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PHYSICS OF REACTOR SAFETY

Quarterly Report

April-June 1975

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ARGONNE NATIONAL LABORATORY, ARGONNE, ILLINOIS

Prepared for the U. S. NUCLEAR REGULATORY COMMISSION
Office of Nuclear Regulatory Research

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9700 South Cass Avenue
Argonne, Illinois 60439

PHYSICS OF REACTOR SAFETY

Quarterly Report
April—June 1975

Applied Physics Division

Work performed for the
Division of Reactor Safety Research
U. S. Nuclear Regulatory Commission

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ABSTRACT

This quarterly progress report summarizes work done in Argonne National Laboratory's Applied Physics Division for the Division of Reactor Safety Research of the U. S. Nuclear Regulatory Commission during the months of April-June 1975. It includes reports on reactor safety research and technical coordination of the RSR safety analysis program by members of the Reactor Safety Appraisals Group, Monte Carlo analysis of safety-related critical assembly experiments by members of the Theoretical Fast Reactor Physics Group, and planning of DEMO safety-related critical experiments by members of the Zero Power Reactor (ZPR) Planning and Experiments Group.

PHYSICS OF REACTOR SAFETY

Quarterly Report

April-June 1975

 TECHNICAL COORDINATION - FAST REACTOR
 SAFETY ANALYSIS
 (A2015)

I. SUMMARY

Progress in reactor safety research and technical coordination of the RSR safety analysis programs by members of the Reactor Safety Appraisals Group, Applied Physics Division, Argonne National Laboratory, for the period March-June, 1975, is reported. Progress in Monte Carlo analysis of safety-related critical experiments and planning of such experiments by other members of the Applied Physics Division is also reported.

On further investigation of the possibility of producing very high ramp rates in the model of a 4000 MWe LMFBR, the highest value it has been possible to produce is \$40/sec, and the reason for the ramp rates of up to \$250/sec found by Bleiweis et al is still not understood.

The rate of gas evolution from low power pins during an initial disassembly in a loss-of-flow (LOF) accident has been studied with the aid of the theory of E. E. Gruber. It appears that about 25% of the retained gas could be expected to be evolved well below the melting point in these pins if release of gas to grain boundaries is the limiting step.

Development has started on an improved fuel-coolant interaction code for eventual incorporation into SAS. A primary motivation for this effort is the need for a better treatment of the failure of low power pins in a LOF accident. This code will permit fuel expulsion into a partially voided coolant channel, will take account of fuel vapor pressure, will allow multiple clad rips, and will give an improved treatment of fuel and sodium motion in the coolant channel. Fuel-coolant heat transfer will be parameterized as before. Fuel freezing and plate-out on clad will be taken into account.

Benchmark core disassembly comparison calculation have been made between FX2-POOLVENS, a special version of FX2-POOL with the free upper boundary eliminated, and VENUS-II. Close agreement was obtained for total energy deposition between the two codes.

II. STUDY OF BASIC PROBLEMS IN ACCIDENT ANALYSIS

A. Initiating Condition Variations1. Pump Cooldown Calculation for a Model of a 4000 MWe Oxide-Fueled LMFBR (H. Hummel, P. Pizzica)

Calculations with pin failure suppressed to a high fuel melt fraction (0.70-0.80) for the 4000 MWe LMFBR model¹ using the PRIMAR-I primary loop

model in SAS² gave a maximum sodium voiding ramp rate of \$36/sec. At fuel melt fractions in the range 0.30-0.50, voiding ramp rates up to \$28/sec were obtained with PRIMAR-I. With PRIMAR-II³ voiding ramp rates were in the range \$9-\$13/sec for low fuel melt fraction and \$11-\$17/sec for high melt fraction. We have not been able to reproduce the very high ramp rates for essentially the same model found by Bleiweis et al.⁴ Lowering the Doppler coefficient to 0.90 of our original value with sodium in and 0.80 of the original value with sodium out, which seemed to be more consistent with the values used by Bleiweis et al, led to a ramp rate of \$40/sec. It was thought that autocatalytic tendencies might be enhanced by this reduction in Doppler effect; but a larger decrease than this would evidently be required. Heames⁵ has suggested that improvements in the voiding model in the SAS2A code may have eliminated very high ramp rates, although in our most recent studies we used a voiding model several years old. This model was thought to be the same one used by Bleiweis et al.

2. Fission Gas Release from Low Power Pin in a Loss-of-Flow (LOF) Accident (H. H. Hummel)

To lay the groundwork for an improved treatment of failure conditions for irradiated low-power pins, the temperature transients they experience during LOF accidents have been studied for various available cases^{1,6} with reference to the rate of release of fission gas during an initial disassembly which causes failure of the low-power pins.

The retained fission gas in steady-state operation in oxide fuel is located in the outer part of the equiaxed region and in the restructured region, with the maximum concentration at about the interface between these regions and with most of the gas in the unrestructured region.⁷ In the present study the temperature considered is that of the first node in the unrestructured fuel, close to this interface, and the temperature gradient considered is between this point and the next node in the unrestructured fuel. In our model there are 11 radial nodes in the fuel, corresponding to 10 mesh intervals, with node 1 being at the edge of the central void or at the center of the pin if there is no void. The unrestructured fuel typically starts at a fractional radius of 0.60-0.70 in the hottest part of the fuel for the low power density of the CRBR, with two or three temperature nodes in this region. Some representative curves for the time-temperature history of the first unrestructured node in the low-power channels are given in Fig. 1. The axial location is about 1/3 of the core height from the top, the hottest part of the fuel. The hottest part of the clad tends to be nearer the top of the core, but this depends on the history of coolant voiding. As more of the channel is voided the region of hottest clad shifts downward toward the center of the core, but by this time there is less concern about the reactivity effect of coolant voiding. There tends to be, however, a fairly long clad region over which the temperature does not vary much, and since the fuel temperature likewise does not vary too greatly over an axial region of 20 to 30 cm in the upper half of the core the possibility of a fairly massive clad failure in a relatively short time seems to be present.

