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ANALYSIS OF HYPOTHETICAL CORE DISRUPTIVE ACCIDENT (HCDA)

APRIL-DECEMBER 1972

U.S. ATOMIC ENERGY COMMISSION
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ANALYSIS OF
HYPOTHETICAL CORE DISRUPTIVE ACCIDENT
(HCDA)
APRIL — DECEMBER 1972

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ABSTRACT

Hypothetical core disruptive accidents were analyzed using available GE and ANL codes to determine the characteristics and mechanical consequences of such accidents, and the adequacy of the codes for HCDA calculations of a representative LMFBR demonstration plant reactor

It is concluded from this study that the equivalent mechanical energy release for an HCDA in the demonstration plant lies between 0 and 100 MW sec based on expansion of core material to 1 atmospheric pressure

All of the codes used in this study were found to need revisions before they can be utilized for evaluation of containment design basis accidents in a demonstration plant. Analytical approaches used in these codes will require continued support from ongoing developmental programs, particularly in the areas relating to fuel coolant interaction processes, sodium voiding dynamics and fuel motions under accident conditions

1. SUMMARY AND CONCLUSIONS

The discussions and conclusions presented are specific to the particular code versions used in this study and may not be appropriate to other versions of these generic codes known to be under development

1.1 SUMMARY

Hypothetical core disruptive accidents were analyzed using available General Electric and Argonne National Laboratory (ANL) codes to determine the characteristics and mechanical consequences of such accidents, and the adequacy of the codes for hypothetical core disruptive accident (HCDA) calculations of a representative LMFBR demonstration plant reactor. Both transient overpower (TOP) and loss of primary coolant flow (LOF) types of accidents were considered. Those codes utilized included, for the predisassembly phase of the accidents, SAS 2A (ANL) and FREADM (GE), for the disassembly phase, VENUS (ANL), for the fuel coolant interactions, MFCI (ANL) and for structural response, RECORD (GE) and REXCO H (ANL).

The initial phase of the HCDA, until gross fuel motions would be likely to occur, was calculated with both FREADM and with SAS 2A computer codes. Parameters examined with FREADM included Doppler magnitude, reactivity insertion rate (for TOP cases), sodium void worth, voiding rate, fuel motion reactivity, and flow coastdown rate (for LOF cases). The FREADM parameters were carried through the core disassembly phase and reactor shutdown using VENUS to determine the energy generated during these transients. For the range of parameters considered in the overpower transients, only reactivity associated with potential fuel motions and with voiding due to fuel coolant interactions had a capability for substantially altering (i.e., greater than ~25%) the nominal energy release. Comparisons of SAS 2A and FREADM resulted in reasonable agreement between the two codes and either code furnishes a satisfactory framework for further development. Modifications should be made to include modeling of fuel coolant interaction induced voiding and of potential fuel motion reactivity effects, consistent with available experimental data.

The VENUS code was used to calculate the transient for both TOP and LOF cases from the time the reactor is near the prompt critical condition through subcriticality caused by core disassembly. Core motions which result in reactor shutdown are based on two dimensional hydrodynamics equations for determining core geometry changes. However, the validity of this model for LOF calculations is in doubt since core pressures may be too low to support this "hydrodynamic" disassembly model. A detailed review of this concern and formulation of an appropriate model is required prior to additional calculations of this accident for the Demonstration Plant Preliminary Safety Analysis Report (PSAR). Recommended changes to VENUS include incorporating models for predicting localized fuel motion, fuel coolant interaction, and voiding reactivity feedback effects.

The pressures generated by molten fuel coolant interactions were calculated with the MFCI code. Unfortunately, these pressures, and the corresponding coolant specific volumes determined by this code, could not be used as a "source term" for evaluating structural damage in the reactor since it incorporates modeling assumptions which are inappropriate to postulated gross core fuel coolant interaction processes in an LMFBR demonstration plant reactor.

geometry. Minimum required changes before the code can become a useful tool for LMFBR demonstration plant HCDA analysis include revised modeling of spatial and boundary conditions and increased flexibility to accommodate reasonable variations in reactor design and in modeling of the physical processes. An MFCI REXCO code which includes latest structural response modeling seems a desirable development. The MFCI code utilized for this study is considered the weakest link in the HCDA calculational sequence.

Though determination of structural response to core disruptive accidents was performed with both RECORD and REXCO H, the limitations of the less sophisticated RECORD code (it does not calculate local strains) restricted its application to that of a scoping tool prior to use of REXCO. Because of inability to establish appropriate input data for REXCO from MFCI output, the structural analysis calculations were performed with pressure volume inputs which would permit assessment of energy distribution in the reactor structures. Though instabilities in the calculations prevented completion of the REXCO calculations, the results did indicate that the accident energy, per se, is not an adequate measure of the damage potential. Recommended changes to REXCO include integration with the MFCI modeling as previously described, revised treatment of modeling of plates and shells (these are membranes in REXCO H) to incorporate bending resistance of plate sections, and changes to improve stability of the numerical procedures.

1.2 CONCLUSIONS

The general conclusion arrived at from the results of the parametric studies and evaluations completed under this task is that the equivalent mechanical energy release for both the TOP and LOF sequences analyzed lies somewhere between 0 and 100 MW sec based on the expansion of core materials to 1 atmosphere pressure. Based on a review of containment evaluations and model tests performed by General Electric for early demonstration plant designs and by the FFTF project for the FTR, supported by the evaluations completed as part of this study, the typical demonstration plant would be expected to have a capability to contain in excess of 300 MW sec based on sodium vapor expansion to 1 atmosphere pressure. Specific conclusions related to codes and models utilized in the study and future development activities are:

- a Most of the codes examined in this study incorporate specific geometric and modeling assumptions which are not always consistent with the expected progress of an accident in an LMFBR demonstration plant reactor. Modifications to SAS 2A, VENUS, MFCI, and REXCO H codes to improve modeling and increase flexibility should be made before they are used for HCDA evaluations in support of a Demonstration Plant PSAR.
- b The MFCI code suffers most from inappropriate assumptions and lack of flexibility. It requires major modifications before it can be used as part of an LMFBR demonstration plant HCDA analysis. Integration of MFCI with latest structural modeling of REXCO is a recommended step which should permit better definition of the boundary conditions for the MFCI and better definition of the pressure volume "source" term for REXCO.
- c Evaluation of containment design basis accidents will require improved code modeling that treats reactivity effects due to potential fuel motions and due to voiding induced by fuel coolant interactions. These phenomena which have appreciable potential for influencing the severity of an accident, constitute major uncertainties in the consequences of HCDA events. Experimental programs should be aimed at furnishing the improved understanding of these phenomena needed to reduce these uncertainties.
- d It is probable that most loss of flow accidents will not result in generation of sufficiently high core pressures to justify the VENUS assumptions of hydrodynamic behavior of core materials resulting in disassembly and shutdown. Modifications should be made to the SAS 2A or FREADM codes to include modeling of potential fuel motions under these accident conditions.
- e Lack of adequate documentation of ANL developed HCDA codes limited code utility and necessitated frequent demands on ANL personnel to clarify code requirements. Increased attention to early issuance of supporting documentation should be part of continued code development efforts.
- f Continued improvements in analytical codes for HCDA analysis require close integration of efforts by reactor design, code development and experimental groups.

2. SCOPE AND INTRODUCTION

2.1 SCOPE

The purpose of this task was to perform an analysis of the characteristics and mechanical consequences of hypothetical core disruptive accidents (HCDA) for an LMFBR demonstration plant utilizing the analytic tools developed by GE, make comparison of these results with those obtained by using similar codes developed by ANL, and to investigate the models and methods used for the FFTF HCDA analysis for their adequacy and applicability to the demonstration plant

Two accident sequences were analyzed (1) reactivity ramp insertion and (2) primary coolant flow coastdown, both with the assumed complete failure of the reactor safety systems. Parametric evaluations of the effects of assumptions and characteristics, such as reactivity ramp rate, flow coastdown time, sodium void patterns and worth, and fuel motion were analyzed as part of this study

Complete failure of the reactor safety system to scram when required, as assumed in these analyses, is a hypothetical event. These hypothetical sequences are postulated to provide bases in testing the safety margins provided in a typical demonstration plant

2.2 INTRODUCTION

The core disruptive accidents which were analyzed in this study are hypothetical accidents, since they postulate the unlikely occurrence of multiple failures in the protective safety systems concurrently with the malfunction of essential components and/or occurrence of events leading to a serious power flow mismatch in the core. These sequences may lead to rapid power excursions, resulting in high fuel and coolant temperatures and abnormal pressures which in turn may result in localized fuel relocations and/or gross disassembly of the core. These accidents are normally characterized by the net thermal energy generated and the potential for conversion of this energy into mechanical loadings on the primary coolant systems and associated containment structures

The HCDA analyses completed in this study investigated the dynamic response of the core and the primary reactor system due to an assumed spurious reactivity addition or loss of primary coolant flow without a reactor scram. The analyses of these hypothetical accidents are conveniently divided into six major phases based upon the dominant physical processes involved. These are (1) predisassembly phase, (2) core disassembly phase, (3) energy conversion phase (fuel coolant interactions), (4) evaluations of structural responses, (5) potential release of radioactive materials, and (6) post accident heat removal. The scope of this study is limited to the first four phases

The predisassembly phase covers the accident from its initiation until it is terminated by local fuel movement or until conditions leading to large rapid core distortions are reached. The accidents studied were initiated by a postulated event with the reactor operating at the design full power condition. Events initiated at low or intermediate power should be considered when completing analysis for the PSAR. Rapid power and temperature rises occur in the core that could lead to coolant boiling and/or channel voiding, fuel pin failure, and fuel relocations. All these events are normally treated in this phase. The accidents in which the power excursions are terminated by localized fuel expulsions are also treated in this phase. However, if the reactor approaches prompt criticality and correspondingly high temperatures exist in the core, the subsequent calculations are performed as part of the core disassembly phase

During the core disassembly phase, the entire core is assumed to behave like a homogeneous fluid. Very rapid power and temperature rises occur leading to the generation of significant hydrodynamic forces, causing the core disassembly. The excursion is moderated by the Doppler reactivity feedback and finally terminated by the disassembly feedback reactivities

The energy generated during the power excursion is initially deposited in the fuel and may subsequently be transferred to the coolant or structure as the fuel mixes with the coolant and comes in contact with structural material. The process involved is defined as the molten fuel coolant interactions (MFCI). These interactions could also occur in either of the first two phases of the event. Because of the computational tools available at this time, these interactions are treated as an independent phase of the analysis. Phenomena treated in this phase include effective transfer of energy from fuel to coolant, sodium pressures generated, and mechanical work potentials due to the expansion of pressurized sodium

The calculation of the damage done to the reactor structures by the pressures generated due to the fuel coolant interactions are treated in the next phase. The source terms for these analyses were selected on the basis of preliminary

MFCI studies and previous work performed in support of the FFTF. As mentioned previously, this study did not consider the radiological effects of these events or the postaccident heat removal from the core debris following the event.

As discussed in the Scope section of this report, the two accident sequences investigated were (1) the spurious reactivity addition identified as "transient overpower" case and (2) the primary coolant flow coastdown, identified as "loss of flow" case, both assuming complete failure of the plant protection system. The predisassembly phase was analyzed by using the FREADM¹ computer code. The results obtained are presented in Subsection 3.1. The nominal or base case for each event was also analyzed using the SAS 2A code. These results are discussed in Subsection 3.5. The core disassembly calculational results obtained by using VENUS² code are given in Subsection 3.2. Subsection 3.3 contains the results obtained from the MFCI³ code which was used to evaluate the fuel coolant interaction and estimate the work energy potential for the events studied. The scoping tool, RECORD, and the two dimensional structural/hydrodynamics code, REXCO H,⁴ were used to evaluate structural responses to the calculated energy releases. The results of these analyses are presented in Subsection 3.4.

Since considerable time was required for code familiarization and for bringing codes to operational status on local computers, scheduling considerations required that the calculations for various phases of the accidents be performed concurrently. Thus, many of the parameters and initial conditions for the various codes were based on prior studies or on engineering considerations. However, the conditions investigated in various phases of the accidents were proved to be sufficiently consistent so as to permit cogent observations on consequences of the HCDA's and on the codes themselves. These overall discussions and conclusions are given in Section 4.

3. ACCIDENT ANALYSES

3.1 PREDISASSEMBLY PHASE – FREADM CALCULATIONS

3.1.1 Introduction

The initial phase of the HCDA sequence is computed by the FREADM¹ code. The specific objectives of this calculation are to determine

- a The expected course of physical events, time scales, and core thermal energy for a nominal set of reactor parameters
- b Appropriate initial conditions required to extend the analysis beyond the applicability of the FREADM code assumptions
- c The dominant mechanism(s), such as hydraulic and hydrodynamic processes, governing fuel motions leading to termination of the nuclear excursion
- d Significant alterations (if any) in the above items due to variations in the important core design parameters and/or analytic models
- e Where additional engineering data are needed to understand or support critical areas of the analyses

To perform the necessary calculations, the reference demonstration reactor core was thermal hydraulically modeled as 10 independent fuel rods (considered representative of annular core regions). A point kinetics neutron solution of the power (including sodium void, Doppler, and assumed fuel motion reactivity effects) is employed to couple the thermal solution of the above fuel rods. The core model employed is further described in Appendices A, B, and C.

The core response to imposed anomalies was calculated with FREADM from the initial condition until gross fuel motions are assumed to occur. A distinction is made herein between two physical mechanisms for fuel motions. A

hydraulic process is defined to represent fuel motions dominated by local subassembly viscous shear forces and pressure pulses. Structural materials are considered to maintain their nominal strength characteristics. The term, hydrodynamic disassembly, is reserved for a rapid, high pressure process where the entire core may be treated as a fluid. It is possible for both processes to occur within a given transient, and the intent of Item c above is to ascertain when one process may be dominant.

For a hydrodynamic process, the FREADM problem was transferred to the VENUS² code. A calculational tool was not readily available to follow a hydraulic process, and in several pertinent cases the FREADM calculation was terminated at 10 seconds of problem time.

One important assumption within FREADM is that of constant fuel rod geometry. While this is not strictly true for the range of conditions studied, one may violate this assumption to some degree and still obtain meaningful results. Such methods were employed to investigate the effects of fuel motions resulting in negative reactivity feedback.

The two reactor anomalies considered as initiating events are a loss of primary sodium flow due to failure of the pumps (LOF) and an arbitrary ramp reactivity insertion of 3 dollars (transient over power – TOP) both of which assumed a complete failure of the reactor protection system. Calculations were performed with a nominal set of reactor variables and parametrically about the nominal condition, for each anomalous sequence.

The variables employed for parametric analysis were chosen on the basis of having the greatest potential impact on the fuel thermal energy and on the mechanisms for attaining nuclear subcriticality. The variables studied were the Doppler magnitude, reactivity insertion rate, sodium void worth, sodium voiding dynamics, fuel motions, and flow coast down rate.

Funding and time did not allow for a detailed computation of the control material response during the transient. Therefore, it has been implicitly assumed that the reactor control material (boron carbide) does not affect the transient behavior, except as a possible initiation source for the assumed 3 dollar reactivity insertion. This is an important assumption which requires detailed validation, especially for highly irradiated control assemblies which have large uncertainties in materials properties, cladding interactions, and helium gas behavior.

The nominal value and range of parameters were established based upon either available experimental data or theoretical evaluations, or both, as discussed in the following subsections.

Reactivity Insertion

A maximum control assembly worth of 3 dollars is typical for an LMFBR. The design limit for reactivity insertion in a typical LMFBR is approximately 5 ¢/sec while an upper limit of 3 \$/sec could result from a hypothetical hydraulic expulsion of the assembly. The more rapid reactivity insertion rates (although the magnitude would be expected to be less than 3 dollars) could also cover an anomalous reactivity insertion from core geometry changes associated with an improperly clamped core. Based on these considerations, the reactivity insertion is defined as 3 dollars of reactivity inserted within a range of 5 to 300 ¢/sec. A nominal rate of 50 ¢/sec was selected, it being an order of magnitude above the design limit.

The nominal rate selected was also equal to that being employed for the fast test reactor (FTR) safety analyses in order to yield an additional benefit. By comparison of the response of an LMFBR design with the FTR, differences in their respective experimental and analytic development requirements could be examined. An early identification and integration of differing system development requirements in the current development programs will maximize their return for required LMFBR safety analyses.

Doppler Magnitude

The nominal Doppler value results from physics calculations* which employ SEFOR experimental calibrations. The range of 30% to +20% applied is an estimate of the uncertainty in the calculation.

*Values employed in these analyses were selected prior to final analyses presented in Appendix B. The parametric range includes the final values.

Sodium Void Worth

Nominal values are based on physics calculations* which employ the BISYN code⁵ and are presented in Appendix C. A range of $\pm 50\%$ was selected based on Hanford Engineering and Development Laboratory (HEDL) estimates for the FTR⁶ and an additional allowance for future design changes.

Primary Flow Coastdown Rate

A typical design curve (Figure 1) for the reactor was employed for the nominal case. An arbitrary increase of 100% in rate was applied for parametric analyses. Initial studies have shown a low energy release dependence on this parameter.

Sodium Voiding Dynamics

The nominal voiding dynamics for the LOF sequence was based on an initial boilout (low superheat) of each reactor channel, modified by assumed, concurrent fuel failures which result from partial sodium voiding.

Sodium voiding which results from fuel failures in the TOP sequence were estimated by a single slug ejection calculation, and a review of the ANL E series flowmeter data. Further details on the considerations of the nominal sodium void behavior can be found in Appendix C.

An arbitrary range of -50% , $+100\%$ in the voiding rates was selected for parametric analyses.

Figure 1. Comparison of Flow Coastdown Characteristics

*Values employed in these analyses were selected prior to final analyses presented in Appendix B. The parametric range includes the final values.

Fuel Motion Reactivity Effects

Applicable engineering data in this critical area is in an early state of development. While most post mortem examination of TREAT overpower experiments indicate a net negative reactivity effect, some tests (GE C4A, B, J, PNL 598) have resulted in partial fuel compaction. The nominal case employs a zero net effect on reactivity due to fuel motions. The time scale, worth, and initiation triggers used for parametric analyses were based on engineering evaluation of the available data. Appendix C provides more information on the basis for this parametric range.

3.1.2 Reactivity Insertion-Transient Overpower (TOP) Results

Core response to the nominal 50 ϕ /sec reactivity insertion leads to fairly rapid fuel melting (50% areal solidus* at midplane of peak rod at 2.18 seconds) while the cladding and sodium remain near operational temperatures. Failure of the cladding, due to rapid fuel expansion (liquid phase density change of 9.6%), is assumed to occur at a core midplane 50% solidus condition* and results in local sodium boiling and liquid displacement. This value for a failure criterion is meant to approximate a low burnup fuel and is reasonably consistent with HEDL estimates. (Note that the actual HEDL failure criterion is based on cladding strain.) Since the core reactivity state is 62 cents above critical at fuel failure, small displacements of sodium from the central core positive void worth regions result in a significant power rise and the generation of hydrodynamic forces. With the introduction of significant hydrodynamic forces, the analysis was reformulated for continuation within VENUS. This model allows for the calculation of fuel motion reactivity feedbacks which ultimately terminate the nuclear transient. At transfer to VENUS, the predisassembly phase of the nominal TOP sequence is concluded. Subsection 3.2 describes the core disassembly phase of the accident sequence.

The sensitivity of the above calculated core behavior to variations in the identified parameters was studied in some detail. Primarily, one is interested in determining if large departures in core response can be affected by variations of the parameters within their known or estimated range. A small dependence on insertion ramp rate is exhibited in Table 1, which compares key reactor variables at time of initial failure in the peak rod (the cause of sodium voiding), and at the prompt critical condition. All cases attained hydrodynamic disassembly characteristics with the primary difference being a greater storage of thermal energy above liquidus conditions at the low ramp rate.

Table 1
COMPARISON OF REACTOR RESPONSES WITH INSERTION RAMP RATE

Insertion Rate (ϕ /sec)	At Initial Rod Failure			At Prompt Critical		
	P/P ₀	k _{eff} , ϕ	Period (sec)	P/P ₀	k̄, \$/sec	Period (msec)
5	2	20	19	58	30	6
50	5	62	~1	38	53	4
300	20	91	~0.1	42	49	4

For the larger ramp rates (≥ 50 ϕ /sec) the choice of a constant melt fraction to initiate rod failure is probably not critical, due to the supercritical reactor state and rapid fuel melting rate. For the 5 ϕ /sec insertion, one would expect the sodium dynamics to be of greater importance since the reactor is much less supercritical and large volumes of the core must void to attain prompt criticality. Also, it should be pointed out that on the time scale of a 5 ϕ /sec insertion (cladding failure at 12+ seconds), neglecting fuel redistributions internal to the cladding may be unjustified. However, it was not possible to consider these effects within the scope of the present task or with the analytic tools available for this study. This area will be studied in more detail under the Safety Engineering Project Agreement.

Variations in Doppler magnitude produced expected variations in the core behavior as shown in Figure 2. The effect on total energy release appears to be a smooth variation of reasonable amplitude. The dominant hydrodynamic mechanism for attaining termination of the nuclear transient was not affected by variations in Doppler magnitude.

*i.e. 50% areal solidus means that the fuel at a radius corresponding to 50% of the cross sectional area has reached a temperature of approximately 5100°F, corresponding to solidus temperature (incipient melt).

