysis to calculate transient thermal striping film coefficients, values used in the striping analysis are conservative. Because of the possibility of mitigating thermal striping effects with reduced film coefficients, this is a promising area for additional investigation.

3.11.5 Structural Mitigation

The striping temperature distribution in a structure is characterized by a fluctuating non linear temperature profile with the steepest gradient at the surface. This is illustrated in Figure 3-75 with the normalized surface temperature, T_{S1} , fluctuating between +1.0 and -1.0. For this illustration

TABLE 3-56
SUMMARY OF OBSERVED CORE COMPONENTS THERMAL STRIPING DATA - PEOV

				<u>Updated Core Orificing Results</u>			
<u>Core Assembly</u>	<u>Location</u>	<u>Test Source</u>		<u>%_{max}</u>	Potential <u>$\Delta T_{OF}(2\sigma)$</u> <u>SOC1(SOC3)</u>	<u>ΔT_{max}</u>	
Fuel	External Nozzle	IRFM	Next to IBA	36*	279(264)	100(95)	
			Next to RBA	25*	278(212)	70(53)	
		7-A Test	Next to PCA	22	303††	67	
			Next to SCA	74*	~303††	124	
			Next to RBA	82*	273(261)†	124(214)	
			w/o Shroud Above	Next to PCA	65*	303††	137
Next to SCA	--	--		--			
			Next to RBA	86	278(212)	239(182)	
Inner Blanket	External Nozzle	IRFM	Next to FA	36*	279(264)	100(95)	
		7-A Test	Next to FA	86	279(264)	240(227)	
	Internal Nozzle	IRFM	Next to RRS	88*	338(306)	297(269)	
Radial Blanket	External Nozzle	IRFM	Next to RRS	75	292	219	
		7-A Test	No Shroud	Next to FA	86	278(212)	239(182)
			Shroud Above	Next to FA	76*	262(244)†	199(185)
	Internal Nozzle	IRFM	Row 1	55	253(227)	139(125)	
			Row 4	36	316(314)	114(113)	
		Local Interstitial+IRFM	Row 1	85(85)	253(227)	214(193)	
Row 4			70(68)	316(314)	220(214)		
Removable Radial Shield	External Nozzle	IRFM	Row 1	75	292	219	
		Local Interstitial+IRFM	Row 4	65(68)	319(316)	207(215)	

†SOC5
††EOC2

*Very limited data (only one to two data points and/or non-prototypic nozzle geometries).

STRUCTURE INTERNAL STRIPING TEMPERATURE AMPLITUDES

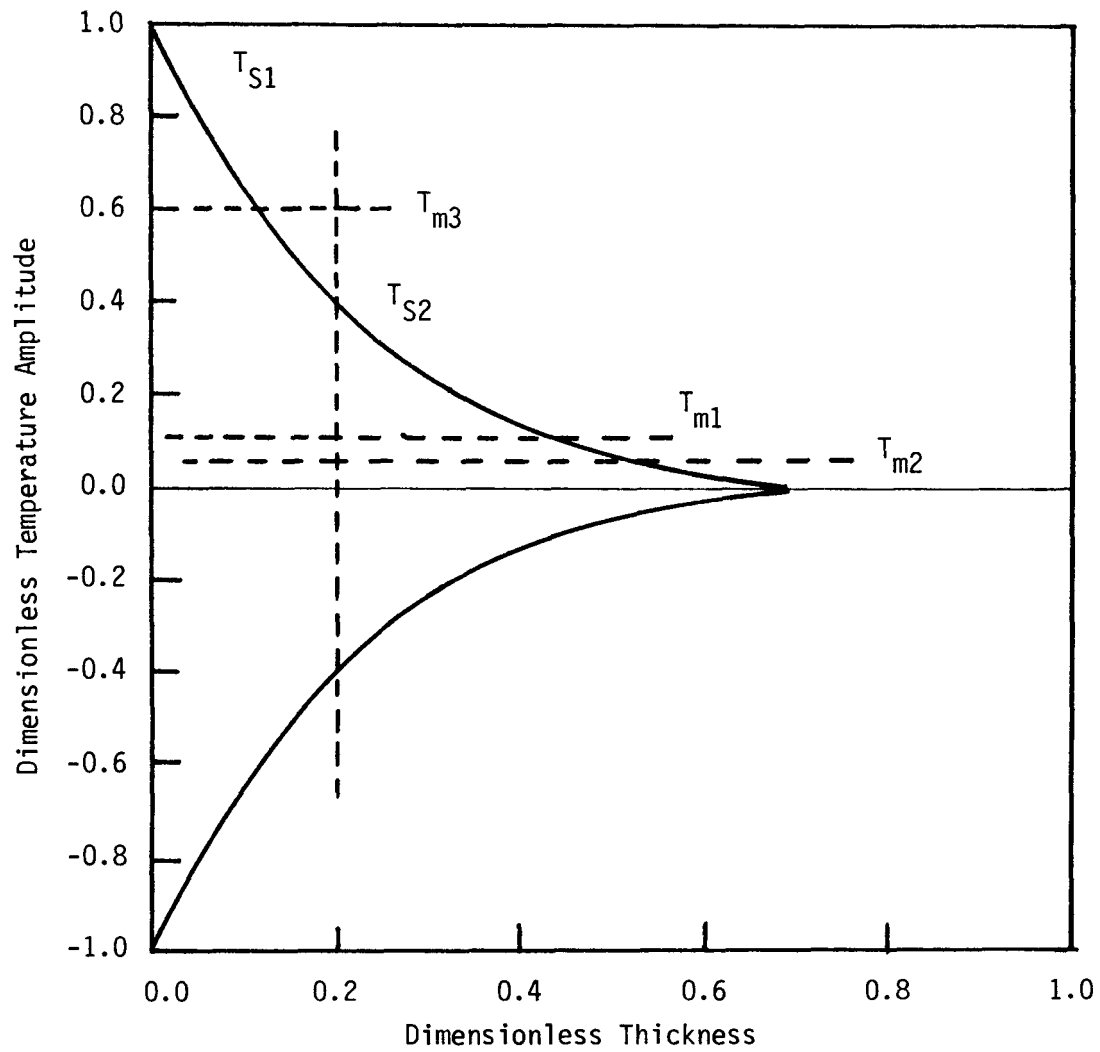


Figure 3-75

the mean temperature, T_{M1} fluctuate between +0.1 and -0.1. The thermal striping strain range is determined by the range of surface to mean temperature differences. This temperature difference, $T_{S1} - T_{M1}$, fluctuates between +0.9 and -0.9.

Two ways to structurally reduce this surface to mean temperature difference, which reduces the strain range, is to reduce the thickness of the structure or add a thermal shield between the fluid and the structure. If a thermal shield of thickness .2 is added, the steep gradient occurs in the shield and the surface temperature of the structure, T_{S2} fluctuates between +0.4 and -0.4. With the mean temperature, T_{M2} , fluctuating between +.05 and -.05, the surface to mean difference, $T_{S2} - T_{M2}$, fluctuation have been reduced to +0.35 and -0.35. This is less than one half the striping seen by the unshielded structure.

Striping effects can also be reduced by reducing the thickness of the structure. With the structure thickness in the illustration reduced to 0.2, the surface temperature, T_{S1} still fluctuates between +1.0 and -1.0 but the mean temperature of the thinner structure, T_{M3} , fluctuates between +0.6 and -0.6. The surface to mean temperature difference, $T_{S1} - T_{M3}$, fluctuations have been reduced to +0.4 to -0.4. Again this is less than one half the original values.

3.11.6 Conclusions and Recommendations

Although thermal striping was identified as a concern early in the design phase of the CRBR, the total magnitude of the phenomenon was not realized until well into the extensive experimental program on thermal striping. Along with a greater understanding of the phenomenon of thermal striping, an analysis methodology was developed during the design of CRBR. This methodology is presented in detail in Section 3.12 on Thermal Striping Design Limits. There are several steps in the evaluation of thermal striping and the thermal striping mitigation process can be approached through any and all of these steps.

- A. The first and most direct method of mitigating thermal striping is to mitigate the potential of the source for striping. A constraint on in-vessel flow management could be to limit temperature differences to allowable levels.
- B. Improved mixing or mixing in plena away from structures can mitigate the magnitude of the coolant temperature fluctuations adjacent to the structures. Tests also help to better define the coolant temperature fluctuations which must be used in design so that overly conservative values such as source potentials need not be used.
- C. Reduced heat transfer film coefficients will mitigate the structural thermal response to fluctuating coolant temperatures. A better understanding of transient heat transfer coefficients could mitigate the conservative values that have been used in the striping evaluations.
- D. The effects of thermal striping can be mitigated by shielding the structure with a thermal liner or by reducing the thickness of the structure.
- E. Thermal striping effects can also be mitigated by the materials selection. Alloy 718 was used extensively in the CRBR internals design as structural and shield components because it has a higher striping limit than stainless steel 316.

3.11.7 References

1. M. D. Carelli, et al., "Final Predicted Steady State Thermal Hydraulic Performance of the CRBR Fuel and Blanket Assemblies," WARD-D-0324, March 1985.
2. J. J. Lorenz and P. A. Howard, "A Study of CRBR Outlet Plenum Thermal Oscillations During Steady State Conditions," ANL-CT-76-36, July 1976 Argonne National Laboratory.
3. D. R. Dickinson & F. R. Fisher, "Measurements of Circumferential Temperature Distributions Inside the Chimneys and of Thermal Striping on an Instrumentation Post in the IRFM of CRBRP," IC-1322, HEDL, July 1980.

4. D. R. Dickinson, et al., "Thermal Striping of the Revised Design of CRBRP Outlet Nozzle Liner as Measured in the IRFM," TC-1323, HEDL, April 1980.
5. D. R. Dickinson & F. R. Fisher, "Tests of Fluid Mixing and Thermal Striping in the IRFM to Determine Behavior of CRBRP Thermal Liner, Outlet Nozzle, Makeup Nozzle and Direct Heat Removal Service," HEDL-TME-80-66, November 1982.
6. M. D. Woods, "Evaluation of UIS Full Scale Striping Tests of the Instrumentation Posts and Lower Shroud Tubes," CRBRP-ARD-0309, January 1982.

3.12 Thermal Striping Design Limits

3.12.1 Introduction

This document is intended to outline the methods and reference the data employed in the analysis of core assembly outlet nozzles and their ability to withstand striping without crack initiation. The failure mode of outlet nozzle fatigue cracking was postulated after a series of flow striping tests in the Integral Reactor Flow Model (IRFM) indicated large temperature oscillations not only in the mixing chamber above the core of the CRBRP but also at the elevation of the outlet nozzles, particularly the removable radial shields.

A study was initiated to resolve this complex problem. The core assembly coolant outlet temperature differences exceeded 300°F at the start of the reactor operation. The allowable temperature differences using conservative ASME fatigue design data and assuming that metal temperature changes equal sodium temperature changes resulted in an allowable striping temperature difference of less than 100°F.

Subsequent studies recognized that the problem can be resolved. This is because:

- o The maximum reactor coolant temperature differences decrease during the reactor operation. In fact, the core assembly outlet nozzle striping is only a first core load problem. In later cores, the blankets share in the power generation and the removable radial shields heat up, and thus decrease the reactor coolant temperature differences.
- o As a result, the number of high temperature striping cycles is finite and not very large, as previously assumed.
- o The fluid heat transfer film coefficient is not as large ($>50000 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$) as previously assumed based on some misleading measurements in water which indicated high transient heat transfer coefficients.

- o The outlet nozzle is a replaceable, essentially unstressed structure. Conservatism required for primary pressure boundaries are not appropriate for the outlet nozzles. As a result, applicable strain controlled in-sodium fatigue data and high cycle in air strain fatigue data were analyzed to determine a conservative fatigue design curve with a margin of ~1.4 on the mean of the data.
- o After achievable and allowable striping temperature potentials are defined, it is possible to reorifice the core and to minimize striping.

3.12.2 The Thermal Striping Fatigue Mechanism

The purpose of this study was to show the adequacy of the CRBRP Core Assembly Outlet Nozzles to withstand thermal striping. This failure mode of the outlet nozzles was originally not considered in the design of the outlet nozzles stress reports because its severity was not known and insufficient data was available at that time to evaluate this damage mechanism.

The thermal striping fatigue mechanism is explained in the following Figures 3-76 and 3-77. Mixing of coolant fluid with large temperature differences is an inherent feature of nuclear reactors. Hot flow from core fuel assemblies mixes with the relatively cooled flow from blanket, control and removable radial shield assemblies in a mixing zone or recirculation region as shown in Figure 3-76. The temperature difference between the hot and the cold flow is defined as the striping potential in Figure 3-77. During the mixing process, temperatures will oscillate randomly. The temperature oscillation or striping can be defined as a fraction of the striping potential as shown in Figure 3-77. The maximum striping is the maximum temperature difference between the hottest and the coldest temperature at the point of measurement and during a time window. Flow fluid with different temperatures impinging on the surface of a structure produces fluctuating metal surface temperatures with calculable temperature profile into the thickness of the structure. Alternating temperatures cause biaxial surface strains. In a restraint surface the strain range is

$$\Delta \epsilon = \alpha \frac{\Delta T - T_m}{1 - \nu}$$

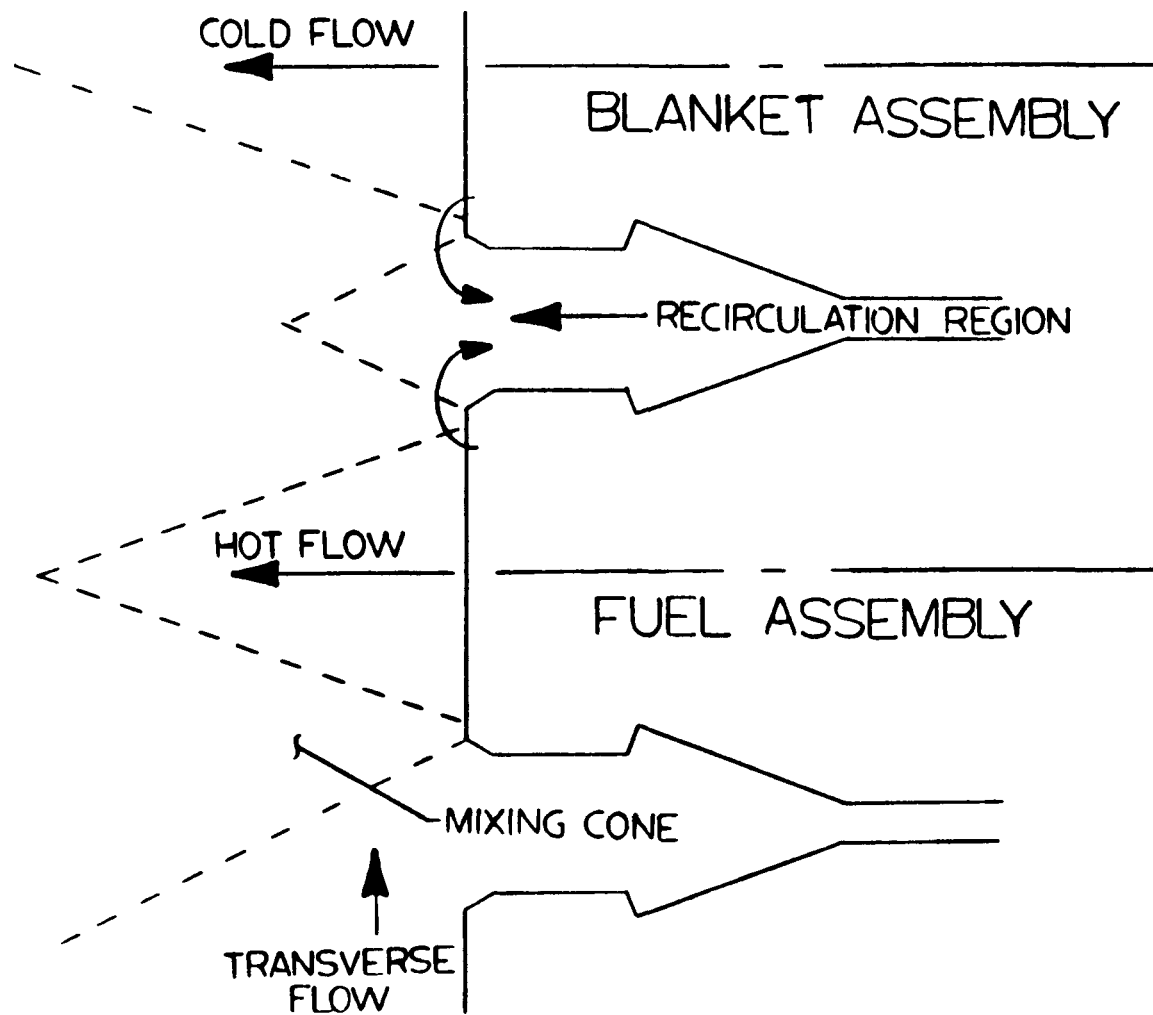


Figure 3-76
Fuel/Blanket Assembly Outlet Nozzle Design

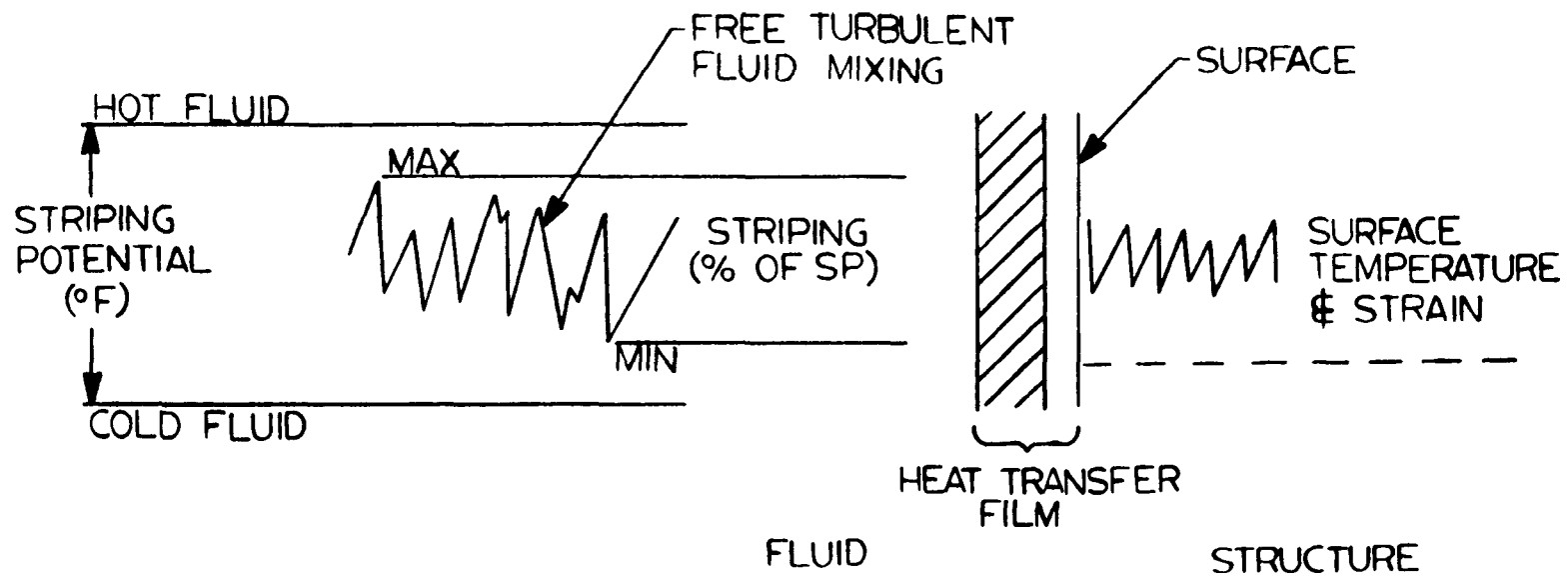


Figure 3-77
Thermal Striping Mechanism

where:

- α = coefficient of thermal expansion
- ν = Poisson's ratio
- ΔT = temperature range
- T_m = mean temperature of the structure

The alternating strains $\Delta\epsilon$ can cause high cycle strain fatigue. With an estimated strain frequency the design life can be estimated from strain fatigue design curves.

3.12.3 Design Description

Figure 3-78 shows a view of a typical outlet nozzle. Figure 3-79 shows a cross section and the dimension of a FA and BA outlet nozzle. The outlet nozzles of all core assemblies have a similar design because they have the same functions. Striping tests in water have shown that only the upper crown, 1.5 to 2 inches from the top edge, is subjected to thermal striping.

During normal reactor operation and transient conditions, no primary stresses are acting on this part of the nozzle and only insignificant secondary stresses due to thermal gradients are present.

Since the crowns of the assembly outlet nozzles have no other important function, their structural integrity must only be maintained to avoid release of broken pieces and debris into the coolant. Initiation of small cracks is not detrimental to the function of the component although it must be prevented. Therefore, this part of the outlet nozzle can be designed with less conservatism than other parts of the core structure and conservative ASME criteria need not be applied.