Identification of the curves in Fig. 1 with pump coastdown cases without scram for the CRBR⁶ and the 4000 MWe oxide-fueled LMFBR model¹ is given in Table I. It was attempted in constructing these curves to cover the possible range of variation in the time-temperature history. Perhaps the most

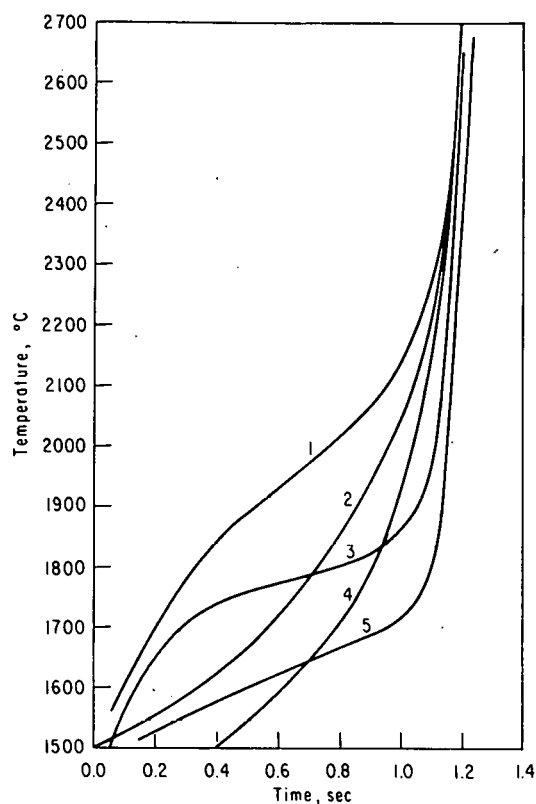


Fig. 1. Representative Time-Temperature Histories for Heating of Inner Part of Unrestructured Fuel for Low-Power Pins in a LOF Accident

TABLE I. Approximate Correspondence Between Time-Temperature Curves in Fig. 1 and Pump Coastdown Cases

Case	Channel	Fractional Height of Core	Curve
CRBR Clad Motion	5	0.67	2
No Axial Expansion	9	0.67	4
CRBR Clad Motion	5	0.67	3
Axial Expansion	9	0.67	5
CRBR No Clad Motion	5	0.67	5
Axial Expansion	7	0.67	2
CRBR No Clad Motion	5	0.67	1
No Axial Expansion	5	0.74	1
4000 MWe Axial Expansion	5	0.67	5
Clad Motion	8	0.85	5
	9	0.85	5
4000 MWe No Axial Expansion	5	0.85	5
Clad Motion (a)	8	0.85	5
	9	0.85	5

(a) No clad motion actually occurs in this case.

significant feature of these curves is the time the fuel spends in the temperature range between about 1700°C, where gas evolution is barely beginning, and 2300°C, when gas release from grains becomes rapid. A key question it was desired to answer with these studies is whether or not there was time for massive gas release from the fuel and therefore clad failure before a very high fuel melt fraction was achieved. This could help to determine if only gas would be released from the pin initially rather than mixed gas and fuel.

Gas evolution was calculated with the aid of the theory developed by Gruber,⁸ in which gas release from fuel grains to grain boundaries is calculated as caused by biased migration under a temperature gradient. Random migration was also considered in this theory, but was found to have a small effect compared to that of biased migration. Time for release from grain boundaries is neglected, although this assumption may not actually be justified in all cases.

Gruber has parameterized the fractional gas release as a function of fuel temperature, temperature gradient, and initial fission gas concentration (C_0). The fractional release is lower at higher C_0 because the greater rate of bubble coalescence leads to larger bubbles, which diffuse more slowly from the fuel grains. Values of C_0 of 1, 2, and 4×10^{20} have been selected as representing a reasonable range, with 2×10^{20} corresponding to about 4% burn-up, taking into account steady-state release. The temperature gradient in the inner part of the unstructured region is generally between 5000 and 10,000°C/cm for the cases in Ref. 1 and 6 and remains fairly constant during a transient until the melting point of the fuel is approached.

Gas release as a function of temperature using the time temperature curves in Fig. 1 is given in Fig. 2 for a temperature gradient of 5000°C/cm and a C_0 of 20×10^{20} . It is seen that, for these time-temperature histories, gas release becomes appreciable at 1900°-2000°C and reaches about 25% of the total available at around 2300°-2400°C, well below the fuel melting point, assumed to be 2767°C. This conclusion is not changed much by reasonable variations in C_0 (Fig. 3) and $\Delta T/\Delta r$, using Curve 2 (Fig. 4). Under what conditions release of ~25% of the contained fission gas would cause clad failure has not been examined yet.