Figure 2. Effect of Doppler Magnitude on TOP Power History (FREADM)

The core power responded in a direct proportion to the total reactivity worth changes represented by variations in sodium voiding rate or reduced worth (see Figure 3). As stated before, the supercritical reactor state and high local sodium positive void worth result in minimizing the core dependence on these parameters. An interesting deviation from the assumed core midplane fuel failure location resulted by assuming that the rod failure is due to rapid melting and fission gas release in the equiax grain region for fuel with significant burnup which has operated at low power. (This is a tentative fuel failure mode proposed by HEDL, see APPENDIX F). The condition of rapid equiax melting existed at the axial end of the peak fuel rod when a 50% fuel melt volume occurred at the midplane. Case 4 of Figure 3 depicts the core response to void initiation at the outlet, due to the hypothesized failure in the equiax region, with downward ejection of sodium at approximately half the nominal rate for full channel voiding. The stabilization of the power due to negative sodium feedback lasted for a time scale which *could* lead to an alteration in the mechanism dominating the termination of the excursion. However, it is more likely that additional failures lower in the core and associated sodium voiding would establish a prompt critical condition and hydrodynamic disassembly. Thus, for the ranges considered, the physical processes and core thermal response are not very sensitive to voiding rate or void worth when voiding initiates near the core midplane. However, gross changes in the void pattern could significantly affect the mechanism for nuclear termination, as well as the energy release.

Assumptions on fuel motions, especially negative reactivity contributions, significantly alter the core energy release and dominant mechanisms for nuclear subcriticality. However, as demonstrated by Case 2 of Figure 4, the large positive sodium void worth of the demonstration LMFBR makes it imperative to integrally and consistently treat the sodium and fuel motions together. For larger reactor cores the importance of sodium void worth during local fuel relocations may be magnified due to the unequal scaling of fuel and sodium worth. Calculation of large fuel relocation effects with FREADM is not consistent with the base assumptions of the heat transfer or kinetics solutions in the code. However, these calculations were performed to investigate the magnitudes and time scales for such motions to become dominant.

3.1.3 Loss of Primary Flow (LOF) Results

The LOF transient is inherently more difficult to compute than the TOP. This is because the ultimate reactor state is determined by increasingly indeterminate, physical events in the entire core, including sodium boiling, loss of cladding strength, cladding melting and, potential fuel slumping. For the demonstration reactor, it appears likely that the large positive sodium void worth will substantially alter the reactor behavior from that seen in the FTR safety analyses, where fuel slumping becomes the controlling physical event.

For the nominal flow coastdown (see Figure 1), sodium saturation conditions are attained near the core outlet at approximately 25% of rated flow. At this point, the core inlet pressure almost balances the saturated vapor pressure (26 psia at assumed 60°C superheat). Thus, very rapid liquid slug ejections are not expected. As boiling and vapor formation progresses downward, sufficient core voiding occurs to initiate a substantial positive reactivity feedback and a prompt critical condition is attained. However, it is not clear whether the mechanism dominating fuel motion in the nuclear transient is hydrodynamic or hydraulic in nature. This is because of the long time scale (200 msec), large void volume, and low reactivity insertion rates (10 \$/sec) prior to prompt criticality. At the same time, the cladding would have lost most of its mechanical strength at the calculated temperatures. Parametrically, the flow coastdown rate and Doppler magnitude are not critical, during the predisassembly phase, in determining the net energy release or disassembly mechanism. The Doppler insensitivity (Figure 5) is attributed to the slow rate of power increase and the complex interaction between Doppler and power for the void pattern assumed. Table 2 compares the predicted core condition at 50 times steady-state power, just prior to prompt criticality. Variations in the assumed voiding rate would not affect the energy release strongly, if a hydrodynamic disassembly mechanism is the dominant shutdown mechanism. However, the strong effect seen on the time scale of the power response (Figure 6) could reasonably affect the mode of nuclear transient termination. It is possible that no single mechanism causing fuel motion is dominant in this event. As one would expect, a 50 percent reduction in total core sodium void worth (resulting in less than 1 dollar net void reactivity) alters the entire transient to an FTR type LOF transient. The history of this event was not pursued further since such high neutron leakage cores resulting in low positive sodium void worths are not likely to be considered for large LMFBRs.

Unfortunately, a detailed review of the LOF response with assumed fuel motions could not be completed due to FREADM code problems. However, the partial results which were obtained led one to the same conclusion as in the TOP sequence, i.e., the fuel motions on the assumed 50 msec time scale would become dominant in affecting the energy release and shutdown mechanism.

Table 2
COMPARISON OF PREDICTED CORE CONDITION JUST PRIOR TO PROMPT CRITICALITY
 $P/P_0 = 50$ (8 kW/gm-MO₂ *)

Case	Net Reactivity (\$)	Reactivity Insertion Projected for 25 msec (\$/sec)	Reactor Period (msec)	Fuel Average Temperature at Core Center	
				(°C)	(°F)
Nominal	0.97	4.9	29	2446	4435
120% Doppler	0.96	5.4	55	2865	5190
70% Doppler	0.98	4.9	36	2590	4695
200% Flow Coastdown Rate	0.97	5.0	48	2774	5025

* Mixed (uranium plutonium) oxide fuel

3.1.4 Problems Encountered in the Analysis of the Predisassembly Phase

Several difficult modeling problems were encountered within the analyses. For convenience and clarity, a discussion of these problems is included in Subsection 3.5.1, along with a discussion of the usage of SAS 2A.

3.1.5 General Results and Conclusions

For the nominal parameter set selected, the demonstration reactor will respond to an LOF or TOP anomaly (while unprotected by its multiple safety systems) with gross fuel melting and a coincident prompt or near prompt critical power burst leading to termination of the nuclear transient by hydrodynamic or hydraulic mechanisms.

Only fuel motions are considered capable of substantially altering the total energy release and termination mechanisms within both anomalous sequences. In the LOF situation, variations in sodium voiding dynamics could also change the character of the reactor transient.

Fundamental physical and engineering data are accumulating in the area of sodium voiding dynamics. However, sufficient knowledge of sodium behavior in a prototypic LMFBR environment is not available, especially under severe accident conditions, to substantiate the rate of voiding as applied in this analysis. More applicable data will result from the various LMFBR supported programs which are in progress (i.e., TREAT L, R, and ETR FEFPL series). However, these results will not be available in time to meet the current schedule of the Demonstration Reactor PSAR.

The treatment of fuel spatial relocations and associated time scales is in an embryonic state of knowledge. The only comprehensive, real time data on such phenomena are the neutron hodoscope measurements within the ANL TREAT program. These data should have qualitative use but have not been available on a detail and scale to be adequately assessed.

The potential effects of control materials on the reactor response have not been adequately treated in the FTR or LMFBR programs. The behavior of this material during these events may be critical because of its high reactivity worth.

The high payoff area in predisassembly analyses is obviously in the fuel motion regime. The large, positive sodium void worth of the LMFBR requires that supporting development programs focus on the combined spatial motions of fuel and sodium during rapid power transients (during either TOP or LOF situations) if a maximum positive impact on LMFBR safety analyses and licensing is to be obtained.

Figure 3. Effect of Voiding Rate, Worth, and Pattern on TOP Power History (FREADM)

Figure 4. Effect of Assumed Fuel Relocations on TOP Power History (FREADM)

Figure 5. Effect of Doppler Magnitude on LOF Power History (FREADM)

Figure 6. Effect of Void Rate and Worth on LOF Power History (FREADM)

3.2 THE CORE DISASSEMBLY PHASE – VENUS CALCULATIONS

3.2.1 Introduction

The core disassembly phase is the second phase in the sequence of the hypothetical accident analysis. The primary objectives of the analysis during this phase are to determine the total thermal energy generated, to evaluate the sensitivity of the energy to the parameters that control the course of the transient, and to provide information necessary to investigate fuel-coolant interactions.

For this phase of calculations, the reactor materials are assumed to behave as a homogeneous mixture with the properties of an isotropic, non-viscous fluid. Consequently, the hydrodynamic effects in the reactor dominate and the associated material displacements control the excursion. Indeed, in this study the criteria used for initiating the disassembly calculations were the criticality condition of the core and temperature distributions. Thus, at the start of this phase, the reactor is at or near a prompt critical condition. As a result of the rapid power and temperature rises, hydrodynamic forces occur, which lead to a gross disassembly of the core. The Doppler and disassembly reactivity feedbacks are the mechanisms which terminate the nuclear excursion. All of these events are treated in this second phase. The computer code, VENUS², is used to perform these disassembly calculations.

A detailed description of VENUS is given in References 2, 7, and 8 and will not be repeated here. Briefly, however, it is a two-dimensional, coupled, neutronics-hydrodynamic computer code. The power level and energy generated are calculated through a standard point kinetics formulation. Input driving reactivity and feedback reactivities due to Doppler and material displacements are included. Through the use of an appropriate equation of state, the generated energy is related to the corresponding internal pressures which constitute the driving force for a hydrodynamic analysis of the core material motions. This analysis is performed in a two-dimensional Lagrangian r - z coordinate system. Solutions of these motions determine core (fuel) displacement and the related feedback reactivities.

The disassembly calculations are performed for two accident conditions. These are noted as (1) the sodium-in cases and (2) the sodium-out cases. The sodium-in cases correspond to the transient overpower accidents, while the sodium-out cases correspond to the loss of primary coolant flow accidents. The reference demonstration plant modeled is shown in Figure A 1 of Appendix A. Details of the models and other input descriptions are given in Appendix D.

The core was divided into a total of eight regions. The first four regions comprise the core (Zones I and II), Regions 5 and 6 are the axial blankets and Regions 7 and 8 are the radial blankets. Three different physically spaced grid systems were used consisting of 14 radial x 11 axial nodes, 20 radial x 18 axial nodes, and 25 radial x 22 axial nodes, respectively. The basis for selecting these different grid systems and the results obtained are discussed in Subsection 3.2.4. Appropriate material worth values, Doppler magnitudes, power peaking factors, and other required design data are given in Appendix B.

The nominal VENUS disassembly cases were generated from the corresponding nominal FREAMD predisassembly cases for both accident sequences. The parametric variations performed within the predisassembly (FREAMD) calculations were consistently carried through the VENUS calculations. In addition, the conditions within the nominal VENUS cases were also parametrically varied, in order to establish independently their sensitivity for affecting the transient. As in the predisassembly phase, the variables employed for the consistent parametric analysis were Doppler magnitude, sodium void dynamics, and flow coastdown rate. In addition, the initial conditions and other parameters such as reactivity insertion rates, sodium void distribution, equation of state and material reactivity worths, were also parametrically varied from those used for the nominal VENUS cases.

Subsection 3.2.2 contains the sodium-in, transient overpower case results. The results of the sodium-out, loss of flow cases are given in Subsection 3.2.3. Specific problems encountered in the analysis and the conclusions are presented in Subsections 3.2.4 and 3.2.5, respectively.

3.2.2 Transient Overpower (Sodium-In) Cases

The FREAMD nominal TOP case was used to define the VENUS "nominal" sodium-in TOP case. The criteria used for selecting the FREAMD time step (time in the transient) to start the VENUS calculations are the temperature

distribution (specifically, peak fuel temperatures) and criticality condition of the core. At the time selected, the reactor was nearly prompt critical ($k_{\text{eff}} = 0.94$). The temperature distribution at this time step in the transient (Figure D 2 in Appendix D) corresponds to approximately 4200 MW sec total energy (above 298°K reference condition) in the core with peak fuel temperatures in excess of 3100°K. Based upon the FREAMD calculations, and equation of state relations, these conditions are projected to correspond to the initiation of the effective buildup of hydrodynamic forces. This condition occurred at 2.2109 seconds into the FREAMD (predisassembly) phase of the transient where the power level is approximately 20 times the design steady state full power. A Doppler coefficient of $T(dk/dT) = 0.0047$ was used in this "nominal" case. The driving reactivity rates were used in the form of a tabular input as predicted by the FREAMD calculations (as given in Figure 7 start of nominal case) for the expected duration of the VENUS phase of the calculations. However, effects of using linear ramp rates were also investigated, parametrically, as explained below.

In the nominal case, a peak power level of approximately 100 times the initial power level (~2000 times rated steady state full power) was reached in about 10 msec before the Doppler feedback started to turn the excursion around. At this time, the Doppler feedback was approximately twice the value of the disassembly feedback, and about 70% of the total energy produced during the disassembly phase had already been generated. However, within the next millisecond, the disassembly feedback became significant and terminated the accident. Results of this VENUS calculation yielded an energy addition of approximately 3650 MW sec, out of which 2540 MW-sec was contained in the molten fuel. The total thermal energy (above 298°K) stored in the fuel at the termination of the transient was approximately 7850 MW sec.

The results of the parametric VENUS studies (initiated from the same FREAMD time step) are presented in terms of the energy generated in the VENUS calculation unless otherwise noted. These parametric studies included (1) Doppler coefficient variations, (2) linear ramp rate variations, (3) void patterns and VOID option, and (4) the variation of starting point. The results from the variations of the Doppler coefficient are presented in Figure 8. Figures 9 and 10 show that attempts to estimate an average linear ramp rate over the duration of the disassembly phase of the transient, where the reactivity insertion rate varies over a wide range (as predicted by FREAMD calculations), introduced significant errors in the predicted energy release. The results from the investigation of the void patterns and VOID option in the code (see Reference 2 for details of the VOID option) pertaining to the pressure calculations in the voided nodes are presented in Figure 11 and Table 3.

Sensitivity to the VENUS starting point is shown in Table 4. Note that the values presented in Table 4 include the energy generated in the FREAMD calculation. The power traces of these three cases are shown in Figure 12.

A total of five FREAMD cases have been consistently followed through the disassembly phase (as five different VENUS cases). These cases consisted of Doppler variations (two cases), voiding rate (two cases) and high reactivity ramp insertion of 3 \$/sec (one case).

The total energy in the core (during predisassembly and disassembly) and energy content in molten fuel at the end of the accident for these cases are presented in Table 5. Also included in Table 5 are the energy releases of the cases where the Doppler coefficient was varied at the start of the VENUS calculation for comparison with those cases where the Doppler coefficient was varied at the start of the FREAMD calculation. Energy releases were the same for the same Doppler coefficient.

Table 3
EFFECT OF VOID IN VENUS

	Energy (MW-sec)	Energy in the Molten Fuel (MW-sec)
Nominal — No Void	3649	2540
Void Pattern	4057	2940
Void Option	3873	2760
Complete Void of Regions 1 and 3	6776	5660

NOTE: Thermal energy content in the nominal case at the start of VENUS calculation is 4193 MW sec.

Table 4
EFFECT OF VARYING THE VENUS STARTING POINT
 (Using the reactivity insertion as in Figure 7)

	Time Entering VENUS Code (sec)	Total Energy Including FREADM (MW-sec)	Energy in the Molten Fuel (MW-sec)
Earlier Start	2.1875	7982	2710
Nominal	2.2109	7842	2540
Later Start	2.2178	6586	1220

Table 5
OTHER PARAMETRIC STUDIES – SODIUM-IN

	Total Energy Including FREADM (MW-sec)	Energy in the Molten Fuel (MW-sec)
2 x Voiding Rate	8814	3510
0.5 x Voiding Rate	7134	1830
3\$ sec Initial Ramp Rate Insertion	7770	2490
-0.0047 Doppler Coefficient (Nominal Case)	7842	2540
-0.0035 Doppler Coefficient	8099	2830
-0.0035 Doppler Coefficient at Start of VENUS	8193	2880
-0.0056 Doppler Coefficient	7626	2320
-0.0056 Doppler Coefficient at Start of VENUS	7643	2350

Figure 7. Venus: Reactivity Insertion – Sodium-In

Figure 8. Energy versus Doppler, Sodium-In

Figure 9. Energy versus Linear Ramp Rate, Sodium-In Nominal Case

Figure 10. Venus: Sodium-In Power Traces of Varying Ramp Rates on Nominal Case

Figure 11. Effect of Voiding Assumptions in Venus "Sodium-In"

Figure 12. Effect of Varying Starting Point in Venus "Sodium-In"

The variations in energy release are consistent with those obtained in previous studies^{8,9}. The largest variations (for the range of parameters studied) occurred for changes in the voiding rate, but even these variations are not large compared to the uncertainties in the conversion from thermal to mechanical energy (MFCI phase). The results of the VENUS calculations based upon varying the starting point show (Table 4) that the energy release is significantly less when the disassembly calculation is commenced unusually late in the transient. Earlier start and nominal start cases yielded essentially similar energy releases. The reason for the significantly under-predicted energy release in the case of the late start is related to the combined effects of the initial steeper single-phase pressure gradients, higher instantaneous Doppler reactivity feedback, and the higher instantaneous input reactivity (due to the nonlinear reactivity ramps) encountered relative to those that exist for the earlier starts.

The nominal FREADM case predicted approximately 1% of the core to be voided of sodium. This voiding is predicted in the midplane nodes of the first and third regions of the VENUS model. The VOID option in VENUS (see Reference 2) was used to introduce small amounts of initial void space to simulate this void pattern. Results using this option lowered the energy slightly from the energy calculated using the actual void pattern. A calculation was also performed assuming complete voiding of Regions 1 and 3 (Figure 13). The energy release was greatly increased. As anticipated from VENUS code, the increase in energy release due to voided condition of the core is generally attributable to the larger energy required to generate equivalent hydrodynamic forces and comparable disassembly feedbacks, as in a nonvoided (or less-voided) core. This sensitivity of the VENUS calculation to the modeling of the void pattern at the start of the VENUS calculation is of significant interest and an area which requires additional investigation.

3.2.3 Loss of Flow (Sodium-Out) Cases

A number of sodium-out cases were studied corresponding to the loss of primary coolant flow sequence. These analyses assumed that the core was completely voided of sodium at the start of the disassembly calculations. The predisassembly FREADM calculations actually predicted that the core was approximately 80% (symmetrically about core center) voided of sodium for this case. However, as in Subsection 3.2.2 of this report, it can be seen that the assumption of a completely voided core is considered conservative for the cases investigated in this study because, in VENUS calculation, a completely voided core usually requires larger energy generation to create hydrodynamic forces equivalent to those encountered in a less-voided core.

Figure 13. Power Trace of Completely Voided 1st and 3rd Venus Regions "Sodium-In"

The FREADM nominal LOF case was used to generate the nominal sodium out VENUS case. A detailed description of the models and other data used in these calculations are given in Appendix D. As discussed previously, the criteria used in selecting the FREADM time step to start the VENUS calculations are the criticality condition of the core and the temperature distributions (peak fuel and cladding temperatures, and average energy density in the core).

At the start of the disassembly calculation in the nominal case, the reactor was nearly prompt critical ($k_{\text{eff}} = 0.985$) and contained total thermal energy of 4770 MW sec (above 298°K reference condition). Figure D 3 in Appendix D presents the corresponding initial temperature distribution. The peak cladding temperatures were above 1400°C (cladding melt temperature) and thus would not offer any constraint to fuel motion. The above conditions are expected to correspond to the initiation of the effective buildup of hydrodynamic forces and the point where the reactor materials are likely to behave as a homogeneous fluid. The geometrical grid system used for the nominal condition consisted of 20 radial and 18 axial nodes. Even though it was necessary to input the driving reactivity in tabular form for the sodium-in cases, reactivities predicted by FREADM in the sodium out cases were essentially linear. Consequently, linear reactivity insertion approximation was used in the sodium out cases. The results of the nominal case show that power level increases only ~4% from the initial power level at the start of the VENUS calculations before the Doppler feedback turns the excursion around. Up to this point, the Doppler feedback is the dominant reactivity feedback. At this point, the disassembly reactivity feedback builds up rapidly terminating the nuclear excursion. The time histories of reactor power, Doppler, disassembly feedbacks, and the energy generated are shown in Figures 14, 15, 16, and 17. The energy addition computed in the nominal case was ~1130 MW sec with peak fuel temperature of ~3850°K at core center. The energy content in the molten fuel by the end of this transient was ~630 MW sec (Table 6).

Parametric studies on the nominal case were performed for several parameters including material reactivity worth values (magnitudes and slopes) and equation of state. The two parameters found to have the most significant influence on the disassembly calculations were, as expected, the Doppler coefficient and the input reactivity ramp rate. The results from the parameter studies on Doppler magnitude are shown in Table 6 and Figure 18. From this table it is seen that the generated energy varies inversely with the Doppler, as expected. The results from the parameter studies of

Table 6
VARIATION OF THE DOPPLER COEFFICIENT
 (Nominal $T(dk/dT) = -0.002585$)
 Nominal Sodium-Out Case*

$T(dk/dT)$	Energy Generated [in VENUS] (MW-sec)	Energy in the Molten Fuel (MW-sec)
70 Nominal	1267	729
Nominal	1139	629
120% Nominal	1039	547

* Based on 20 radial nodes x 18 axial nodes grid system

reactivity insertion rates (Table 7 and Figure 19) concur with an earlier study⁹ which reported that the generated energy (in disassembly calculations only) varies with the ramp rate raised to an exponent of 0.7. This empirical relationship appears to be applicable over the range of reactivity ramp rates encountered in this study.

Several of the parametric FREADM cases have been consistently carried through the disassembly calculations as VENUS cases using the coarse grid model consisting of 14 radial nodes and 11 axial nodes. It is to be noted that the VENUS nominal case energy release from the coarse grid model is slightly higher than that obtained from the fine grid model consisting of 20 radial nodes and 18 axial nodes. The parametric study included Doppler variations, more rapid flow coastdown rates, and faster sodium voiding rates. The results are summarized in Table 8. From these, it is seen that the faster coastdown rate of primary flow did not alter the excursion, excepting the duration of the power pulse. Faster sodium voiding rates resulted in greater energy generated. This is due to a larger effective reactivity ramp existing at the start of the disassembly calculations.