During reactor operation, the coolant outlet flow velocity is:

- o 11.2 to 13.7 ft/sec for the high flow FA's
- o 5.8 to 2.2 ft/sec for the low flow BA's
- o 0.18 to 0.12 ft/sec for the RRSA's

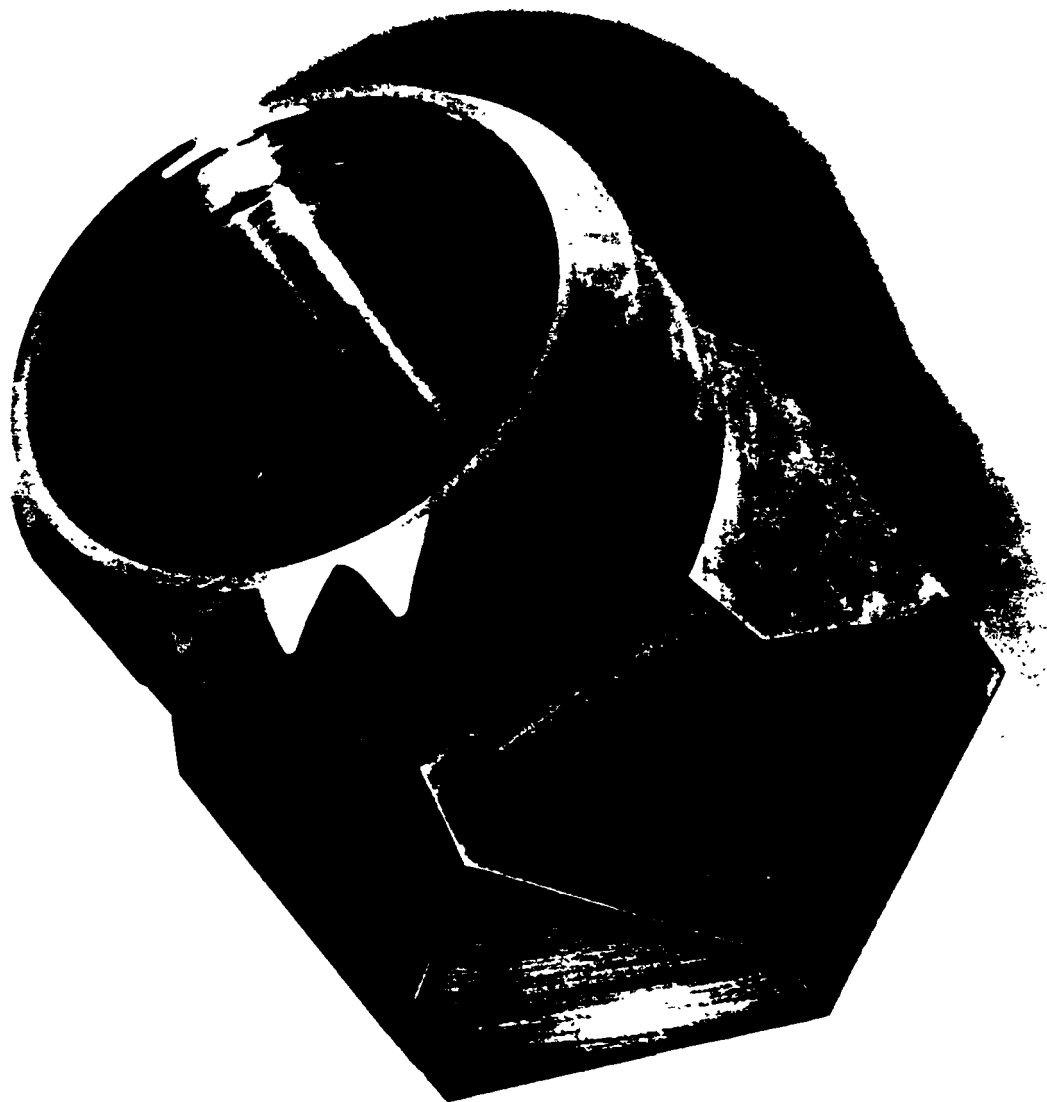


Figure 3-78

**FUEL & BLANKET ASSEMBLY
OUTLET NOZZLE**

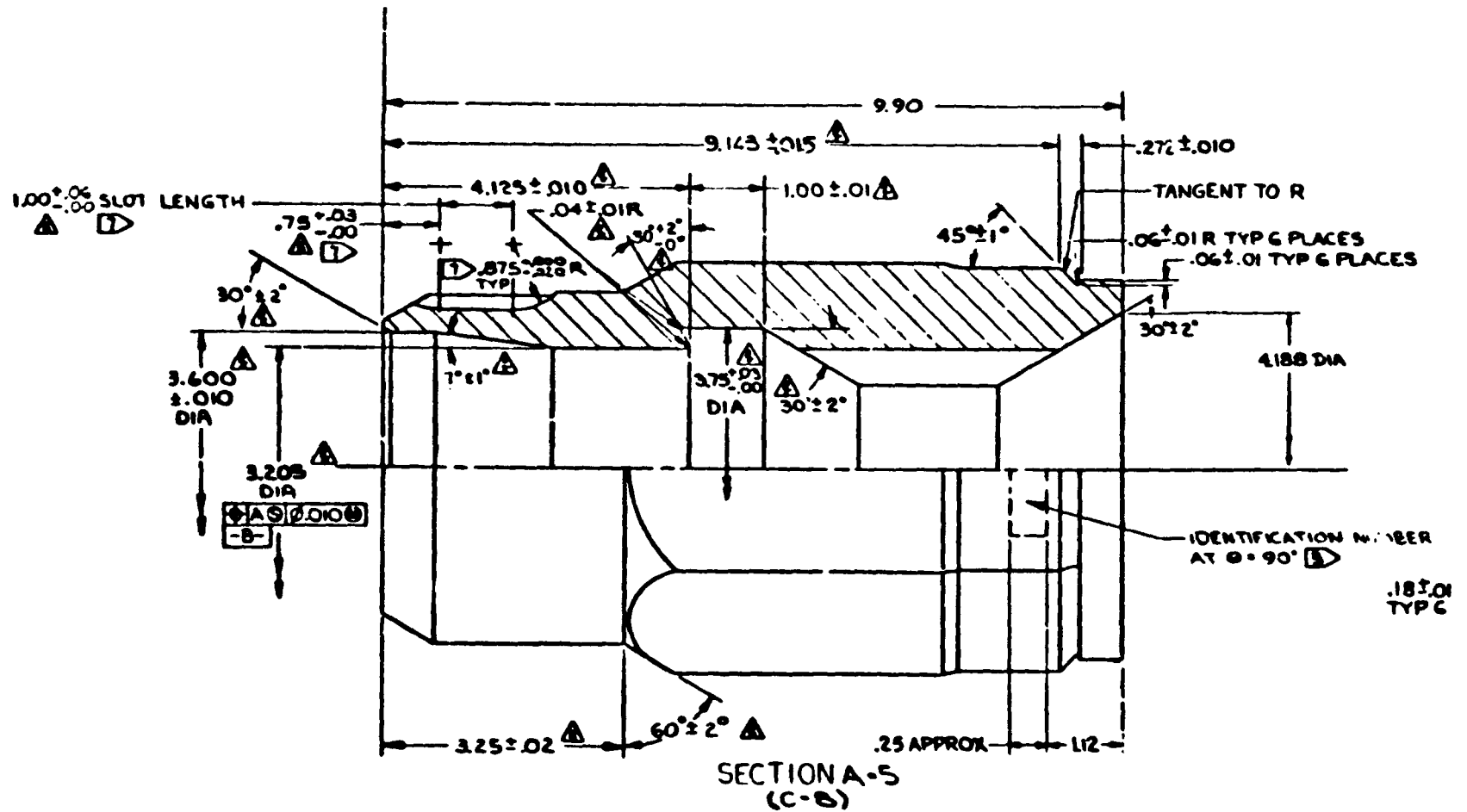


Figure 3-79
Section of F/A Outlet Nozzle
(Drawing #1182E45)

The fluid exits from the nozzles as shown in Figure 3-76. No mixing of fluid occurs in the cone of the jet. At the edge of the jet, fluid from adjacent assemblies is entrained and recirculates into the gap between the crowns of the nozzles. The recirculation was minimized through a design change by minimizing the space below the nozzle crowns. This mixing and recirculation between assemblies with large temperature differences is the primary cause for striping. The seven assembly striping test in water identifies the upper edge of the nozzle as the point of maximum striping.

This striping pattern is complicated by larger flow velocity differences between adjacent assemblies and by transverse flow across the assemblies. These secondary flows are caused by the mixing patterns in the mixing chamber above the core exit nozzles shown in Figure 3-80.

Special flow conditions occur in the control assembly outlet nozzles due to the presence of the driveline and the internal configuration of their outlet nozzles. The striping patterns in these outlet nozzles were also determined experimentally.

Figure 3-78 indicates a gap between the vertical skirt of the mixing chamber and the upper edge of the RRS outlet nozzles. Through this gap of approximately 0.75 inch, approximately 15% of the core flow escapes radially from the mixing chamber. This causes a strong transverse flow across and between the RRS outlet nozzles of approximately 14 ft/sec. Mixing of hot and cold coolant in this flow and the very slow and cold flow from the RRS causes striping on the RRS outlet nozzles. However, due to the long traveling distance of the mixing coolant from the mixing chamber into the gap, the maximum striping is only 47% of the difference between selected maximum hot and cold assembly coolant temperatures.

3.12.4 Design Criteria

The design criteria shall prevent crack initiation at the surface of the outlet nozzles subjected to thermal striping fatigue. The criteria shall provide margin for creep fatigue damage due to secondary loading mechanisms not considered in this report.

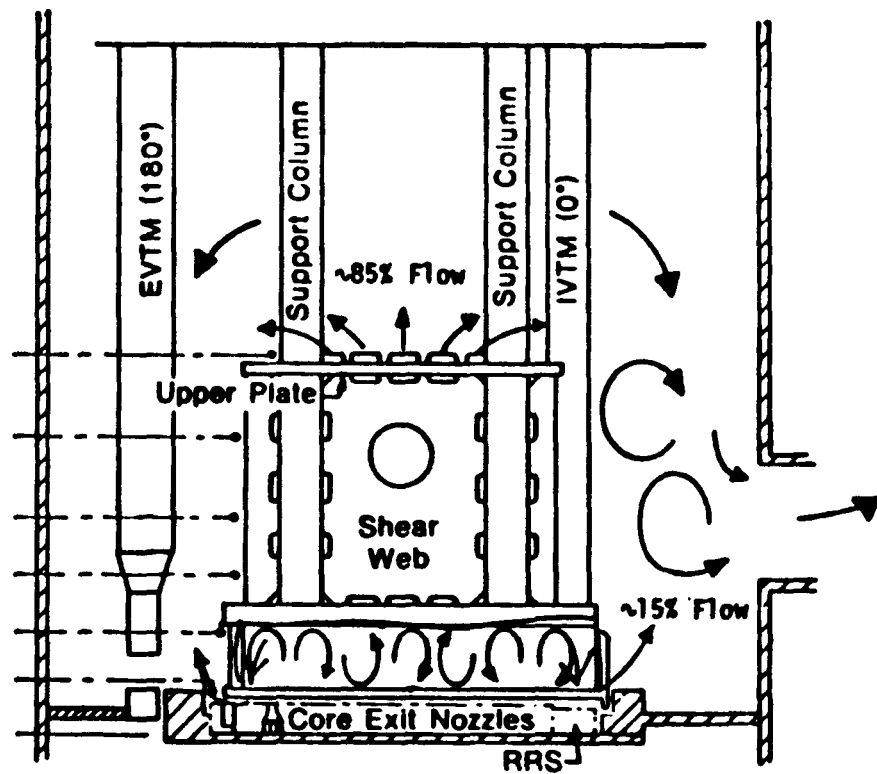


Figure 3-80
Schematic of Na Flow Pattern from Reactor Core

The following criteria shall be applied:

A. Temperature

The core assembly striping potentials, the maximum temperature differences between hot and cold fluid, shall be based on conservative (+2 σ) values of plant expected operating values (PEOV).

B. Time

- (1) The design lifetime of the fuel and inner blanket assemblies is 2 cycles or 328 days (128 + 200 days).
- (2) The design lifetime of the radial blanket assemblies is 4 cycles or 878 days for the inner row and 5 cycles (1153 days) for the outer row. Note: Only the inner row of radial blanket is exposed to significant thermal striping.
- (3) For striping analysis, the design lifetime of the RRS is 30 years (for other damage mechanisms 10 years). The adequacy of the design can be demonstrated by extrapolating the fatigue damage accumulated during the first four reactor cycles to 30 years.
- (4) The striping temperatures were found to be maximum during the initial reactor cycles and envelope later reactor cycles. Therefore, striping damage of equilibrium cycles is enveloped by first core load data.

C. Fluid Striping

Worst fluid striping time histories, normalized in % of the striping potential, shall be obtained from water test simulating sodium flow. No credit shall be taken for the approximately 20% higher striping measured in water than in sodium.

D. Metal Surface Striping

The metal surface temperature shall be calculated with a constant heat transfer film coefficient of 16000 BTU/hr-ft²-°F.

E. Stresses and Strain

The time dependent metal surface strains ranges shall be calculated for a fully restrained thick plate.

$$\Delta \epsilon = \frac{\alpha \Delta T}{1-\nu}$$

where:

α = coefficient of thermal expansion

ν = Poisson's ratio

ΔT = maximum temperature difference between surface temperature and the average temperature of the plate

If detailed or finite element stress analysis of the outlet nozzle is employed, mean stress effects shall be considered.

F. Fatigue Damage

(1) The fatigue damage D of the nozzle due to thermal striping alone shall be:

$$D < 0.1$$

(2) Linear damage summation shall be employed:

$$D = \sum_{n=1}^m \frac{N}{N_f}$$

where:

N = Number of cycles
N_f = Number of cycles to failure

(3) Non-periodic strain cycles shall be analyzed with the RAINFLOW method.[1]

(4) The elastic and plastic strain damage shall be calculated as follows:

$$\epsilon_E = \frac{1}{E} \frac{\sigma}{\left(1 - \frac{\sigma_m}{\sigma_u}\right)}$$

where:

σ = stress amplitude
 σ_m = mean stress
 σ_u = ultimate stress

$$\epsilon_{PL} = \epsilon_{tot} - \epsilon_{el}$$

(5) A fatigue curve of the following form shall be employed.

$$\frac{\Delta \epsilon}{2} = A (2N)^a + \frac{\epsilon_{en}}{2}$$

where:

$\Delta \epsilon$ = strain amplitude
A, a = constants
 ϵ_{en} = endurance limit

The material is solution annealed 316 Stainless Steel with a grain size of 5 per the American Society for Testing Materials (ASTM).

A fatigue design curve can be derived from in-sodium low cycle ($<10^5$ cycles) fatigue data and combined with a high cycle ($>10^6$ cycles) endurance limit. The design fatigue curve shall envelop the lower (-2σ) statistical limit of the data.

3.12.4.1 Conservative Assumptions

The conservative features of the criteria can be summarized as follows:

- A. The striping temperature potential is selected at the $+2\sigma$ estimate of the hot coolant temperature and at the -2σ estimate of the cool temperature. This increases the nominal temperature differences by approximately 45°F or 20%.
- B. Striping in sodium is 20% lower than in water due to the better conductivity of sodium which decreases temperature differences between hot and cold eddies.
- C. A fluid heat transfer film of $H = 16000 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$ was selected as a design value. Based on measurements in sodium and calculations, realistic maximum values should not exceed 8000 to 12000 $\text{BTU/hr-ft}^2\text{-}^\circ\text{F}$ for optimum transient heat transfer conditions.
- D. A simplified strain analysis is employed to calculate the strain ranges in a fully restrained thick plate. This strain is 14% larger than the strain calculated with a finite element model in ANSYS for a temperature fluctuation of 155°F and a round nozzle.
 - (1) In this analysis, a uniform change of the surface temperature is assumed. In reality, the eddies are small, probably fractions of the nozzle outlet diameter. Therefore, the temperatures around the nozzle circumference are non-uniform, and so is the hoop strain.

- (2) Maximum striping occurs at exposed edges of the nozzles. There the restraint and resulting strain are much smaller than in a fully restrained plate.

E. The fatigue design curve is derived from documented strain controlled fatigue data. A conservative (-2σ) statistical limit on the data is found from a statistical analysis of the data. In-sodium low cycle fatigue data are used to determine the plastic fatigue damage curve, and are combined with an endurance limit for in-air fatigue. However, the in-sodium fatigue data, for example the TSTF data, are approximately 0.07% strain higher than the in-air data at 10^6 cycles. It is known from other fatigue tests that the fatigue endurance limit in an inert environment is higher than the endurance limit in air.

3.12.4.2 Non-Conservative Assumptions

Some non-conservative features of the criteria can be summarized as follows:

- A. It is well documented that the fatigue damage accumulation is non-linear. For the given striping scenario, the maximum strain amplitudes occur after start-up of the reactor. This enhances crack initiation during early cycles. For this reason, the allowable damage was limited to $D < 0.1$.
- B. The existence of a fatigue endurance limit is assumed at approximately 10^7 cycles. Recent strain fatigue tests by EGG and ANL on material with a grain size of 3.2 ASTM indicates that the fatigue strain may continue to drop to approximately 10^8 cycles.[2] For this reason, analysis was performed with a fatigue curve in which a plastic fatigue curve was combined with an elastic fatigue curve of a shallow slope. The assumptions of this analysis and the results will be presented. The calculated damage increases, but not significantly to change the conclusion of the analysis.

- C. The striping analyses are based on finite striping windows of 40 to 60 seconds data. Maximum striping amplitudes are assumed to occur during this time. Since striping is a random process, it needs to be shown that the window length is indeed sufficient to detect maximum amplitudes with a large (95%) probability. A larger probability is not required because it can be shown that these large signals are rare, occur only with large return periods, and increase the damage only insignificantly.

3.12.5 Data Collection

In this section, the data and methods used to obtain the analysis input will be discussed. Since the available literature on some items is very extensive, only data actually used will be discussed.

A. Striping Potential

Reference 3 is the source for the assembly striping potentials and temperatures. All uncertainties in calculating the temperatures are discussed in these documents. Figure 3-81 is a picture of the core layout which gives typical assembly-to-assembly temperature gradients. Figure 3-82 shows an example of the time dependence of these temperatures. It was necessary to consider the time dependence of these temperatures to demonstrate the adequacy of the nozzle to resist striping.

B. Fluid Striping

The fluid striping as a function of time was measured in water tests simulating in sodium striping for prototypical Reynolds numbers and flow velocities. The Richardson number, a parameter which models the ratio of buoyant flow energy to turbulent kinetic energy, need not be considered since the turbulent energy is much larger than buoyancy potential.



572

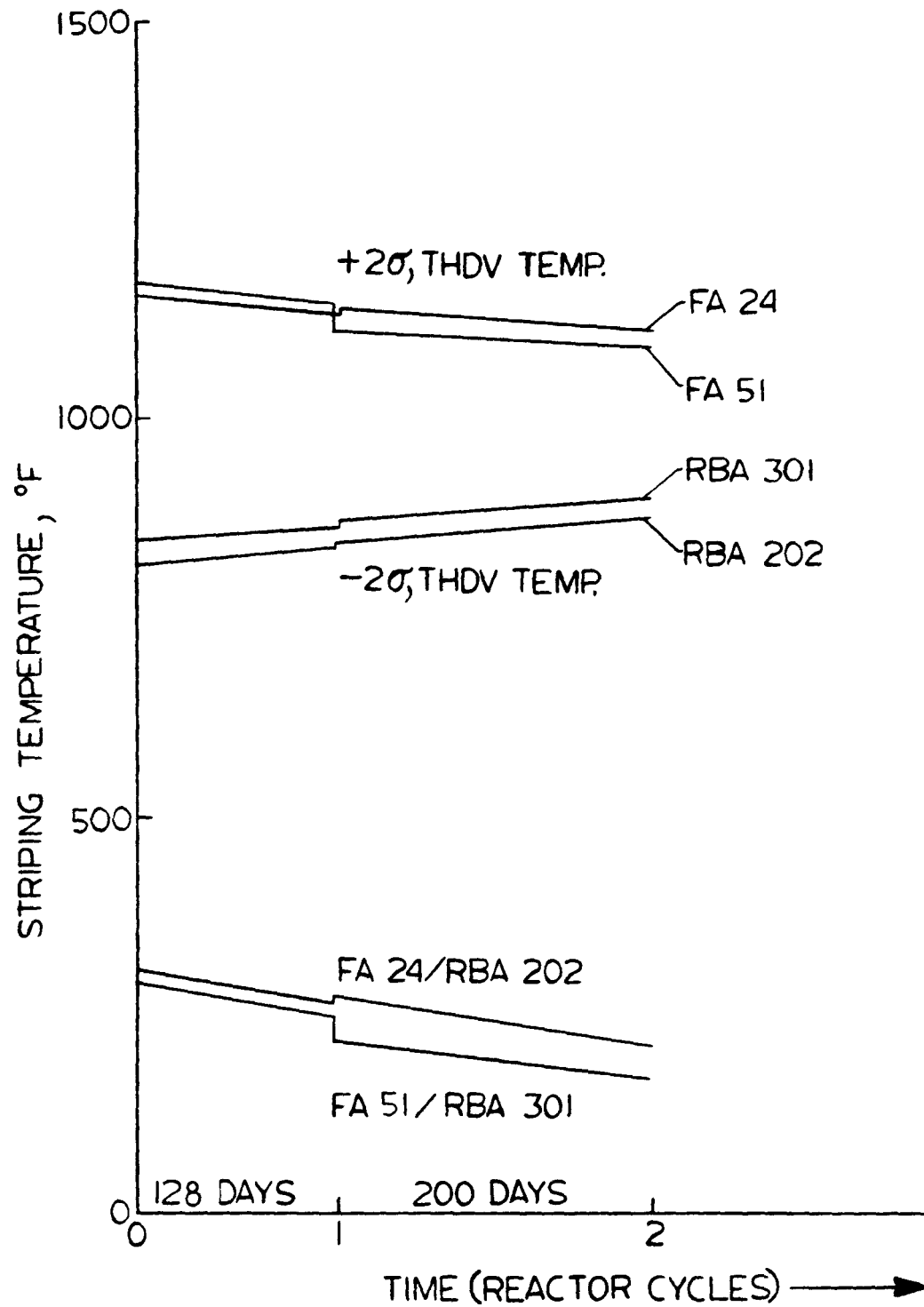


Figure 3-82
Striping Temperature History of Fuel and
Radial Blanket Assembly Interface

The fluid striping time histories used in design were obtained from three test series:

- (1) The Seven Assembly Outlet Nozzle test - This test is the principal data source for the analysis. Hundreds of striping measurements on fuel, blanket primary and secondary control assembly outlet nozzles were taken during these tests. Figures 3-83 and 3-84 show typical fluid striping time histories. The striping temperature amplitudes were normalized by the difference between hot and cold temperatures. These signals were characterized by their maximum temperature range in percent of the hot-cold temperature difference. The location of maximum striping (larger than 30%) are plotted on the map of the seven outlet nozzle assemblies in Figure 3-85. Figures 3-86 and 3-87 show the maximum temperature range between a maximum and a minimum for increasing time windows. Figure 3-86 shows a case where a saturation of the maximum surface temperature range occurs after approximately 40 seconds. Figure 3-87 shows the probability distribution of the striping ranges on normal probability paper. The figure shows a near normal distribution of temperature ranges measured over 25 seconds. However, after 65 seconds, a saturation of the maximum amplitude values occur. The shallow slope of this curve at maximum amplitudes indicates the low probability for larger amplitudes to occur.