B. Model Studies

1. Improved Fuel-Coolant Interaction (FCI) Model (P. Abramson, P. Pizzica)

We have found that meaningful results cannot be obtained for loss-of-flow (LOF) calculations for large LMFBR's past a certain point with the SAS-3A code¹⁰ because of failure of the SAS/FCI module¹¹ of that code to treat adequately the failure of low-power pins. Channels in which boiling is occurring cannot be treated by SAS/FCI, multiple clad rips are not allowed, fuel vapor pressure is not taken into account, and the treatment of fuel and sodium motion is too crude. Activity is under way in the RAS Division of ANL to remedy these deficiencies by incorporating an improved PLUTO code^{12,13} into SAS. Because of the urgent nature of the problem (some studies have indicated that very high reactivity ramp rates can be produced by sodium expulsion from low-power regions) and the relatively long time that is expected to be required for completion of the RAS effort we have thought it best in order to meet the

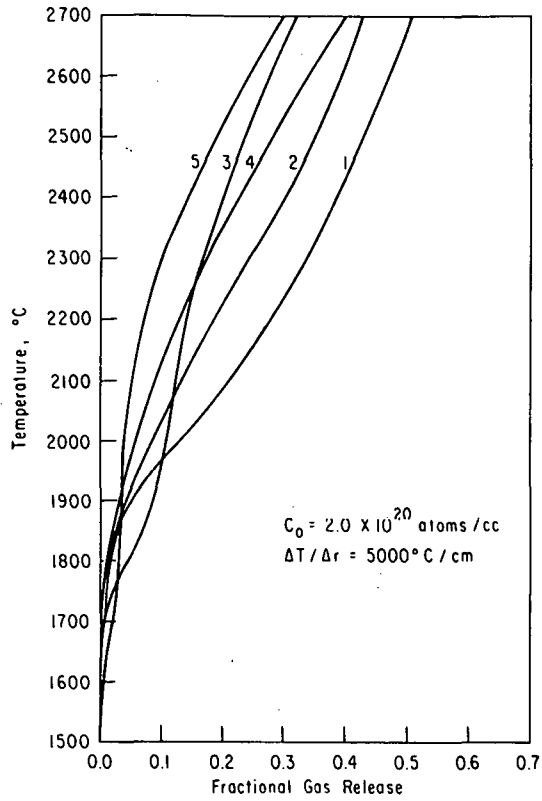
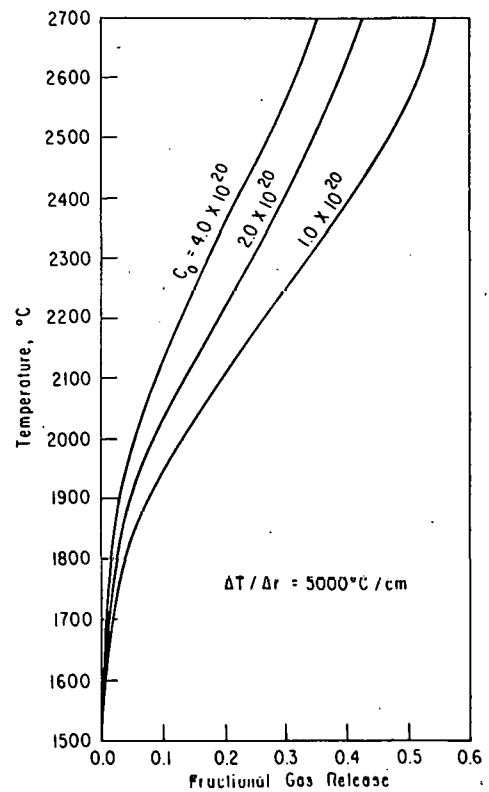


Fig. 2. Fractional Gas Release vs. Temperature for Time-Temperature Curves of Fig. 1, Using $C_0 = 2.0 \times 10^{20}$ and $\Delta T / \Delta r = 5000^\circ\text{C/cm}$

Fig. 3. Fractional Gas Release as a Function of Temperature for Various C_0 , Using Time-Temperature Curve 2 From Fig. 1 and $\Delta T / \Delta r = 5000^\circ\text{C/cm}$



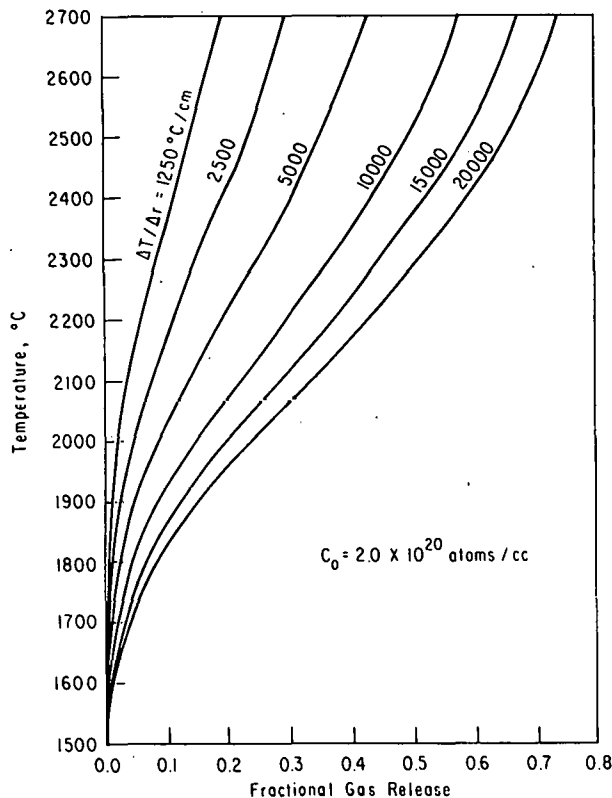


Fig. 4. Fractional Gas Release as a Function of Temperature for Various Values of $\Delta T/\Delta r$, Using Time-Temperature Curve 2 From Fig. 1 and $C_0 = 2.0 \times 10^{20}$

needs of the NRC to embark on an independent effort to develop a new code to calculate energy transfer and material motion in a channel in which fuel is ejected while some liquid sodium is still present. Our code will be less elaborate than the RAS one and is expected to be available considerably sooner.