Figure 14. Nominal Sodium-Out (LOF) Power Pulse

Figure 15. Nominal Sodium-Out (LOF) Doppler Feedback Reactivity

Figure 16. Nominal Sodium-Out (LOF) Displacement Feedback Reactivity

Figure 17. Nominal Sodium-Out (LOF) Energy Generated

Figure 18. Energy versus Doppler Variations, Sodium-Out Cases

Table 7
REACTIVITY INSERTION RATE VARIATIONS ^(a)
NOMINAL SODIUM-OUT CASE

Rate (\$/sec)	Energy Generated [in VENUS Only] (MW-sec)	Energy in the Molten Fuel (MW-sec)
5	1230 ^(b)	590 ^(b)
15	3110	2354
20	3860	3154
40	5710	5171

^(a) Based on 14 radial-nodes x 11 axial-nodes grid system.

^(b) Value used in the nominal case, with a thermal energy of 4770 MW-sec computed in FREADM.

Table 8
CONSISTENT PARAMETRIC VARIATIONS
SODIUM-OUT CASES

Parameter	Total Energy Including FREADM (MW-sec)	Energy in Molten Fuel (MW-sec)
Nominal Case	6000	590
Doppler Coefficient		
70% Nominal	6220	710
120% Nominal	5830	530
Coastdown Rate		
200% Nominal	5975	565
Voiding Rate		
200% Nominal	7200	1810

From the results in Tables 6 and 8, it can be seen that the variations of the Doppler coefficient in the parametric studies of the nominal case (VENUS only) and in the consistent parametric cases (FREADM and VENUS) showed essentially similar sensitivity with respect to the energy generated and energy content in the molten fuel.

The general behavior of the transients for these sodium-out cases is essentially the same, in the sense that reactivity insertion rates of the order of 5 to 10 \$/sec were encountered at the start of the calculations. Consequently, the duration of the excursions were long (of the order of 40 msec) and smaller amounts of energies were generated. These are essentially characteristic of "mild" excursions, in which the localized fuel motions and fuel-coolant interaction are likely to be more significant than the gross disassembly of the core, as has been assumed in these calculations.

3.2.4 Problems Encountered

Several significant problems were encountered in the course of performing the disassembly calculations using VENUS. Most of these, however, could be attributed to the lack of complete understanding and experience in

Figure 19. Energy versus Ramp Rate Variations, Sodium-Out Cases

operation of the code and did not necessarily affect the accident analysis. These problems are discussed below, to provide a better understanding of the utilization of the code by other users.

It was found that one of the important considerations in modeling the reactor core in VENUS is the geometrical mesh description. The choice of number of nodes is closely related to the computer costs for the analysis. Initially, 12 radial nodes and 7 axial nodes were used (excluding blanket nodes). This mesh size was selected to minimize the amount of input data adjustment necessary to transfer from the FREADM mode to VENUS, and is referred to as the "coarse grid" model. The average mesh size was about 10 cm. This was found to be adequate for sodium-out cases. However, it was not adequate for the "sodium-in" cases since the "coarse grid" model gave quite different results than a "fine grid" model (see Figure 20). For sodium-in, "fine grid" with 18 radial and 14 axial nodes (excluding blankets) had to be used with an average mesh size of about 5 cm. This sensitivity to mesh size is a result of the modeling of hydrodynamic effects and large single-phase pressures that occur for the sodium-in core. Figure 20 shows the effect of grid system on the power trace for sodium-in cases and Figure 21 presents similar information for the sodium-out cores.

Other problems encountered consisted of numerical instabilities. Two types of instabilities were encountered. These were (1) problem related to "White" stability criteria (see Reference 2 for the details) and (2) nonconvergence of pressure calculations. The stability criteria were found to be closely related to the time step size in the computations. About 20-microsecond maximum step sizes were found to be adequate to avoid these instabilities. However, nonconvergence of pressure calculations continued in sodium-in cases. This problem was traced to the highly sensitive single-phase pressure calculations and the resulting high numerical precision required. The modified single precision version of VENUS written by ANL/RAS and made operational on the GE-635 computer system was not adequate for computing sodium-in cases. Cognizant personnel at ANL were requested to make the latest improved double precision version of VENUS (VENUS-II)² operation on the CDC-7600 computer at Berkeley, California. All sodium-in cases were then investigated using this version.

3.2.5 General Discussions and Conclusions

The results of the nominal cases and parametric studies using the VENUS code for the two accident sequences of TOP and LOF have been presented above. In the TOP cases, VENUS calculations show that the energy generated was

Figure 20. Sodium-In Case Grid System Effects

Figure 21. Sodium-Out Case Grid System Effects

sensitive to the input reactivities and the Doppler magnitude. However, the uncertainty associated with the input reactivity is likely to be much higher than any possible uncertainty in the Doppler magnitude. Another parameter that was found to influence the VENUS results was the sodium void fraction in the core. As reported in other studies¹⁰, however, this sensitivity could be attributable to steep gradients in the pressure energy density relationship dependence on void fraction for low initial void fractions.

The LOF cases studied also showed similar parametric sensitivity to energy generation in VENUS calculations, as in TOP cases. However, the input reactivities generally encountered in LOF cases were of the order of 5 to 10 \$/sec. The pressure time histories and low energy density behavior observed due to the low reactivity rates and large void fraction appear to be contrary to the basic assumption of the hydrodynamic behavior of the core. Thus, in these LOF cases, localized fuel motions and fuel coolant interactions appear to be more significant than the gross hydrodynamic disassembly of the core. Further modeling efforts should provide capability to treat these conditions more correctly.

On the whole, the VENUS studies showed that the variations in energy release are consistent with those observed in earlier studies and the energy releases calculated to the condition of nuclear subcriticality were ~ 7000 MW sec $\pm 25\%$. The largest variations of 25% in energy release observed, however, are not large compared to the anticipated uncertainties in the conversion from thermal to mechanical energy (MFCI phase).

Regarding the code, it has been the experience that VENUS could be used easily and efficiently because a good code description is available. For the TOP cases where steep gradients of pressure are anticipated, high numerical accuracy in the calculations is essential. For instance, single precision version of VENUS was not adequate to obtain acceptable convergence in the pressure calculations for TOP (sodium in) cases.

Similarly, proper selection of the spatial grid system is essential to assure acceptable convergence of the calculations. Usefulness of the code could be extended for treating the "milder" excursions by incorporating the treatment of fuel coolant interactions and the coolant expulsion effects.

3.3 FUEL-COOLANT INTERACTION – MFCI CALCULATIONS

3.3.1 Introduction

Hypothetical accidents of the type investigated in this study cause substantial increases in core fuel thermal energy which could result in failure of fuel cladding. The ejection of fuel into the sodium coolant may produce violent interactions between the molten fuel and the sodium prior to, or shortly after, termination of the nuclear excursion, with consequent generation of large and potentially damaging pressures. The ANL developed MFCI³ code was utilized to characterize these pressures. The current version of the code used for this study reflects the state of uncertainty with respect to many of the important factors which influence the fuel sodium reaction rate by treating them as user selected input parameters. The one dimensional nature of this code's calculational routine and other assumptions with respect to coolant channel boundary conditions are inconsistent with physical conditions which could reasonably be expected to accompany a gross core fuel coolant interaction accident in a demonstration plant reactor. Consequently, the scope of this phase of the accident analysis was restricted in calculational time to about 10 msec and in extent to three different cases. The initial fuel and coolant conditions for these cases are representative of transient overpower type accidents. The assumption that the interaction between the fuel and sodium occurs in the unvoided core region is inappropriate to a loss-of-flow accident. Models being developed at ANL in support of FTR Final Safety Analysis Report (FSAR) specifically treating this condition will be evaluated under the Safety Engineering Project Agreement.

3.3.2 Calculations and Discussions

The code for fuel coolant interaction utilized for this phase of the calculations is the FCI model developed by Argonne National Laboratory. The code is described in ANL 7919³ with supplementary information on recent modifications^{17,18,19} supplied by personnel from the Reactor Analysis and Safety Division of ANL. The model

considers the molten fuel particles to be uniformly dispersed in sodium over the length of a mixing zone. The pressures assumed to be constant over the length of the mixing zone, are determined from equations which describe the two competing processes of heating and expansion of the sodium within this zone tending to increase pressures, and the increase in volume provided by expansion of this zone against the constraints provided by the surroundings. Though this ANL model incorporates a simplified representation of the processes which take place, the parameters required to adequately represent the processes described by the model are not known. Since supporting experimental information is unavailable for many of the variables and processes modeled, these variables are input to the code as user determined parameters.

The MFCI code version which was utilized incorporates several calculational assumptions inconsistent with the likely course of events or conditions existing in the demonstration plant reactor. These include

- The equations of the code provide only for one dimensional behavior of the coolant. They also assume that geometry of the flow channel is preserved during the accident and provide for limited "elastic" distention of the coolant channel. Since the calculated pressures are in the order of hundreds of atmospheres, the channel would be expected to fail. Because of nonuniform core power generation and/or voiding during an accident involving gross core fuel coolant interactions, fuel assembly failures are not expected to be "coherent" and effective radial restraint would be much lower than represented by this code.
- The code assumes symmetrical behavior about the core midplane. This may not be a plausible assumption, particularly if radial constraint is relaxed so that interaction pressures are lower.
- The equations incorporate a transition from an "acoustic" to an inertial phase with the time for this transition based on the transit time of a pressure front to a free surface above the core plus the time for the reflected rarefaction wave to return to the interaction zone. Since large amounts of steel structure are in the path between the core region and the sodium surface of the demonstration plant, pressure fronts striking these surfaces may be totally or partially reflected as pressure fronts, and these formulations in the code may be inappropriate.

Future work in this area should utilize models that better represent the demonstration plant configuration. Until additional experimental information on fuel coolant interaction and structural response of reactor internals becomes available, parametric evaluations will still be required. A criterion for such evaluations should be that the "nominal" conditions represented be consistent with the environment in the reactor and with current understanding of processes which occur. The MFCI calculations which are reported in this document do not meet that criterion and, consequently, have not been utilized for characterizing the pressure "source" term for the REXCO structural response calculations. The REXCO HT program being developed by ANL may come close to meeting this criterion and should be evaluated for this purpose.

Three molten fuel coolant interaction (MFCI) cases were considered for this study. The initial conditions for these cases are representative of conditions in the average core fuel for either a VENUS sodium in TOP calculation assuming a linear ramp rate of approximately 25 to 30 \$/sec or for nominal sodium out conditions. Initial conditions for these calculations are shown in Table 9. The channel wall has been assumed rigid in all cases. (Prior ANL studies have indicated little difference in results for flexible vs rigid channels.) The fuel particle radius of 0.0117 cm has been used for previous parametric studies³ of the FFTF core and is supported by limited experimental data. The length of the molten zone selected is half the core height. This corresponds to the entire length of fuel region of the core being molten since the code assumes symmetry about the midplane. The unheated sodium column length is the distance from the top of the core to the top of fuel assembly channels.

The constraint condition used for the three cases was acoustic constraint (application of compressible one dimensional equations for an infinite distance) up to the acoustic "unloading time" and inertial constraint (incompressible flow equation applied to the unheated column length) at longer times. The acoustic unloading time is determined as the time for a pressure wave in the interaction zone to travel round trip to the sodium "free surface." The calculation assumed a free surface condition existed at the top of the fuel channel. The accuracy of this determination of unloading time using this assumption is dependent on the degree of coherence of the core interaction.

Table 9
INPUT DATA FOR FCI CALCULATIONS

Sodium Temperature	1144° K
Sodium Specific Volume	1.34 cc/g
Initial Pressure (mixing zone)	3.5 atm
Fuel Temperature	3273° K
Fuel/Sodium Mass Ratio	8.45 g/g
Flow Area	0.110 cm ² /g
Total Sodium Mass	1.32 x 10 ⁵ g
Pressure in the Undisturbed Sodium	1.5 atm
Fuel Particle Radius	0.0117 cm
Length of Molten Zone	38.85 cm
Equivalent Diameter of Subassembly	86.80 cm
Length of Unheated Sodium Column	204.95 cm
Plenum Pressure	34 atm
Fragmentation and Mixing Time Constant	0.0035 sec
Initial Gas/Vapor Blanketing of Fuel Particles	None
Wall Deformation	None

processes. For a single channel failure, the pressure pulse may tend to reflect from the sodium plenum above the channel as a rarefaction wave. For simultaneous failure of many channels, the reflection from structures above the channels would cause a pressure increase.

The three cases calculated included one case assuming heat transfer throughout the transient. Based on this, a second case was run stopping heat transfer at approximately 50% sodium vapor volume fraction. Effects of stopping heat transfer at the end of the acoustic portion were also investigated. Studies were initially carried out (using a version of the MFCI code available on the GE-635) investigating several other input parameters of the code. However, due to instabilities in the code available on the GE-635, it became necessary for GE's liaison engineer at ANL to carry out the calculations at ANL's computer facility.

Results of these calculations are presented in Figures 22 and 23 for the three cases. Figure 22 shows the time dependence of pressure and specific volume. The heat transfer rate prior to heat transfer cutoff was determined from quasi-steady-state equations for fuel to liquid sodium heat transfer without gas or vapor blanketing of the fuel particles. Since vapor generation during fuel-coolant mixing can be expected to reduce the average heat transfer rate in the interaction region to below that from fuel particles directly to liquid sodium, this assumption gives a conservative evaluation of heating rate and pressure of sodium, within other constraints of the code calculational methods. The heat transferred to the sodium is highest in Case 3, where no heat transfer cutoff has been utilized in the calculation. Such cutoff could be produced by vapor blanketing of fuel particles when the sodium pressure drops below the vapor pressure corresponding to the sodium temperature. Case 3, therefore, implies that the vapor blanketing phenomenon does not occur.

Case 1 assumes that vapor blanketing and heat transfer cutoff occur when the sodium vapor fraction in the coolant reaches 50% by volume. Though this assumption is based on judgment with respect to the amount and distribution of voids, it also corresponds to peak pressure during the inertial phase of the calculation. Thus, both Cases 1 and 3 reach the same peak pressure of about 200 atmospheres. Case 2 is based on heat transfer cutoff at the start of the inertial phase, when vapor first starts to form. The maximum pressure after the acoustic phase is about 60 atmospheres.

Figure 22. MFCI: Pressure versus Time; Specific Volume versus Time

Figure 23. MFCI: Pressure versus Specific Volume

The potential damage to the reactor vessel and vessel components is caused by the work done by the core materials as they expand. This work, which is determined by $W = (\text{Mass}) \int p dv$, is proportional to the area under the pressure (p), specific volume (v) curves in Figure 23. It is seen that the work done during the acoustic phase is small compared to that during the longer duration inertial phase. To estimate the work energy expended in expansion to low pressures, it is necessary to extrapolate the curves to considerably lower pressures. Isentropic expansion of the two phase sodium, which behaves much like a $p v = \text{constant}$ relation, would result in an estimate of 150 MW sec for Case 1 for expansion to one atmosphere final pressure. The actual expansion for an accident is likely to be to a pressure of tens of atmospheres as determined by the reactor vessel strength, and the work substantially lower. For expansion to either pressure, the required extrapolation is so great as to be suspect. As mentioned previously, the MFCI analysis was not extended to cover the expansion to one atmosphere final pressure because the one-dimensional model with the simple axial constraint is not valid for the large changes in specific volume that must occur to achieve this final pressure.

3.3.3 Conclusions and Recommendations

- The current version of the MFCI code utilized for these studies includes one dimensional modeling and other assumptions with respect to coolant channel boundary conditions inappropriate to gross core fuel coolant interactions in a demonstration plant reactor.
- Code improvements should be instituted which (1) better reflect the multidimensional character of the processes accompanying fuel coolant interaction and (2) permit improved representation of the channel boundary conditions of a demonstration plant. Until adequate supporting experimental information becomes available, flexibility of input assumptions should be provided to permit user parametric evaluations.
- Representation of gross core coherent fuel coolant interaction process could best be achieved with a code which also provides for determination of core structural response. A combined MFCI REXCO code seems desirable. Provision should be made for multiple reaction zones in order to account for lack of a coherence in the core fuel coolant interaction processes, and for heat generation which occurs during the interaction process.
- Priority should be given to obtaining supporting experimental data in the several areas to which the calculational results appear sensitive. These include
 - a Heat transfer between fuel particles and sodium, including improved sodium properties data
 - b Fuel rod and fuel channel failure data
 - c Channel and fuel rod structural response to coolant interaction pressures
 - d Effect of boundary conditions on pressures in the interaction region

3.4 STRUCTURAL RESPONSE TO HCDA

3.4.1 Introduction

In this subsection the prediction of structural damage in hypothetical accidents is discussed. The energy deposited in the core during the power excursion results in a significant quantity of molten fuel. The thermal interaction of the molten fuel with the sodium coolant is an energy transfer mechanism (discussed in the previous section) in which sodium becomes a pressurized thermodynamic working fluid, capable of performing mechanical work on the constraining structures in the reactor assembly and the primary coolant system boundary. The partition and distribution of this potential work energy is evaluated using REXCO-H,⁴ a two-dimensional structural/hydrodynamics code developed by ANL which performs a finite difference solution to the conservation and continuity equations of compressible media written in Lagrangian coordinates for a network of connected material elements.

Scoping calculations were also made using RECORD, a simplified code available at GE.

The major emphasis with respect to structural response evaluations was placed upon acquiring and running the REXCO H code developed at ANL using a typical geometry of the demonstration plant as a model. As is usual with

computer codes of the scale of REXCO, a significant effort was required to understand the detailed requirements on input preparations and eliminate errors in interpretation of the user supplied information. The code was converted from use on the ANL IBM machine to the GE 635 machine to facilitate this familiarization and checkout effort. A reactor model was defined and checkouts were performed by running through the first few time steps to eliminate errors. In the later stages of the program, some early administrative difficulties which had precluded consulting directly with ANL personnel were resolved and the GE liaison engineer at ANL confirmed that the reference calculations for the initial time steps using the IBM computer gave results identical to those obtained on the GE computer. Detailed analyses conducted were limited to two cases which were run on the IBM computer using GE supplied data decks since this proved somewhat more economical to the project and some added output options were available at ANL.

The two analyses were carried out for approximately 30 msec of real time. The geometry was held constant in each, and two source terms ($p-v$ relations) for the core expansion were used. These were selected such that the mechanical work potential of each was the same, i.e., the same expansion work (integral $p dv$) to 1 atmosphere. Since the work was accomplished in parallel with the MFCI calculations, these source terms were arbitrarily chosen to provide information on energy partition in the reactor.

The initial pressure and the functional relationship of pressure to volume were varied. In the first (referred to as HCDA 1), an expansion of the form $p v = \text{constant}$ from an initial pressure of 456 bars was used. This is representative of the simulation used for FFTF in the Stanford Research Institute (SRI) experiments. In the second case (HCDA 2), a "slower" expansion from a lower initial pressure (159 bars) was used to simulate a variation in the $p v$ relationship which varying assumptions in the MFCI analysis could induce.

A series of calculations were also made using the RECORD code. This code is much less sophisticated than REXCO and was written to provide an economical method of estimating the energy distribution in the reactor structures resulting from a core excursion. The analyses made of the FFTF SRI tests indicate a reasonable correlation can be made based on gross energy distribution, but local deformation and local loading history are not predicted in RECORD. If the REXCO series of codes prove to correlate satisfactorily with experiments, and if adaptation of REXCO to the CDC 7600 machine results in the anticipated reduction in computer costs, the economic considerations which have restricted GE to the use of the RECORD code in the past will be reduced and emphasis placed on the use of REXCO codes.

3.4.2 REXCO Analysis

3.4.2.1 Introduction

Evaluation of the reactor structural response to a hypothetical core disruptive accident has been accomplished using ANL's REXCO H⁴ code. The more advanced REXCO HEP code did not become operational sufficiently early to allow for adequate code and computer familiarization by GE personnel within the PA 67 contract period. This work is continuing under the Safety Engineering Project Agreement. REXCO H is an IBM 360 computer program for determining the two dimensional hydrodynamic response of a primary reactor containment system to a high energy excursion. The code numerically solves the equations of hydrodynamics and the materials equations of state in finite difference form and determines pressures, specific internal energies, densities, strains, and velocities at each space point and time step. This analysis is decoupled from the disassembly and MFCI analysis. It uses a $p-v$ relationship for the pressurized material in the core supplied by the user. All regions are treated as non viscous fluids and geometry is restricted to that of right circular cylindrical segments. Vessels (including plates and shells) are treated as membranes having mass but no resistance to bending. This imposes some limitations on modeling an actual reactor geometry, but judicious selection of equivalent thickness and density can produce a reasonable solution. REXCO HEP is intended to improve the limitations of REXCO H in this area.

Application of the REXCO H program for structural response calculations was limited to two cases, referred to as HCDA 1 and HCDA 2 in the following paragraphs. To assure obtaining useful information on the distribution of accident energy release within the reactor, the source terms for the two runs were selected to be of somewhat greater energy release than that indicated by preliminary MFCI results. Since uncertainties in the current understanding of the fuel failure coolant interaction process could readily alter any pressure volume relations currently in use, this parameter was varied for the two REXCO runs.

Major objectives of these calculations were

- a Evaluation of the characteristics and consequences of HCDA's for a reference reactor concept
- b Evaluation of methods and models used

Some difficulties were encountered in formulating the code input which delayed getting the code operational. Most problems encountered could be attributed to misinterpretations of code input requirements. These problems were resolved after arrangements were completed which resulted in active assistance from personnel of ANL's Containment Analysis Section and from GE's liaison engineer at Argonne, who also ran the two HCDA cases on ANL's IBM 360 computer. Because of the somewhat limited operational success with REXCO, comments relative to the above two objectives are preliminary and subject to confirmation or modification through continued experience with the REXCO program(s) and consultations with cognizant individuals at ANL.