Three striping test series were performed, the primary control assembly (PCA), secondary control assembly (SCA), and fuel and blanket assembly (FA/BA) test series, with the following results:

- (a) PCA and SCA series - Three signals exceeded 75% striping: 75%, 76%, 80%.
- (b) FA/BA Series - Eight signals exceeded 75% striping: 75%, 78%, 78%, 80%, 81%, 82%, 86%, 86%.

Figure 3-83

SEVEN ASSEMBLY STRIPING TEST - FA-08
TC LOCATION FN 7N

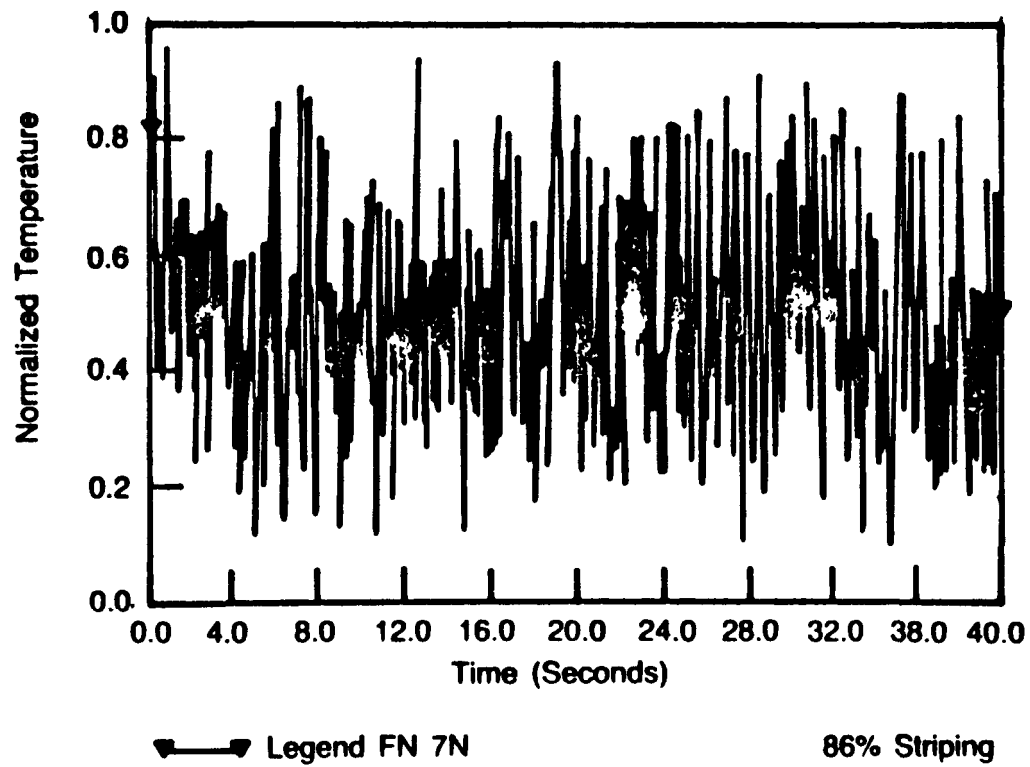


Figure 3-84

SEVEN ASSEMBLY STRIPING TEST - FA-08
TC LOCATION FN 7JJ

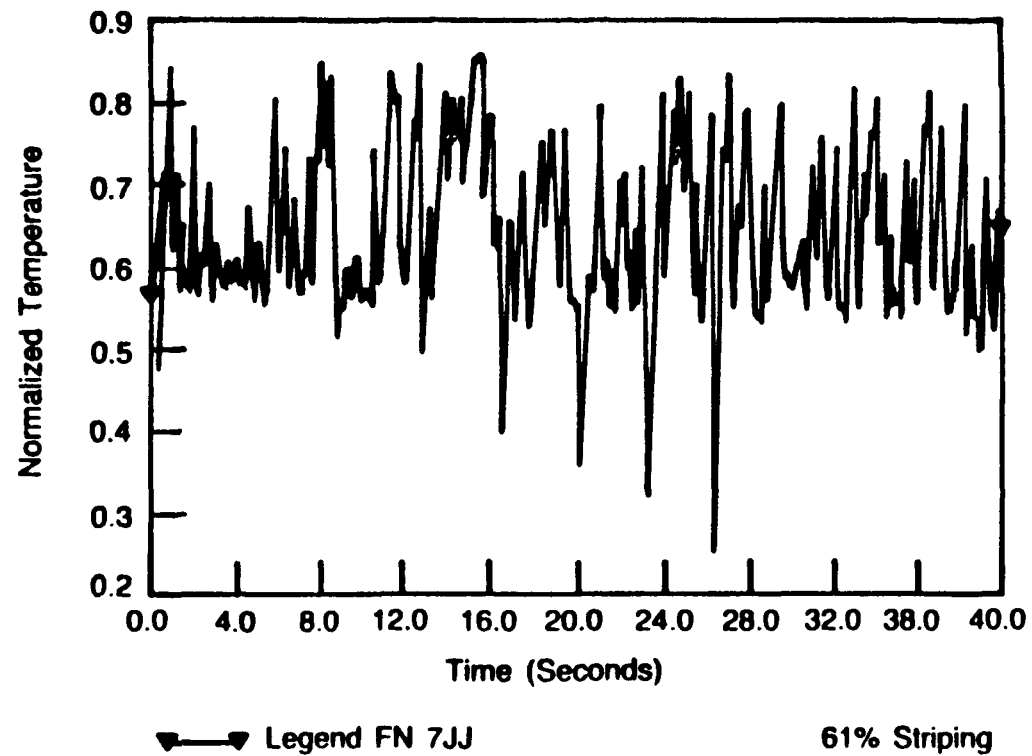


Figure 3-85
7 Assembly Test Results

FA-8

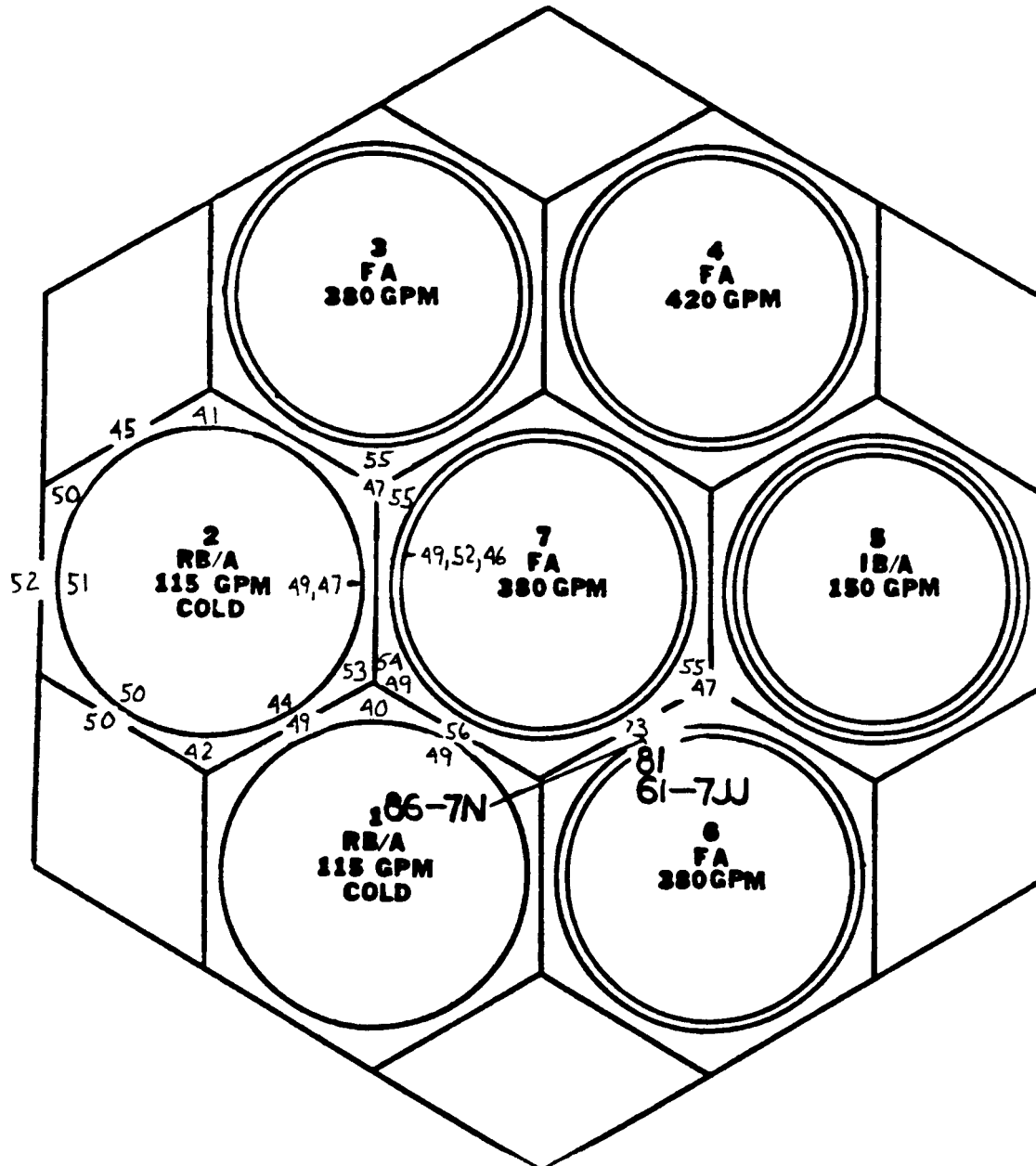


Figure 3-86
Saturation of Maximum Striping with Time
Example: Test on-2, T/C B-7, 20K Film

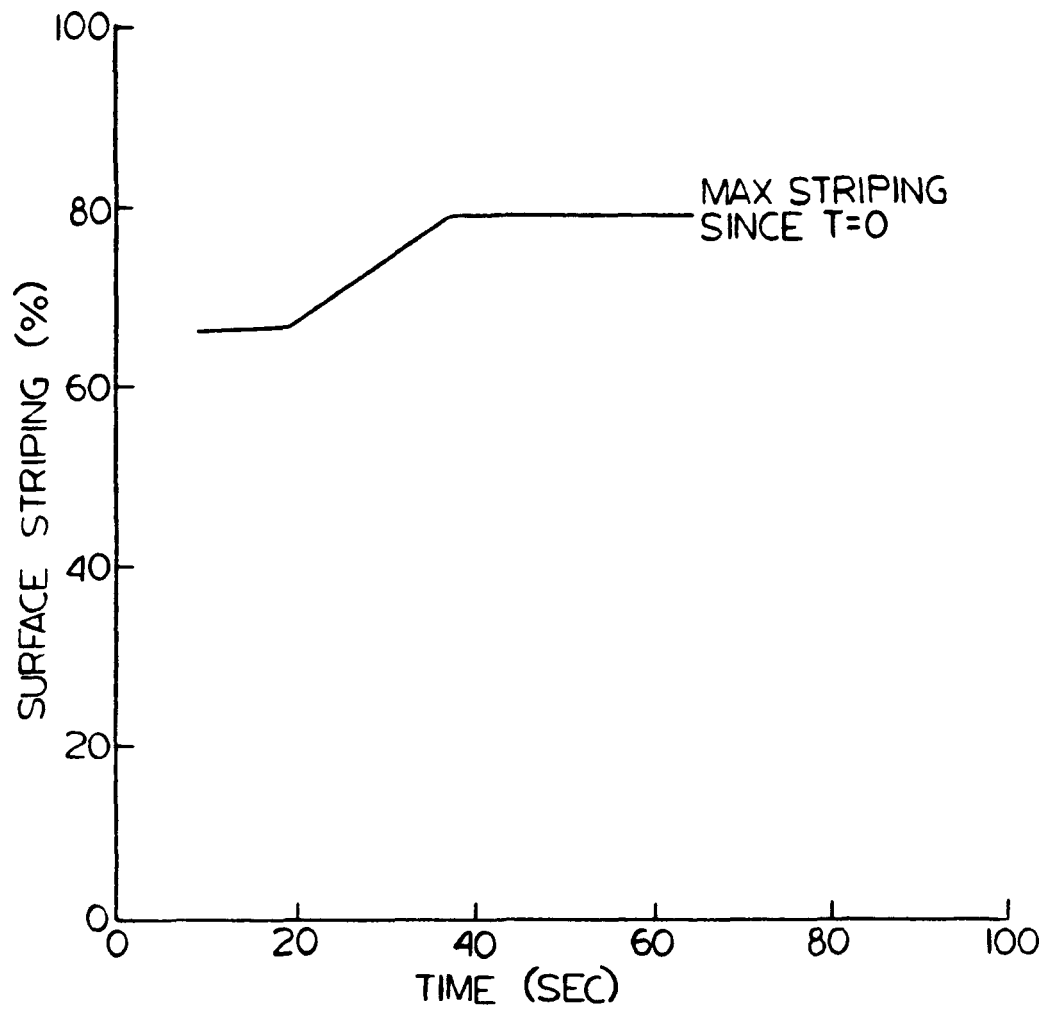
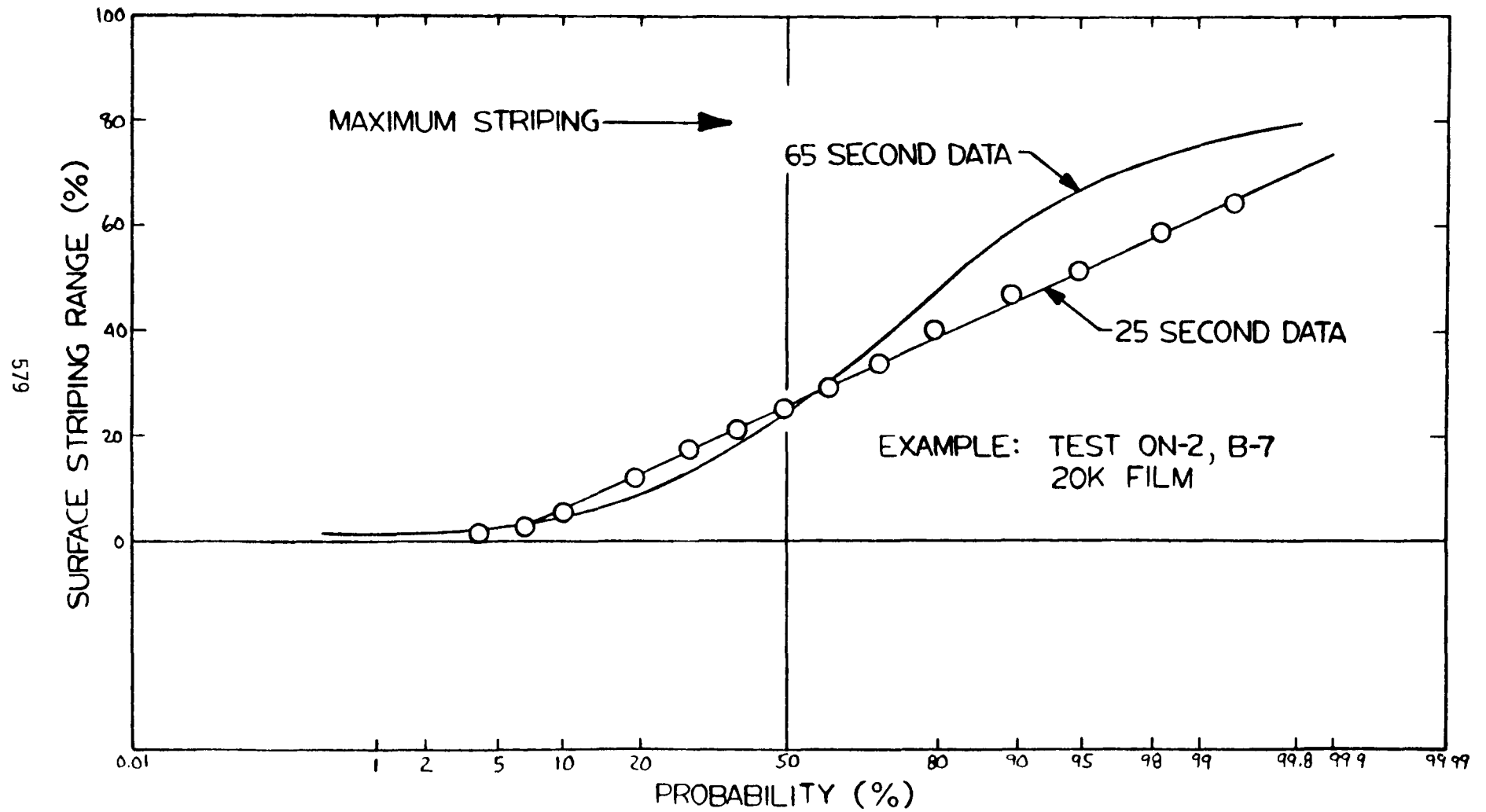


Figure 3-87
Probability of Measuring Larger Striping
in Longer Test Is Small



This data and some additional data were evaluated for their fatigue striping damage potential with a procedure which will be described in this report.

It was found that the maximum striping range of a signal is not a clear indicator of its striping fatigue damage potential. Striping fatigue damage evaluations were required to select the most damaging striping signals.

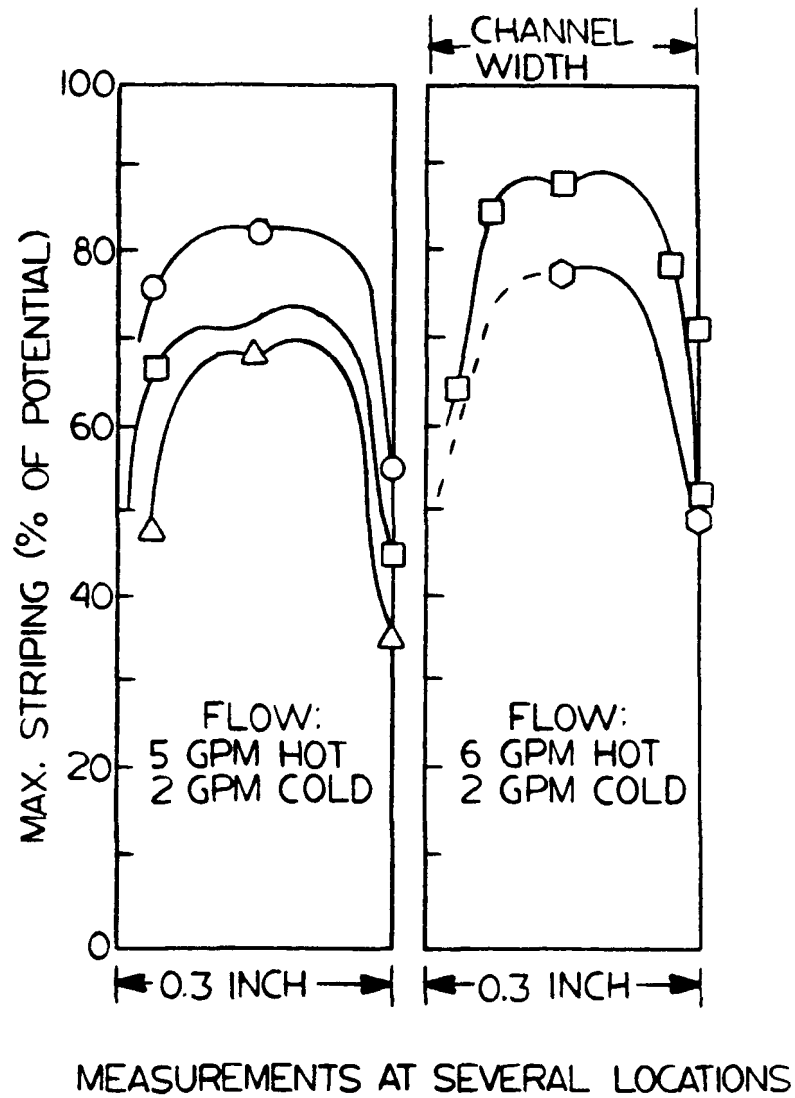
- (2) A striping signal from a previous test series, the core assembly outlet nozzle feature tests, was selected as a striping signal to be used in design. Striping signals from this test series did yield higher damage than the seven assembly test series for the following reasons:

- (a) Hot water was used in the low flow "cold" blanket assemblies as tracer fluid for striping. This changes the flow recirculation pattern between nozzles. The technique was changed in the seven assembly test by installing a hot water tank with larger capacity to feed the high flow fuel assembly nozzles with hot water.
- (b) The gap between the assembly crowns was larger than in the final design which provided more space for recirculation flow. The assembly nozzles were changed to decrease the recirculation space between nozzles.
- (c) The simulated assemblies did not include fuel rod bundles. As a result, the flow did not have the rotation and velocity distribution of the flow from a fuel assembly. Simulated rod bundles were installed into the seven assembly nozzle test.

One or all of these changes, which were identified in the outlet nozzle feature test, may have contributed to the lower striping intensity measured in the seven assembly test series. The outlet nozzle feature test was also instrumental in developing the data analysis procedure for the seven assembly test series.