To the extent possible this code will first be tested as a stand-alone model requiring results of the SAS-3A code as input. It is planned to incorporate it subsequently into the SAS code to make use of the SAS heat transfer model in the fuel pin and the SAS coolant dynamics model in the parts of the coolant channel outside of the FCI zone.

The model assumes like the SAS code that a group of subassemblies (called a channel) can be represented by a single fuel pin and its associated coolant and structure. There are no subchannel effects taken into account. There will be provisions for as many channels as in the SAS code, allowing for different power and burnup levels and voiding histories, etc. The single fuel pin representing a group of subassemblies is assumed to have a central cavity filled with a molten fuel/fission-gas froth with an axially varying temperature and fission-gas content. The coolant channel may be unvoided or partly or totally voided. (However, the coolant is assumed always to contain some liquid and some vapor fraction, no matter how small, to avoid hydrodynamic problems arising with a single phase.) Both the fuel pin and the coolant channel are treated with one-dimensional Eulerian hydrodynamics. Slip is treated between the free fission gas and the molten fuel/fission gas froth in the fuel pin as well as between the fuel particles in the channel and the mixture of fuel vapor, fission gas and sodium vapor, which itself is assumed to move without

internal slip. First order perturbation theory will be used for the reactivity calculation and when the incorporation into SAS is made, the SAS point-kinetics model will be used.

The area of clad failure criteria in a transient over-power situation is one of the most uncertain in fast reactor safety analysis, and will have to be treated parametrically to a great extent for the time being. A crude mechanistic model of clad failure by fission gas loading will be provided, using Gruber's theory^{8,9} for fission gas release from fuel grains. Other options, similar to those in SAS/FCI, will be triggering of clad failure on fuel melt fraction, on fuel temperature, or on fractional fission gas release. Clad rip locations may be specified if desired and may be multiple. The whole issue of the mechanism of ejection of the fuel and fission gas into the coolant channel will be avoided of necessity. Once a clad failure has been calculated to occur (and it should be kept in mind that it is difficult, if not impossible to calculate the extent and location of the rupture), the pressures in the coolant channel and in the pin are assumed equalized and a backwards calculation is done to determine the amount of fuel/fission-gas froth expelled necessary to cause the pressure in the coolant channel to rise to the level of that in the pin.¹⁴

A simple pin model has already been programmed. It has no heat transfer capabilities yet, however. Also the fission-gas distribution and degree of release from the fuel is assumed in lieu of a fission-gas release model. Of course the calculation of the rate of fuel-froth expulsion cannot be tested until the coolant channel calculation is complete.

The equations for the coolant channel have been developed and programming is about to begin. When the fuel/fission-gas froth is ejected into the channel, it is assumed to fragment into spherical particles of one size. This size can be crucial to the course of the FCI but must of necessity be an input parameter because of the lack of experimental data on particle sizes and shapes due to fragmentation. The fuel-sodium heat transfer coefficient must also be parameterized by the user. There will also be an option to allow only a fraction of the ejected fuel to fragment, spreading the rest along the clad in some fashion, and reducing the channel size. Modification of the steady-state Cho-Wright formulation¹⁵ is used for the heat transfer coefficient between the fuel particles and sodium liquid:

$$h = \frac{k_{fu}}{r_p} \cdot F ,$$

where k_{fu} is the fuel thermal conductivity, r is the radius of the spherical particle, and F is a factor which may be varied by the user to take into account surface and convective effects as well as the fact that temperature profile in the fuel particles is non-linear. This is multiplied by the surface area of all the spherical particles in the node and then by the sodium liquid fraction (following Wider¹⁶) and the temperature difference between the fuel and sodium to give the heat transferred to the sodium liquid in the channel per unit time. Only the conductivity of the fuel is taken into account since most of the resistance to the conduction of heat between the particle and the sodium comes from the fuel with its low conductivity.

All heat transfer (aside from that in condensation and evaporation) will be ignored except that between the sodium liquid and the fuel particles, and between the clad and the sodium film and the frozen fuel on the clad. Radiation heat transfer to the sodium is ignored since the reflectivity of the sodium is so high.¹⁷ There will be a sodium film in the FCI-voided zone in the coolant channel and condensation of sodium vapor onto the film and evaporation of the film into the channel will be taken into account.

In the channel there will be a mixture of fuel vapor, fission gas and two-phase sodium which moves without slip of the components. Both the two phase fuel particle/fuel vapor and the two phase sodium liquid droplet/sodium vapor are assumed to follow their respective saturation curves, so that each is always in thermodynamic phase equilibrium and there is never any non-equilibrium boiling. The pressure in the coolant channel in the FCI zone is the sum of the saturation pressures of fuel and sodium for their respective temperatures and the fission gas pressure. The fission gas pressure is computed using an ideal gas approximation for a given volume for the gas mixture, a temperature which is a weighted average of the sodium and fuel temperatures, and the mass of fission gas present in the channel. The code will handle the situation of a fission gas voided channel as well.