3.4.2.2 Input Information and Modeling

Source Terms

The input source term for the REXCO calculation is a pressure volume relation for the reactor core material. Since scheduling considerations required that REXCO calculations be carried out in parallel with the Molten Fuel Coolant Interaction runs, selection of the source term for REXCO was made when only preliminary results were available from the MFCI calculations. Since those results indicated maximum core pressure of only about 200 atmospheres (even with continuation of heat transfer), it was decided to utilize a more severe pressure volume relation for one of the REXCO calculations in order to obtain better information on energy deposition within the reactor for potentially limiting accident magnitudes. An arbitrary choice of a 300 MW sec energy release (based on expansion to one atmosphere pressure) was selected for both REXCO cases. The REXCO code requires that this p-v information be input as an equation of state for the core materials.

The equation of state used for both REXCO calculations was of the form $p v^n = \text{constant}$, where n values of 1.0 and 0.75 were selected. The former value provides a good approximation to the isentropic expansion line for a high pressure two phase sodium liquid vapor mixture. As such, it may be considered representative of the sodium expansion subsequent to heat transfer cut off in a severe MFCI type calculation. The latter value of n was selected as a sufficient variant on the former value to permit assessment of the consequences of a change in this parameter on the distribution of accident energy. Because of the greater volume change associated with a given pressure decrease for this latter case, the initial pressure corresponding to a 300 MW sec accident is only 159 bars vs 456 bars for the value of $n = 1.0$.

Overall Reactor Modeling

Major regions of the reactor model are illustrated in Figure 24, which shows a simplified description of the reactor superimposed on the radial and axial mesh lines utilized for the REXCO calculations. (Additional information on the geometry of the mesh and materials data utilized are furnished in Appendix E.) These mesh lines result in dividing the reactor into ring segments of rectangular cross section.

Core

The core is represented as nine such segments. It has been assumed that, at the start of the REXCO calculation, the vaporized core material resulting from the power excursion is present in all the segments of the cylindrical core except the two outermost corner segments where the core power density is low. Only these seven segments, then, have been used to input the high pressure source term for this model. The remaining two core segments are, like the rest of the reactor regions, in their "pre excursion" condition at the start of the calculations.

Figure 24. Reactor Model and Mesh System

Cylindrical Vessels

Two vessels and two structural cylinders were modeled in REXCO. These were:

- Reactor vessel closure
- Lateral core support cylinder
- Reactor vessel
- External arresting cylinder

Since a primary objective of the REXCO calculation was to become familiar with the reactor system modeling and computer program capabilities of REXCO-H, only one set of materials properties was utilized for all structural elements. The properties of Type-304 stainless steel at about 1000°F were utilized, with some allowance made for strain rate effects. From information developed at HEDL,¹¹ the primary consequence of higher strain rate at the reference temperature is an increase in yield strength. For these calculations, the 0.2% strain yield strength was taken as 30,000 psi and the ultimate strength and strain at 50,000 psi and 15%, respectively.

Core Support

The high resistance to bending provided by the reactor core support structure has been represented in this simplified model by using a fictitious thickness of 60 cm for the reactor vessel core support plate. The mass represented by this thickness is approximately that of the steel and sodium of the support plate and reactor inlet plenum. The latter has not otherwise been included.

Reactor Vessel Closure

The reactor geometry has been truncated from the region extending upward immediately above the bottom of the reactor closure crush structure. The mass of the plate below the crush structure, and the load deflection characteristics of the crush structure itself, have been incorporated in the optional input for the "plug and platform" calculations performed by REXCO. The load is assumed to increase linearly with the first centimeter of deflection and then remains constant at 20×10^6 pounds.

The upper axial shielding of the reference demonstration plant (located above the lower plate of the vessel closure) is constructed as a series of plates which would resist bending and tend to stiffen the lower plate of the closure to large dynamic loads. Since REXCO-H structures are restricted to membranes (no bending stiffness), an artificially thick plate (25 cm) was modeled to simulate a stiff structure and the density of adjacent nodes representing the lower closure was adjusted to maintain the correct overall mass, i.e., inertia.

3.4.2.3 Results and Discussion

Response of Reactor Containment

At the start of the REXCO calculations, the core region, or at least an appreciable fraction of it, is assumed to contain high-pressure vapor. This high pressure causes a pressure front to travel outward from the high-pressure source at a velocity greater than the speed of sound. Reflections occur from surfaces of different material composition, which modify the pressure field in the reactor. The material behind the pressure front is compressed and accelerated as the front passes. The outward velocities can reach high values within a few milliseconds resulting in a substantial amount of kinetic energy in the system. Much of this energy will eventually be dissipated by absorption as strain energy in the reactor container, closure, and other structural elements.

For the two demonstration plant cases calculated on REXCO, referred to here as HCDA 1 and HCDA 2, the initial pressures were taken as 456 bars and 159 bars, respectively. Figure 25 shows the deformation of the mesh system at 15.9 msec into the accident. (The original mesh is shown in Figure 24 for HCDA 1.) The core approximates a spherical shape, indicating that inertial and deformation constraints to expansion of the core remain approximately symmetrical for an appreciable time interval. This results in obvious deformation of the lateral core support cylinder and of the reactor vessel at core elevation. This is unlike the results of a similar calculation for FFTF¹² which indicate the core to be deformed preferentially upward. The high mass and crush resistance of the reactor closure of the

Figure 25. HCDA No. 1—Reactor Geometry at 15.91 msec

demonstration plant seems to effectively suppress this effect. It also apparently diverts the upward flow of material, caused by the core expansion, to a radial direction. It is obvious from the figure that this radial flow impinging on the reactor vessel wall has caused local deformation of that vessel. Despite the not inconsiderable mass of the core support structure (modeled as the lower plate of the reactor vessel) and of the lower plate of the reactor closure, noticeable deflections of both these structural elements have occurred.

HCDA 1 is used here only to illustrate some of the effects of this type of accident. Qualitative and quantitative differences with HCDA 2 will be discussed in the following paragraphs.

Energy Distribution

Differences in the equation-of-state and initial pressure used for HCDA 1 and 2 result in a substantial difference in the rate at which the potential energy of the core is expended. This is graphically illustrated on Figure 26, which shows the work done by expanding core vapor products as a function of time after the start of the accident. At 10 msec into the accident, the core vapor expansion has produced 5.5 times as much work for HCDA 1 as for HCDA 2; at 20 msec, the factor has decreased to about 4. The corresponding changes in core pressure and the specific volume of the core vapor products, at the center of the core, are plotted on Figure 27. Because of the much higher initial pressure, HCDA 1 results in greater accelerations within the core and, therefore, greater initial dissipation of the core internal energy. The lower value of the isentropic exponent for HCDA 2 causes a slower decrease in pressure with core volume change, and a slower rate of energy expenditure, which is expected to result in a larger fraction of the work being accomplished at lower pressures and at longer times after the start of the accident.

Each of these two accidents was calculated on REXCO out to 2000 time steps, or to about 32 msec. However, the system total energy, which is used to give an indication of the cumulative error in the numerical calculation routine, indicates the calculational results to be unsound at that time. The time and core work at which the error in total system energy reaches approximately 1% for the two runs are indicated in Table 10. Although calculational accuracy for energy partition may be satisfactory beyond these times, the above is probably an upper limit at which calculations of local strains from REXCO should be utilized.

Figure 26. HCDA No. 1 and No.2—Core Work versus Time

Table 10
CORE WORK AT ENERGY CHANGE OF 1%

	Time at 1% ΔE (msec)	Core Work (MW-sec)
HCDA 1	15.8	98
HCDA 2	25.0	34.7

Figure 27. HCDA No. 1 and No. 2—Pressure and Specific Volume at Center of Core

The upper limit for evaluating energy partition is signaled by an incremental change in total energy of over 10% which occurs in a single time step. The maximum valid computational time for energy partition occurs just prior to this jump. The times and corresponding core work energy release is as given in Table 11. Time did not permit applying rezoning options to the grid and running the analyses out further in time.

Table 11
CORE WORK AT MAXIMUM VALID COMPUTATIONAL TIME

	Maximum Computational Time (msec)	Core Work (MW-sec)
HCDA 1	22.3	120
HCDA 2	28.0	38.3

Figure 28 shows the distribution of energies within the reactor assembly as a function of time. It is seen that the largest fraction (40%) of the core work energy initially appears as radial kinetic energy, with decreasing relative amounts as upward kinetic energy and downward kinetic energy. This last quantity continually increased over the time interval of the REXCO calculation and does not appear to be "leveling off" after 22 msec, at that time about 46% of the total core energy release of 120 MW-sec has been absorbed as shell strain energy in the lateral core support cylinder and reactor vessel. The radial kinetic energy peaked at about 10 msec, apparently due to absorption of the radial kinetic energy by the lateral core support and reactor vessel. Only a small fraction (~6%) of the 120 MW-sec core energy release at 22 msec has been absorbed by the shield plug, with most of that being absorbed by the crush structure. The energy absorption in the vessels and crush structure could be expected to increase at longer times, as there is still substantial and increasing amounts of kinetic energy in the system.

Figure 29 shows a similar curve of accident energy distribution vs time for HCDA 2. It is significant to note that the entire 28 msec of this calculation results in a core energy release of only 38 MW-sec, a value that occurs at a time of 6.3 msec in HCDA 1 (Figure 27). Comparing the energy partition at 38 MW-sec indicates some significant differences in energy partition.

Table 12 indicates that HCDA 2 has a much lower radial kinetic energy, a higher downward axial kinetic energy, and more energy which has been transferred to the shield plug. It is evident that accident energy release is not an adequate index to accident energy distribution for the demonstration plant reactor system, the effects of the core pressure volume curve which determine the dynamic response of the reactor containment system must also be considered.

Effects on Components

Reactor Vessel and Lateral Core Support

The energy absorption in HCDA 1 at 15.9 msec has resulted in circumferential strains of the lateral core support cylinder and reactor vessel as shown on Figure 30 (Axial strains are much smaller). The largest circumferential strain, approaching 7%, has been produced in the support cylinder opposite the core midplane. The reactor vessel strain at that elevation is only one-third as great with the peak strain occurring near the base of the reactor core's cross flow plenum. The strain "bulges" which are evident just above the top of the core support and the bottom of the cross flow plenum are apparently caused by absorption of kinetic energy diverted from an axial to a radial direction by the horizontal core support structure and the lower face of the reactor closure. The pressure, radial velocity, and radial displacements histories which occur near the base of the cross flow plenum at the reactor vessel are presented on Figure 31. The pressures at the centerline of the outlet nozzle for the course of these calculations was found to be about the same magnitude as that at the base of the plenum. The figure indicates that the pressure and velocities are decreasing which implies that the radial displacement and, therefore, the circumferential strain is nearing its maximum value at that location. The maximum reactor vessel strain occurs well below the level of the reactor outlet nozzle. This may be explained by reference to Figure 25, which indicates that the flow striking the base of the reactor closure is being diverted outward and downward rather than in a purely radial direction. At longer times, the base of the closure can be expected to move upward as the crush structure continues to deflect and the pressure at the outlet nozzle elevation may, consequently, rise. Longer computing times will be necessary in order to make assessments of both outlet nozzle pressure history and crush structure energy absorption.

Table 12
APPROXIMATE ACCIDENT ENERGY DISTRIBUTION
AT CORE ENERGY RELEASE OF 38 MW-sec

	HCDA 1	HCDA 2
Radial Kinetic Energy	13.8	3.4
Shell Strain	7.3	7.6
Positive Axial Kinetic Energy	10.2	9.4
Negative Axial Kinetic Energy*	7.7	16.0
Shield Plug	0	3.6

(*Negative $\cong$ Downward)

Figure 28. HCDA No. 1—Accident Energy Distribution versus Time

Figure 29. HCDA No. 2—Accident Energy Distribution versus Time

Figure 30. HCDA No. 1—Circumferential Strain of Lateral Core Support and Reactor Vessel at 15.91 msec

Figure 31. HCDA No. 1—Pressure, Radial Velocity, and Displacement of Sodium at Base of Cross-Flow Plenum Adjacent to Reactor Vessel

Figure 32 shows reactor vessel and lateral core support strains, analogous to those in Figure 30 but for HCDA 2 at 24.7 msec after the transient started when the core energy release is about 34 MW-sec. Also shown are curves for HCDA 1 at a similar energy release. Though the strains and deflections in the lateral core support are similar, the reactor vessel strain distributions are quite different. Figures 33 and 34 showing the distortion of the mesh lines for these two accidents indicate the reason for the differences. Much greater core volumetric expansion has occurred in HCDA 2 at the reference energy release, with greater movements of the material just above the horizontal core support structure and at the base of the cross-flow plenum. For this lower core pressure of HCDA 2, the time required for dissipation of kinetic energy is likely to be greater than for HCDA 1 and the previous comments on longer computing time required for evaluation accident consequences is even more apropos.

Lower Core Support Structure

Another area of interest which will require further evaluation is the behavior of the lower core support structure. This is necessarily modeled in REXCO-H as a strong, heavy membrane without ability to develop bending moments across its thickness. Though the calculated (membrane) stresses in it are small, deflections and curvature of the support structure (as evidenced on Figures 25 and 33) are considerable. The bending resistance is actually very high and the shape of the lower support structure calculated by REXCO-H is, therefore, not realistic. The calculated pressure-time history of the support structure is required for evaluation of its structural response to an HCDA. The pressure-time history is not expected to be strongly dependent upon the deflection history of the support structure since this is not expected to be a major source of volume generation required to reduce the source pressure in the core. Detailed stress analysis of the actual complex structure would not be performed by REXCO and a structural code such as NASTRAN would be used. The primary requirement on REXCO is a reasonable prediction of the pressure history applied to the support structure.

Figure 32. HCDA No. 1 and HCDA No. 2—Comparison of Circumferential Strain of Lateral Core Support and Reactor Vessel

3.4.3 RECORD Analyses

3.4.3.1 Introduction

Two sets of calculations were performed using the RECORD code to estimate the structural response of the reactor assembly to HCDA loading.

In the first set, the experimental models used at Stanford Research Institute to simulate the loading and geometry of the FFTF reactor were analyzed to provide a basis for selecting some of the assumed input parameters to the code. In the second series of calculations, analyses were performed on typical geometries of the demonstration plant as a prelude to selecting cases to be analyzed in REXCO.

3.4.3.2 Correlation to Experiments

The code uses a simplified representation of the reactor assembly and neglects the effect of compressibility of material. In effect, motion of mass and structure away from the core under pressures generated in the core is simulated as three one-dimensional "pistons" one in each of the following directions: upward, downward, and radial. Each "piston" is assigned an effective mass, effective area, and effective resisting force. Approximations to the three masses and the two vertical forces can readily be made. Due to geometric divergence, the effective area and force resistance in the radial direction is less easily estimated. In the vertical directions, since the pressure is initially confined within the core region, the initial effective area is much less than the final effective area. Toward the end of the expansion, the pressure would tend to be acting on the full area of the vessel, hence the effective area of the closure (upward) is not readily estimated.

CYCLE = 350
CORE WORK = 32 MW-sec

Figure 33. HCDA No. 1—Reactor Geometry at 5.51 msec

CYCLE = 1550

CORE WORK = 34 MW-sec

Figure 34. HCDA No. 2--Reactor Geometry at 24.71 msec

Based upon previous studies using a one dimensional hydrodynamics code, the effective radial resistance and effective area are given by

$$F_R = 2 R' L \frac{f_b h_b}{R_b} + \frac{f_r h_r}{R_v}$$

where f_b, f_v = hoop stress in barrel (lateral core support cylinder), vessel

h_b, h_v = thickness of barrel, vessel

L = effective length

$$R = \sqrt{\frac{R_v^2 R_o^2}{2 \ln (R_v/R_o)}}$$

R_b, R_v = radius of barrel, vessel

R_o = inner radius of blanket

and $A_R = 2\pi R' L$

In the analyses of the FFTF model experiments,²⁰ the loading was closely represented by a pressure volume relation of the form "pv = constant". Initial conditions for p and v were selected to match the measured data and work in the expansions of 8, 14, and 20 gm charges

In the model experiments, the static resistance against downward motion was high enough to preclude such motion. The radial modeling used the above formulations with an effective length equal to the core barrel length

In the upward direction, the mass of sodium in the vessel above the core barrel was used since the experiments indicated this moved essentially as a slug. The effective area of the sodium slug was varied parametrically. The calculated energy in the slug was compared to the measured energy. An effective area corresponding to the inner diameter of the core barrel was found to be the best approximation as shown in the following comparison

Charge Weight (gm)	Measured Slug Energy (%)	Calculated Slug Energy (%)	Error (%)
8	50	46	-8
14	30	30.3	+1
20	26	30.0	+13

These calculations provided a basis for selecting the parameters in the model of the demonstration plant

3.4.3.3 Reactor Analyses for Demonstration Plant

Variations of the reference configuration of the demonstration plant were analyzed. The RECORD code was used as the available tool prior to acquisition, familiarization, and use of the REXCO-H code developed at ANL. The two codes are not equivalent since REXCO-H tracks the response of each element of a fine gridwork of mass and structural elements including the effects of compressibility. The pressure and deformation of each node is predicted resulting in a detailed description of the response of the reactor assembly. RECORD uses a simpler approach and has been used to get an estimate of the damage distribution from HCDA loading. Figure 35 shows the RECORD model superposed on the reactor geometry. Deformation and pressure loading on the upper vessel and outlet piping is not predicted. For hypothetical accidents in which pressures of several thousand psi are predicted to exist for several milliseconds in the core region, significant attenuation between the core and piping nozzles is required for the piping to survive. The pressure loading in the core region is a prescribed relationship between pressure and volume. Arbitrary pv diagrams were selected for parametric study. The form of the relationship selected, $pv^S = c$ (constant) and variations in initial volume, exponent "s" and the constant "c" were made to simulate loadings over a range of damage potential. Table 13 lists the relationships used.

Figure 35. Vessel Assembly Arrangement

Table 13
RELATIONSHIPS USED IN PARAMETRIC STUDY

Number	Initial Pressure (psi)	Initial Volume (in. ³ x 10 ⁵)	S	C (x 10 ⁸)	Integral pdv to One Atmosphere (MW-sec)
1	2000	1.06	1.0	2.12	100
2	4000	0.395	1.0	1.58	100
3	4000	1.18	1.0	4.72	300
4	6000	0.74	1.0	4.44	300
5	8000	0.525	1.0	4.20	300
6	6000	1.55	1.3	238.0	300
7	6000	0.252	0.8	0.197	300
8	4000	1.97	1.0	7.88	500
9	6000	1.23	1.0	7.40	500
10	8000	0.875	1.0	7.0	500
11	12000	0.55	1.0	6.60	500
12	8000	2.17	1.3	448.0	500
13	8000	0.29	0.8	0.295	500

Note: Initial volume of sodium in the active core is approximately 0.49×10^5 in.³.
Relationships 2, 6, and 10 are plotted in Figure 36 and are termed "nominal."

Figure 36. Nominal P-V Diagram

The estimated deformations obtained using these nominal relationships with two variations in radial geometry are shown on Figures 37 and 38. Variations in the exponent "s" resulted in slightly smaller deformations than the nominal and the deformations increased somewhat with increasing initial pressure for a given integral pdv. The maximum increase over nominal for the cases analyzed, however, was approximately 10% indicating the nominal values are a reasonable basis pending a more detailed formulation of the pv diagram. The pressure-time history in the reactor core and the loading at the reactor vessel support for the nominal cases are shown in Figures 39 and 40, respectively. The effective area of the closure subjected to pressure was assumed to increase from the initial value to the full vessel area at time 4.0 msec. This is judged to provide a conservative estimate of closure deformation.

3.4.4 Code Operational and Modeling Considerations

Discussions of calculational results in previous paragraphs have included comments on operational problems encountered, as appropriate. This section contains a brief summary of preliminary observations and suggestions based on GE-BRD's initial experiences with using the REXCO-H program version dated October 26, 1971.

The problems ran stably and successfully for the first 1000 time steps (~16 msec) and the RESTART option was utilized to continue operation beyond 1000 cycles. The major operational problem encountered was a limitation on the number of time steps following restart as a consequence of a large incremental increase in total system energy. High distortion of the Lagrangian grid is a likely cause for the problem and use of ANL's REZONE program should permit continued operation. However, for low initial accident pressures where the time response of the reactor system is relatively slow (as it was for HCDA 2), more than one RESTART and possibly more than one use of REZONE may be required.

The necessity of representing the lower core support structure as a membrane does not result in realistic stresses and strains, but is expected to produce acceptable pressure loading history for detailed structural analysis of that member using codes capable of representing the actual detail of the real structure.

Coupling of the MFCI analysis to the REXCO analysis by taking the p-v diagram from MFCI and using this as an input to REXCO is an area requiring further study under the Safety Engineering Project Agreement. Some iterations between the two codes may be necessary to obtain a corresponding p-t relationship as well as p-v relationship in each analysis. The additional consideration of representing an MFCI in various regions of the core which are at differing fuel temperatures by a smeared out single pressurized fluid requires further study. The two cases conducted indicated that pressure level in addition to total energy is a significant variable affecting the energy distribution in a reactor system which does not have uniform inertia and resistance in all directions.

The random pulse nature of the pressure in many nodes, in which the pressure varies between different levels and drops to zero for short periods of time, is not readily explained. With multiple reflections, many pulses are to be expected but it is not completely evident whether these are physically real or a consequence of the finite size grid and the numerical solution method. Similarly, the spacial distribution in pressure at any time follows a pattern of large variations between adjacent nodes including zero pressures. A clearer understanding of these observations is required before using the results to design the reactor equipment. Time has not permitted discussions with ANL personnel on these items. This will be followed up on the continuing Safety Engineering Project Agreement.

3.4.5 Conclusions and Recommendations

- a. HCDA energy distribution in reactor system components depends on dynamic response of the containment to the pressure-specific volume relation for the core materials. Accident "energy release," per se, is not an adequate measure of accident damage potential.
- b. Energy partition during the early stages of an HCDA is unlike that of the FFTF reactor; a large portion of the energy for the demonstration plant is directed downward. For the first 20 msec of an HCDA, the crush structure in the demonstration plant reactor closure absorbs little of the accident energy release. Indications are that a significant fraction of the energy in the fluid displaced upward from the core may be directed onto the reactor vessel.