- (3) The RRS outlet nozzle striping magnitude was obtained from the In-Reactor Flow Model (IRFM) test. The striping magnitude near the RRS outlet nozzles measured in water is significantly lower than on other core assembly outlet nozzles for two reasons:
 - (a) Hot flow from the fuel assemblies mixes with cold flow from blanket assemblies in the UIS mixing chamber before it reaches the RRSA nozzles.
 - (b) The cold flow volume from the RRS is small and is being entrained in small eddies of the fast (14 ft/sec) core flow through the gap between the UIS and the nozzles.
- (4) Several water table tests were performed to study striping of other components than the core assembly outlet nozzles which did yield relevant information for outlet nozzle striping considerations. Figure 3-88 shows results of striping measurements as a function of the distance from the wall in a gap. These measurements indicate the sharp drop of the striping amplitude over a distance of 0.05 inches near the wall. In all other striping measurement in water, the beads of the thermocouples are placed outside this boundary such that the boundary wall effect is negligible. This data has been documented in Reference 4.
- (5) Finally, a number of striping tests have been performed in sodium and have been compared with in water tests. Some of the results have been published. These reports demonstrate conclusively that the fluid striping in sodium is approximately 20% less than in water. These tests include:

Figure 3-88
Mixing of Hot and Cold Fluid in a Channel Indicates
250% Decrease of Striping Near Wall



- (a) The ANL Mixing Tee Tests [5]
- (b) Japanese Striping Tests
- (c) AI Striping Tests [6]
- (d) UK Striping Tests [7]

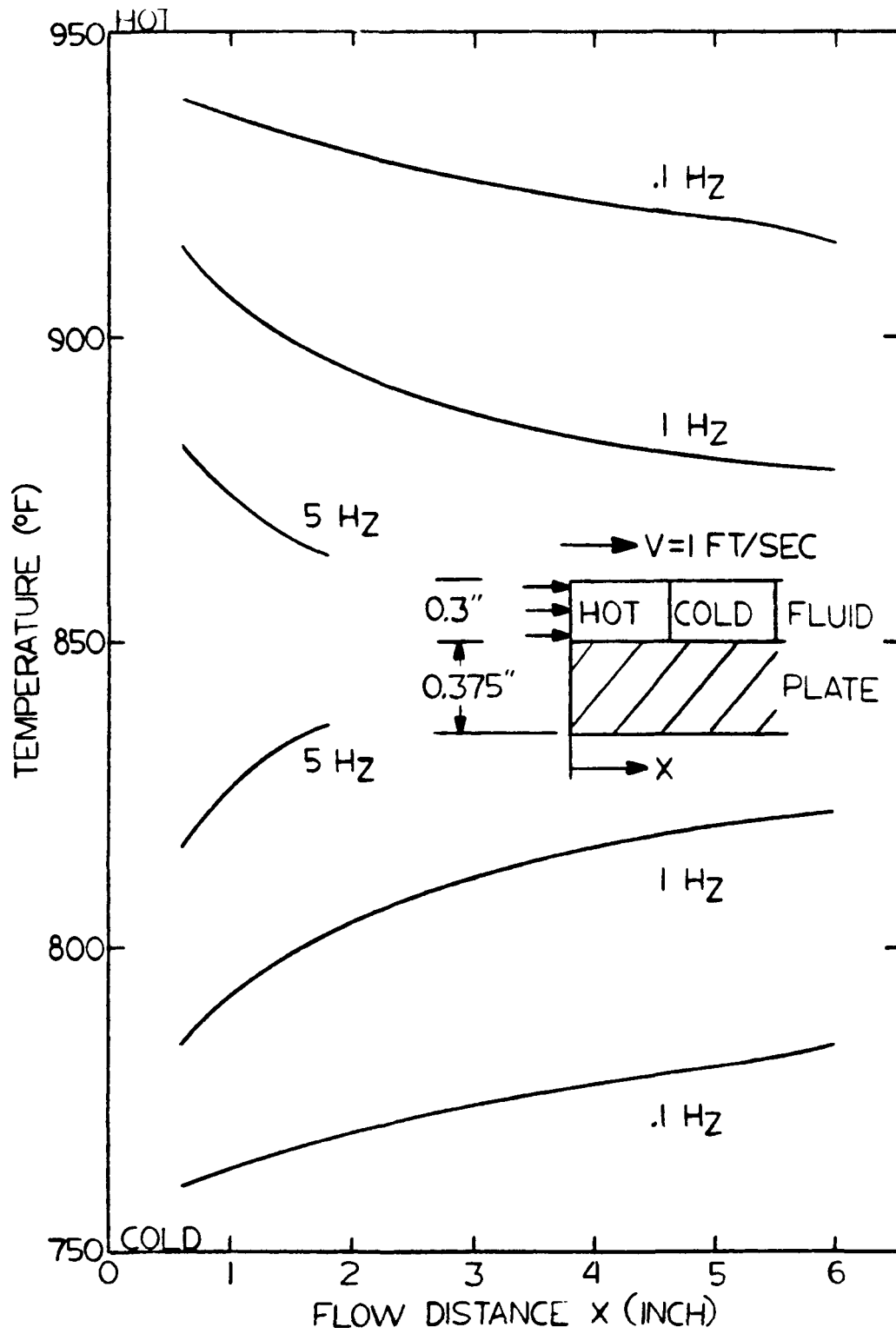
C. Film Coefficient

A thermal heat transfer film coefficient of $16000 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$ was selected for design. This value was selected based on limited measurements and theoretical calculations. Since directly applicable measurements are not available, a probable overly conservative value was selected based on the following information:

- (1) From temperature calibration tests of the Thermal Striping Test Facility (TSTF) under optimum heat transfer condition (a jet of sodium directed on a rotating cylinder) a film coefficient of $6500 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$ was determined. The test and results of this evaluation have been published.[8]
- (2) Thermal calibration of a thermal cycle fatigue dunk test did yield a film coefficient (under a sodium jet) of $4000 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$ immediately after the dunk. The film coefficient increased to $14000 \text{ BTU/hr-ft}^2\text{-}^\circ\text{F}$ after seconds.
- (3) Finite element analysis of the heat transfer between sodium and a plate indicates that the film coefficient decreases rapidly in flow direction of the fluid, more rapidly than indicated by the steady state heat transfer correlations, for example: $Nu = 0.53 Pe^{0.5}$.

Figure 3-89 shows the decrease of the hot-cold metal surface temperature range as a function of flow distance for three striping frequencies. The reason for this decrease is that in transient heat transfer due to striping between hot cold eddies and the structure, not only the structure is being subjected to

Figure 3-89
Calculated Surface Striping



thermal shocks, but also the sodium. Within a few inches, depending on flow condition, the sodium and the structure temperature near the surface equilibrate to an average temperature.

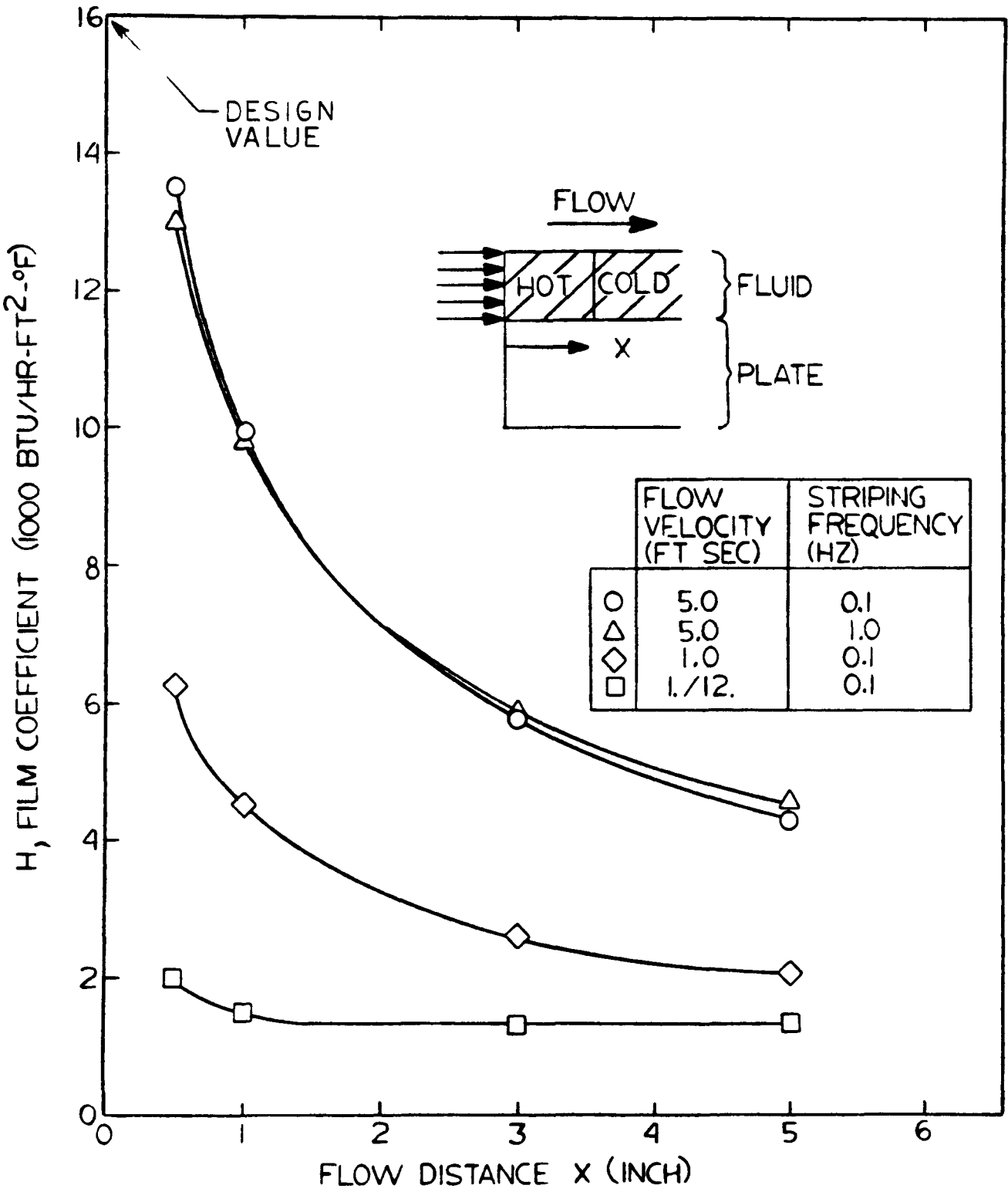
Figure 3-90 shows the calculated film coefficient. Within the first fraction of an inch, the film coefficient drops below the design value. Figure 3-91 shows the variation of the calculated film coefficient during a temperature cycle. Due to a phase difference between the surface heat flux and the temperature difference ($T_{\text{Surface}} - T_{\text{Bulk Sodium}}$), the film coefficient, which is ratio of both values, assumes infinite values when the $T_{\text{Surface}} - T_{\text{Bulk Sodium}}$ is zero. However, in engineering analysis and in Figure 3-90, a constant film coefficient is assumed.

- (4) Finally, the literature was reviewed for applicable data; however, none were found.[9]

D. Fatigue Data

Available strain controlled fatigue data obtained in-air and in-sodium have been compiled in a separate document for the striping analysis of the outlet nozzles. This data was grouped into three temperature ranges. The in-air and in-sodium fatigue data were statistically analyzed. The mean of the data and a constant variance factor for each curve was found. A conservative (-2σ) statistical limit on the data was selected as design curve. The existence of an endurance limit was assumed. Figure 3-92 shows the in-air fatigue data in the temperature range of 538°C to 566°C. Figure 3-93 shows a comparison of the in-sodium fatigue data with the in-air fatigue curves. The higher fatigue strength of the in-sodium fatigue data is obvious from this figure. The higher fatigue strength of metal in an inert environment is a well documented phenomenon.[1] Details of this analysis will be reported separately in this design review package.

Figure 3-90
Calculated Striping Film Coefficient-H



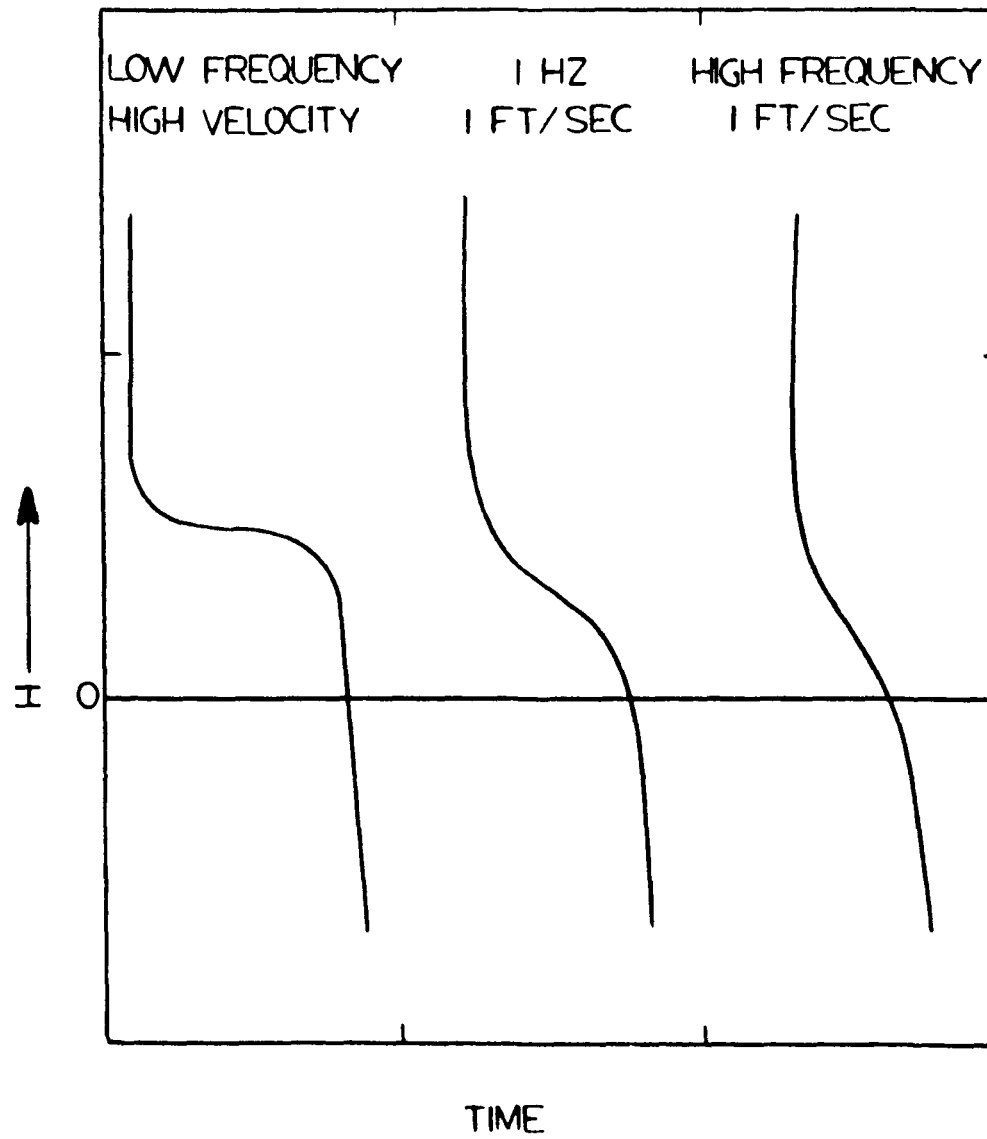


Figure 3-91
Qualitative Comparison of Thermal Film Coefficients

FIGURE 3-92

**FATIGUE OF SA 316 SS IN AIR AT
538 TO 566 °C**

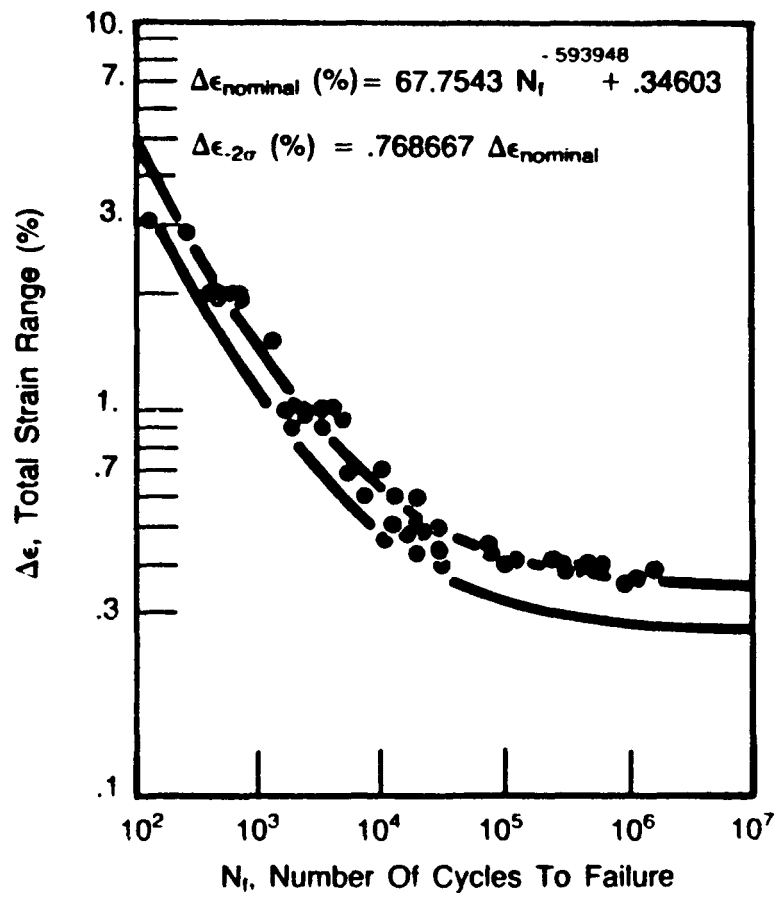
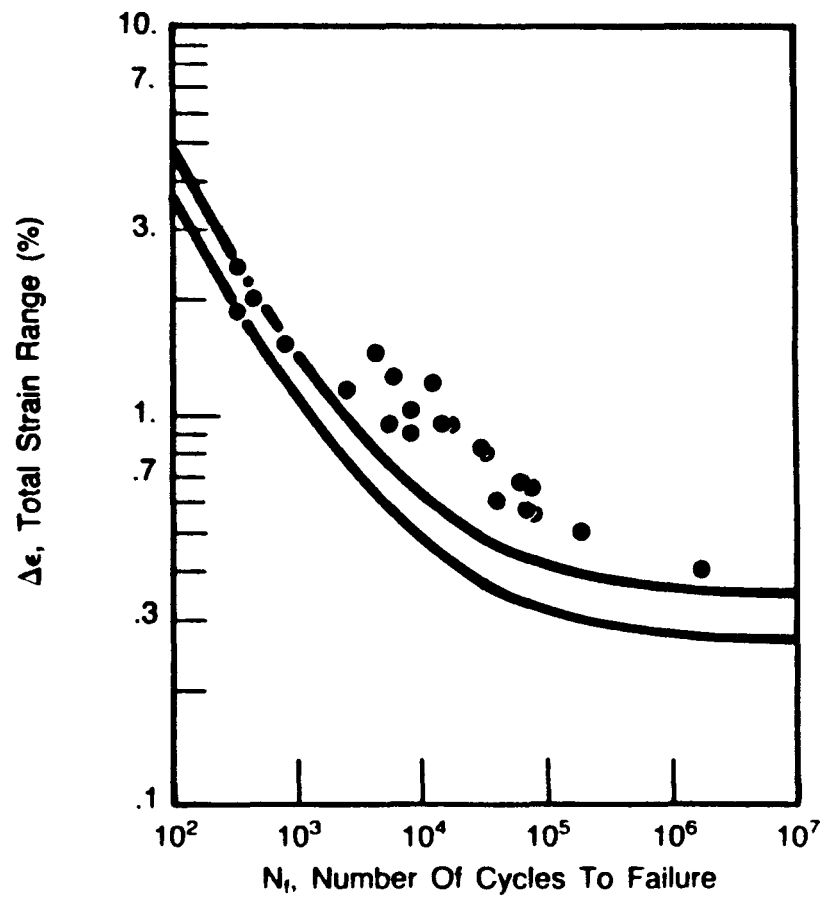


FIGURE 3-93

**FATIGUE OF SA 316 SS AT 538 TO 566°C,
IN SODIUM DATA VERSUS IN AIR CORRELATION**



During the course of the striping analysis it became obvious that plastic fatigue damage caused by the high striping potentials at the start of the reactor cause low cycle fatigue damage. To limit the calculated fatigue damage, it was necessary to take advantage of the higher in-sodium fatigue strength. However, high cycle in-sodium fatigue data (10^6 cycles) are not available. Therefore, the low cycle in fatigue data was combined with the in-air fatigue endurance limit of Figure 3-92 to construct the fatigue design curve shown in Figure 3-94. Since less in-sodium fatigue data are available than in-air fatigue data, the statistical uncertainty factor for the curve is larger, 0.706, than for the curve in Figure 3-92 which has an uncertainty factor of 0.769. Only one fatigue curve is being used in design because maximum nominal coolant outlet temperatures from nozzles do not exceed 1050°F significantly at the start of the first cycle and are below 1050°F in later cycles. In addition, coolant outlet temperatures from those nozzles, which are subjected to striping, are generally lower. At lower temperatures, the fatigue strength increases only slowly with decreasing temperature.

The endurance limits derived for the three in-air fatigue curves is compared in Figure 3-95 with other published fatigue endurance limits.[10,11] The agreement between this work and the Jaske and Frey results is adequate.

The in-air fatigue strain ranges at high cycles are considerably lower than those measured in this sodium TSTF test. Figure 3-96 lists the results of this test series without discussion. The test procedure has been described in Reference 8. No cracking was observed in tests performed with striping temperatures below ~325°F ΔT or a strain range of ~0.40% strain amplitude. These test results support the conservatism of the selected design fatigue curve with an endurance limit of 0.244% strain.