Condensation of fuel vapor on the clad will be calculated to account for a situation with a high ramp where fuel vapor has voided the channel and is the primary determinant in the pressure calculation.¹⁸ Fuel freezing will be calculated to study plate-out and the plugging of coolant channels by molten fuel. The clad temperature will be calculated as well as that of the frozen-molten fuel film on it but no clad motion or fuel slumping will be computed since this would greatly increase the complexity of the code, whose purpose is to expand the capabilities of the present FCI code but not to provide an integrated treatment of moving fuel and clad with all possible material interactions. Thus the clad serves the function of a stationary heat sink only, even though its motion could be highly significant for thermal interactions as well as reactivity effects. This model, then, will calculate the heat transfer between the sodium liquid and the fuel particles on the basis of an admittedly artificial fuel-coolant heat transfer coefficient, which is in reality unknown and which the user must parameterize. Heat transfer between most other materials (especially the gaseous ones) is ignored. The justification for this is that, since it is impossible to be strictly mechanistic about all the complex phenomena involved (especially in the coolant channel) it is better to have two parameters (the modification to the theoretical heat transfer coefficient referred to above and the fuel particle size) to vary than many (since only a parametric analysis is possible anyway). In this way it is easier to treat other effects (such as vapor-vapor heat transfer) by merely changing the fuel particle-sodium liquid heat transfer.

2. Development of FX2 POOL - A Dynamic Neutronic, Hydrodynamic and Thermodynamic Hypothetical Core Disruptive Accident (HCDA) and Post Core Disruptive Accident (CDA) Code (P. Abramson)

POOL¹⁹ was substituted for VENUS²⁰ as a subroutine in the space time kinetics code FX2²¹ and the two new versions of FX2 were developed. The first version, FX2-POOL, is intended for the study of the neutronic, hydrodynamic and thermodynamic behavior of boiling pools of fuel and steel. The second version, FX2-POOLVENS, is a modification of FX2-POOL which was developed specifically for comparison to FX2-VENUS and to the stand-alone version VENUS.

Since there are no existing codes designed to study boiling fuel/steel pools, benchmark comparison tests between FX2-POOLVENS and VENUS II have been conducted and the results, described below, show excellent agreement. During the course of these comparisons it was found that FX2-VENUS was unable to perform the calculations and was consequently dropped from the study.

The specific reactor configuration chosen for comparison is shown in Fig. 5 and the material distributions are described in Table II. This configuration is similar to the CRBR.

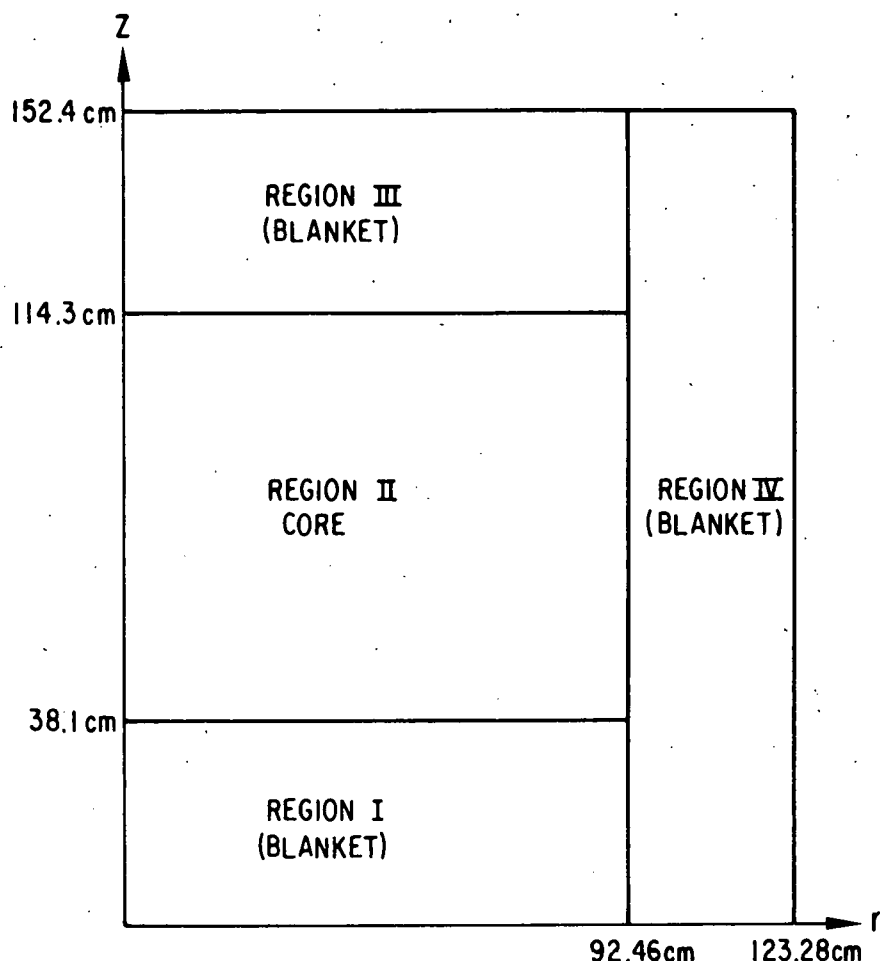


Fig. 5. Reactor Configuration Used in Benchmark HCDA Calculations

Table II. Reactor Data (1000 MW Nominal Thermal Power)

Region	Function	Volume Fractions		
		Fuel	Steel	Void
I	Axial Blanket	0.308	0.285	0.407
II	Core	0.308	0.285	0.407
III	Axial Blanket	0.308	0.285	0.407
IV	Radial Blanket	0.495	0.189	0.316

Two cases were run for comparison. Case A started at a power of $1.7 \cdot 10^{12}$ watts with an initial reactivity of $\$1.05$ and a flat temperature profile at 2500°K . Case B started from 10^9 watts at $\$1.00$ with a flat temperature profile at 1500°K . These were chosen as representative of the extremes one might encounter in HCDA calculations. In both cases a ramp of $\$100/\text{sec}$ was applied.