Figure 37. HCDA (Nominal Deformations) Geometry 1

Figure 38. HCDA (Nominal Deformations) Geometry 2

Figure 39. HCDA Pressure-Time History

Figure 40. Foundation Loading

- c Stability of the calculational procedure limited valid computational results to accident elapsed times less than is required for assessing energy partition within the reactor
- d The preliminary efforts (under PA 67) spent in understanding, evaluating and using REXCO HEP should be extended in the future under the Safety Engineering Project Agreement. Recently REXCO HEP has been made operational on CDC 7600 machine at LRL Berkeley by ANL. This facility is more efficient and economical than using the GE or ANL computer facilities and the HEP version may alleviate the stability problem encountered with REXCO H.
- e Further work is needed to determine appropriate ways of establishing REXCO input from MFCI output since energy distribution is sensitive to the rate of energy release, pressure levels in the process, as well as to total energy released.
- f Difficulties encountered with understanding input requirements for the REXCO H program were, in part, attributable to inadequate documentation. More thorough documentation of REXCO code operational requirements are desirable.
- g The REXCO H code representation of vessel plate sections as membranes which do not resist bending moments introduces potentially serious inaccuracies in calculation of structural response of thick plates for sustained high pressure loadings. The REXCO HEP code has eliminated this drawback and it should be used for future calculations.

3.5 CODE COMPARATIVE STUDIES

3.5.1 Introduction

An objective of the task is to perform an HCDA analysis with a consistent set of assumptions and conditions, utilizing two code sets. One of these code sets consist of those available to GE such as FREADM, RECORD, and other code systems developed by ANL, such as SAS, VENUS, and REXCO. The purpose of this is to perform a direct comparison of the results of both sets.

The results of the comparative studies between SAS and FREADM are given in the following section. In the area of disassembly calculations for sodium-out cases, a modified and updated single precision version of VENUS was used. This version was adopted to be operative on GE 635 computer. For sodium in cases, the VENUS II version, operational on the CDC 7600 computer at LRL Berkeley was used. Even though all of these are different versions of VENUS, comparative test cases were run with satisfactory results. In the area of MFCI, a special version of the code, operational on the GE 635 computer, was originally used. However, after encountering numerical stability problems, these calculations were performed using ANL's code and its computer facilities. In the area of RECORD and REXCO, discussion of these codes and the results have already been given in Subsection 3.4.

3.5.2 Predisassembly Code Comparison (FREADM/SAS-2A)

The Accident Analysis Group of ANL/RAS provided a version of SAS 2A^{1,3} on the CDC 7600 to be employed in the comparison phase of this effort. The SAS code is a series of codes currently under development. The version employed herein is described in an RAS draft report, and essentially identical with Reference 13.

A two step approach was adopted to compare the nominal FREADM result with SAS computation. An initial SAS model was developed to duplicate, within the code constraints, the FREADM model characteristics. The objective was to establish a direct comparison of the codes in as pure a form as possible. This comparison would determine the effects of basic differences between the code calculational procedures, e.g., the sodium density induced reactivity changes (single phase), and heat transfer after void initiation are computed within SAS but not in FREADM. This approach, which also allowed for a check on the SAS model by FREADM, is referred to as the SAS *comparative case*.

In the second step of the analysis, advantage was taken of the inherent SAS modeling capabilities for geometry and heat transfer. Performance of the SAS nominal calculation for each malfunction sequence placed primary emphasis

on the code module considered most relevant to the predisassembly phase. In the LOF situation, emphasis was on the Coolant Dynamics Module while the DEFORM (fuel cladding interaction) module was selected for the TOP analysis. This method provides for an orderly extension of the code capabilities, in critical modeling areas, beyond the comparative case. These calculations are termed the *SAS nominal* cases.

Steady State

By employing similar geometry, heat transfer, power generation and material properties, the steady state core conditions could be matched quite well between the two codes. Figures 41 and 42 present the sodium axial and fuel radial temperature profiles in the peak fuel assembly. The consistently high FREADM fuel temperatures ($\sim 100^\circ\text{C}$) are attributable to its less accurate treatment of thermal resistance and heat transfer near the fuel surface. This disparity would not be expected to change the character of the transient analyses, however.

LOF Analysis

For the comparative case, the sodium voiding transient was modeled via the table lookup fission gas model which incorporates a temperature dependent cladding failure hoop stress. The cladding outlet temperature at void initiation from FREADM was chosen as the cladding failure temperature in SAS. The fission gas release was then used to match the FREADM void pattern.

Although the flow decay transient in the peak power region was well matched between the codes (Figure 1), the internal treatment of SAS resulted in a slightly different decay curve among the channel types. The trend was to have higher normalized flow in the lower power channels. Additionally, the sodium thermophysical properties, which are a subroutine within SAS, also affect the transient through the SAS calculated single phase sodium density change reactivity feedback.

The combined result of the above deviations was to expand the time scale for void initiation throughout the core (by up to several hundred milliseconds) relative to FREADM. The related, initially negative, reactivity feedback in SAS is not as coherent or large and is reflected by a very minor power decline and more rapid power increase.

The SAS nominal calculation incorporated refinements in the heat transfer model. These included a channel hydraulic diameter which accounts for the spacer wire, additional structural material, Nusselt correlation with Peclet number¹⁴, etc.

As in the comparative case, all other SAS models which are not directly related to the coolant dynamics treatment (e.g., DEFORM, structural expansion, implicit fission gas release) were deleted or forced to have a negligible effect by careful selection of the input data.

The resulting SAS computation of reactivity and power is given in Figures 43 and 44. A major shortcoming of the analysis is that the code could not handle fuel rod failures due to the rapid power transient which resulted from voiding of the high power regions. For example, between 5.3545 and 5.5075 seconds the average reactor power increased from 0.85 to 14 kW/gram-MO₂, while the midplane fuel volume melt fraction varied from zero to approximately 75% in approximately 36% of the core fuel. Based on the modeling available, SAS predicts liquid sodium to remain in most all of these fuel assemblies, whereas one would have expected a condition similar to the TREAT S-series autoclave tests, where the rods fail and rapid voiding occurs. Since the net sodium void reactivity remaining in these regions is 30 cents positive, a prompt critical condition would have been expected earlier in the transient.

Based on the above argument, the FREADM/SAS nominal results would then appear to agree reasonably well with respect to the core energy generation and dominant mechanism resulting in nuclear subcriticality.

TOP Analysis

In order to perform a comparison with FREADM, it was necessary to request ANL/RAS to modify the code. In the SAS initial configuration, there was no way to match the FREADM criteria for rod failure during transient overpower. The resolution of this problem came about by a programming modification within the PREA function.

Figure 41. Comparison of Sodium Axial Temperature Profile at Steady State

Figure 42. Comparison of Fuel Radial Temperature Profile at Steady State

Figure 43. Reactivity Component Histories for FREADM/SAS LOF Analyses

Figure 44. Power Histories for FREADM/SAS LOF Analysis

subprogram which would trigger the table lookup fission gas release model based upon an input fuel calculational point and temperature. Sodium voiding was described by the fission gas release model to be consistent with the FREADM assumptions.

SAS/FREADM results are given in Figure 45 and 46. The slight differences between them are due to the sodium single phase reactivity treatment and the heat transfer model. One would expect essentially identical conclusions for the predisassembly analysis of this event.

Unfortunately, due to schedule problems and required modifications, the nominal SAS case could not be completed. This work will be continued under the Safety Engineering Project Agreement. It was planned to employ the DEFORM module to calculate the cladding plastic strain and, via the PREA function subprogram, to model rod failure and sodium voiding with the table lookup fission gas formulation. The ANL/RAS MFCI treatment was not included in the SAS coding which was available.

3.5.3 Problems Encountered

Although separate funding (Safety Engineering Project Agreement) was available for the acquisition of SAS 2A, severe schedule problems were encountered. This occurred despite the excellent cooperation of the ANL/RAS Accident Analysis Group. Adaptation of complex computer codes is a tedious, difficult, and time consuming operation. Additionally, an entirely new computer facility for GE (CDC 7600 at UC LRL) was involved in the SAS calculations.

Modeling problems exist in both FREADM and SAS 2A. It should be recognized that detailed modeling of every particular phenomena is not necessarily required or desirable. However, the code capabilities must include sufficient options and information on key parameters to describe the current state of the art. The necessity for inclusion of detailed models is reactor system dependent. For example, the large, positive sodium void worth associated with the LMFBR minimizes the problem time scale and the extrapolation necessary to extend the analysis beyond a well known physical core configuration for whole core LOF sequences. Thus, the transient could be reasonably described in FREADM by engineering judgment and simple models. For the smaller FTR sodium void worth characteristic, long time scale physical voiding is possible, and the calculations must be extended into less deterministic geometric regimes. Therefore, the SAS formulation developed in support of the FTR safety analysis required a detailed treatment of the sodium dynamics and post voiding events, such as cladding melting and fuel slumping.

The spatial distribution of sodium void worth in the demonstration reactor is incompatible with the model assumed in FREADM. The correct void distribution could be constructed, but at the expense of code capabilities in other areas.

A major drawback in FREADM is the termination of heat transfer to sodium at the instant voiding initiates anywhere in the reactor channel. The uncertainty in cladding and fuel temperatures rapidly increases beyond calculational usefulness for the time scales of interest. Other minor changes occur due to the neglect of the reactivity effects of sodium density changes.

It should be noted that FREADM does not provide an implicit calculation of fuel/cladding mechanical interactions. Failure is initiated based on user supplied information related to fuel, cladding, or other calculated temperatures. Reasonable modeling may be obtained by iteration among cases.

Several calculation problems were encountered in the use of SAS 2A. Use of a proper value for an equivalent spring constant (SPRINK — input location 230 239) led to oscillations in the reactivity solution and caused solution divergence within the DEFORM module. Consultation with ANL/RAS resulted in a successful suggestion to employ a very small, non prototypic value for this parameter.

Divergence of the solution also occurred when a single phase vapor friction factor (FVAPM — input location 634) was employed for use within sodium vapor bubbles. Based upon recommendation of ANL/RAS, a switch to the two phase vapor friction factor eliminated the divergence.

Figure 45. Power Response to $0.50/\text{sec}$ Reactivity Ramp Insertion—FREADM/SAS Comparison

Figure 46. Reactivity Response to \$0.50/sec Reactivity Ramp Insertion—FREADM/SAS Comparison

A satisfactory resolution of all of the computational problems was not completed. Although debug printout options are available, a detailed writeup on this option was not. Also, in such a complex code, one requires substantial familiarity with it before rapid debugging can be reasonably accomplished. This is being continued under the Safety Engineering Project Agreement.

For modeling of rapid overpower transients, the DEFORM module in SAS 2A code calculates the differential expansion between fuel and cladding, leading to plastic strain of the cladding. However, a code capability did not exist to fail the cladding based on this loading mechanism. ANL/RAS did provide consultation on modifications to the PREA subprogram to trigger failure, but at too late a date to accomplish the analysis. The absence of such a cladding failure/voiding mechanism also led to modeling problems during the LOF sequence, as described earlier.

The ability to model a proper restraint for core voiding in the SAS formulation due to hydraulic/inertial forces at the reactor outlet for the demonstration reactor design used for this study is quite difficult. SAS provides for user input (DZINTV, DZINBV — input location 5919-5938) of sodium inertial lengths above and below the core. An equivalent rational input for the demonstration reactor could not be ascertained within the scope of this effort (nor was a parametric determination of its effect completed) and ANL/RAS could not provide satisfactory consultation.

One final calculational problem encountered is that ANL/RAS indicated that the SAS computation has a lower time step limit for stability of approximately 0.3 millisecond. However, this is within a range of expected time step size for a fast reactor on a near prompt critical burst.⁹ After iteration with ANL/RAS, program modifications are being considered to better treat this calculational limitation.

3.5.4 Conclusions and Recommendations

Initial comparisons indicate that a reasonable agreement exists between FREADM and SAS 2A for predisassembly calculations of an LOF sequence in the demonstration reactor. However, both codes have serious limitations and modeling problems. As discussed above, however, efforts are in progress to correct these problems and limitations.

The dominant phenomena, in the accident modes considered, were identified as fuel motions and sodium voiding dynamics. Understanding of these phenomena, under accident conditions, is in an early state of development.

In order to adequately treat these phenomena within predisassembly analyses, analytic formulations must be integrated with and tested in a detailed manner against the current experimental programs. This is especially true for large LMFBR designs which incorporate high positive sodium voiding effects.

4. OVERALL DISCUSSIONS AND CONCLUSIONS

The present study allowed for a systematic evaluation of the mechanical effects on the reactor and containment structures resulting from an HCDA. The objectives of the analyses were to (1) carry through a consistent calculation of the nominal mechanical effects starting from the accident initiation, (2) estimate the range of mechanical consequences due to parametric variations in key variables and assumptions, (3) comment on the applicability of present analytic methods being employed at GE and ANL for HCDA analyses of a demonstration reactor design.

To discuss the objectives, the analyses can be divided into three calculations consisting of (1) the thermal energy generated during the nuclear transient, (2) a source term for mechanical loadings, and (3) the structural dynamic response. An impasse was reached in attempting to calculate a source term for the mechanical loading. Thus, the structural dynamics were calculated based on mechanical loadings which were considered to envelop the mechanical effects and to provide a didactic exercise for the REXCO H formulation.

The applicability of the methods employed were reviewed in the performance of the GE parametric analyses and by exercising the appropriate ANL code for the nominal transient calculation.

FREADM-VENUS calculations were performed to determine the thermal energy release during the nuclear transient. Generally, the energy release calculated, to the condition of nuclear subcriticality, was not overly sensitive (7000 MW-sec $\pm 25\%$) to the parametric variations considered, with one major exception. Assumptions on the relocation of fuel material, prior to attaining hydrodynamic disassembly conditions in the core, were capable of consistently effecting major changes in the energy release for both accident sequences considered.

Fuel relocations within the core were initiated at fuel rod failure (50% areal melt in LOF sequence) with an assumed time scale of 50 msec. The associated negative reactivity effects (see Appendix C) terminated the nuclear transient before hydrodynamic disassembly conditions could be attained. The peak fuel temperatures and energy releases at nuclear subcriticality were in the range of 5050 to 5200°F and 4310 to 4170 MW-sec (298°K reference), respectively. To a lesser extent, the assumptions on sodium voiding behavior affected the LOF sequence energy release. Analytic code development efforts are actively under way on fuel motions at ANL and HEDL with some early encouraging results (e.g., Reference 15 and Appendix F — Trip Reports). The ability to employ these developing techniques in the licensing process for the demonstration reactor will be strongly influenced by successes in comparisons of the analytic predictions with experimental data on fuel motions.

The VENUS results were found to be primarily sensitive to reactivity insertion rate and Doppler magnitude. The uncertainty in the insertion rate is recognized as being much higher than any possible uncertainty in the Doppler magnitude. Additionally, the TOP sequence results were strongly influenced by changes in the void fraction available. This is recognized as being due to steep gradients in the pressure-energy density relationship dependence on void fraction for initial low void fractions.

The appropriateness of the VENUS calculation for the LOF sequence is in doubt. The pressure-time history which results from the low reactivity insertion rate and large void fraction does not support the basic assumption of hydrodynamic core behavior. A detailed examination of this concern is recommended prior to additional calculations for a demonstration plant PSAR.

The SAS-2A calculation of the LOF nominal sequence compared favorably with the FREADM results. Both methods are considered as being appropriate frameworks within which to perform the HCDA calculations for the demonstration plant. However, both codes have serious limitations and modeling problems. A much heavier reliance on experimental support of the analytic formulations is recommended along with more flexibility in the modeling of the demonstration design for the PSAR and FSAR efforts.

The interaction of molten fuel with sodium (MFCI) to provide a source term for the structural analysis is considered the weakest link in the calculational sequence. It was concluded that the currently available methods are inadequate in modeling assumptions and flexibility to predict the source term for the demonstration plant containment analysis.

Near the termination of the nuclear transient, very large radial and axial gradients exist in the reactor core. For the pressures predicted by the MFCI code, the strength of the subassembly ducts is insufficient to prevent radial communication between core regions. To generate a realistic p-v relationship for the core materials to be employed in structural dynamics calculations, it is considered imperative to treat the more complex two-dimensional problem. It is recommended that a two-dimensional model be formulated which allows for greater flexibility in applied spatial and boundary conditions to prescribe variations in reactor design and material conditions. A coupling of the MFCI and REXCO formulations is considered a desirable development.

As a result of the conclusions in the MFCI area it was decided to perform the structural analyses with core material expansion relations which represented 100 to 500 MW-sec of potential work and peak pressures typical of recent TREAT and SRI experimental data (see Table 14). This level of work potential is considered to allow for a reasonable discrimination of spatial energy and strain distributions in the reactor and containment structures. However, these magnitudes of work potential are much higher than one would calculate by application of the available mechanical energy conversion efficiency data (see Table 14) to the thermal energy values generated in the first part of this study.

The RECORD scoping calculations indicated that the gross energy distribution in the structures was not overly sensitive ($\pm 10\%$) to variations in the source term description. However, the more detailed REXCO-H calculations

Table 14
COMPILATION OF TREAT S-SERIES PISTON AUTOCLAVE RESULTS PUBLISHED IN REFERENCE 16

	S-3	S-4	S-5	S-6	S-7	S-8
Average fuel specific energy (cal/gm·UO ₂)	460	720	509	493	514	784
Amplitude of largest pressure pulse (psi)	540	1800	2940	2050	430	780
Calculated fractional conversion from nuclear to mechanical energy for						
— Initial meltdown only	2.1×10^{-5}	2.0×10^{-4}	2.5×10^{-4}	6.0×10^{-5}	6.0×10^{-6}	6.0×10^{-6}
— Total including delayed interactions	2.1×10^{-5}	2.5×10^{-4}	1.9×10^{-3}	1.5×10^{-3}	6.0×10^{-6}	2.0×10^{-5}

resulted in the final loading distribution being dependent on the dynamic response of the structures which was strongly coupled to the pressure-volume relationship employed for the expansion of the core materials. Although code instabilities prevented the completion of the analyses, it was concluded that the accident energy release in MW-sec, per se, is not an adequate measure of the damage potential.

Based on the current understanding of thermal-to-mechanical energy conversion, the magnitude of the HCDA mechanical loading for the demonstration reactor is in the range of 0 to 100 MW-sec. This value is considered to be well within the design capability of the containment structures. Current development programs are addressing those parts of the analysis which were herein determined as most affecting the mechanical consequences of these accidents. The one exception is in the MFCI calculation where a new approach is recommended.

It is also recommended that continued analytic code development be performed within an organizational structure which requires frequent and critical interaction among the code development, experimental, and design (manufacturer) groups. An integrated combination of the functions within the Safety Engineering Project Agreement would be a desirable vehicle to accomplish this.

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REFERENCES

1. D. D. Freeman, et al., *The FREADM-1 Code, A Fast Reactor Excursion and Accident Dynamics Model*, September 1970, (GEAP-13608).
2. J. F. Jackson and R. B. Nicholson, *VENUS-II, An LMFBR Disassembly Program*, 1972 (ANL-7951).
3. D. H. Cho, et al., *A Rate-Limited Model of Molten Fuel/Coolant Interactions: Model Development and Preliminary Calculations*, 1972 (ANL-7919).
4. Yao-Wen Chang, J. Gvildys, and S. H. Fistedis, *Two-dimensional Hydrodynamics Analysis for Primary Containment*, 1969 (ANL-7498).
5. P. Greebler, et al., *BISYN - A Two-Dimensional Synthesis Program*, 1965 (GEAP-4922).
6. L. O'Dell and W. Little, *Uncertainties in the Sodium Void Worth Calculations for the Fast Test Reactor (FTR)*, 1971 (HEDL-TME 71-151).
7. W. T. Sha and T. H. Hughes, *VENUS - A Two-Dimensional Coupled Neutronics-Hydrodynamics Computer Program for Fast-Reactors Power Excursions*, 1970 (ANL-7701).
8. R. B. Nicholson and J. F. Jackson, *A Sensitivity Study for Fast Reactor Disassembly Calculations*, to be published (ANL-7952).
9. R. A. Meyer, et al., *Fast Reactor Meltdown Accidents Using Bethe-Tail Analysis*, 1967 (GEAP-4809).
10. O. D. O'Dell and A. E. Walter, "Effect of Distributed Voids in LMFBR Core Disassembly Calculations" *Trans. Am. Nucl. Soc.*, **15**, 358 (1972).
11. J. M. Steichen and M. M. Paxton, *The Effect of Strain Rate on the Mechanical Properties of Austenitic Stainless Steel*, Hanford Engineering Development Laboratory, USAEC, 1971 (HEDL-TME 71-56).
12. *Reactor Development Program Progress Report - May 1972*, Argonne National Laboratory, Argonne, Illinois, June 26, 1972 (ANL-RDP-5).
13. F. E. Dunn, et al., "The SAS-2A LMFBR Accident Analysis Computer Code" *Proce. Cont. Reactor Math. and Applications*, CONF-710302 (1), 120, (1971).
14. V. M. Bovishanskii, et al., "Heat Transfer to Liquid Metals in Longitudinally Wetted Bundles of Rods," *Atomnaya Energiya*, **27** (1969).
15. W. L. Partain, et al., *Effects of Molten Fuel Movement During a Fast Reactor Overpower Transient*, 1972 (HEDL-TME 72-109).