There was some residual concern about the existence of a fatigue endurance limit for strain controlled fatigue of austenitic stainless steels because very high strain controlled fatigue data

FIGURE 3-94

**FATIGUE OF SA 316 SS AT 538 TO 566°C,
IN SODIUM DATA VERSUS IN SODIUM CORRELATION
WITH IN AIR CORRELATION ASYMPTOTE**

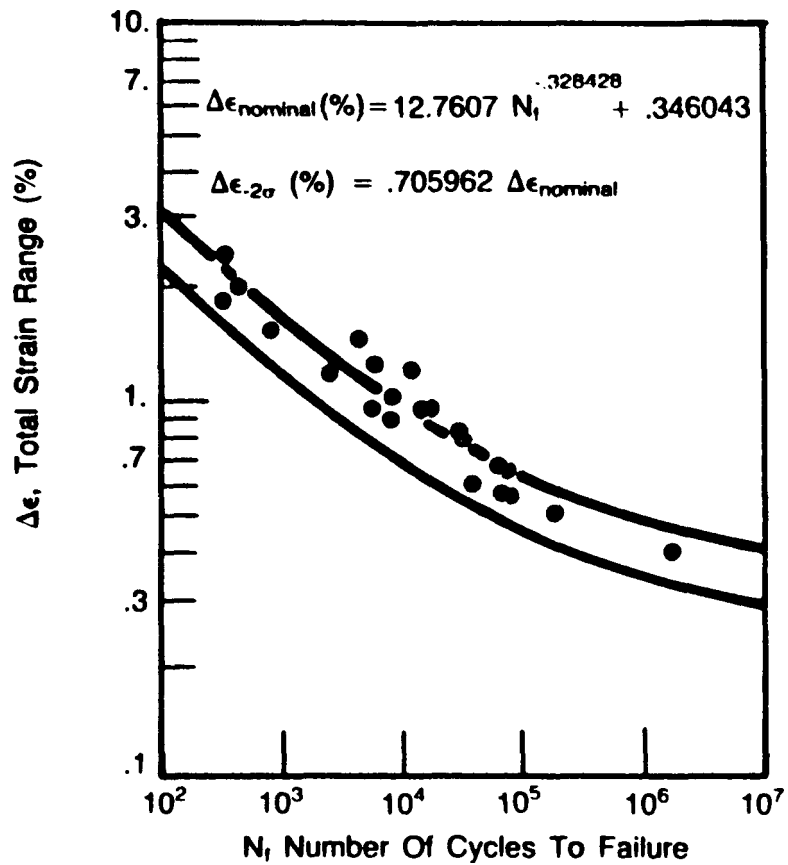


Figure 3-95

COMPARISON OF IN-AIR FATIGUE LIMITS AT 10^8 CYCLES

TEMPERATURE ____°C____	STRAIN $\Delta\epsilon\%$		
	PRESENT STUDY*	JASKE ** FREY	YOSHIDA ** LOAD CONTROLLED
20 TO 25	--	0.279	0.242
400 TO 427	--	0.353	0.252
456 TO 500	--	0.347	0.260
538 TO 566	0.346	0.339	--
593 TO 600	0.281	0.292	0.249
625 TO 650	0.345	--	--

* HIGHER VALUES IN SODIUM

**REF. 10 AND 11

Figure 3-96

SUMMARY OF 316SS FSTF THERMAL
STRIPING FATIGUE RESULTS

Specimen	Cycling Frequency (HZ)	Fluid ΔT (a) °F (°C)	Surface @ 84% of Fluid ΔT @ 1 HZ		Total Cycles	Observation
			Surface ΔT °F (°C)	Strain Range (c,d) (%)		
316-6	0.2	600 (334)			2.7×10^4	Isolated Defect Indications
316-4	1.0	600 (334)	468 (261)	0.81	5×10^4	No Cracking
					2×10^5	Mud Flat Cracking Pattern Max. Crack Depth = 0.086 in. (2.18 mm)
316-5	1.0	500 (278)	390 (217)	0.67	2×10^5	Initiation of Cracking in Mud Flat Pattern
316-7	1.0	400 (222)	312 (174)	0.51	10^6	Large cell size Mud Flat Crack Pattern Max. Crack Depth = 0.179 in.
316-8	1.0	325 (181)	254 (141)	0.41	10^6	20 Axial Cracks
					1.25×10^6	40 Axial Crack Max. Crack Length 1.35 in.
316-10	1.0	300 (167)	234 (130)	0.36	8.5×10^5	No Cracking
					1.4×10^6	No Cracking
316-13	1.0	325	254 (144)	0.41	4×10^5	2 Axial Cracks Max. Length 0.125 in.
316-14	1.0	400	312 (174)	0.51	2×10^5	27 Axial Cracks Max. Length 0.3 in.

Notes: (a) $T_{\text{mean}} = 700^\circ\text{F}$ (c) Strain range computed range from a 1-D inelastic analysis of a axisymmetric model of the specimen subject to a square wave surface temperature fluctuation. $E = 23.7 \times 10^6$ PSI $\nu = 0.29$ $\sigma_y = 20,600$ PSI $E_p = 0.554 \times 10^6$ PSI $\alpha = 9.9 \times 10^{-6}/^\circ\text{F}$

$$(d) \Delta \epsilon = \frac{2}{3} [(\epsilon_{\text{radial}} - \epsilon_{\text{hoop}})^2 + (\epsilon_{\text{hoop}} - \epsilon_{\text{axial}})^2 + (\epsilon_{\text{axial}} - \epsilon_{\text{radial}})^2]^{1/2}$$

(>10⁶ cycles) is not available. To eliminate this concern, and determine the effect of an elastic fatigue curve on the striping damage, such a curve was constructed as shown in Figure 3-97.

Recently generated high cycle fatigue data of 316 SS with a grain size 3.2 ASTM was not considered because the grain size of the outlet nozzles is 5 ASTM. The effect of the much lower in-air nominal fatigue correlation:

$$\frac{\Delta \epsilon}{2} = 0.33785 (\text{Log } N_f)^{-3.63} + 0.00103$$

which was derived from these tests was evaluated. This fatigue curve is very conservative because the high cycle fatigue data was generated under load control.

3.12.6 Analysis Methodology

The fatigue analysis was performed with published and verified methods. A flow diagram of the analysis is shown in Figure 3-98. Digitized and normalized fluid striping data are input from the water striping test evaluation. The digitized temperature-time-histories are used in the TAP-A thermal hydraulic program to calculate the metal surface temperature. A 3/8 inch nozzle wall thickness and a constant heat transfer film coefficient are input. The output (in 20 millisecond intervals) is the difference between the metal surface temperature and the average nozzle temperature. All relative minimum and maximum temperatures are being calculated and stored as shown in Figure 3-99. The signal contains many small temperature amplitudes below the striping damage limit. All relative striping amplitudes smaller than 10% of the striping potential are eliminated as shown in Figure 3-99 and without the reduction of the damaging striping amplitudes. The signal is then statistically analyzed. This analysis is being performed by a separate computer program STRIPE.

Figure 3-97

CONSTRUCTION OF ELASTIC-PLASTIC STRAIN FATIGUE DESIGN CURVE

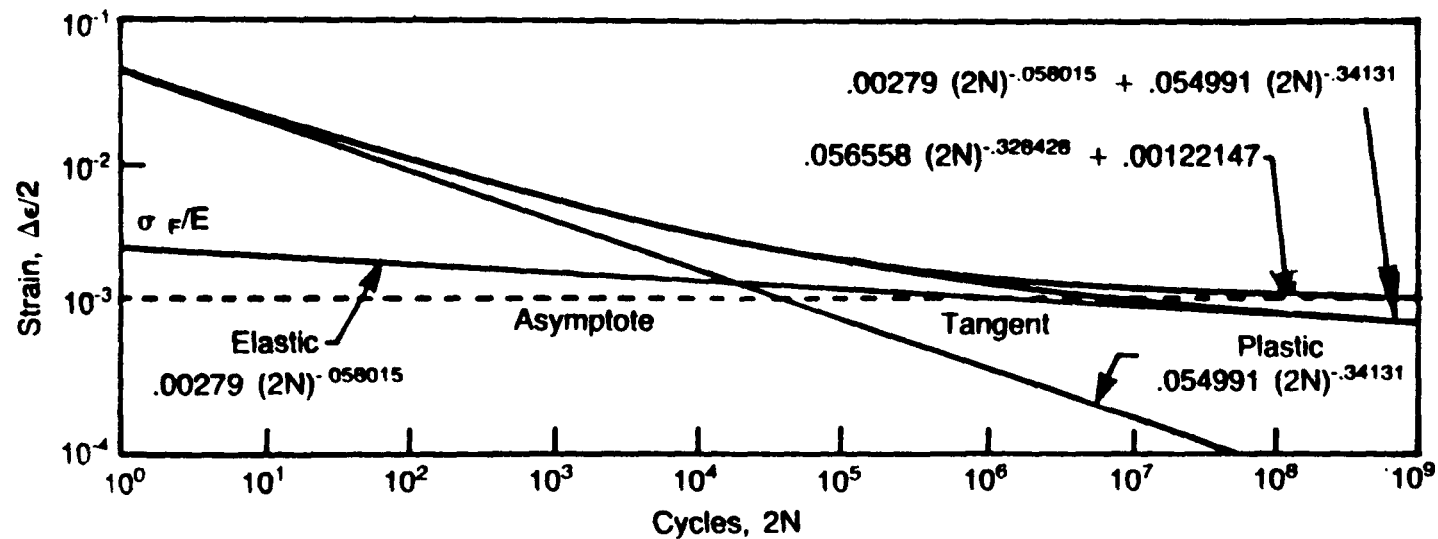
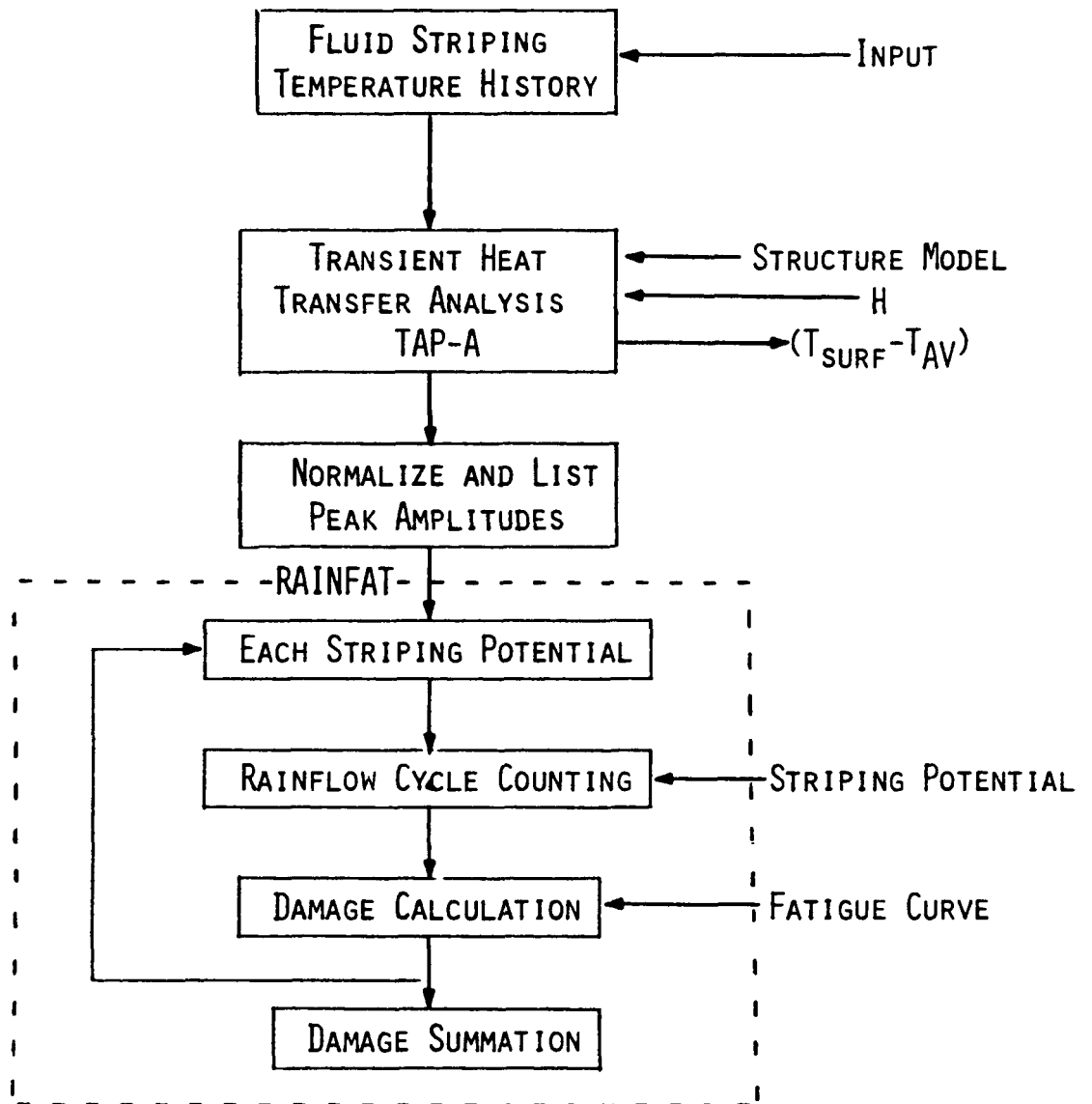


Figure 3-98

STRIPING FATIGUE ANALYSIS METHOD



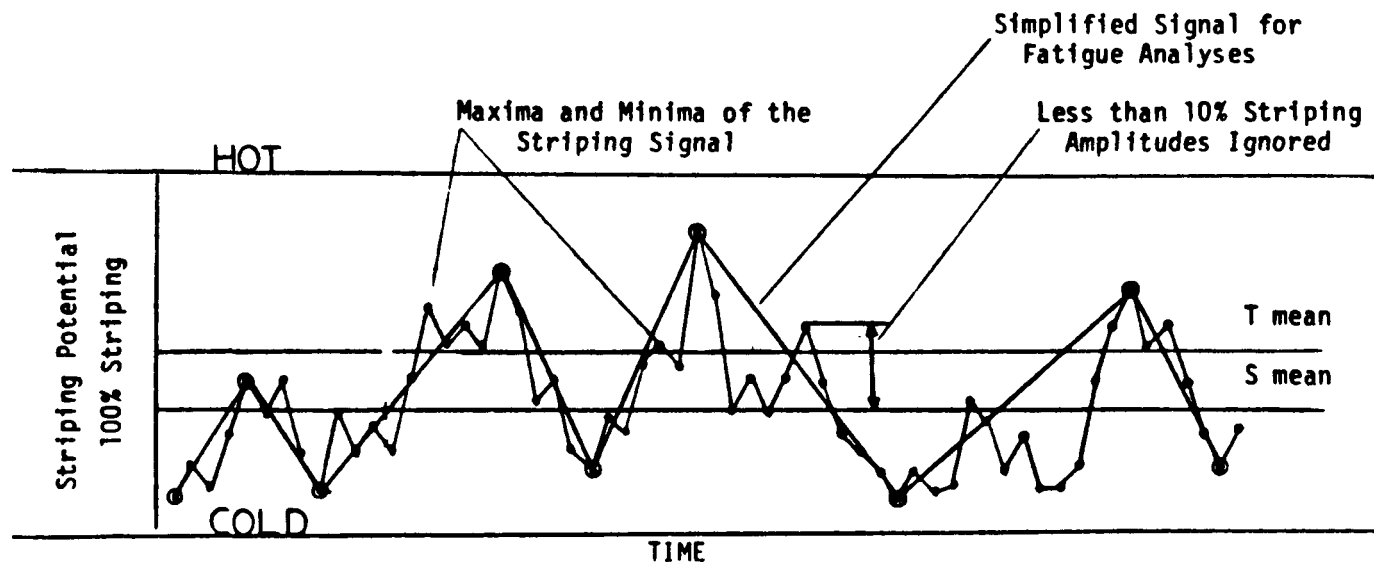


Figure 3-99
Record of Minimum and Maximum Temperature Stripping

The surface temperature calculations with TAP-A are started at a mean metal temperature of 700°F which may not be the averaged striping temperatures. As a result an initial fictitious transient strain is calculated from the difference between the mean temperature input and the calculated mean temperature. Running the TAP-A program twice with the same striping data eliminates this startup problem; the first calculates an average nozzle temperature profile, and the second time to calculate the striping temperatures, eliminates this mean strain effect. However, the additional fatigue damage due to this transient mean strain effect may increase the damage by 10 to 15%.

The fatigue damage analysis is being performed with the RAINFLOW method which is well documented in Reference 1 and in a series of papers by N. E. Dowling. [13,14] No attempt will be made to describe the method here except that it partitions the temperature time history in a series of decreasing amplitudes, as shown in Figure 3-100. A computer program was written to calculate the fatigue damage. The analysis approach needed some modifications because the input is strain and not load. The program, named RAINFAT has been checked.

For a simple fatigue equation of the form,

$$\frac{\Delta \epsilon}{2} = A (2N)^b + \frac{\epsilon e}{2}$$

the calculation in RAINFAT avoids complicated iterations required for load controlled fatigue and simplifies to a calculation of

$$\frac{\Delta \epsilon - \epsilon e}{2} = A (2N)^b$$

for each RAINFLOW strain cycle. Changing mean strain effects during the strain cycles have been evaluated in detail. Mean strain effects decrease the FA, BA, and CA damages. It increases the RRSA damage and was considered there. Simple Miners rule is applied to sum the damage of all RAINFLOW strain cycles of the striping window as shown in Figure 3-101. Each reactor cycle is being divided into 10 intervals, and the damage for each of the intervals

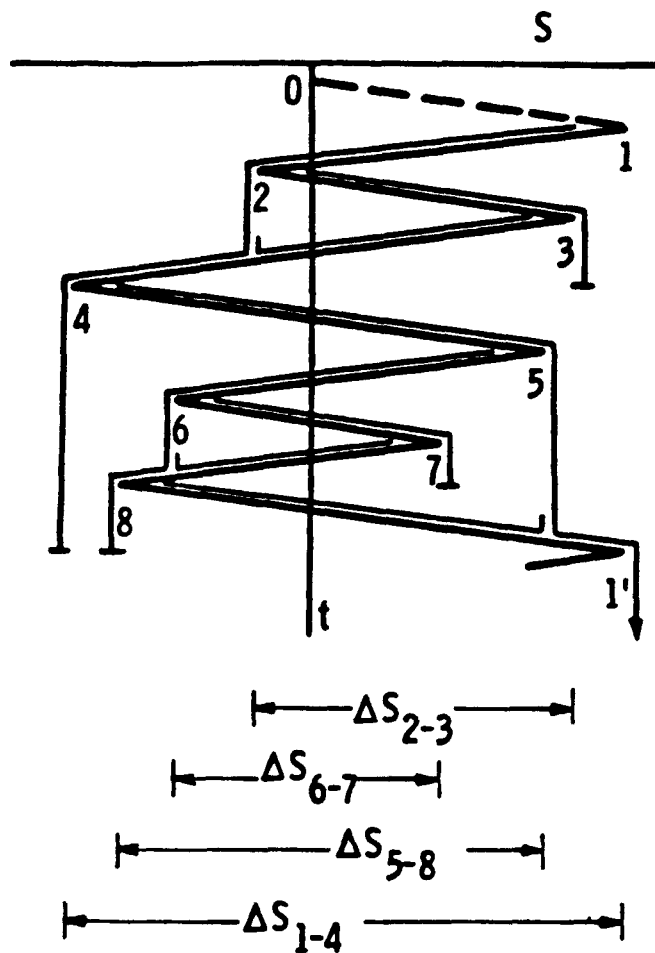
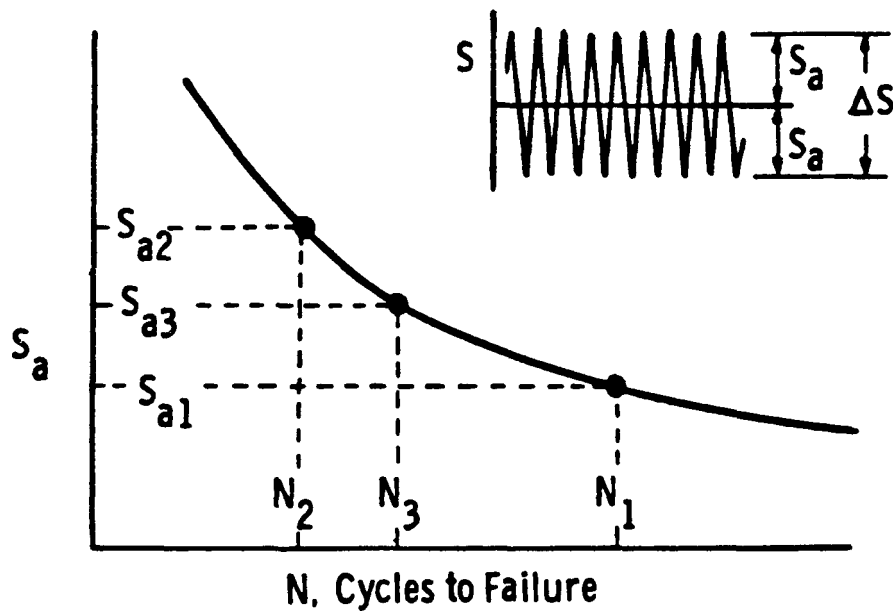


Figure 3-100
Rain-Flow Cycle Counting Method



Failure Predicted When:

$$\frac{n_1}{N_1} + \frac{n_2}{N_2} + \frac{n_3}{N_3} + \dots = \sum \left(\frac{n_i}{N_i} \right) = 1$$

Figure 3-101

Use of a constant amplitude S-N curve with the Miner rule to predict fatigue life for variable amplitude loading

is being calculated for the striping potentials at the beginning of each interval. Figure 3-82 shows a variable temperature striping potential time history. Finally, the damage is summed over all intervals.

The striping analysis method is described in more detail in Appendix A, including the analysis steps, input parameters, applied parameter sensitivity and the method to select the design analysis conditions for each component.

3.12.7 Analysis Results

The coolant outlet temperature differences of adjacent core assemblies were systematically searched to define locations of worst striping on the core map. Through analysis it was determined for all core assembly outlet nozzles that the striping fatigue damage was less than the limit of 0.1 for the first core and equilibrium core design lifetimes. The assembly combinations with the worst striping involved corner blanket assemblies 302 and 306 in the inner blanket row shown in Figure 3-81 and combinations of fuel assemblies, inner blanket assemblies and control assemblies. The maximum gradients between assemblies were iteratively reduced by increasing the flow to hot assemblies and decreasing the flow to cold assemblies with the available flow and within the limits of other design constraints (lifetime, transient temperature, exit temperature, number of orifice zones). The allowable interassembly striping gradients were defined on a parametric plot Figure 3-102 for beginning of life conditions. This figure presents a plot yielding the maximum allowable gradient depending on the lifetime history. The abscissa provides the scale of the maximum allowable gradient at BOL; a family of ascending diagonal lines of constant decreasing gradients during the lifetime; and a family of descending diagonal lines A, B and C representing lines of equal assembly lifetime. Loci below the two characteristic curves of decreasing gradient and lifetime are acceptable, conditions above these lines are not acceptable; for example, for L_{mt} , A_a maximum gradient of 236°F is acceptable throughout the entire lifetime; a maximum gradient of 247°F is acceptable at BOL provided that the gradient decreases by 80°F at EOL. The lengthy derivation of this chart has been omitted here, along with an explanation of their computerized use in the orificing procedure.