Fig. 6 is a plot of Reactor Power vs Time for Case A and Fig. 7 is for Case B. Although there are differences between the power curves, the total energy deposition predicted by VENUS II and by FX2-POOLVENS differ by only 0.1% in Case A and by less than 20% in Case B.

These results indicate that the modeling and numerical work in POOL is adaptable to HCDA calculations and serve as additional initial checkout.

A paper²² was presented at the June ANS meeting on the modeling and benchmark checkout of POOL.

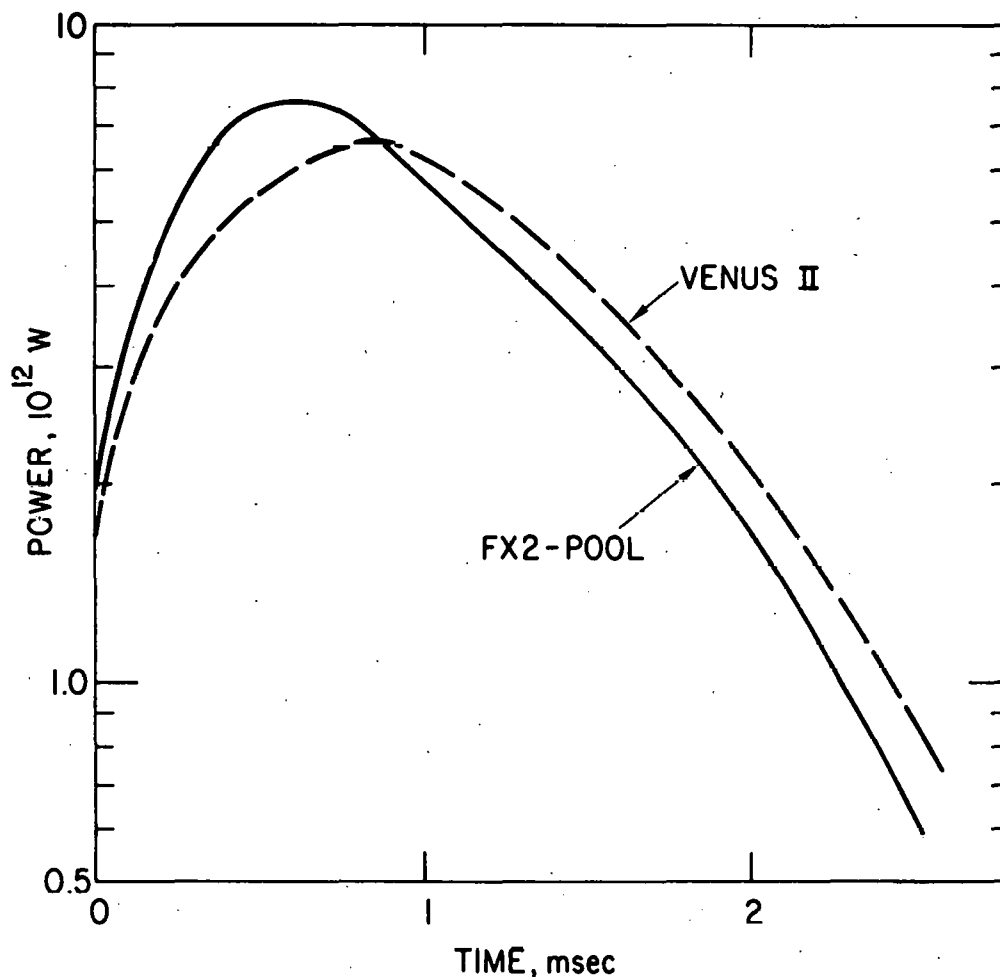


Fig. 6. Reactor Power vs. Time for Case A of Benchmark HCDA Calculations

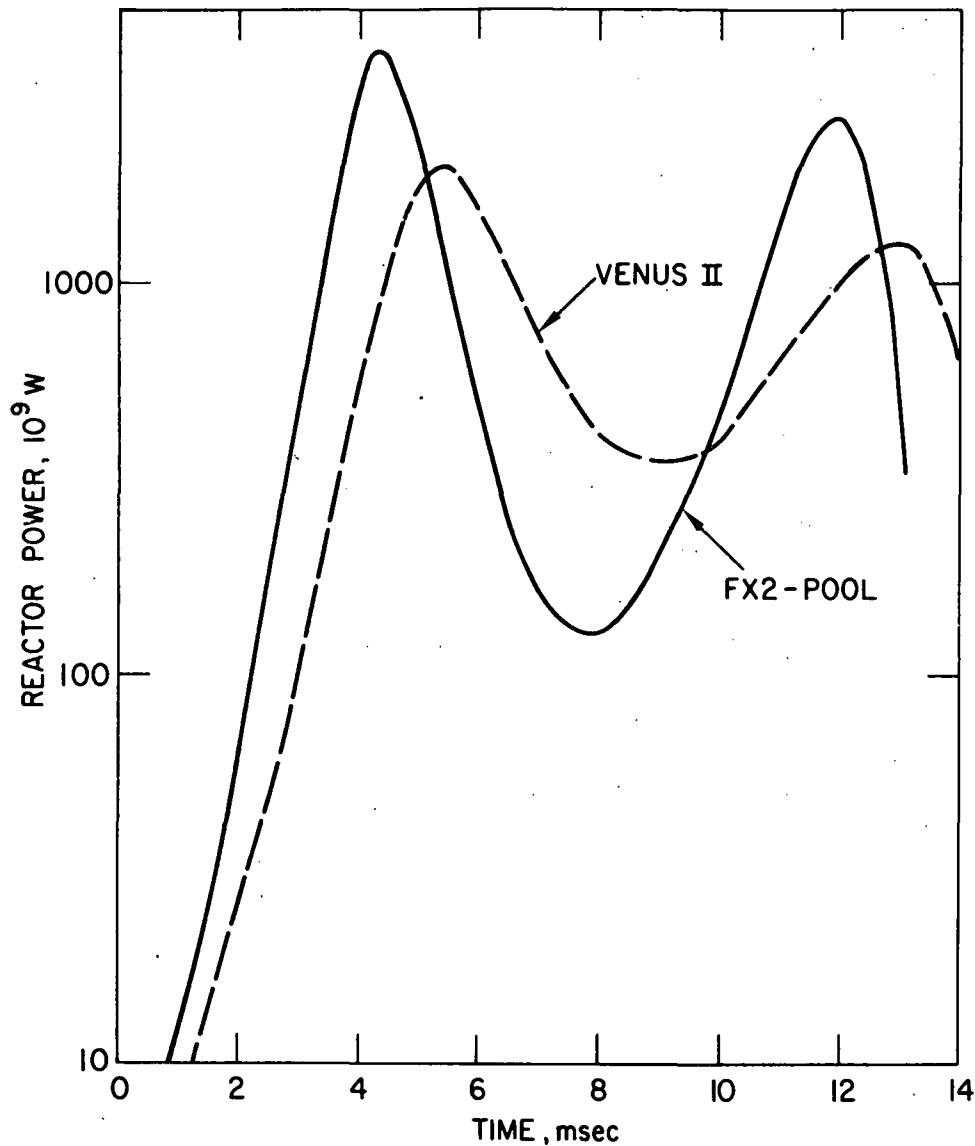


Fig. 7. Reactor Power vs. Time for Case B of Benchmark HCDA Calculations

3. Primary Loop Modeling (Kalimullah)

It is recognized that the present SAS3A code¹⁰ does not contain adequate modeling of the primary coolant loop of an LMFBR i.e., the primary pump, intermediate heat exchanger (IHX), guard vessels, primary loop piping, etc. The SASRUP code²³ being developed in the RAS Division of ANL is directed toward remedying this situation. In order to understand better the problems involved, we have undertaken an effort in this direction also as described below. Some parts of our work are to be used in SASRUP, for example, the modeling of the characteristics of centrifugal pumps. Close contact has also been maintained with the Super System Code (SSC) development at BNL. It is believed that some of our work could be used in the SSC code also.

Some of the assumptions and capabilities in our work are as follows:

1. The primary piping will be divided into user-specified mesh intervals over which the temperature is constant.
2. Single-phase flow is assumed in the primary loop, with no carry over of debris from a damaged core.
3. The IHX will be modeled by splitting into a user-specified number of sections that will allow wide flexibility in accomodating different designs. The check valve characteristics will also cover a wide class of valves, with suitable input changes.