16. *Reactor Safety, Reactor Development Program Progress Reports* (Monthly 1971-72), Argonne National Laboratory, Argonne, Illinois.
17. D. H. Cho and W. L. Chen, *Improvement of Basic Parametric Model of MFCI*, ANL Reactor Development Program Progress Report, pp. 8.15-8.17, September 1971, (ANL-7861).
18. D. H. Cho and W. L. Chen, *Improvement of Basic Parametric Model of MFCI*, ANL Reactor Development Program Progress Report, pp. 8.31-8.33, January 1972, (ANL-RDP-1).
19. D. H. Cho and W. L. Chen, *Improvement of Basic Parametric Model of MFCI*, ANL Reactor Development Program Progress Report, pp. 8.32-8.34, February 1972, (ANL-RDP-2).
20. A. L. Florance and G. R. Abrahamson, *Simulation of a Hypothetical Core Disruptive Accident in a Fast Flux Test Facility*, Stanford Research Institute, Menlo Park, California (Second Interim Report, SRI Project) November 1971, (PYD 1109).

APPENDIX A

GENERAL DATA

A.1 PLANT PARAMETERS

Following are the parameters used in the present study corresponding to a typical LMFBR Demonstration Plant.

General

Thermal Power	MWt	950
Net Electrical	MWe	344.6
Gross Electrical	MWe	374
Reactor Coolant		Sodium
Reactor Outlet Temperature	°F	1000
Reactor Inlet Temperature	°F	720

Core Description

Number of Fuel Assemblies, Total		162
Enrichment Zone 1		72
Enrichment Zone 2		90
Number of Blanket Assemblies, Total		180
Number of Control Assemblies, Total		19
Core Height	in.	30.6
Axial Blanket Top and Bottom	in.	15
Radial Blanket Height	in.	60.6
Radial Blanket Thickness	in.	~14
Equivalent Active Core Diameter	in.	67
Active Core Volume	liters	1840
Core Assembly Pitch	in.	4.835

Fuel Assembly Description

Rods per Assembly		217
Fuel Rod Length	in.	120
Rod Diameter	in.	0.230
Rod Wall Thickness	in.	0.015
Channel Inside Across Hex.	in.	4.335
Assembly Length Total	in.	174
Pellet Diameter	in.	0.1945
Pellet	Type/%	TD Solid/90.4
Planar Smear Density	% TD	85.5
Pellet-Cladding Cold Gap	in.	0.0055
Fuel O/M		1.97
Axial Blanket Material		Depleted UO ₂
Fission Gas Accommodation		Sealed Plenum

Reactor Vessel

Design Pressure	psig	140
Design Temperature	°F	1025
Normal Operating Pressure		
Inlet	psig	131
Outlet	psig	1

A.2 FISSION POWER DISTRIBUTION (MWT per Reactor Region)

	Start-of-Cycle	End-of-Cycle
Region		
Core	865	806
Axial Blanket	33	63
Radial Blanket	<u>52</u>	<u>81</u>
Total	950	950

A.3 POWER PEAKING FACTORS

Ratio of Core Power at Start-of-Cycle to Mid-Cycle	1.03
Axial Peak-to-Average Power Ratio	1.18
Radial Peak-to-Average Power Ratio	1.20
Local Peaking Factor for a New Fuel Bundle	1.08
Maximum Control Rod Peaking During a Cycle	1.10*

*1.06 at Start-of-Cycle where maximum overall peak power density occurs in the core.

Figure A-1. Core Arrangement

Figure A-2. Vessel Internals

APPENDIX B

BASIC PHYSICS DATA

The major reactivity coefficients for the large bundle demonstration plant design containing no BeO were updated for the HCDA analysis in FY72. In preparation for a more refined set of calculations, a sixteen energy-group cross-section file, resonant self-shielded for the current reactor composition, was generated. Preliminary calculations were then performed to evaluate the Doppler effect, sodium voiding worth and spatial composition worths for use in core accident analysis.

One conclusion of the present analysis is that evaluation of the Doppler constant involving the heterogeneity of the control/fuel matrix is called for. The removal of the BeO as a "buffer" between fuel and control has made the Doppler value more sensitive to the degree of control inserted in the core. Estimates of the expected magnitude of the cell effect on the Doppler coefficient have been included in this report.

B.1 DOPPLER COEFFICIENT

The Doppler coefficient, $T dk/dT$, was calculated at the beginning of an equilibrium cycle using BWR discharge plutonium as fuel material. This is the limiting condition for the present design because it requires the greatest reactivity control insertion. The Doppler coefficient values from these calculations are felt to be conservative (small magnitude) because of the use of a conservative estimate of the effect of control heterogeneity. Calculations are in progress to evaluate the control heterogeneity influence on the Doppler coefficient. Estimates indicate that the magnitude of the Doppler coefficient will increase ~ 7 percent when fast recycle plutonium is used in the equilibrium refueling cycle, due to the decreased control insertion for burnup compensation. Thus, it is expected that for the equilibrium recycle core the minimum Doppler will be close to -0.005 .

The Doppler coefficient values are given in Table B-1 for several partially voided conditions. The contribution from each zone is given so that a "power-squared" spatial distribution can be generated. Anticipating that the given Doppler constants will be used in the FREADM transient analysis code, the following correction factors have been applied to the as-calculated value to obtain the values in Table B-1.

Normalization to SEFOR	0.975
Fuel Rod Temperature Distribution	1.03
Blanket Contribution	1.03
Estimated Cell Effect (Conservative)	<u>~ 1.05</u>
Net	1.086

Note that the "spatial temperature and power distribution" factor of 1.04 would be applied also for a "non-spatial" calculation or for quoting a single value Doppler value resulting in the following:

Condition	Doppler, $T dk/dT$
Na In	-0.0043
Na Out, Core	-0.0022
Na Out, Core and Blankets	-0.0020

Table B-1
ZONAL DOPPLER CONTRIBUTION FOR VARIOUS
PARTIAL VOIDING CONDITIONS, $T dk/dT$

	Core Zone 1	Core Zone 2	Total
Na-In	-0.00266	-0.00139	-0.00405
Na-Out:			
Maximum Void Worth Pattern	-0.00153	-0.00139	-0.00292
Core Zone 1	-0.00148	-0.00129	-0.00277
Total Core	-0.00133	-0.00082	-0.00215
Core and Blankets	-0.00121	-0.00071	-0.00192

B.2 SODIUM VOIDING

A few select voiding patterns were analyzed and the results used to renormalize the previous axial integral voiding worth distributions

As before, the integral axial sodium voiding worth curves (by hexagonal ring) presented in Figure B.1 assume progression of voiding from the core midplane to either the upper or lower core blanket interface. Thus, total axial voiding for a given ring would have a value double that presented on the curve.

Examining the voiding worth curves presented, it is observed that the latest calculations produced a slightly larger positive voiding effect as compared to previous calculations. Thus, the maximum voiding worth is seen to be 4\$ while voiding of core zone 1 is worth 3\$ and the total core void worth is equal to 1.8\$. If voiding includes both core and blankets, the reactivity effect would be -1.4\$ (it is estimated from the above, and previous calculations, that the blanket voiding effect is -2.0\$ for the axial blankets and -1.2\$ for the radial blankets).

The total voiding worth is quite sensitive to small design changes and calculational differences (refinements) and for that reason the values will continue to change somewhat until more definitive calculations and correlations with experiments have been performed. This extreme sensitivity is due to relatively large (~12\$) positive spectrum effects compensating for large negative leakage effects (~12\$) producing a small difference (4\$ maximum in this case), thus, a small functional change in either component is amplified in the net result.

B.3 SPATIAL WORTHS

The worth per unit volume of homogenized composition by region was calculated for the beginning of cycle condition. The appropriate control for that condition is included in the homogenized composition. Figures B.1 and B.2 give the radial and axial worth distributions for the homogenized composition plus the contribution by fuel, structure, and sodium. Note that the individual worth contribution for each of the components (\$/cc) is in terms of the volume of material in a unit volume of reactor as dictated by the volume fraction of that material.

The effect of partial sodium voiding on the homogenized composition worth distribution is shown in Figure B.3. For this case, sodium has been voided entirely from core Zone 1 but left in elsewhere. It is observed that although the magnitude of the composition worth changes with voiding, the slope of the distribution through a region is not sensitive to voiding, either partial or complete.

The sodium worth distribution, both radially and axially, is given in Figures B-4 and B.5 for both the sodium in spectrum and sodium out spectrum. By integrating both curves (Na-in and Na out) and combining in a manner that reproduces calculated partial and total sodium void cases, the integral axial voiding worth curves given in Figure B.6 are produced.

Figure B-1. Composition Worths Along Core Midplane, Sodium-In Environment

Figure B-2. Composition Worths Along Core Center Line, Sodium-In Environment

Figure B-3. Composition Worth Along Core Midplane

Figure B-4. Sodium Worth Along Core Midplane

Figure B-5. Axial Sodium Worth Distribution at Selected Radii

Figure B-6. Integral Axial Voiding Worth by Ring

APPENDIX C

FREADM COMPUTATIONAL MODELS

C.1 CORE GEOMETRY AND PHYSICS MODELS

The reactor core is physically described by ten independent fuel rods which are each representative of a radial region. Power generation, sodium flow, Doppler, sodium void worth, etc., are assigned their proper values within each region. The thermal-hydraulic analyses of the rods are coupled through the reactor neutron kinetics solution, point reactor model. Thus, the total core transient response can be calculated via the coupled thermal hydraulic-neutronic solution.

Specific information on the core geometry, physics characteristics, and mathematical solutions were detailed in Appendices A and B and the FREADM description.^{C-1}

C.2 MATERIAL THERMAL PROPERTIES

The material properties employed are consistent with the general reactor community and are described in Table C-1, Figure C-1, and Reference C-2.

Table C-1
MATERIAL PROPERTIES

Material	Property	Value	Units
Fuel	Density	588	lbm/cu ft
Cladding	Density	502	lbm/cu ft
Sodium	Density	52.5	lbm/cu ft
Cladding	Specific Heat	0.120	Btu/lbm-°F
Sodium	at Constant Pressure	0.3035	Btu/lbm-°F
Cladding	Thermal Conductivity	11.8	Btu/h-ft-°F
Sodium		39.2	Btu/h-ft-°F
Fuel	Heat of Fusion	129.2	Btu/lbm

C.3 COOLANT VOIDING MODELS

Voiding models were generated for the core region; voiding of the blankets is ignored. It is important to note that when voiding is initiated, the FREADM code negates any further heat transfer from the cladding to the coolant in the entire rod. Thus, FREADM is only calculating the void reactivity term in the kinetics solution.

Sodium voiding was assumed to initiate whenever a local superheat of 60°F was attained. This choice is based on a review of the Atomics International^{C-3} and KFKC^{C-4} data. Additionally, voiding was assumed initiated during overpower transients when a fuel node attained a 50 percent radial melt fraction (solidus). The experimental data on this phenomena is very preliminary at this time. Fuel rod rupture and fuel expulsion has been estimated to have occurred over a very wide range of melt fractions.^{C-5} Previous unreferenced analyses of overpower transients have indicated that the precise time of cladding rupture during overpower conditions is not as important as the resulting sodium voiding rate. Therefore, the above estimated value of 50 percent is considered reasonable.

For a 60°F superheat condition, the sodium saturation vapor pressure is about equal to the estimated core inlet pressure during the nominal demonstration reactor flow coastdown. A rather complex thermal hydraulic situation exists within the reactor channels. It is considered that the voiding will progress as a boiling wave through the channel rather than a rapid liquid slug ejection, which occurs at high values of superheat. (The detailed solution of the coupled thermal hydraulics-neutronics problem for the core is considered beyond the scope of the present effort.) To estimate the voiding rate, a FREADM analysis was performed which considered the flow to stagnate when the cladding reached the superheat temperature required. Then, the axial progression of the temperature was determined with the reactor at approximately constant power (Doppler feedback considered).

The above void description was then employed in FREADM to calculate the core power transient upon voiding. The resulting fuel temperature transient was used to make a first order correction to the voiding rates of the stagnant coolant solution. The correction was based on fuel rod rupture induced void initiation which increased the voiding rate throughout the core. This modified model as shown in Figure C 2, was chosen as the nominal sodium void transient during core flow coastdown.

For voiding which is initiated by the fuel rupture threshold during overpower transients, the model was formulated by reviewing liquid slug ejection data for the ANL E series experiments and previous primary system hydraulic restraint calculations.^{C 6} A linear expulsion model was constructed whose primary consideration was to estimate the nominal time scale for the reactivity event. Based on failure at the core midplane, linear void growth was assumed to reach the axial blankets at 75 and 125 msec in the upward and downward directions, respectively.

Plenum fission gases are not expected to strongly influence the voiding for operating fuel rods, because fuel cladding gap closes at very low burnup (say ≥ 15 MWd/te). This conclusion is based on a review of the General Electric B3B and F3A experiment data.^{C 7 8} The B3B fuel rod failed at power in a sodium environment and demonstrated very low gas communication between the plenum and core region. The F3B rods were punctured in an external gas environment but still resulted in lengthy gas release times (~ 10 sec) when compared with the void models assumed herein.

C 4 FUEL MOTION AND ASSOCIATED WORTH MODEL

Real time data on fuel motion during failure conditions is essentially non-existent. Examinations of fuel distributions following TREAT capsule transients have shown substantial axial fuel movement (for example, References C 9 and C 10). Much of the data supports an ultimate fuel redistribution which would generate a negative reactivity effect on the reactor, however, several experiments (GE C4A, B, J, PNL 59 8) have resulted in partial fuel compaction and/or indicate initial fuel movement toward the cladding failure location, usually expected near the midplane for overpower transients. Additionally, the TREAT experiment environment often includes several non-prototypic conditions (compared with a power reactor) which are considered to strongly influence fuel motions, e.g., axial and radial distribution of fuel power generation versus retained fission gases, cladding metallurgy, etc.

Fuel motions employed in the parametric analyses were based on a close examination of the fuel and cladding temperature histories in the defined nominal transients. Consideration was given to the two-dimensional extent of fuel melting, local fuel grain substructure, cladding strength (temperature), and time-dependent specific power. As a first order approximation, the model assumed fuel relocations would occur as specified in Table C 2. The time scale was chosen to be on the order of the peak sodium flow rate. Direct calculation of the reactivity worth of such relocations were not made. Again, a first order approximation was made by normalizing the perturbation theory (generated radial fuel worth) to a two-dimensional calculation of the removal of a peak worth fuel assembly. Within each region, the axial worth was established by approximating a flux-squared worth dependence. In the actual cases, only half of the estimated worth was employed as a conservatism. In addition, sodium voiding was assumed to occur simultaneously with the initiation of fuel motions.

Table C-2
FUEL MOTION MODELS EMPLOYED IN FREADM ANALYSES

Core Region	Initiate at % Solidus at Midplane	Fuel Axial Motion Within Core	Calculated Reactivity Worth per Region (—\$)
1, 3, 7, 8	50	1/3 of fuel in axial 2/3 of core to upper 1/3	0.75, 1.22, 1.68 1.68, 0.97
4, 9	36	1/4 of fuel in axial 2/3 of core to upper 1/3	1.35, 2.25
5, 6	36	1/2 of fuel in axial 1/2 of core to upper 1/3	0.75, 0.58
10	25	1/5 of fuel in axial 1/2 of core to upper 1/2	0.63

Figure C-1. Fuel Energy Density as a Function of Temperature

Figure C-2. Sodium Voiding Model in Nominal LOF Analysis

REFERENCES

- C-1. Freeman, D. D., et al., *The FREADM-1 Code, A Fast Reactor Excursion and Accident Dynamics Model*, GEAP-13608, September, 1970.
- C-2. Rubin, B. F., *Summary of (U,Pu)O₂ Properties and Fabrication Methods*, GEAP-13582, November, 1970.
- C-3. Logan, D., et al., *Studies of Boiling Initiation for Sodium Flowing in a Heated Channel*, AI-AEC-12767, September 30, 1969.
- C-4. Peppler and Schultheiss, *Liquid Metal Boiling Research*, KFK-874, (EUR4157e), October, 1968.
- C-5. GEAP-10028-34 to -40, *Sodium Cooled Reactors, FCR Development Program*, Quarterly Reports, 1970-1971.
- C-6. ANL Reactor Development Program Progress Reports, 1971-1972.
- C-7. Gregoire, K., et al., *Failed Fuel Performance in Natural Convecting Liquid Metal Coolant*, GEAP-13620, June, 1970.
- C-8. McCarthy, W. H., et al., *Performance of Unencapsulated (U,Pu)O₂ Fuel Rods, P Series F3A (Irradiated in EBR II to 50 K MWd/t)*, GEAP-13735, April, 1972.
- C-9. Thomas, G. and Field, J., *Transient Overpower Irradiation of Axially-Restrained Zero-Burnup Fast Reactor Fuel Specimens*, GEAP-13562, September, 1969.
- C-10. WADCO Quarterly Technical Report, Vol. 4, HEDL-TME 71-29, December 1970.

APPENDIX D

VENUS MODELS

Following are VENUS mockup (regions) of Demonstration Plant model, temperature distributions used as input for nominal cases and the code input edits

FINENEW,11,1500,150000.803326, FRIEDMAN

*7600

*NOTAPES

LIBCOPY,VENUS,LGUB/BR,FILE01.

LGOR.

NEP 2 66326-127-NA IN REACT INPUT TABLE

20	18										
1	0	0	0	1	120	1	100	1	4	1	1
1	1	1	1	1	3	2	2	2	0	0	0
2	11										
2	11										
3800.0	1.50E-05	0.20E-04	0.50E-07					1.0		1.0	
0.0	4.733	9.467	14.200					18.933		23.667	
28.400	33.583	38.766	43.950					49.133		54.316	
59.500	62.667	65.833	69.000					74.933		80.867	
86.800	104.600	122.400									
0.00	19.05	38.10	43.65					49.20		54.75	
60.30	65.85	71.40	76.95					82.50		88.05	
93.60	99.65	104.70	110.25					115.80		134.85	
153.90											
1.20	1.20	1.19	1.18					1.17		1.15	
1.12	1.09	1.06	1.02					.98		.93	
1.16	1.13	1.09	1.02					.94		.81	
0.00	0.00										
0.00	0.00	0.74	0.84					0.95		1.02	
1.10	1.15	1.18	1.18					1.15		1.10	
1.02	0.95	0.84	0.74					0.00		0.00	
8	6		12								
8.19E-5	.0128	9.79864E12									
7.225E-4	.0314	3.71179E13									
6.158E-4	.1352	9.31358E12									
1.1873E-3	.3434	9.59631E12									
5.241E-4	1.3660	1.85674E12									
1.689E-4	3.8040	2.89438E11									
0.0	0.0	0.0	5.76E-07					0.99			
0.0	0.001	0.002	0.003					0.0034		0.004	
0.006	0.0065	0.007	0.0096					0.0140		0.0215	
3.076E-3	3.164E-3	3.270E-3	3.360E-3					3.400E-3		3.480E-3	
3.800E-3	3.890E-3	4.000E-3	4.740E-3					5.870E-3		7.880E-3	
1.0E15	1.66380E10	1.0E8	3.0								
0.0	0.0	0.02	0.02								
-7.812E-4	-3.906E-4	0.0	0.0					0.00		0.0	
0.005E-3	0.003E-3	0.0									
2	4	8	4								
4	9	6	14								
115.80	38.10	0.00	28.40								
0.0	9.2	23.4	28.4								
38.1	43.65	54.75	65.85					76.95		88.05	
99.15	110.25	115.8									
1.87E-7	1.85E-7	1.74E-7	1.69E-7								
2.00E-7	1.98E-7	1.86E-7	1.82E-7								
2.20E-7	2.17E-7	2.06E-7	2.00E-7								
2.34E-7	2.32E-7	2.20E-7	2.13E-7								
2.39E-7	2.37E-7	2.25E-7	2.18E-7								
2.34E-7	2.32E-7	2.20E-7	2.13E-7								
2.20E-7	2.17E-7	2.06E-7	2.00E-7								
2.00E-7	1.98E-7	1.88E-7	1.82E-7								
1.87E-7	1.85E-7	1.74E-7	1.69E-7								
0.3884	-1.619E-04	8.781E-08	0.548					0.0		0.0	
64.30679	-79870.111	-5.13247	3089.0					305.0		0.0	
0.0	-0.0047	0.0	0.0					0.1407			
9.408	0.85	7.9	0.3014					.4781		0.2205	