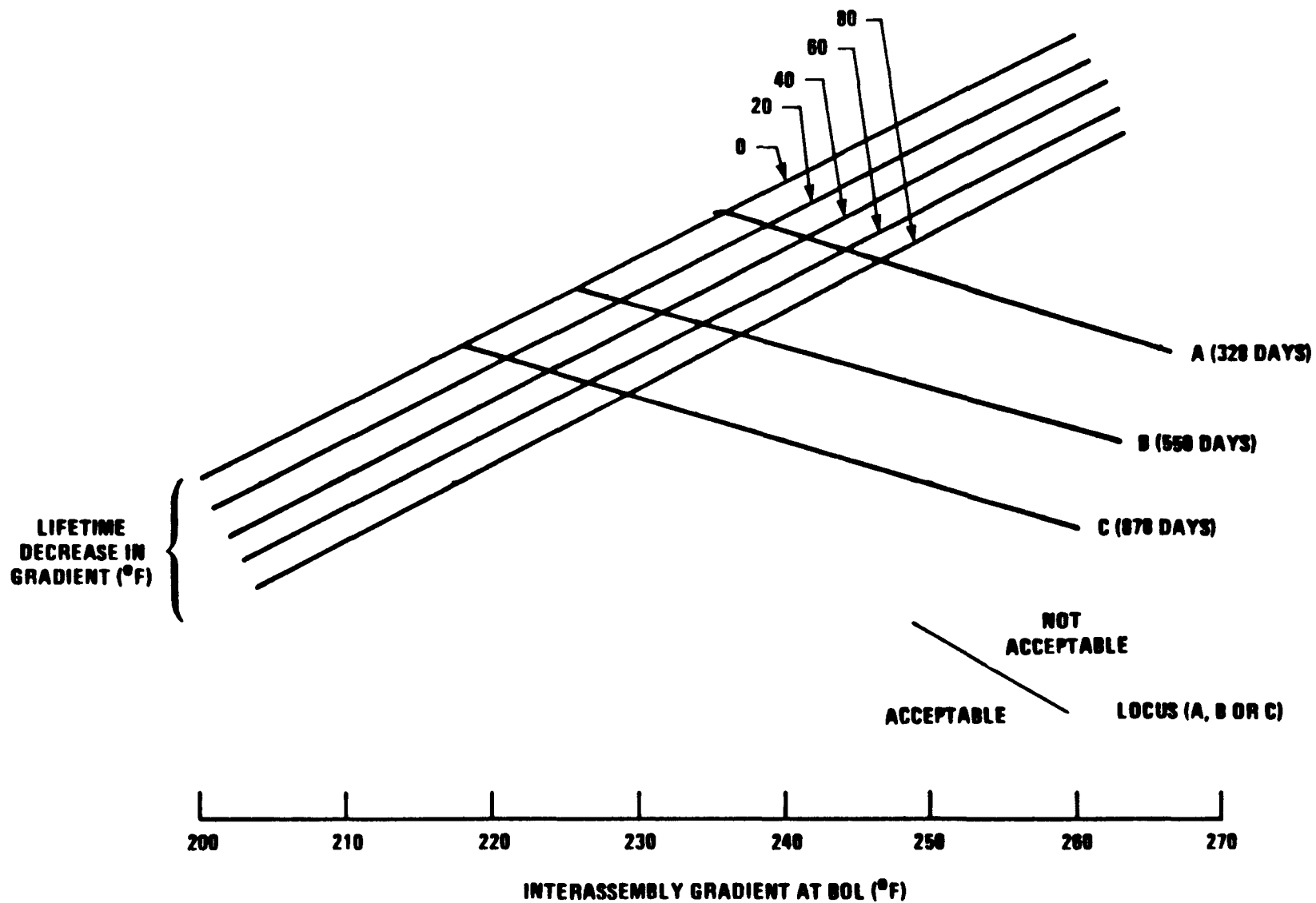


Figure 3-102 Allowable Gradient (Striping) Correlations

The acceptability of assembly temperature gradients determined in the orificing procedure was verified in detailed calculations as described in Appendix A. The results of the detailed analysis are described in Table 3-57 for 14 assembly combinations. The numbers in the first row of the table refer to assembly combinations in the core layout shown on Figure 3-81. The cycle temperature combinations give the beginning and end of cycle temperatures. The next four rows the temperature decrease during each cycle. The last three rows list the calculated damage for the first two cycles of 328 days, the fuel and inner blanket assembly design lifetime. The damage during cycles 3 and 4 during the next 550 days is often significantly lower than during the two start-up cycles. The total damage accumulated over four cycles, the inner row of radial blanket assembly design lifetime is listed in the last row.

All fuel assemblies meet the design criteria during the start-up cycles 1 & 2 and the equilibrium cycles 3 & 4. All blanket assemblies meet the 4 cycle lifetime requirement.

Table 3-58 lists the same type of data for the primary control assembly evaluated with fluid shipping data from Test PC5. This test had a maximum peak to peak striping of 84%, which was significantly higher than the fuel and blanket assembly nozzle striping due to local recirculation in the nozzle. The damage was calculated with two fatigue equations referenced in Section 3.12.5; the Westinghouse derived equation and, for conservatism, an equation derived from load controlled test data with a lower endurance limit, the EG&G equation. The calculated damage is less than 0.1 with both equations.

For the RRS assemblies with a striping value of 47%, calculated from the IRFM striping test results, the damage after 30 years is negligible. The damage evaluation of the secondary control assemblies was not completed. The results were not expected to differ from the primary control assembly results.

TABLE 3-57
THERMAL STRIPING - RESULTS

FUEL AND BLANKET ASSEMBLY NOZZLES

ASSEMBLY COMB+	10/74	15/74	16/74	31/74	1/72	4/72	17/302
CYCLE 1	207/221	230/236	225/229	--	231/224	242/232	257/217
CYCLE 2	216/232	212/223	203/213	221/243	233/218	234/217	183/139
CYCLE 3	195/228	222/237	220/231	--	228/216	238/216	222/250
CYCLE 4	192/206	174/193	164/183	225/252	236/213	240/213	238/86
CYCLE 1 ΔT	14	6	4	--	7	10	40
CYCLE 2 ΔT	16	11	10	22	15	17	44
CYCLE 3 ΔT	33	15	11	--	12	17	72
CYCLE 4 ΔT	24	19	19	27	23	27	52
CYCLE 1&2							
DAMAGE 328 DAYS	.020	.031	.015	.0395	.0335	.0514	.0416
CYCLE 3&4							
DAMAGE 550 DAYS	.009	.038	.026	.0894	.0450	.0731	.0025
CYCLE 1 TO 4							
DAMAGE 878 DAYS	.029	.069	.041	.1284	.0785	.1245	.0441
ASSEMBLY COMB	24/3076*	26/304*	8/75	9/75	13/75	14/75*	8/39*
CYCLE 1	244/206	238/191	236/215	233/213	212/184	212/184	258/180
CYCLE 2	185/141	186/128	208/173	232/193	182/138	190/144	186/74
CYCLE 3	225/153	208/121	225/190	224/189	219/167	218/167	2633/140
CYCLE 4	149/96	123/60	199/158	198/158	170/123	170/123	138/14
CYCLE 1 ΔT	38	47	21	20	28	28	78
CYCLE 2 ΔT	44	58	35	39	44	46	112
CYCLE 3 ΔT	72	87	35	35	52	51	123
CYCLE 4 ΔT	53	63	41	40	47	47	124
DAMAGE 328 DAYS	.0169	.0085	.0135	.0189	.0004	.0004	.0261
DAMAGE 550 DAYS	.0036	.0002	.0059	.0053	.0020	.0017	.0554
DAMAGE 878 DAYS	.0205	.0087	.0194	.0242	.0024	.0021	.0815

+ FA & CA HAVE TWO DIGIT NUMBERS

BA HAVE THREE DIGIT NUMBERS

* REDUCED IN THE FINAL ORIFICING

TABLE 3-58
PRIMARY CONTROL
ASSEMBLY COMBINATION 31/75

Test	PC5*	PC5**
PERCENT STRIPING	84%	84%
CYCLE 2	221/243	221/243
CYCLE 4	225/252	225/252
NO MEAN STRESS		
DAMAGE 200 DAYS	1.44 E-6	3.83 ER-3
DAMAGE 275 DAYS	2.16 E-4	1.48 E-2
MEAN STRESS		
DAMAGE 200 DAYS	1.0 E-5	5.2 E-3
DAMAGE 275 DAYS	4.7 E-4	2.0 E-2

* W EQUATION

$$\Delta\Sigma/2 = .056558 (2N)^{-.328428} + .001222$$

**EG&G EQUATION

$$\Delta\Sigma/2 = .337850 (\text{LOGN})^{-3.63} + .00103$$

3.12.8 Lessons Learned

Thermal striping fatigue damage has been recognized as an inherent potential failure mechanism of all liquid metal cooled reactor systems. It can be caused by leakage flow, due to coolant temperature differences between assemblies, and flow stratification. Only the core assembly outlet nozzles were found to accumulate significant damage. Striping of core assembly duct walls due to mixing of hot and cold interstitial flow between ducts was found to be insignificant. Large fluid temperature differences in small gaps are mitigated by a large drop of the striping in the fluid film as shown in Figure 3-88 and due to the small heat capacity of the fluid in a small gap. Striping was calculated to decrease even faster than shown in Figure 3-89.

One of the first reactions of a designer to a potential fatigue failure problem is the selection of a material with improved fatigue strength. Alloy 718 was selected as an optional high strength material and outlet nozzle designs were completed. However, difficulties in fastening or welding the nozzles to the stainless steel duct and the need to accommodate differential thermal expansion between the two materials made this design an undesirable option. Other options to increase the fatigue strength are:

- o A fine material grain size. At a cost of \$30 per nozzle the required grain size number was increased from 3 to 5.
- o Shot peening of the surface. At TSTF fatigue specimen with different shot peen treatments was fabricated near the end of the program but not tested.

Striping in the fluid can be minimized:

- o By increasing the fluid turbulent intensity at the cost of a higher pressure drop (for example by inducing flow rotation to increase mixing in chemical processes).
- o Through controlled flow separation in small eddies (to mitigate flow induced vibrations).
- o By avoiding shear flow of fluid with large temperature differences, and
- o By avoiding dead space recirculation areas.

Striping can be avoided by design:

- o Core orificing reduces the core outlet temperature gradients to acceptable levels.
- o Effective options not selected for CRBRP design were blanket shuffling and fuel assembly shuffling. These options have the additional benefit of improved fuel utilization, a flatter reactor outlet temperature profile and increased efficiency through improved coolant utilization. A resulting penalty is the increased down time required to shuffle assemblies.
- o Design configurations and features to minimize striping can be incorporated at the conceptual and preliminary stage of a design.

At the time when the severity of the striping problem was determined it was too late to incorporate significant design changes to mitigate striping in CRBRP. The problem had to be solved through tedious analysis. Simplified approaches were insufficient to demonstrate design adequacy and data to develop a statistical design procedure using striping signal power spectral density characteristics were not available. Such methods to evaluate a design are available [15,16] and were applied. However, their validity and accuracy was not compared with more detailed calculations. Such methods could be very useful in future liquid metal systems designs and could avoid flow testing, the most expensive way to verify the absence of thermal fluid striping.

3.12.9 Summary

The thermal striping fatigue damage of core assembly components was evaluated. Only the core assembly outlet nozzles were found to accumulate significant fatigue damage due to mixing and impinging flows with large temperature differences. All components met the design criteria.

3.12.10 References

1. Fuchs, H. O. and R. I. Stephens, "Metal Fatigue in Engineering," John Wiley & Sons, 1980.
2. ORNL/MSP/13-83-1, "Mechanical Properties Design Data Program, Semiannual Progress Report for Period Ending January 31, 1983," ORNL Report, dated April 1983.
3. WARD-D-0324, "Final Predicted Steady State Thermal Hydraulic Performance of the CRBRP Fuel Blanket Assemblies," dated March 1985.
4. Engel, F. C. and R. A. Markley, "Interstitial Flow Striping in LMFBR Cores," to be presented at the National Heat Transfer Conference in Niagara Falls, August 1984, No. 236, Volume 80, AICE, 345 East 47 St. NY, NY.
5. Kasza, K. E. and T. P. Babis, "Thermal Hydraulic Design Guide for an Advanced Design Thermal Mixer," Report ANL-CT-79-4, dated October 1978.
6. EPRI NP-2672, "LMFBR Thermal-Striping Evaluation," Report dated October 1982.
7. Bitts, C., C. Boorman, N. Sheriff, "Thermal Striping in Liquid Metal Cooled Fast Breeder Reactors," Paper presented at the ANS Meeting in Santa Barbara, CA, October 1983.
8. Garkisch, H. D., "Evaluation of Sodium Thermal Striping Test Results," Technical Review, Clinch River Breeder Reactor Plant, Spring 1983 Reactor Research & Development; U.S. Department of Energy.
9. Snyder, P. H., "A Review of Literature of Unsteady Forced Convection Heat Transfer With Application to LMFBR Thermal Striping," Westinghouse Report TID-28937, West Lafayette, Indiana.
10. Jaske, C. E. and N. D. Frey, "Long Life Fatigue of Type 316 Stainless Steel at Temperatures Up to 593°C," Transactions at the ASME, Journal of Engineering Materials and Technology, Vol. 104, April 1982.
11. Jaske, C. E., J. S. Perrin, H. Mindlin, Low Cycle Fatigue of Reactor Structural Materials, Reactor Technology, Volume 15, No. 3, Fall 1972.
12. Deleted
13. Dowling, W. E., "Fatigue Life Predictions for Complex Load Versus Time Histories," Decade of Progress in Pressure Vessel and Piping, October 31, 1979.
14. Brose, W. R., "Fatigue Life Predictions for a Notched Plate with Analysis of Mean Stress and Overstrain Properties," 1977.

15. Wirshing, P. H. and M. C. Light, "Fatigue Under Wide Band Random Stresses," Journal of the Structural Division ASCE, Vol. 106, July 1980, pp. 1593-1607.
16. Wirshing, P. H. and Shekata, A. M., "Fatigue Under Wide Band Random Stresses Using the RAINFLOW Method," Journal of Engineering Materials and Technology, Vol. 99, No. 3, American Society of Mechanical Engineers, July 1977, pp. 205-211.

3.13 In-Vessel Flow Management

3.13.1 Summary of Functions and Description

The key function of the in-vessel flow management is to properly distribute the primary system flow to the in-vessel components, especially the power producing core elements. The flow must be allocated to the various components in order to provide a thermofluids environment conducive to guarantee their integrity, satisfactory performance and attainment of design objectives. Sophisticated design procedures and analytical methods which were developed during the CRBR design to optimize the coolant flow distribution will be discussed in this report.

Figure 3-103 graphically summarizes the in-vessel flow paths, their respective flow allocation and the basis for its determination. This basis can be defined as the satisfaction of the functional design requirements imposed on the components; its elaboration has been one of the most significant accomplishments of the CRBR core thermal-hydraulic design and is of lasting value to the design of liquid metal reactors. The chronological evolution and the design philosophy adopted in determining the flow allocation to the in-vessel components will therefore be discussed in detail in the following. Since the greatest percentage of flow (i.e., 94%) is allocated to the fuel and blanket assemblies, most of the analyses and design concentrated in optimizing their flow allocation; therefore most of this report will be devoted to discussion of the fuel and blanket flow management. The impression should not be conveyed, however, that design of the other in-vessel components was "shortchanged"; rather, detailed design and analyses were performed. The key difference was that for the non-power producing components a sophisticated optimization procedure was not warranted.

3.13.2 Evolution of Design

3.13.2.1 Fuel and Blanket Assemblies Flow Allocation

The major evolution in the determination of the flow allocation (orificing) to the fuel and blanket assemblies was due to:

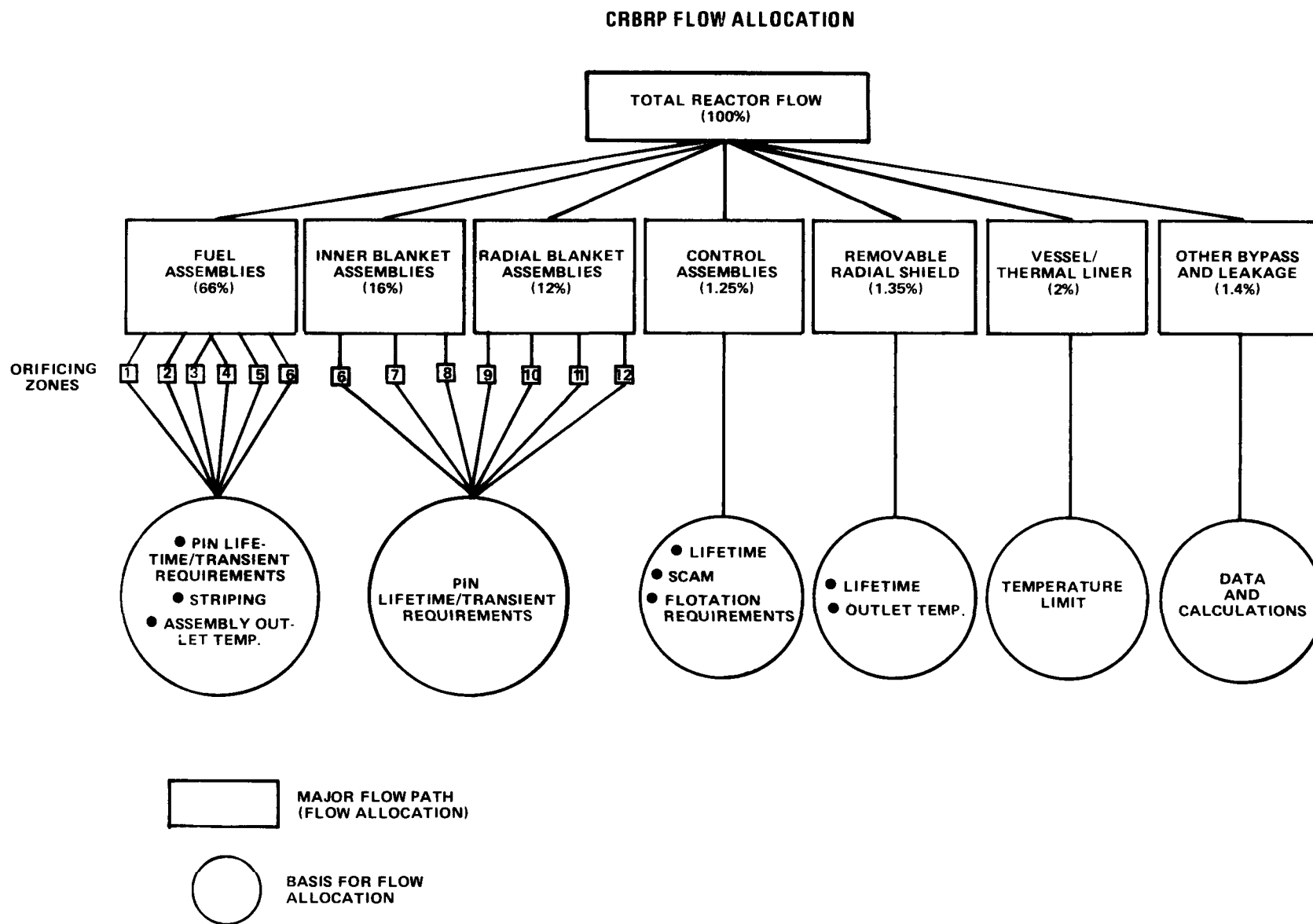


Figure 3-103 CRBR In-Vessel Flow Paths: Flow Allocation and its Basis.

A. Change from homogeneous to heterogeneous core design.

B. Optimization of the orificing procedure.

The fuel and blanket orificing procedure adopted in the CRBR design was based on the philosophy that the design constraints should be identified and satisfied a priority. By determining a flow allocation which satisfies such design constraints, major design iterations and the "trial and error" procedure which is typical of many preliminary designs can be eliminated. Ideally, if all the design constraints are accounted for and satisfied, no iterations are necessary and a "one pass design" has been achieved. The evolution and increased sophistication in the fuel and blanket orificing can therefore be characterized by what design constraints were considered and how they were factored in the flow determination.

3.13.2.1.1 Conceptual Designs

The first criterion was equalization of the assemblies outlet temperature, which at that time (1970-71) was the state of the art. The procedure was very simple; a few hours of hand calculations were all that was needed to orifice the core. However it did not address any design concern (or "constraint") and it provided no guidance in allocating flow to the fuel versus the blanket. Equalization of both the fuel and blanket outlet temperatures to a single value was of course impossible due to the very large difference in produced power. This method was abandoned very early and no documentation remains.

The next step was to allocate flow to equalize the maximum cladding temperature occurring through the assemblies' lifetime. This was generally beginning of life for the fuel assemblies (in some locations the maximum occurred at the beginning of the second cycle) and always end of life for the blanket assemblies. In this phase the hot rod in the assembly was recognized as the limiting component and the maximum cladding temperature as the operating limit. A quantum improvement was obtained in respect to the previous procedure since, for the first time, design limiting performance

parameters were addressed. After feasibility calculations this approach was modified and improved as discussed in the next section.

3.13.2.1.2 Preliminary Designs

In the early phase of the CRBR project, attainment of burnup and lifetime objectives was the overriding consideration: the initial burnup goal for the fuel assemblies was 80,000 MWd/ton, with an ultimate goal of 150,000 MWd/ton, while the design lifetime of the blanket assemblies was 6 years. To guarantee satisfaction of the burnup/lifetime objectives, the total cumulative cladding strain and/or cladding damage function had to be within specified allowable limits. Principal contributors to the cladding cumulative strain and damage are the cladding temperature, fission gas pressure and accumulated fluence. Since the fission gas pressure increases during life and the strain/fluence relationship is not linear (the strain increases rapidly when the burnup approaches end-of-life conditions), the end-of-life temperatures were assumed as a close approximation of the effective temperature which determines the cladding strain integrated over the assembly life.

At this point the criterion was therefore equalization of the maximum cladding temperature at end-of-life and the constraint satisfied was attainment of the burnup objectives as represented by the cumulative strain or damage. A "temperature merit number" was calculated for each assembly, to rank them relatively in terms of the flow to be allocated to satisfy the above constraint. The merit number was an approximate evaluation of the temperature difference between the hot rod nominal cladding midwall and the coolant inlet. Thus, it provided an indication of the relative thermal behavior of the various assemblies and guidance in grouping assemblies of similar thermal behavior in the same orificing zones.