4. For each component a guard vessel may be specified.
5. The position of a pipe rupture may be in any piping mesh interval.
6. The secondary coolant mass flow rate and temperature will be assumed constant at steady-state value.
7. Heat loss to the primary loop piping will be taken into account as it may have a significant effect on natural circulation characteristics.

The equations for liquid metal heat transfer in the geometries of interest have been developed using recent surveys of this field.

A model of the complete characteristics of centrifugal pumps has been developed in terms of a diagram called a Karman-Knapp diagram, in which pump speed and discharge are respectively plotted as abscissa and ordinate, with the sign convention that speed, discharge, head and torque in the normal pump design direction are positive. In this plane various lines of constant head and torque are shown. Since a complete analysis relating the various pump characteristics has not been possible, use has been made of a few experimental complete characteristics available in the literature. None of these experiments was performed with liquid metal, but a dimensional analysis shows that the characteristics of a pump are independent of the liquid pumped if the four quantities speed, discharge, head, and torque are expressed as percentages of their rated values, and, furthermore, the shape of the characteristics then depends only on the rated specific speed of the pump.^{24,25} This neglects viscosity effects, which guarantees validity of the above statement for pumps of a given design. For pumps of differing design the statement is found true experimentally within a few percent in the normal operating range.²⁵

A FORTRAN program has been developed to compute empirical constants based on fitting the experimental data on pump characteristics to a simplified version of the equations in Ref. 25. Empirical constants for pump head and torque at a specific speed of 1800 (gpm units) have been determined. The root mean square errors for fits to the head and torque data are respectively 9.1% and 16.3%.