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1000.0	1200.0	0.0	5.32		
8 4	14 4	8 18	14 18	3	
6 9	6 14				
115.80	38.10	28.40	59.50		
28.4	33.95	44.55	52.45	57.40	59.50
38.1	43.65	54.75	65.85	76.95	88.05
99.15	110.25	115.8			
1.69E-7	1.62E-7	1.44E-7	1.34E-7	1.27E-7	1.24E-7
1.82E-7	1.74E-7	1.55E-7	1.44E-7	1.36E-7	1.33E-7
2.00E-7	1.92E-7	1.70E-7	1.58E-7	1.50E-7	1.47E-7
2.13E-7	2.05E-7	1.84E-7	1.72E-7	1.63E-7	1.61E-7
2.18E-7	2.10E-7	1.89E-7	1.77E-7	1.68E-7	1.66E-7
2.13E-7	2.05E-7	1.84E-7	1.72E-7	1.63E-7	1.61E-7
2.00E-7	1.92E-7	1.70E-7	1.58E-7	1.50E-7	1.47E-7
1.82E-7	1.74E-7	1.55E-7	1.44E-7	1.36E-7	1.33E-7
1.69E-7	1.62E-7	1.44E-7	1.34E-7	1.27E-7	1.24E-7
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0
0.0	-.0047	0.0	0.0	0.3620	
9.408	0.85	7.9	0.3014	.4781	0.2205
1000.0	1200.0	0.0	5.32		
14 4	17 4	14 18	17 18	3	
4 9	3 14				
115.80	38.10	59.50	69.00		
59.5	62.55	67.30	69.00		
38.1	43.65	54.75	65.85	76.95	88.05
99.15	110.25	115.8			
1.47E-7	1.42E-7	1.30E-7	1.26E-7		
1.70E-7	1.64E-7	1.52E-7	1.47E-7		
2.10E-7	2.03E-7	1.91E-7	1.85E-7		
2.41E-7	2.34E-7	2.21E-7	2.16E-7		
2.53E-7	2.46E-7	2.33E-7	2.27E-7		
2.41E-7	2.34E-7	2.21E-7	2.16E-7		
2.10E-7	2.03E-7	1.91E-7	1.85E-7		
1.70E-7	1.64E-7	1.52E-7	1.47E-7		
1.47E-7	1.42E-7	1.30E-7	1.26E-7		
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0
0.0	-.0047	0.0	0.0	0.1977	
9.408	0.85	7.9	0.3014	.4781	0.2205
1000.0	1200.0	0.0	5.32		
17 4	20 4	17 18	20 18	3	
4 9	3 14				
115.80	38.10	69.00	86.80		
69.0	74.40	83.30	86.80		
38.1	43.65	54.75	65.85	76.95	88.05
99.15	110.25	115.8			
1.26E-7	1.15E-7	.71E-7	.67E-7		
1.47E-7	1.30E-7	.80E-7	.75E-7		
1.85E-7	1.73E-7	1.13E-7	1.12E-7		
2.16E-7	2.01E-7	1.38E-7	1.36E-7		
2.27E-7	2.10E-7	1.48E-7	1.46E-7		
2.16E-7	2.01E-7	1.38E-7	1.36E-7		
1.85E-7	1.73E-7	1.13E-7	1.12E-7		
1.47E-7	1.30E-7	.80E-7	.75E-7		
1.26E-7	1.15E-7	.71E-7	.67E-7		
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0
0.0	-.0047	0.0	0.0	0.2996	
9.408	0.85	7.9	0.3014	.4781	0.2205
1000.0	1200.0	0.0	5.32		

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2	2	20	2	2	4	20	4	4		
4	3	18	2							
	36.10		0.00		0.00		86.80			
	0.00		39.63		69.35		86.80			
	0.00		20.00		38.10					
	.21E-9		.23E-9		.32E-9		.34E-9			
	.38E-8		.34E-8		.40E-8		.40E-8			
	1.65E-8		1.54E-8		1.80E-8		2.10E-8			
	0.3884		-1.619E-04		8.781E-08		0.548		0.0	0.0
	64.30679		-79870.111		-5.13247		3089.0		305.0	0.0
	0.0		0.0		0.0		0.0		0.0	
	9.408		0.85		7.9		0.3014		.4781	0.2205
	1000.0		1200.0		1366.0		5.32		0.05	
2	18	20	18	2	20	20	20	4		
4	3	18	2							
	153.90		115.80		0.00		86.80			
	0.00		40.00		70.00		86.80			
	115.80		132.00		153.90					
	1.65E-8		1.54E-8		1.80E-8		2.10E-8			
	.38E-8		.34E-8		.40E-8		.40E-8			
	.21E-9		.24E-9		.32E-9		.34E-9			
	0.3884		-1.619E-04		8.781E-08		0.548		0.0	0.0
	64.30679		-79870.111		-5.13247		3089.0		305.0	0.0
	0.0		0.0		0.0		0.0		0.0	
	9.408		0.85		7.9		0.3014		.4781	0.2205
	1000.0		1200.0		1366.0		5.32		0.05	
20	2	22	2	20	12	22	12	4		
3	6	2	10							
	82.50		0.0		86.8		122.4			
	86.8		110.0		122.4					
	0.0		20.0		38.0		56.0		76.0	82.50
	.09E-9		.10E-10		.20E-11					
	.15E-8		.13E-09		.26E-10					
	.23E-7		.12E-08		.23E-09					
	.26E-7		.16E-08		.38E-09					
	.27E-7		.19E-08		.49E-09					
	.27E-7		.19E-08		.48E-09					
	0.3884		-1.619E-04		8.781E-08		0.548		0.0	0.0
	64.30679		-79870.111		-5.13247		3089.0		305.0	0.0
	0.0		0.0		0.0		0.0		0.0	
	9.408		0.85		7.9		0.5007		.3135	0.1858
	1000.0		1200.0		1366.0		5.32		0.05	
20	12	22	12	20	20	22	20	4		
3	5	2	8							
	153.9		82.50		86.8		122.4			
	86.8		110.0		122.4					
	82.50		96.0		114.0		132.0		153.9	
	.27E-7		.19E-08		.48E-09					
	.26E-7		.16E-08		.40E-09					
	.21E-7		.11E-08		.21E-09					
	.15E-8		.13E-09		.26E-10					
	.09E-9		.10E-10		.20E-11					
	0.3884		-1.619E-04		8.781E-08		0.548		0.0	0.0
	64.30679		-79870.111		-5.13247		3089.0		305.0	0.0
	0.0		0.0		0.0		0.0		0.0	
	9.408		0.85		7.9		0.5007		.3135	0.1858
	1000.0		1200.0		1366.0		5.32		0.05	
	2498.		2485.		2470.		2454.		2439.	2416.
	2596.		2585.		2573.		2559.		2545.	2525.
	2698.		2691.		2681.		2668.		2654.	2637.
	2816.		2809.		2798.		2782.		2767.	2748.

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2950.	2940.	2923.	2903.	2882.	2857.
3047.	3033.	3014.	2991.	2967.	2937.
3108.	3091.	3070.	3046.	3021.	2990.
3120.	3102.	3081.	3057.	3033.	3001.
3081.	3065.	3046.	3024.	3002.	2971.
3004.	2990.	2973.	2953.	2934.	2907.
2888.	2876.	2661.	2846.	2830.	2809.
2777.	2766.	2754.	2740.	2726.	2709.
2694.	2684.	2673.	2660.	2648.	2631.
2598.	2591.	2582.	2573.	2564.	2545.
2385.	2343.	2286.	2230.	2174.	2127.
2499.	2464.	2418.	2370.	2320.	2263.
2617.	2587.	2549.	2507.	2461.	2405.
2725.	2695.	2655.	2614.	2571.	2520.
2825.	2786.	2737.	2692.	2651.	2609.
2901.	2855.	2800.	2749.	2707.	2667.
2951.	2902.	2842.	2786.	2738.	2694.
2962.	2912.	2852.	2795.	2746.	2703.
2932.	2885.	2829.	2777.	2732.	2693.
2872.	2831.	2784.	2738.	2697.	2657.
2783.	2752.	2716.	2679.	2640.	2595.
2687.	2659.	2624.	2587.	2546.	2497.
2608.	2576.	2534.	2491.	2446.	2395.
2517.	2478.	2426.	2377.	2335.	2282.
2433.	2390.	2345.	2540.	2503.	2461.
2649.	2619.	2586.	2761.	2728.	2692.
2877.	2829.	2781.	2958.	2906.	2848.
3005.	2957.	2894.	3015.	2969.	2905.
2990.	2940.	2881.	2926.	2880.	2829.
2824.	2788.	2750.	2724.	2691.	2657.
2648.	2612.	2574.	2569.	2526.	2485.
2246.	2101.	1942.	2380.	2243.	2078.
2515.	2383.	2216.	2623.	2497.	2338.
2705.	2585.	2445.	2764.	2647.	2517.
2802.	2681.	2555.	2811.	2691.	2567.
2792.	2676.	2553.	2751.	2636.	2505.
2687.	2571.	2425.	2594.	2474.	2320.
2501.	2375.	2220.	2399.	2265.	2108.

a

a

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93571 360 6 LOP NA OUT FINE GRID										
20	16									
1	0	0	0	1	60	1	100	1	4	1 1
1	1	1	1	1	3	2	2	2	2	
2	11									
2	11									
1800.0	1.50E-05	0.20E-04	0.50E-07					1.0		1.0
0.0	4.733	9.467	14.200					18.933		23.667
28.400	33.563	38.766	43.950					49.133		54.316
59.500	62.667	65.833	69.000					74.933		80.867
66.800	104.600	122.400								
0.00	19.05	38.10	43.65					49.20		54.75
60.30	65.85	71.40	76.95					82.50		88.05
93.60	99.65	104.70	110.25					115.80		134.85
153.90										
1.20	1.20	1.19	1.18					1.17		1.15
1.12	1.09	1.06	1.02					.98		.93
1.16	1.13	1.09	1.02					.94		.81
0.00	0.00									
0.00	0.00	0.74	0.84					0.95		1.02
1.10	1.15	1.18	1.18					1.15		1.10
1.02	0.95	0.84	0.74					0.00		0.00
8	6									
8.196E-5	.0128	9.71375E12								
7.225E-4	.0314	3.64100E13								
6.158E-4	.1352	8.88657E12								
1.1873E-3	.3434	9.35159E12								
5.241E-4	1.3660	2.44421E12								
1.689E-4	3.8040	6.27652E11								
3.200E-03	.166E-1	0.0	5.76E-07					.9995		
1.0E15	5.19909E10	1.0EA	3.0							
0.0	0.0	0.03	0.03							
-15.026E-4	-9.966E-4	0.0	0.0					0.0		0.0
.022E-3	.015E-3	0.0								
2	4	8	4	2	18	8	18	3		
4	9	6	14							
115.80	38.10	0.00	28.40							
0.0	9.2	23.4	28.4							
38.1	43.65	54.75	65.85					76.95		88.05
99.15	110.25	115.8								
2.16E-7	2.14E-7	2.02E-7	1.96E-7							
2.30E-7	2.29E-7	2.15E-7	2.11E-7							
2.54E-7	2.52E-7	2.39E-7	2.23E-7							
2.70E-7	2.68E-7	2.55E-7	2.47E-7							
2.76E-7	2.73E-7	2.59E-7	2.52E-7							
2.70E-7	2.68E-7	2.55E-7	2.47E-7							
2.54E-7	2.52E-7	2.39E-7	2.23E-7							
2.30E-7	2.29E-7	2.15E-7	2.11E-7							
2.16E-7	2.14E-7	2.02E-7	1.96E-7							
0.3884	-1.619E-04	8.781E-08	0.548					0.0		0.0
64.30679	-79870.111	-5.13247	3089.0					305.0		0.0
0.0	-2.585E-3	0.0	0.0					0.1407		
9.408	0.85	7.9	0.3014					0.0		0.2205
1000.0	1200.0	0.0	5.32							
8	4	14	4	8	18	14	18	3		
6	9	6	14							
115.80	38.10	28.40	59.50							
28.4	33.95	44.55	52.45					57.40		59.50
38.1	43.65	54.75	65.85					76.95		88.05
99.15	110.25	115.8								
1.96E-7	1.88E-7	1.70E-7	1.40E-7					1.33E-7		1.28E-7
2.11E-7	2.02E-7	1.84E-7	1.50E-7					1.41E-7		1.37E-7

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2.32E-7	2.23E-7	2.02E-7	1.62E-7	1.54E-7	1.49E-7
2.47E-7	2.38E-7	2.17E-7	1.70E-7	1.61E-7	1.57E-7
2.52E-7	2.43E-7	2.22E-7	1.73E-7	1.63E-7	1.59E-7
2.47E-7	2.38E-7	2.17E-7	1.70E-7	1.61E-7	1.57E-7
2.32E-7	2.23E-7	2.02E-7	1.62E-7	1.54E-7	1.49E-7
2.11E-7	2.02E-7	1.84E-7	1.50E-7	1.41E-7	1.37E-7
1.96E-7	1.88E-7	1.70E-7	1.40E-7	1.33E-7	1.28E-7
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0
0.0	-2.585E-3	0.0	0.0	0.3620	
9.408	0.85	7.9	0.3014	0.0	0.2205
1000.0	1200.0	0.0	5.32		
14 4	17 4	14 18	17 18	3	
4 9	3 14				
115.80	38.10	59.50	69.00		
59.5	62.55	67.30	69.00		
38.1	43.65	54.75	65.85	76.95	88.05
99.15	110.25	115.8			
1.74E-7	1.66E-7	1.59E-7	1.48E-7		
1.97E-7	1.89E-7	1.77E-7	1.70E-7		
2.37E-7	2.28E-7	2.15E-7	2.09E-7		
2.68E-7	2.59E-7	2.45E-7	2.38E-7		
2.78E-7	2.74E-7	2.53E-7	2.50E-7		
2.68E-7	2.59E-7	2.45E-7	2.38E-7		
2.37E-7	2.28E-7	2.15E-7	2.09E-7		
1.97E-7	1.89E-7	1.77E-7	1.70E-7		
1.74E-7	1.66E-7	1.59E-7	1.48E-7		
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0
0.0	-2.585E-3	0.0	0.0	0.1977	
9.408	0.85	7.9	0.3014	0.0	0.2205
1000.0	1200.0	0.0	5.32		
17 4	20 4	17 18	20 18	3	
4 9	3 14				
115.80	38.10	69.00	86.80		
69.0	74.40	83.30	86.80		
38.1	43.65	54.75	65.85	76.95	88.05
99.15	110.25	115.8			
1.48E-7	1.30E-7	.78E-7	.70E-7		
1.70E-7	1.52E-7	.95E-7	.87E-7		
2.09E-7	1.92E-7	1.26E-7	1.17E-7		
2.38E-7	2.22E-7	1.56E-7	1.48E-7		
2.50E-7	2.32E-7	1.69E-7	1.60E-7		
2.38E-7	2.22E-7	1.56E-7	1.48E-7		
2.09E-7	1.92E-7	1.26E-7	1.17E-7		
1.70E-7	1.52E-7	.95E-7	.87E-7		
1.48E-7	1.30E-7	.78E-7	.70E-7		
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0
0.0	-2.585E-3	0.0	0.0	0.2996	
9.408	0.85	7.9	0.3014	0.0	0.2205
1000.0	1200.0	0.0	5.32		
2 2	20 2	2 4	20 4	4	
4 3	18 2				
38.10	0.00	0.00	86.80		
0.00	39.63	69.35	86.80		
0.00	20.00	38.10			
.42E-8	.34E-8	.31E-8	.17E-8		
.80E-8	.78E-8	.75E-8	.60E-8		
2.67E-8	2.45E-8	3.37E-8	4.00E-8		
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0

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0.0	0.0	0.0	0.0	0.0	0.0	
9.408	0.85	7.9	0.3014	0.0	0.2205	
1000.0	1200.0	1366.0	5.32	0.40		
2 18	20 18	2 20	20 20	4		
4 3	16 2					
153.90	115.80	0.00	86.80			
0.00	40.00	70.00	86.80			
115.80	132.00	153.90				
2.67E-8	2.45E-8	3.37E-8	4.00E-8			
.88E-8	.78E-8	.75E-8	.60E-8			
.42E-8	.34E-8	.31E-8	.17E-8			
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0	
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0	
0.0	0.0	0.0	0.0	0.0		
9.408	0.85	7.9	0.3014	0.0	0.2205	
1000.0	1200.0	1366.0	5.32	0.40		
20 2	22 2	20 12	22 12	4		
3 6	2 10					
82.50	0.0	86.8	122.4			
86.8	110.0	122.4				
0.0	20.0	38.0	56.0	76.0	82.50	
.02E-8	.08E-10	.12E-12				
.18E-8	.25E-9	.22E-10				
2.13E-8	.15E-8	.17E-9				
2.65E-8	.21E-8	.23E-9				
2.68E-8	.30E-8	.07E-9				
2.65E-8	.26E-8	.23E-9				
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0	
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0	
0.0	0.0	0.0	0.0	0.0		
9.408	0.85	7.9	0.5007	.3135	0.1858	
1000.0	1200.0	1366.0	5.32	0.05		
20 12	22 12	20 20	22 20	4		
3 5	2 8					
153.9	82.50	86.8	122.4			
86.8	110.0	122.4				
82.50	96.0	114.0	132.0	153.9		
2.68E-8	.30E-8	.07E-9				
2.65E-8	.21E-8	.07E-9				
2.13E-8	.15E-8	.17E-9				
.18E-8	.25E-9	.22E-10				
.02E-8	.08E-10	.12E-12				
0.3884	-1.619E-04	8.781E-08	0.548	0.0	0.0	
64.30679	-79870.111	-5.13247	3089.0	305.0	0.0	
0.0	0.0	0.0	0.0	0.0		
9.408	0.85	7.9	0.5007	.3135	0.1858	
1000.0	1200.0	1366.0	5.32	0.05		
2600.	2585.	2569.	2553.	2537.	2513.	
2741.	2730.	2717.	2702.	2688.	2667.	
2863.	2853.	2842.	2830.	2818.	2800.	
2980.	2971.	2961.	2950.	2939.	2923.	
3058.	3047.	3035.	3023.	3011.	2995.	
3120.	3109.	3096.	3081.	3067.	3050.	
3166.	3155.	3141.	3125.	3108.	3088.	
3180.	3169.	3155.	3138.	3121.	3100.	
3160.	3150.	3137.	3121.	3105.	3085.	
3116.	3108.	3098.	3085.	3072.	3055.	
3048.	3045.	3038.	3030.	3022.	3009.	
2983.	2980.	2975.	2967.	2960.	2947.	
2922.	2915.	2906.	2896.	2885.	2869.	
2855.	2844.	2833.	2821.	2809.	2790.	
2481.	2440.	2387.	2332.	2273.	2213.	

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2659.	2601.	2550.	2496.	2438.	2375.
2776.	2742.	2693.	2641.	2584.	2521.
2902.	2872.	2830.	2785.	2732.	2673.
2974.	2949.	2918.	2882.	2839.	2788.
3028.	3003.	2976.	2945.	2909.	2864.
3063.	3034.	3004.	2973.	2942.	2902.
3074.	3046.	3015.	2985.	2956.	2918.
3062.	3037.	3009.	2981.	2951.	2913.
3035.	3012.	2985.	2955.	2922.	2879.
2993.	2971.	2941.	2907.	2867.	2816.
2931.	2906.	2871.	2832.	2788.	2733.
2847.	2816.	2774.	2730.	2683.	2629.
2763.	2725.	2673.	2622.	2572.	2519.
2536.	2492.	2446.	2685.	2644.	2598.
2615.	2760.	2740.	2938.	2905.	2871.
3014.	2979.	2949.	3071.	3033.	3002.
3108.	3068.	3031.	3117.	3080.	3042.
3097.	3067.	3033.	3064.	3039.	3009.
3017.	2996.	2970.	2957.	2933.	2905.
2663.	2850.	2813.	2810.	2765.	2715.
2346.	2185.	1995.	2506.	2350.	2161.
2650.	2495.	2307.	2792.	2645.	2461.
2887.	2755.	2580.	2950.	2830.	2666.
2960.	2869.	2720.	2992.	2886.	2743.
2987.	2881.	2736.	2960.	2849.	2698.
2913.	2790.	2629.	2839.	2710.	2545.
2738.	2608.	2448.	2636.	2506.	2348.

ENDJOB

Figure D-1. Geometrical Mockup of the Core, VENUS

Figure D-2. VENUS Geometrical Mockup, Sodium-In Nominal Case Initial Temperature Distribution

Figure D-3. VENUS Geometrical Mockup, Sodium-Out Nominal Case Initial Temperature Distribution

APPENDIX E

REXCØ-H MODEL

E.1 MODEL DESCRIPTION

This appendix supplements the previously furnished basic modeling information presented in the body of the report.

The REXCØ code used for the demonstration plant analyses is basically that described in ANL-7498,^{E-1} with some modifications to input information in accordance with supplementary documentation* provided by ANL. The reactor containment model is similar to that of a typical demonstration plant Design. Figure E-1 shows the reactor cross section and the radial mesh lines in the REXCØ program.

The mesh lines divide the reactor into rectangular cross section ring segments (referred to as zones in the code description). A number of these segments having similar properties are grouped together into "sections," which are assigned arbitrary numbers. These section numbers are also shown as Figure E-1. The geometry of the sections is determined by the mesh structure indicated on Table E-1. The "external arresting cylinder," not shown on the cross-section sketch, but included in the model, is a backup structural cylinder in the argon gas space between the vessel and the reactor cavity, which is installed concentric with the reactor vessel.

E.2 MATERIALS INFORMATION

Table E-2 relates the section numbers to the materials composition and properties assigned the sections. Many of the sections consist of a mixture of solid materials and sodium. For these mixtures, the initial density used in the REXCØ-H calculations is based on the average density, i.e., the total mass of section divided by volume of section.

Another necessary input to the code is the pressure versus the volume relation for each material. For these initial REXCØ calculations, a simplified equation-of-state relating pressure, p , linearly to specific volume, v , and bulk modulus β , was utilized for both liquids and solids. That is:

$$\rho = \beta \left(1 - \left(\frac{v}{v_0} \right) \right)$$

For mixtures of solids or of liquids and solids, the bulk modulus of the mixture was calculated as the reciprocal of the sum of the volume weighted reciprocals of the bulk moduli of the constituents. In calculating the bulk modulus of the core fuel plenum region, the compressibility of the argon was not included. (The argon volume was treated as steel when calculating the mixture's modulus.)

E.3 ADDITIONAL INPUT INFORMATION FOR REXCØ-H

The geometry of the structural components modeled is presented in Table E-3. A complete listing of the input deck appears in Table E-4.

*REXCØ-H (Release 2), Input Information Required, dated October 26, 1971.