Several alternate configurations were examined and finally the CRBR homogeneous core was orificed with 5 flow orificing zones in the fuel assemblies and 4 in the radial blanket. The maximum number of discriminators for the fuel assemblies was 8 since the inner fuel enrichment zone had three

orificing zones and the outer enrichment zone had five orificing zones. Another important feature of this design was that the "driver" assemblies (i.e., the assemblies determining the flow to be allocated in each zone) were selected to maximize the flow to assemblies in the inner fuel enrichment zone, which experienced a higher burnup and had higher beginning-of-life temperature than outer core assemblies. Thus, this was a first attempt to lifetime strain equalization which was then fully exploited in the next phase.

The aforementioned orificing procedure and design for the homogeneous core are documented in Reference 1.

Following adoption of the heterogeneous core design a revised core orificing was necessary. In addition, the orificing design procedure was greatly improved by expanding the number of design constraints to be considered, by introducing an optimization procedure and by developing a computer code to perform orificing optimization calculations.

The design constraints to be satisfied were:

- A. Maximum cladding temperature must be compatible with lifetime and burnup objectives, which can be expressed in terms of maximum allowable inelastic cladding strain and cladding cumulative damage function (CDF).
- B. Maximum coolant temperature conditions must be such as to assure, with adequate margin, that no sodium boiling occurs during the worst emergency transient (e.g., the three-loop natural circulation event), accounting for uncertainties at the 3σ level of confidence.
- C. Maximum assemblies mixed mean outlet temperature and radial temperature gradient at the assemblies exit must be compatible with upper internals structure (UIS) limitations.
- D. Maximum of eight discrimination zones (fuel plus inner blanket) are allowed.

- E. Flow allocation to fuel, inner blanket and radial blanket assemblies must not exceed 94.0% of the total reactor flow to account for cooling requirements of other reactor components.

To put the lifetime/burnup and transient temperature constraints on the same quantitative basis, the concept of equivalent limiting temperature was employed. The equivalent limiting temperature was defined as that cladding temperature at a specified radial position and time in life which must not be exceeded in order to satisfy the considered constraint. Three equivalent limiting temperatures were defined to represent the lifetime/burnup and transient constraints, i.e., SELT, DELT and TELT. They are defined as the end-of-life maximum cladding ID temperatures for Plant Expected Operating Conditions (PEOC), considering uncertainty factors at the 2σ level of confidence, such that accounting for the assembly temperature/pressure lifetime history, the limiting value of the inelastic cladding strain (SELT), or cumulative damage function (DELT), or worst time-in-life transient coolant temperature (TELT) is not exceeded. As it appears from the above definition, the equivalent limiting temperatures were calculated for each assembly. In fact, all the various assemblies have individually different lifetime histories of cladding temperature and fission gas pressure, and therefore, the limiting equivalent temperatures are necessarily different from assembly-to-assembly to stay within a constraint common to all assemblies. Calculations were performed for PEOC, which are the conditions where the CRBRP is expected to operate on a probabilistic basis and the conditions used in the design of replaceable components such as the core assemblies. The other set of plant conditions used in thermofluids analyses are plant thermal-hydraulic design values (THDV), which are the plant rated conditions and, being more conservative than plant expected conditions, were used in the design of permanent components and in transient and safety analyses. Orificing of core assemblies was performed on the basis of plant expected conditions.

Both first and second core conditions were considered in defining the core orificing, therefore the SELT, DELT and TELT were calculated for both cores. In the case of the radial blanket assemblies, where the lifetime spans both cores, only one set of limiting temperatures was calculated. The assemblies

minimum flow in the first and second core necessary to satisfy the most restrictive of the limiting conditions was calculated for each assembly. Subsequently, the various assemblies were grouped in zones and the orificing arrangement was selected such that the flow allocated to each assembly was at least equal to the larger of the flow requirements in first and second core. This assured meeting of the lifetime and transient constraints for both cores. If excess flow existed, this was to be allocated among the fuel assemblies to minimize and equalize the assemblies exit temperature and temperature gradient. This was indeed the case, and approximately 1% of reactor flow remained after grouping of the core assemblies into orificing zones and was distributed to the fuel assemblies. The OCTOPUS code was developed to perform these calculations, i.e., determination of the assemblies minimum flow to satisfy the lifetime/burnup and transient constraints, grouping of the assemblies and selection of the orificing arrangement, and optimization of the assemblies flow by redistributing the excess flow. The final CRBR orificing featured 12 orificing zone: 5 in the fuel, 2 in the inner blanket, 1 for the six alternating assemblies (thus complying with the 8 discriminators limit) and 4 in the radial blanket.

A detailed discussion and documentation of this orificing design for the heterogeneous core is reported in Reference 2.

This design for the first time introduced the following key elements in the orificing philosophy of the CRBR core:

- o identification of all possible constraints and their quantitative evaluation
- o flow allocation to satisfy the most restrictive constraint
- o consideration of all core assemblies, rather than a few representatives (drivers)
- o use of a sophisticated optimization code

The final design, to be described in the next section, therefore, rather than representing a drastic change as it had been in the past, modified and improved upon this concept.

Two weaknesses existed in the orificing procedure discussed above:

- A. The exit temperature and temperature gradient constraint was not dealt with similarly to the other constraints, i.e., it was satisfied through the optimization process if excess flow existed, rather than directly.
- B. The constraints were expressed in terms of temperatures, from which flows had to be derived; use of limiting flows rather than temperatures would have been more efficient.

3.13.2.1.3 Final Design

In the final CRBR design, reorificing of the core was necessary as the best option to accommodate thermal striping at the fuel and blanket nozzles and "near field" internals. The aforementioned weaknesses were also eliminated during this redesign. The constraints to be satisfied in the final design were:

- A. The rod cladding temperature during anticipated steady state and transient conditions must be compatible with lifetime and burnup objectives; i.e., the cladding temperature throughout the assembly lifetime must be within certain limits to assure that premature failures do not occur.
- B. Maximum coolant temperature conditions must assure, with adequate margin, that no sodium boiling occurs in the hottest channel during the worst transients; similarly, the maximum cladding temperature must be limited to prevent transient failure.
- C. Maximum assemblies exit temperature must be compatible with upper internals structure (UIS) limitations.

- D. Maximum exit temperature gradient between adjacent assemblies must be limited to prevent thermal striping failure.
- E. Maximum of eight discrimination zones (fuel plus inner blanket) are allowed.
- F. Flow allocation to fuel, inner and radial blanket assemblies must not exceed 94% of the total reactor flow to account for cooling requirements of other reactor components.

A comparison of these constraints with the previous ones in Section 3.13.2.1.2 shows: (1) that thermal striping was introduced as an explicit constraint and (2) that the transient constraint was extended to cover transient cladding failure in addition to coolant boiling. The other constraints (lifetime/burnup, assemblies exit temperature, discriminators and cooling requirement of other components) remained the same. A much more detailed analysis was performed to quantitatively translate the various constraints on an equal basis. First the conditions applying to the various constraints were determined:

- o Constraint 1 (lifetime) depends on the hot rod cladding temperature at Plant Expected Operating Conditions (PEOC), 0σ uncertainties and circumferentially averaged (i.e., no wire wrap hot spot factor considered) since fuel cladding mechanical interaction (FCMI) was the life limiting mechanism.
- o Constraint 2 (transient) depends on the hot rod coolant (or cladding) temperature at Thermal Hydraulic Design Value (THDV) conditions and 3σ uncertainties (hot spot under the wire wrap).
- o Constraint 3 (UIS) depends on the assembly exit temperature at THDV conditions and 2σ long term uncertainties.
- o Constraint 4 (striping) depends on the temperature gradient between two adjacent assemblies, PEOC conditions and $+2\sigma/-2\sigma$ uncertainties.

The four constraints thus address different parameters (coolant, rod cladding and outlet temperatures and exit gradient), plant conditions (PEOC, THDV), and level of uncertainties (0σ , 2σ , 3σ). The most immediate way of putting them on an equal basis was to calculate the flow necessary for each constraint to yield the limiting temperature. These were the limiting flows and the largest of the four is the flow which must be guaranteed in each assembly to simultaneously satisfy the four constraints.

Previously, only the first two constraints were specifically addressed and they were characterized by equivalent limiting temperatures; the assembly flow to be guaranteed was the one yielding the lowest of the limiting temperatures. Constraints 3 and 4 were addressed only by redistributing the excess flow, if any, remaining after the first two constraints had been satisfied. Thus, the procedure adopted in the final design was the most complete, since all four constraints were considered on an equal basis, as well as more relevant, since the parameter of concern, the required assembly flow, is calculated and compared.

Of course one potential problem is that when the assemblies' orificing is based on the limiting flow determined above and on a fixed number of orificing zones (Constraint 5), the total flow needed could be more than the 94% available (Constraint 6). In this case better definition of the limits, refined optimization analysis or just relaxing of the constraints is needed. It did indeed happen during the CRBR orificing process that the total orificed and allocated flow originally estimated was higher than available. This discrepancy was resolved through more detailed analyses and better definition of the limits. None of the constraints was relaxed.

The number of orificing zones selected was as before: 5 in the fuel, 2 in the inner blanket, 1 for the alternating assemblies and 4 in the radial blanket.

Figure 3-104 presents the CRBRP final design core orificing map. Table 3-59 presents the orificing zone flows for the first four cycles of lifetime.

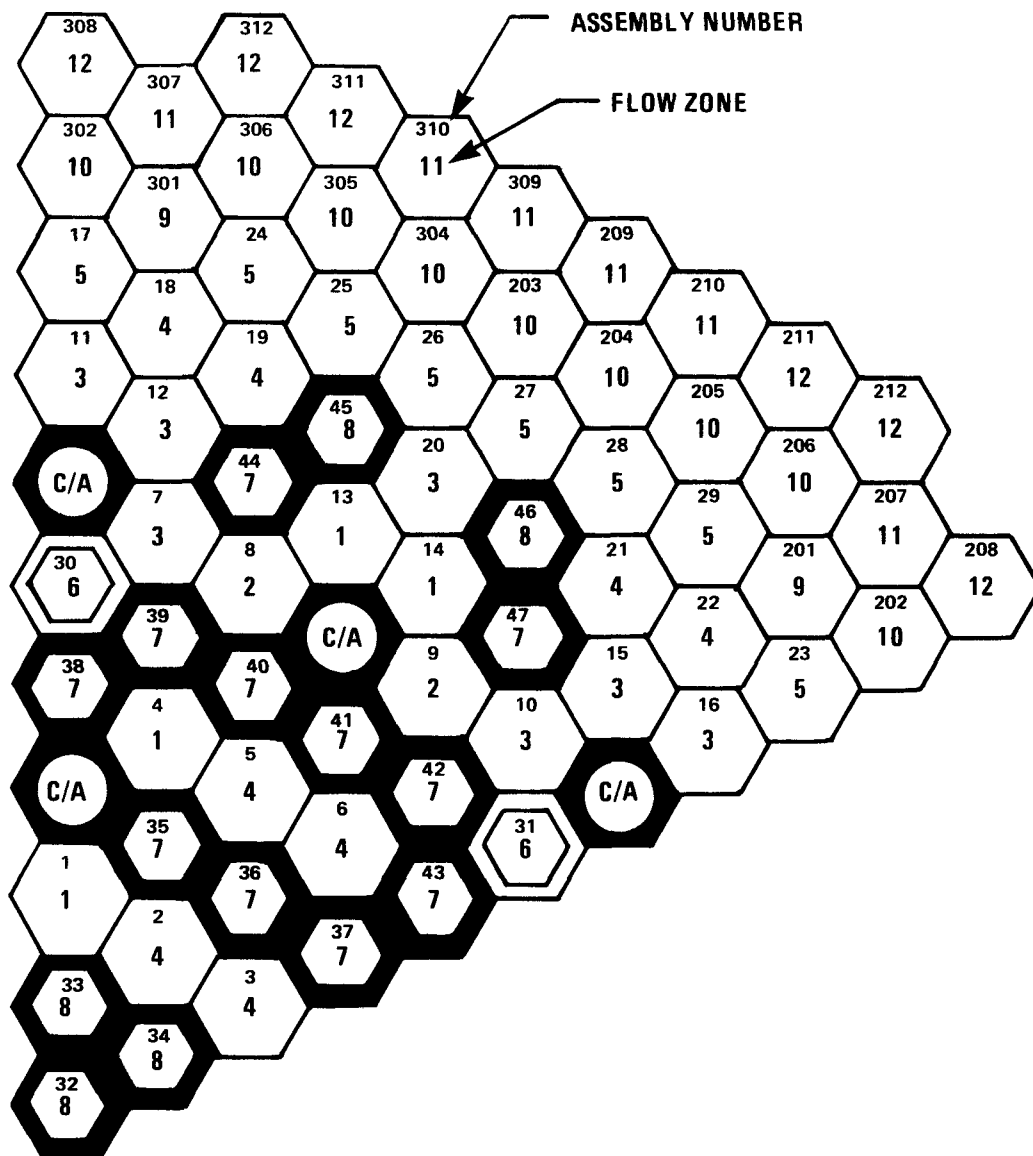


Figure 3-104 CRBRP Core Orificing Map.

TABLE 3-59
FUEL AND BLANKET ASSEMBLIES ORIFICING ZONES AVERAGE FLOWS

Orificing Zone	Type*	No. Assemblies	Zone Average Flow (PEOC, lb/hr)			
			Cycle 1	Cycle 2	Cycle 3	Cycle 4
1	F	21	226,770	225,750	220,760	218,780
2	F	12	201,000	199,960	203,100	200,860
3	F	36	206,740	205,760	198,400	196,610
4	F	45	201,000	199,960	193,150	191,530
5	F	42	180,280	179,470	166,070	164,750
6	F	6,3,0	--	205,760	--	190,120
6	IB	0,3,6	64,030	63,700	62,600	-
7	IB	57	76,720	76,290	92,240	91,310
8	IB	19	63,990	63,700	80,480	79,840
9	RB	12	68,070	67,770	67,900	67,360
10	RB	48	53,040	52,810	52,910	52,490
11	RB	36	37,290	37,130	37,200	36,910
12	RB	30	28,990	28,870	28,920	28,700
Total Reactor Flow ($\times 10^6$ lb/hr)			45.492	45.700	45.326	45.700

* F = Fuel Assembly
IB = Inner Blanket Assembly
RB = Radial Blanket Assembly

Finally, it should be clearly pointed out that the core orificing, even though quite critical and important, is not the final step in the core thermal-hydraulic design. While optimum orificing will provide an optimum design without further iterations, an approximate orificing will require further feedback and iterations to yield a viable design. In any case, it will be the detailed evaluation of the thermal-hydraulic performance parameters which will indicate if the design is adequate and objectives have been met. For CRBR, predicted performance parameters as evaluated by the structural designers were found indeed to be adequate and the orificing was optimum, i.e., no iteration was necessary.

Documentation and results of the final orificing study can be found in Reference 3.

3.13.2.2 Other Components Flow Allocation

As previously mentioned, determination of the flow allocation to the other in-vessel components has not gone through as many iterations as for the fuel and blanket assemblies.

The primary control assemblies' flow has been dictated by the conflicting requirements of satisfying lifetime and transient limitations (which require minimization of temperatures, hence maximization of flow) and scram insertion and avoidance of flotation (which require minimization of flow). Analyses performed to characterize the primary control assemblies flow allocation and predict their thermal-hydraulic performance are reported in Reference 4 for the homogeneous core design. Similar analyses had been completed for the heterogeneous design, but a final document had not been issued at the time of project termination. The flow allocation to the primary control assemblies in the homogeneous core was 1.6% of the total reactor flow, while in the heterogeneous core it was 1%. This significant reduction did not reflect dramatic changes in the basis for flow allocation, but rather the reduction in number of the primary control assemblies from 15 in the homogeneous to 9 in the heterogeneous design.

The flow allocated to the secondary control assemblies (SCA) was essentially dictated by maximum temperature limitations and was specified by the component designer. Most of the SCA flow is downflow which is then used as a source of vessel cooling. The assembly flow did not change significantly during the design: i.e., the total flow allocated to the SCAs was ~1% of the reactor flow (~0.3% upflow and 0.7% downflow) for the heterogeneous design which featured 6 secondary control assemblies; the flow allocated was ~0.7% for the homogeneous design where only 4 SCAs were featured.

The flow allocated to the radial shield assemblies was initially based on conservative maximum temperature requirements and was as high as 3% of the total reactor flow; however refinement of the analytical methods and realization of the thermal striping potential introduced by the cold radial shield assemblies led to a progressive decrease of their flow. The total flow allocated at the completion of the homogeneous core design was 1.5%. Further reduction was featured in the heterogeneous design in spite of the conversion of 6 radial blanket assemblies to radial shield. As reported in Figure 3-103 the allocated flow was 1.35%; however most recent calculations performed prior to CRBR termination put the actual radial shield allocation to 1.24% of the total reactor flow. Again it was the intent of the designer to minimize the amount of flow to the shield assemblies compatible with their lifetime, transient and structural requirements and thus lessen the potential for thermal striping.

The flow to the reactor vessel and thermal liner was established to maintain its temperature at a safe level: 2% of total reactor flow had been allocated. Actually some of the leakage flow will eventually end up cooling the reactor vessel; the 2% therefore represent the minimum guaranteed flow, while the actual flow will be higher. Calculation of the leakage through the various flow paths was performed for both the homogeneous and heterogeneous cores. Reference 5 documents for the homogeneous core the complete flow allocation to all the reactor flow paths: fuel, blanket, control, shield, bypass and leakage. Flow management calculations were completed for the heterogeneous core, but were not formally documented prior to CRBR termination.

Table 3-60 reports the flow allocation to the reactor components other than fuel and blanket assemblies. Figure 3-105 represents a complete schematic of the CRBR in-vessel flow paths.

3.13.3 Key Problems and Resolutions

There are no outstanding key problems. The purpose of the in-vessel flow management is to supply sufficient coolant to the various in-vessel components and subsequent performance predictions indicated that the CRBR flow distribution satisfied all the various design requirements. Throughout the CRBR project, a very large experimental program was conducted to support the analytical effort. Out of pile testing included proof-of-principle, simulated scale models and prototypic tests to support the design of the flow control components. Out of pile heat transfer testing in sodium of 61 rod fuel and blanket bundles was conducted. Detailed bundle hydraulics were studied in out of pile air flow tests of scaled fuel and blanket bundles. Extensive in-pile testing of fuel and blanket bundles was also conducted. Results of these extensive experimental testing were factored into the analytical evaluations.

3.13.4 Final System Status

3.13.4.1 Outstanding Changes

At the time of CRBR termination there were no outstanding changes to the in-vessel flow management.

3.13.4.2 Outstanding Activities to Complete Design

At the time of CRBR termination the in-vessel flow management design had been completed. A detailed flow distribution had been determined and design calculations for all the reactor components showed the adequacy of the coolant supply.

TABLE 3-60
FLOW ALLOCATION TO NON-FUELED COMPONENTS/FLOW PATHS

<u>Component/Flow Path</u>	<u>Flow Allocation (% of Total Reactor Flow)</u>
Primary Control Assemblies	0.98
Secondary Control Assemblies	
Upflow	0.27
Downflow	0.73
Removable Radial Shield	
Flow Path #8 (see Figure 3.13-3)	0.02
Flow Path #12	0.15
Flow Path #16	1.07
Total Shield	1.24
Core Support Structure	1.50
Total Leakage	0.89
Total Flow to Reactor Vessel	2.25

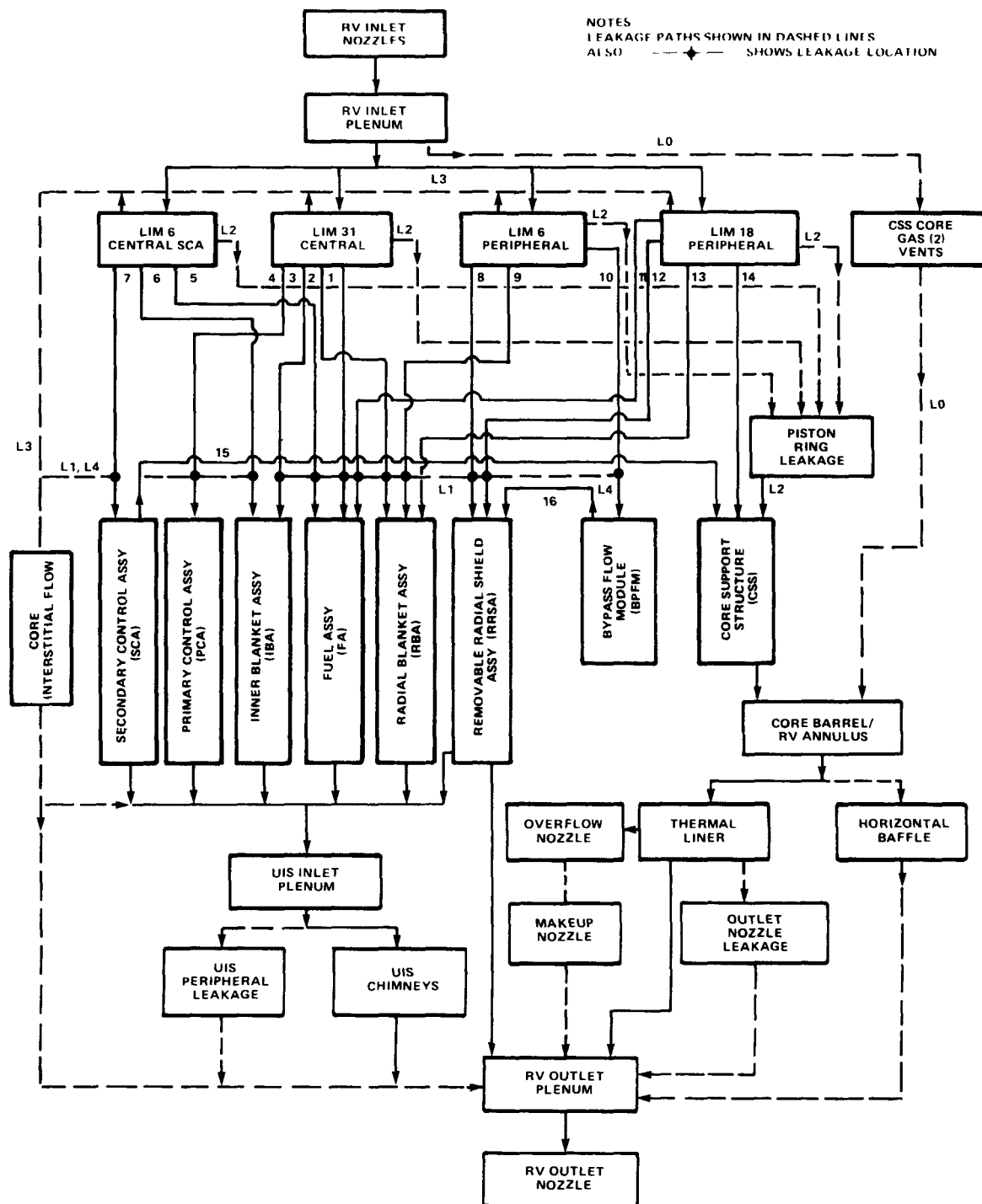


Figure 3-105 Complete Schematic of CRBR In-Vessel Flow Paths.