Equations for the transient behavior of natural circulation of coolant in a closed loop with a constant cross section of any shape have been developed.

III. COORDINATION OF RSR SAFETY ANALYSIS RESEARCH

Comments on the first drafts of the detailed work plan for the experimental safety research program sponsored by RSR at Sandia Laboratories have been prepared and submitted to RSR. P. Abramson also visited Sandia to discuss short and long range research plans in LMFBR safety with R. Coats' staff and to be briefed on Sandia capability and plans. There is special interest in relating Abramson's theoretical work on boiling pools with experimental work planned at Sandia.

Abramson also had a meeting at LASL with J. Boudreau, J. Jackson, and T. McLaughlin to discuss and compare benchmark calculations at ANL and at LASL using FX2-POOL, VENUS-II, and McLaughlin's code PPD.

Work is under way on a number of elements of the RSR Program Plan.

IV. EVALUATION OF PROGRESS IN SAFETY RESEARCH

As part of the preparation of elements of the RSR Program Plan, state-of-the-art reviews have been prepared for transition phase kinetics, transient overpower tests, transient undercooling tests, out-of-pile tests of initial material motions propagation of local failures post-disassembly material motion and reactivity coefficient calculations.

MONTE CARLO ANALYSIS AND CRITICALS PROGRAM PLANNING FOR SAFETY-RELATED CRITICALS (A2018)

V. MONTE CARLO ANALYSIS OF SAFETY RELATED CRITICALS

A. Status of Work on ZPR-3 Assembly 27 (E. Gelbard)

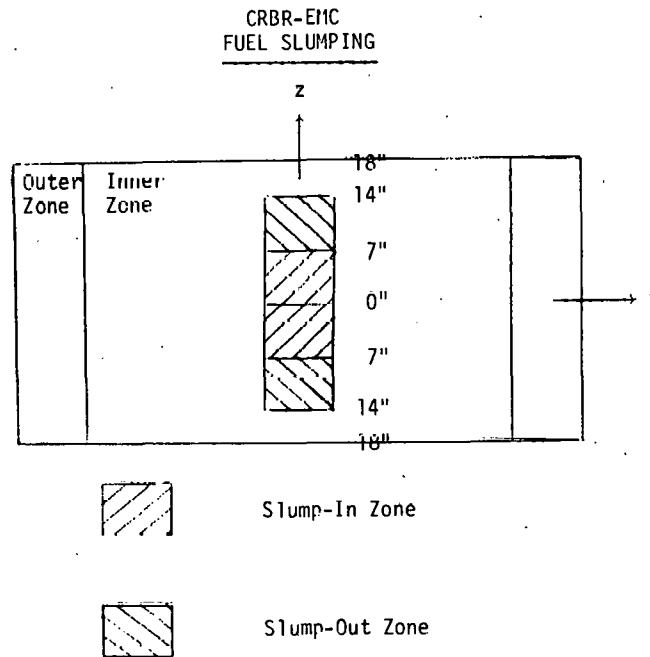
The VIM input deck for the analysis of ZPR-3/27 is complete and has gone through preliminary checking procedures. It is expected that the VIM computation will be run during August, although it must be anticipated that final debugging of the input will take some time. Compilation of loading diagrams for ZPR-3/28 is now in progress.

VI. PLANNING FOR CORE MELTDOWN SIMULATION EXPERIMENTS ON THE ZPR FACILITIES (L. G. LeSage)

As part of the planning effort for core meltdown simulation experiments, the recent meltdown experiments performed in the ZPPR-5 program are being studied. ZPPR-5 is an engineering mockup of the CRBR design. Experiments performed in this assembly were designed to represent the initial stages of a hypothetical meltdown accident sequence as predicted by SAS calculations. A series of accident-sequence core configurations were constructed on ZPPR-5 representing sodium voiding followed by clad melting and redistribution, and, finally, by fuel slumping. The data obtained included reactivity changes between accident steps, axial reactivity traverses, and power distributions.

The important results to consider for the planning of meltdown experiments to test Monte Carlo calculations are the fuel slumping measurements. These measurements are summarized in Table III. Fuel slumping in the axial direction was simulated for both a slump-in and slump-out configuration (see figure in Table III). For the slump-in of 18 central subassemblies, a measured reactivity change of +\$1.8 was obtained. This is compared to a calculation (2-D, R-Z, diffusion theory, eigenvalue difference calculation) of +1.44. This measured reactivity change is considered too small to serve as an accurate test of Monte Carlo calculations. The slump-out measurement was performed in three steps; first for three central subassemblies, then nine subassemblies, and finally for all 18 central subassemblies. The interesting result to note here is the significantly smaller negative reactivity change (total cumulative change) obtained for the final slump-out step. This result indicates that positive reactivity changes due to fuel slump-out may be possible during an accident sequence depending upon the exact sequence and coherence of fuel motion in a CRBR type core. The standard design calculation compares poorly with the slump-out type measurement. Thus, it appears to be useful to perform certain slump-out type measurements during core meltdown simulation experiments designed to facilitate tests of Monte Carlo calculations.

Table III. Fuel-slumping Configuration in ZPPR-5 and Summary of Resultant Reactivity Changes



No. Subassemblies in Slump Zone	$\Delta\rho(\%)$			
	Slump-In		Slump-Out	
	Measurement	Preanalysis	Measurement	Preanalysis
3	-	-	-0.070	-
9	-	-	-0.115	-
18	+1.8	+1.44	-0.086	-0.48

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