3.13.4.3 Outstanding Problems and Descriptions of Actions Needed

The only outstanding problem remaining at the time of CRBR termination was documentation of the design analyses performed. Documents having a bearing on the in-vessel flow management are:

- A. Performance predictions of the fuel and blanket assemblies. A topical report (Reference 3) has been completed and issued.
- B. Performance predictions of the control assemblies. Design calculations were completed, but documentation was only partially performed. Some analyses were reported in internal memos, others only in the form of calculations. A substantial effort would be required to provide this documentation.
- C. Thermal-hydraulic design of the radial shield assemblies. Internal memos were issued and therefore only minor effort would be required for issuance of a formal document.
- D. Overall flow management distribution report. Internal memos were issued. A medium effort would be required to integrate these memos in to a topical report for external distribution.

3.13.5 References

1. M. D. Carelli, et. al., "Predicted Preliminary Thermal-Hydraulic Performance of CRBRP Fuel and Blanket Assemblies," WARD-D-0054, January 1976.
2. M. D. Carelli and C. W. Bach, "Predicted Steady State Thermal-Hydraulic Performance of the Fuel and Blanket Assemblies in the CRBRP Heterogeneous Core," WARD-D-0210, Rev. 1, January 1979.
3. M. D. Carelli, et al., "Final Predicted Steady State Thermal-Hydraulic Performance of the CRBR Fuel and Blanket Assemblies," WARD-D-0324, March 1985.
4. D. Y. Nee, "Preliminary Thermal-Hydraulic Performance of CRBRP Primary Control Assemblies," CRBRP-ARD-0151, June 1977.
5. P. S. Marinkovich, "Clinch River Breeder Reactor Flow Management Distribution," WARD-D-0157, August 1976.

3.14 Reactor Internals Flow Induced Vibration

3.14.1 Summary of Functions and Descriptions

3.14.1.1 Function

The purpose of the Reactor Internals Flow Induced Vibration Program is to demonstrate the structural and functional adequacy of reactor internals components for flow induced vibrations and to delineate margins of safety. In addition, the Reactor Internals Flow Induced Vibration Program serves to demonstrate that the Safety Evaluation Report [1] guidelines, PSAR [2] commitments and intent of Regulatory Guide 1.20 [3] pertaining to flow induced vibration of reactor internals are satisfied.

3.14.1.2 Description

The Reactor Internals Flow Induced Vibration Program consists of the following elements:

- o Design to avoid flow induced vibration
- o Assessment of susceptibility of each reactor internals component to flow induced vibration
- o Modal analysis of each component and detailed analysis at critical locations
- o Testing of reduced and full scale models
- o Utilization of FFTF experience in defining the reactor internals flow induced vibration assessment program
- o Monitoring reactor internals responses during pre-operational testing

3.14.2 Evolution of Design

CRBRP components are designed to avoid excessive flow induced vibration. Heavy structures are utilized, where possible, to withstand vibratory forces. Generally, structures and components are designed to have natural frequencies as high as practical to avoid coincidence with known forcing mechanisms. Mechanical stresses that are caused by flow induced vibrations that are unavoidable must meet the fatigue requirements of the ASME Boiler and Pressure Vessel Code, Section III, and Code Case N-47. When combined with the effects of other operating conditions, maximum displacements are limited to assure proper functioning of the components.

3.14.2.1 Conceptual Design

A general survey of all CRBRP reactor internals was made. In this survey the following considerations were given to each component:

- o Type of possible component excitation - vortex shedding, turbulence, pump pulsation, jet impingement, jet reaction, gap modulation or other fluid elastic mechanisms.
- o Importance Index - based on the relative importance of the component and type of excitation.
- o Investigative priority - priority is based on degree of concern and whether the component can be evaluated by state-of-the-art methods.

Table 3-61 summarizes this survey and delineates the methods selected to address flow induced vibration questions in subsequent stages of the design.

3.14.2.2 Preliminary Design

Finite element analysis methods (ANSYS, WECAN) were used to compute the natural frequencies and mode shapes. Component responses were calculated assuming a fluid forcing mechanism and the resulting stress or component motion evaluated. The design approach used to minimize the occurrence of flow

induced vibration problems was to separate structural natural frequencies from expected excitation frequencies by a factor of two. Vortex shedding or parallel flow were generally assumed to be the excitation mechanisms in the analysis since those forcing functions can be readily characterized. Other flow excitation mechanisms were also of concern as indicated in Table 3-61, however, experimental means were relied on to assure flow induced vibration adequacy.

3.14.2.2.1 Testing

A comprehensive series of model tests, both reduced and full scale, were defined. These tests reproduced flow fields closely matching those which were expected in the CRBRP reactor. The planning and performance of the experimental programs was carried out in conjunction with the Argonne National Laboratory, Hanford Engineering Development Laboratory (HEDL) and the Westinghouse Research Laboratories.

The major models used in the experimental vibration assessment program were the following:

- o Inlet Plenum Feature Model (IPFM)
- o Bench Test Models
- o Selected Full Scale Models
- o Integral Reactor Feature Model (IRFM)

3.14.2.2.1.1 Inlet Plenum Feature Model

The IPFM modeled all hydrodynamically wetted surfaces in a full 360 degree (0.248 scale) sector of the inlet plenum and lower internal components. Seven of the 61 Lower Inlet Modules (LIM) were dynamically simulated with accelerometers mounted on four of these seven modules to monitor their vibrational response. The LIMs which were dynamically simulated (i.e., equal Strouhal number) are those located nearest to the reactor vessel inlet nozzles, because they are subjected to the highest cross-flow velocity and thus most susceptible to vibration. Test results indicated that the maximum

TABLE 3-61
FLOW VIBRATION OF CRBRP INTERNALS
GENERAL SURVEY

	<u>Reactor Internals Components</u>	<u>Type of Excitation</u>	<u>IMPORTANCE INDEX</u>			<u>Analysis</u>	<u>Methods Selected for Resolution</u>		<u>Bench Test</u>
			<u>High</u>	<u>Medium</u>	<u>Low</u>		<u>IRFM Testing</u>	<u>Full Scale Test</u>	
632	1. Entire Upper Internals Structure (Cross motion)	Vortex Shedding	-	-	X	X	X	-	-
		Jet Impingement	-	X	-	-	X	-	-
		Jet Reaction	-	X	-	-	X	-	-
		Turbulence	-	X	-	X	-	-	-
		Pump Pulsations	-	-	X	X	X	-	-
632	2. UIS Guide Tubes for Control Rods	Vortex Shedding	X	-	-	X	X	-	-
		Turbulence	X	-	X	X	X	-	-
		Pump Pulsations	-	-	X	X	X	-	-
		Gap Modulation	X	-	-	-	-	X	-
	3. UIS Chimneys	Vortex Shedding	X	-	-	X	X	-	-
		Turbulence	-	-	X	X	X	-	-
		Pump Pulsations	-	-	X	X	X	X	X
		Jet Reaction	-	-	X	-	-	X	X
	4. UIS Support Columns	Vortex Shedding	X	-	-	X	X	-	-
		Turbulence	-	-	X	X	X	-	-
		Pump Pulsations	-	-	X	X	X	-	-
	5. UIS Instrumentation Leads	Vortex Shedding	X	-	-	X	X	-	-
		Turbulence	X	-	-	X	X	-	-
		Pump Pulsations	-	-	X	X	X	-	-
	6. UIS Support Plates and Plate Liner	Vortex Shedding	-	-	X	-	X	-	-
		Turbulence	-	-	X	-	X	-	-

TABLE 3-61 (Continued)
FLOW VIBRATION OF CRBRP INTERNALS
GENERAL SURVEY

	<u>Reactor Internals Components</u>	<u>Type of Excitation</u>	<u>IMPORTANCE INDEX</u>				<u>Methods Selected for Resolution</u>		
			<u>High</u>	<u>Medium</u>	<u>Low</u>	<u>Analysis</u>	<u>IRFM Testing</u>	<u>Full Scale Test</u>	<u>Bench Test</u>
		Pump Pulsations	-	-	X	-	X	-	-
7.	UIS Instrumentation Posts	Vortex Shedding	X	-	-	-	X	-	X
		Turbulence	-	X	-	-	X	-	X
		Jet Impingement	-	X	-	-	X	-	X
8.	UIS Cylindrical Liners Sleeve	Vortex Shedding	-	-	X	-	-	-	X
		Turbulence	-	-	X	-	-	-	X
		"Base Motion"	-	-	X	-	-	-	X
9.	UIS IVTM Port Plug	Vortex Shedding	X	-	-	X	X	-	-
		Turbulence	-	-	X	X	X	-	-
		Pump Pulsations	-	-	X	X	X	-	-
		Gap Modulation	X	-	-	X	-	X	-
10.	Control Rod Drivelines	Vortex Shedding	X	-	-	X	X	X	-
		Turbulence	X	-	-	X	X	-	-
		Gap Modulation	-	-	-	-	-	X	-
11.	Core Support Structure Core Barrel	Turbulence	-	-	X	X	-	-	-
		Pump Pulsations	-	-	X	X	-	-	-
12	Core Support Structure Module Liners	Vortex Shedding	-	-	X	X	X	-	-
		Turbulence	-	-	X	X	X	-	-
		Pump Pulsation	-	-	X	X	X	-	-

TABLE 3-61 (Continued)
FLOW VIBRATION OF CRBRP INTERNALS
GENERAL SURVEY

Reactor Internals Components	Type of Excitation	IMPORTANCE INDEX				Methods Selected for Resolution		
		High	Medium	Low	Analysis	IRFM Testing	Full Scale Test	Bench Test
13. Horizontal Baffle	Vortex Shedding	-	-	X	X	-	-	-
	Turbulence	-	-	X	X	-	-	-
	Pump Pulsation	-	-	X	X	-	-	-
14. EVTM Guide Tube	Vortex Shedding	X	-	-	X	X	-	-
	Turbulence	-	-	X	X	-	-	-
	Pump Pulsation	-	-	X	X	-	-	-
15. Suppressor Plate Supporting Hangers	Vortex Shedding	X	-	-	X	X	-	-
	Turbulence	-	-	X	X	X	-	-
	Pump Pulsation	-	-	X	X	X	-	-
634 16. Suppressor Plate Segments	Vortex Shedding	-	-	X	X	X	-	-
	Turbulence	-	-	X	X	X	-	-
	Pump Pulsation	-	-	X	X	X	-	-
17. Liquid Level Monitor (LLM) Tubes and Support Members	Vortex Shedding	X	-	-	X	X	-	-
	Turbulence	-	-	X	-	X	-	-
	Pump Pulsation	-	-	X	-	-	-	-
18. Vessel Thermal Liner	Turbulence	-	-	X	X	X	-	-
	Pump Pulsation	-	-	X	X	X	-	-
19. Outlet Nozzle Liner	Turbulence	-	-	X	X	X	-	-
	Pump Pulsation	-	-	X	X	X	-	-
	Jet Reaction	-	-	X	X	X	-	-

prototypic vibrational amplitude of a LIM is 0.2 mils. This value is very low and was judged to be structurally acceptable. The tests also showed that dynamic coupling between the Core Support Structure and the LIMs is negligible.

3.14.2.2.1.2 Bench Tests

Based on component evaluations, an experimental program to evaluate the flow induced vibration characteristics of selected regions of the Upper Internals Structure (UIS) was established. Testing was performed by Argonne National Laboratory in a 1/3 scale facility which structurally and hydraulically simulated the instrumentation post and a chimney with a lower shroud tube of the UIS.

Test results indicated an extremely small vibrational response, less than 0.4 mils in bending. Testing of an outer chimney (chimney located above a blanket region) showed small vibration amplitudes and slight rattling between the chimney and its support. Testing of a central chimney with a shroud tube (chimney located above an active region of the core) indicated similar results. As a result, full scale testing was performed on the UIS chimney in order to provide an improved assessment of impacting.

3.14.2.2.1.3 Full Scale Model Tests [4,5]

Full scale tests were conducted on portions of the UIS where similarity criteria or Reynolds number similarity could not be satisfied by scale model testing. These full scale tests included the gap modulation flow induced vibration test of the control rod upper to lower shroud tube slip joint, the UIS chimney vibro impact test, and the IVTM port plug gap modulation flow induced vibration tests. The UIS chimney vibro-impact test was successful in that no impacting was detected. No self excited motion of the IVTM port plug was observed, only small random motions which were thought to be turbulence related.

3.14.2.2.1.4 Integral Reactor Flow Model (IRFM)

The outlet plenum region of CRBRP contains components which are suspended from the head and are comparatively slender in configuration. There are numerous close fitting gaps and complex flow paths. Consequently, a 1/4 scale flow and vibration model of this region was constructed. Since the design of the model preceded final design of the outlet plenum components, the testing was divided into two phases. The objectives of the first phase were to characterize the flow characteristics of the outlet plenum region and to obtain a preliminary survey of the flow induced vibration behavior of outlet plenum components. In Phase II, flow induced vibration data was obtained for the final design configuration of the outlet plenum components as well as components not modeled during Phase I.

The results of the Phase I tests can be summarized as follows [6]:

- o The measured responses of instrumented components were generally small and proportional to the flow rate over the full flow range.
- o There were no observed unstable dependencies upon flow rate nor abnormally high fluid excited forced components.
- o There were no observed occurrences of vortex shedding synchronous with component resonant frequency over the full flow range.

The vibrational characteristics of the UIS and instrumented outlet plenum components were essentially independent of core configuration, loop mode operation, and UIS/core alignment. Impacting was observed on the following components, with the location of impacting coinciding with gaps incorporated in the prototype design and scaled in IRFM:

- o UIS Keys/Core Former Ring

At 110% flow a maximum impacting of approximately 2.1 g's peak-to-peak was measured on an accelerometer mounted on the model core former ring

radially outward from the keyways. The cyclic rate of this impact was less than 1 cycle/sec. The corresponding g value in the plant is 0.43 g's.

- o UIS Upper Shroud Tube/Lower Shroud Tube

At 110% flow a maximum impacting of up to 3.5 g's peak-to-peak at a cyclic rate of 4 to 5 impacts/sec was detected. These measurements were recorded from a shroud tube containing a control rod driveline (CRD). Another shroud tube without a CRD exhibited impacting level of 3 g's peak-to-peak with a cyclic rate of 2 impacts/sec. These g levels when scaled to the reactor are 0.71 and 0.62 respectively. Based on these results, a full scale model test of the upper to lower shroud tube joint was performed for the final design configuration. As noted above, no vibrational problems were found.

- o Chimney/Spider/Lower Shroud Tube

This region was not directly instrumented for Phase 1 but responses on neighboring accelerometers indicated impacting was probable. The results of the full scale chimney test are described above.

- o UIS Column/Closure Head

Indications of impacting were detected with maximum levels of response less than 1 g peak to-peak and a cyclic rate less than 1 impact/sec. The corresponding g value for the plant is 0.21 g's.

- o Control Rod Driveline

The control rod driveline response was acceptable at 100% and higher fluid velocities. Impacting at the dashpot/piston interface was infrequent.

Results from the Phase II testing are as follows [7]:

The measured responses of the Phase II components were generally small.

The thermal liner, outlet nozzle liner, and the Liquid Level Monitor Port Plug responses exhibited no unstable flow rate dependency nor excessively high fluid excited responses over the entire range of flow rates tested. Suppressor plate responses were generally consistent and small over the range of test flow rates. The measured displacement amplitudes were less than 0.5 mils rms.

The gross motion of the UIS during simulated refueling conditions (UIS raised to remove keys from the core former structure) was generally of low amplitude and erratic. The dominant measured lateral frequency was 12 Hz with calculated displacements on the order of 0.1 to 0.2 mils rms. Infrequent large amplitude responses were recorded. The maximum peak model displacement at the lower end of the UIS during these large amplitude responses was 16 mils. The motion was very sporadic and random occurring once every two to five seconds on the average.

3.14.2.3 Final Design

The unresolved flow induced vibration questions remaining from Phase II were impacting at the UIS-core former keyway gaps, potential for impacting at the UIS thermal liner plates and the prototypicality of the restraint of the UIS columns at the head. These issues were to be resolved in a final phase of testing designated as Phase IIE [8]. In preparation for this test phase, two chimneys were modeled to dynamically simulate the prototype. In addition four accelerometers were mounted in the IRFM at positions corresponding to the instrumented locations in the prototype UIS. It was expected that the correspondence of these four accelerometers would aid in the interpretation of data during pre-operational testing.

Preparations for Phase IIE testing were in progress at the time of CRBRP termination. As a result this phase of testing was not performed.

3.14.3 Key Problems and Resolutions

3.14.3.1 Pre-Operational Vibration Measurement and Inspection During Pre-Operational Testing

Pre-operational vibration measurement and inspection of the reactor internals are two elements of the comprehensive vibration assessment program described in NRC Regulatory Guide 1.20. In light water reactors the vibration instrumentation installed for this test can be removed during the post-test removal and inspection of the reactor internals. However, removal and inspection of LMFBR reactor internals following pre-operational testing is not considered feasible. The concern is that exposure of these components to out of reactor environment and cleaning media can degrade their structural capabilities and disqualify their further use in-reactor. Consequently, vibration instrumentation for LMFBR reactor internals must be permanently installed, remaining in plant throughout operating life. An additional consideration is that in-reactor vibration instrumentation is functional only at low and intermediate temperatures (up to 600 degrees F) and in a non or low irradiation environment. As a result, this instrumentation is usable only during the brief period of pre-operational testing. The above considerations together with cost and complexity in design create a strong incentive for minimizing permanently installed vibration instrumentation. The approach selected in CRBRP to balance the competition between satisfying Regulatory Guide 1.20 and design simplicity was to locate four permanently installed accelerometers on the Upper Internals Structure, which is the most critical component for flow induced vibrations. A comparison of readings from these accelerometers with corresponding accelerometers on the quarter scale model UIS would provide a correlation parameter for applying the extensive quarter scale model data base to the operating plant.

To complement the flow induced vibration instrumentation being installed on the UIS, ex-vessel instrumentation with the capability of detecting rattling and impacting due to loose parts was planned. This instrumentation would be

used in conjunction with in-vessel instrumentation during pre-operational testing and would be a means of monitoring the vibrational status of the reactor internals during subsequent operation.

3.14.4 Final System Status

3.14.4.1 Outstanding Changes

There are no outstanding changes in the reactor internals flow induced vibration program.

3.14.4.2 Outstanding Activities to Complete Design

At the time of CRBRP Project termination, the reactor internals flow induced vibration program was proceeding along three fronts: design, testing and analysis.

3.14.4.2.1 Design

During IRFM testing, impacting at UIS keyway interface on the Upper Core Former Ring was measured (2.1 g's peak-to-peak at a frequency less than 1 cycle/sec). Order of magnitude calculations indicated that the wear at this location over the plant lifetime could be of concern. The two ways of addressing the concern were: a) eliminate the impacting by design or b) determine experimentally that the anticipated impact loading would not generate undue wear. The former approach was chosen since it was believed that convincing impact data could not be obtained at a reasonable cost. The tentative design approach selected was to slightly rotate the intermediate plug on the head so as to force the UIS keys to bear against the Core Former keyways. Studies to determine the feasibility of this approach had yet to be initiated at the time of CRBRP termination.

3.14.4.2.2 Testing

Final modifications of the IRFM model in preparation for Phase IIE testing were underway at the time of CRBRP termination. Planning was also in progress on specific tests which were needed to provide information on outstanding issues. The main areas of investigation were potential impacting of UIS keys at the core former keyways and the UIS thermal liner plates against the UIS

instrument posts. Discussions were being held with the IRFM test performer on how to simulate head rotation in the IRFM model to cause the UIS keys to bear against the core former keyways.

3.14.4.2.3 Analysis

Planning for the analysis and evaluations necessary to satisfy the intent of NRC Regulatory Guide 1.20 was proceeding. The first planned effort was to compile and evaluate the structural and functional margins for the reactor internals components. The methodology used at HEDL for the evaluation of the FFTF Instrument Tree - Instrument Guide Tubes cumulative damage was reviewed in preparation for its application to the CRBRP reactor internals [9]. In addition planning was proceeding on the development of plant acceptance criteria and the prediction of sensor responses in preparation for CRBRP pre-operational testing.

3.14.4.3 Outstanding Problems and Descriptions of Actions Needed

There are no outstanding problems for which resolution is needed. Courses of action had been determined for pending design issues as noted in Section 3.14.4.2.

3.14.5 References:

1. NUREG-0968, Safety Evaluation Report Related to the Construction of the Clinch River Breeder Reactor Plant, March, 1983, U.S. Nuclear Regulatory Commission, Washington, DC.
2. CRBRP Preliminary Safety Analysis Report - Section 3.9.1.3.1
3. U.S. Nuclear Regulatory Commission Regulatory Guide 1.20, Comprehensive Vibration Assessment Program for Reactor Internals During Preoperational and Initial Startup Testing, U.S. Nuclear Regulatory Commission, Washington, DC.
4. ANL-83-xx, IVTM Port Plug Gap Modulation Flow Induced Vibration Test, T. M. Mulcahy, to be published.
5. ANL-83-xx, CRBRP Upper Internals Structure Chimney Model Vibro-Impact Retest, T. M. Mulcahy, to be published.
6. HEDL-TC-1017, Integral Reactor Flow Model Phase I/IA Flow Induced Vibration Tests, January 1982.
7. HEDL-TC-1259, Integral Reactor Flow Model Phase II Flow Induced Vibration Tests, April 1980.
8. DRS 31.14.07, Addendum 7, Revision 2; IRFM Phase 2E, Flow Induced Vibration Testing of the Final UIS Design, August 1983.
9. Deleted