Figure E-1. Reactor Model for REXCO - H

Table E-1
REXCØ MESH GEOMETRY FOR DEMONSTRATION
PLANT REACTOR MODEL

Radial or Axial Line	Radius (cm)	Vertical Height (cm)
2	0	0
3	28	19
4	56	28
5	85	53
6	108	76
7	131	99
8	152	122
9	173	145
10	199	168
11	225	191
12	251	210
13	273	229
14	280	254
15		279
16		305
17		324
18		343
19		373
20		404
21		434
22		465
23		495
24		526
25		546
26		566
27		587
28		607
29		635
30		663
31		691
32		719
33		739
34		762
35		808
37		831

Table E-2
SECTION AND MATERIAL DATA

Section	Name	Material	Section	Name	Material
1	Lower Reactor Cavity	4	11	Upper Axial Blanket	16
2	Core Support	6	12	Core Outlet Plenum	8
3	Lower Axial Shielding	13	13	Cross Flow Plenum	2
4	Lower Axial Blanket	17	14	Upper Axial Shielding	3
5	Lower Central Core	1	15	Lower Closure Sodium	2
6	Lower Outer Core	17	16	Radial Blanket	7
7	Middle Central Core	1	17	Internal Shielding	6
8	Middle Outer Core	1	18	External Shielding	5
9	Upper Central Core	1	19	Upper Vessel Sodium	2
10	Upper Outer Core	17	20	Radial Reactor Cavity	4

Material	Na	Composition (%)		Initial Density	Bulk Modulus
		Steel	Argon	(gm/cc)	(Bars x 10 ⁻⁶)
1	45	55	—	4.804	0.102
2	100	—	—	0.824	0.0426
3	—	100	—	6.24*	1.21
4	—	—	100	0.0015	pn(n = 1.67)
5	40	60	—	5.16	0.108
6	17.5	82.5	—	6.772	0.222
7	30	70	—	5.87	0.140
8	52	21	27	2.12	0.0794
13	52	48	—	4.302	0.0891
16	45	55	—	4.787	0.0908
17	45	55	—	4.804	0.102

*As modeled—actual density estimated as 8.03.

Table E-3
STRUCTURAL VESSELS AND CYLINDERS

Vessel No. and Name		Zone Nos.		Thickness (cm)
		Axial	Radial	
1.	Reactor Closure Bottom Plate	28	2-7	25.0*
1.	Reactor Closure Cylinder	28-36	8	5.10
2.	Lateral Core Support Cylinder	5-23	9	5.08
3.	Reactor Core Support Plate	4	2-11	60.0**
3.	Reactor Vessel Cylinder	4-25	12	3.81
		26-36	12	7.62
4.	External Arresting Cylinder	3-36	13	12.7

*As modeled (see body of report) actual thickness 5.10.

**As modeled to include mass of steel and sodium in support structure and in lower plenum.

Table E-4
LISTING OF INPUT DECK

TFST	CASE -	MODEL	NO. 8 -	HCCA	NO. 1							
12	35	0	2	2	0	.000001	.000032	.000001				
	10.		.005	1.0E10	1.							
1	1	1	0	0	0	0	0	0				
20												
2	4	10	4	11	5	11	14	11	30	11	35	
2	36	10	36	2	13	2	15	4	13	4	15	
2	25	2	30	8	6	8	14	-2	13	2	-13	
-2	15	2	-15									
	0.	28.	56.	85.	108.	131.	152.	173.				
199.	225.	251.	273.	280.								
0.	19.	28.	53.	76.	99.	122.	145.					
168.	191.	210.	229.	254.	279.	305.	324.					
343.	373.	404.	434.	465.	495.	526.	546.					
566.	587.	607.	635.	663.	691.	719.	739.					
762.	785.	808.	831.									
20												
2	11	2	3	4	0.	0.						
2	11	4	4	6	0.	0.						
2	6	5	10	13	0.	0.						
2	4	11	12	17	0.	0.						
2	3	13	13	1	0.	0.						
4	4	13	13	17	0.	0.						
2	3	14	14	1	0.	0.						
4	4	14	14	1	0.	0.						
2	3	15	15	1	0.	0.						
4	4	15	15	17	0.	0.						
2	4	16	17	16	0.	0.						
2	6	18	23	8	0.	0.						
2	11	24	27	2	0.	0.						
2	7	28	31	3	0.	0.						
2	7	32	36	2	0.	0.						
5	6	11	17	7	0.	0.						
7	8	5	23	6	0.	0.						
9	11	5	23	5	0.	0.						
8	11	28	36	2	0.	0.						
12	13	2	36	4	0.	0.						
11												
1												
5.47	.1292E12	4.804	.579E9	4.55E8	7.	0.						
.010675					10.							
.455	.002222	.33676	.003	.25232	.004	.20165	.005					
.10032	.01	.049652	.02	.019252	.05	.0040532						
.001320	.4	0.	1.0									
2												
3.6	.112E11	.824	0.		7.	0.						
.824					4.							
4.261	0.9	0.853	0.98	0.427	0.99	0.	1.0					

Table E-4
LISTING OF INPUT DECK--(Continued)

3	7.	.375E12	6.24	0.	0.	7.	0.	
6.24						4.		
121.001	0.9	24.201	0.98	12.101	0.99	0.1.		
4								
1.67	0.	0.0015	0.	0.	4.	0.		
.67								
5								
5.64	.1465E12	5.16	0.	0.	7.	0.		
5.16					4.			
10.801	0.9	2.161	0.98	1.001	0.99	0.1.		
6								
6.405	.228E12	6.772	0.	0.	7.	0.		
6.772					4.			
22.201	0.9	4.441	0.98	2.221	0.99	0.1.		
7								
5.98	.18E12	5.87	0.	0.	7.	0.		
5.87					4.			
14.001	0.9	2.801	0.98	1.401	0.99	0.1.		
8								
3.79	.791E11	2.12	0.	0.	7.	0.		
2.12					4.			
7.941	0.9	1.589	0.98	.795	0.99	0.	1.0	
13								
5.232	.1072E12	4.302	0.	0.	7.	0.		
4.302					4.			
8.911	0.9	1.783	0.98	.892	0.99	0.	1.0	
16								
5.47	.1292E12	4.787	0.	0.	7.	0.		
4.787					4.			
9.081	0.9	1.817	0.98	.909	0.99	0.	1.0	
17								
5.47	.1292E12	4.804	0.	0.	7.	0.		
4.804					4.			
10.201	0.9	2.041	0.98	1.021	0.99	0.1.		
1	2	12	3	0	3.67E7	2	12	0.
8.89E12	200.	8.89E12	1.	0.	0.			
		2	36	290.	4	2	2	13
								-381. 4 2
4	1							
8	28	1	1	5	25.	5.1		
9	5	1	1	6	0.	5.08		
12	4	1	1	5	60.0	3.81		
13	3	1	1	6	0.	12.7		
24								
9 24	0.	9 25	0.	9 26	0.	9 27	0.	9 28
9 30	0.	9 31	0.	9 32	0.	9 33	0.	9 34
9 36	0.	12 26	7.62	12 27	7.62	12 28	7.62	12 29
12 31	7.62	12 32	7.62	12 33	7.62	12 34	7.62	12 35
2.069E9	.002	3.4475E9	.150	7.85	.3			

\$ ENDJOB

***EOF

REFERENCES

- E-1. Chang, Yao-wen, Gvildys, J., and Fistedis, S. H., *Two-dimensional Hydrodynamics Analysis for Primary Containment*, 1969 (ANL-7498).

APPENDIX F TRIP REPORTS

To take advantage of the FFTF Project experience in HCDA analyses and regulatory review, General Electric personnel held informal meetings with cognizant members of the staffs of both HEDL and ANL/RAS. Additionally, telephone contacts were continued throughout the PA-67 contract period with both organizations, to expand on the meeting discussions and maintain cognizance of FFTF experience. This appendix presents the GE-prepared meeting notes of the informal meetings held in accordance with PA-67 requirements.

F.1 GE/HEDL Discussions on FFTF HCDA Analysis

The subject meeting was held at Richland, Washington, on May 10, 1972. GE objectives for the meeting were as follows:

1. Determine current status and future schedule of FFTF/HCDA analysis and Regulatory review.
2. Determine experimental bases for analytical models employed.

Attendees

GE	HEDL	AEC-RDT
K. Hikido	R. Peterson	G. Gallagher
R. Meyer	D. Simpson	
A. Brown	A. Waltar	
D. Switick	W. Partain	
B. Sarma	D. Stepnewski	
	G. Fox	
	R. Madsen	
	J. Scott	
	J. Hagan	
	J. Hanson	
	A. Schade, et al.	

Agenda for GE/HEDL Meeting on HCDA Analysis for the FFTF May 10, 1972

Morning

Topic	Discussion Lead
Introductions and Agenda	R. Peterson
Scope and Objectives of GE Effort	R. Meyer
History and Status of FFTF Regulatory Review	D. Simpson
Predisassembly and Disassembly Analysis LOF and TOP	A. Waltar
Mechanical Consequences Analysis	W. Partain
G. Fox, R. Madsen	D. Stepnewski
	G. Fox
	R. Madsen

Afternoon — Separate Discussion

	Attendees
I. Post Accident Heat Removal	R. Peterson, et al.
Pu Aerosol and Seal Leakage	K. Hikido, R. Meyer
Safety Assurance Diagrams	G. Gallagher (RDT)
System Design Descriptions	
HEDL Relationship to Development Organizations	
II. Mechanical Consequences of HCDA	D. Stepnewski, et al.
	A. Brown
III. MELT III Development	J. Scott, A. Waltar
Fuel Failure Models and Experiments	W. Partain
VENUS Modeling and Disassembly Criteria	D. Switick, B. Sarma

Summary – Morning Session

The original PSAR for the FFTF was submitted by HEDL, 1.5 years ago in September 1970. In January 1972, a completely new set of HCDA analyses were presented to the ACRS by HEDL. The new analyses attempt to support more recent experimental understanding of fuel behavior during gross transient overpower (TOP) conditions and to end the loss of flow (LOF) analysis of the PSAR toward a more logical conclusion. The prime responsibility for the PSAR analyses rests with HEDL for the TOP and with ANL/RAS (G. Fischer's group) for the LOF. The PSAR is scheduled for submittal in April 1973.

The analyses were not presented to the ACRS as completely defensible at this time, but rather as the direction HEDL felt it could go with further safety experimental test support. HEDL still considers the 150 MW-sec available work value as the correct ballpark to size the containment. They are now concentrating on using experimental data to show that the damage potential of a HCDA is very small, relative to the 150 MW-sec value.

The January 1972 ACRS letter says, in effect, go ahead with construction while the "balance of the review continues." HEDL was not able to further clarify the meaning of that statement. The post accident heat removal question has been delayed due to the FTR fallback position of an out of vessel meltdown pan. (Final decision point is about one year from now when HEDL says the reactor vessel will be installed.) RDT's position is that effort will not be spent on a meltdown structure unless an identifiable need can be demonstrated.

A June meeting is scheduled with AEC-Regulatory in Denver; identified topics for discussion are:

1. Core design
2. Core restraint system
3. Materials
4. Aerosol behavior

Transient Overpower Sequence (TOP) – A. Waltar, W. Partain

HEDL looked at various mechanisms which could cause accidental reactivity insertions (inc. test vehicles) before choosing a \$0.50/sec ramp for analysis. HEDL considers this ramp magnitude to be an upper envelope for identifiable reactivity sources.

The ejection of sodium by fission gas (F.G.) in the fuel rod plenum, as employed in the PSAR, is now considered totally unrealistic. HEDL bases this new view on the GE irradiated F3A rod puncture experiments. Sodium voiding is now considered to result from fuel ejection upon rod rupture into the coolant. Further, HEDL believes that fuel rod failures can be classified into three modes based on prior irradiation history of the fuel. These classifications are:

1. Fresh fuel (<3000 MWd/Te)
2. Irradiated ~ low power <8 kW/ft
3. Irradiated ~ high power >8 kW/ft

These groups were formulated from observations of TREAT transient fuel rod experiments; the assigned axial location of the cladding failure is placed near the midplane for 1. and near the core outlet for 2. and 3. (Refer to afternoon Session III for further discussion.) This is based on PECT II analyses which employ temperature dependent cladding mechanical properties and simulate F.G.-induced fuel swelling as an additional term in the fuel thermal expansion function. The rod failure condition is also a function of the fission generation rate, e.g., PNL 2-1 was driven at 6200°F/sec (peak) and rod rupture occurred (estimated) at 3500°F in the high gas retention region.

In summary, the fuel dynamics assumptions in the revised HEDL approach to the TOP sequence are:

1. Three classifications of fuel behavior based on irradiation history
2. Isolate fuel/sodium dynamics from fission gas in plenum

3. Molten fuel is ejected from failed rod into coolant
4. Fuel is locked to cladding and forces axial expansion (A. Waltar says this is a small effect).

To implement the above philosophy, HEDL is developing the MELT III code. The calculation will attempt to track fuel-sodium dynamics following cladding failure without requiring a classical disassembly code such as VENUS.

Calculations on the FTR equilibrium core have resulted in small molten fuel energies, typically on the order of 20 MW-sec. HEDL has parametrically varied the axial failure location, but the net effect of fuel motions was always strongly negative and terminated the excursion.

HEDL has also tried to model a large pressure due to MFCI at fuel expulsion, with subsequent reduction of fuel expulsion; however, the model is quite crude and requires more effort to substantiate it. HEDL will try to incorporate within MELT III the fuel expulsion model being developed by L. Smith at ANL for inclusion in SAS-2A. (However, this model is not in any better shape with respect to experimental confirmation.)

K. Hikido raised the question on safety limit trip points for the fuel which might fail below melt conditions (e.g., PNL 2-1). HEDL agreed that this could be a problem if some fuel could experience transient temperature rates in the high 100's to 1000's °F/sec prior to scram. HEDL is just starting transient tests in this range with scram (J. Scott).

Loss of Flow Analysis — A. Waltar, W. Partain

HEDL realized that the analysis presented in the PSAR left the problem in an undefined condition and that the analysis would have to be extended in time to investigate the ultimate deposition of core debris and the total energy release. As stated earlier, this analysis responsibility has been given to G. Fischer's group at ANL. The chronology of events in the ANL fresh core analysis are:

1. Normal pump coastdown
2. Boiling starts and cladding failures occur with fission gas bubbling out.
3. A fairly stable void is formed in each channel with Na vapor generation at the core inlet and condensation at the outlet. (The potential effects of Na vapor streaming on the cladding and fuel are under investigation.)
4. Extensive melting and upper portion of core collapse at gravity rate (12 \$/sec) and generate a mild disassembly excursion (VENUS).
5. Maximum energy in fuel above melting point has been 1000 MW-sec.
6. Options not considered are the employment of fission gas and steel vapor to enhance pressures during disassembly. (It is considered that this could reduce the energy release by an order of magnitude.)

Questions posed by GE:

GE: Do you consider a major pipe break as an HCD initiator?

HEDL: No, the probability of such an event is so low it will not be considered; however, we expect the consequences to be about the same as a nominal LOF.

GE: What expectation do you have of obtaining more experimental support for your FDSAR?

HEDL: Not much hope of any new data, especially in LOF area.

GE: What are your feelings on the industry's capability in the MFCI area?

HEDL Satisfactory understanding does not exist at this time, the S series tests are not providing the basic phenomena logical understanding necessary to substantiate modeling efforts

Mechanical Consequences — D. Stepniewski

Using REXCO H through ANL

“COREDYNE” code is being used to estimate transmission of forces through reactor vessel wall to support straps. More recent analysis in this area has been completed using BAM (G Fox). BAM is used to do plastic deformation of pipes and attenuation of pressure wave. These results are then used in WHAM to look at pressure response throughout the system. (Model fails approximately 45 msec into transient due to computation of negative absolute pressures.) This occurs prior to one cycle time of wave in system, therefore, results are of little value. As a result HEDL is now employing a W code, BLOWDOWN — II (originally a pressurized water code) to complete the analysis. The results are not yet complete.

HEDL intends to use REXCO HEP when it is available from ANL. Currently, HEDL and ANL are negotiating on exactly what each will do in this area.

Modeling of axial shielding in REXCO H is currently incorrect (hydrodynamically) and allows energy storage as axial kinetic energy, however, HEDL considers the solution of the slug kinetic energy reasonable enough for studies.

HEDL has tried PICIES but with poor results due to Physics International slippage on schedules and also due partly to employment of complex subroutines. A realistic comparison of PICIES/REXCO H has not been accomplished. Dan Stepniewski warned of promises given by PI. Although not too well documented, HEMP, a new computer code, is capable of matching the performance of the REXCO H code.

SL 1 vessel deflection have been modeled to within 20% with the DYSI code, a spring mass approximation to vessel deflection.

Afternoon Sessions

Session I — Topics Discussed

- 1 Post Accident Heat Removal (PAHR)
- 2 Pu Aerosol and Seal Leakage During HCDA
- 3 HEDL Relationship to Development Organizations
- 4 Safety Assurance Diagrams (SAD's)
- 5 System Design Descriptions (SDD's)

Attendees

HEDL	GE	AEC-RDT
R Peterson	K Hikido	G Gallagher
A Schade	R Meyer	
D Simpson		
J Hagan		

Summary of Discussions

1 Post-Accident Heat Removal (PAHR)

R Peterson presented the FFTF position on PAHR, and the status of related development work. ANL has overall responsibility to develop information required to defend the position that the core debris can be satisfactorily cooled inside the primary coolant system in the event of a HCDA. Assuring system integrity (a necessary precursor to cooling inside the reactor vessel) is HEDL's responsibility and will be based on SRI

model test results and REXCO analysis (REXCO calculations performed to date have been done by ANL, but HEDL plans to develop their own capability with REXCO).

There has not been any extensive analytical work in the PAHR area. ANL is developing a computer code to analyze cooling of particle beds with sodium cooling underneath the supporting structure. This basic configuration is applicable to the first two levels of defense; the first level being the top grid support plate, and the second, the bottom plate of the core support structure. They believe the entire core can be cooled at either location if the material is properly distributed. Only limited analyses have been performed to answer the question of possible recriticality.

The third and final line of defense for cooling inside the reactor vessel is to cool core debris in the bottom of the reactor vessel. The vessel contains sodium with the bottom surface of the vessel gas insulated. The sand-water and UO_2 -Na small scale test results are being used to cover this case. The potential for an energetic interaction between hot core debris and sodium as the debris relocates within the vessel is being investigated by a rather large test in which molten UO_2 (kg quantities) will be dumped into sodium.

Some conceptual studies have been performed on ex-reactor PAHR systems. The FFTF position is that such a system will not be installed unless it is proven necessary. No date have been set for making a final decision. Two types of ex-reactor PAHR systems are still being considered. These are the silica-bed type (possibly requires external cooling coils), and the more conventional tank-type system with cooling trays and an active cooling system.

2. Pu Aerosol and Seal Leakage During HCDA

A. Schade discussed preliminary Pu aerosol analysis in an effort to reduce the amount of Pu becoming airborne above the sodium in the reactor vessel which can then leak to the containment. With the 4.5 mile exclusion radius, noble gases are no problem for the HCDA, so the concern is for Pu release. Motivation for this effort is grounded in the fact that no practical method has been found to design the large head seals to the tight leakage requirements of the reactor cavity (1000%/day at 35 psig). Apparently, the head doming due to the HCDA loading (Na slug impact on the underside of the head) makes it impractical to avoid flange-seal separation of the large non-circular penetrations in the head. Also abandoned is the canopy seal between the reactor vessel head and the cavity liner in favor of another type of packing seal. HEDL has the back-out position of closing up the head access cell, making it equivalent to the reactor cavity in pressure and leakage capability. In effect, HEDL is still trying to support what is now a single containment barrier by significantly reducing the plutonium aerosol predicted to become airborne in the containment. Analytical results should be available from this analysis in the next few months. If this approach appears to be feasible, experimental data at the very high temperatures (3000 to 5000°F) would probably be required to support the analysis since the experiments with aerosols to date have been conducted at near-room temperature conditions.

3. HEDL Relationship to Development Organizations

Discussions were held with R. Peterson concerning the system used by HEDL to provide direction of development work supporting the FFTF. Samples were obtained of their documentation system which could be useful in developing the GE position on liaison activities with development organizations, such as ANL.

In summary form, development requirements are identified in a document called the "Development Test Request Form" which can be initiated by any of the contractors or by HEDL. The DTRF's are transmitted by HEDL directly to candidate development organizations with a copy to RDT. Selection of the organization and the inevitable adjustment of priorities and funding are done by RDT and HEDL with participation of the development organization as appropriate. The next step is the generation of a work plan (example is: ANL/RAS 72-8 on Post Accident Heat Removal). Considerable attention is now being given to directly relating the work plans on safety-related tests to the Safety Assurance Diagrams. HEDL also generates the section of the work plans covering schedules and milestones to assure conformance with

the FFTF schedule and needs. RDT approves work plans and any modifications. Typical time from a test request to an approved work plan is currently 3 to 6 months. Status of development programs is determined by frequent telephone contacts between the responsible HEDL engineer and the responsible development engineer, and through monthly progress reports.

4 Safety Assurance Diagrams (SAD's)

RDT has made the decision to use SAD's on the FFTF project. Failure Modes and Effects Analysis (FMEA) and fault tree analysis can be used by the safety engineers and designers to support the lines of defense in the SAD's. The latest version of the SAD's has been released and is available. A task has been proposed by GE as part of the new Safety Engineering TA (Contract AT(04-3) 893 Task Agreement 2) to investigate the possible application of the SAD's to the D Plant project.

5. System Design Description (SDD's)

A letter summarizing the current status of all SDD's was received from R. Peterson. This information will be useful in requesting revised SDD's to update the BRD file.

Session II – Mechanical Consequences of HCDA

Attendees

HEDL	GE
D. Stepniewski	A. Brown
G. Fox	
D. Simpson (part time)	

Summary of Discussions

D. Stepniewski of HEDL made a summary presentation of the work being done to predict structural damage from the HCDA. In response to questions regarding the structural design, it was indicated that the conical shell support for the core support structure was selected based on cost and manufacturing considerations, after considering alternates. The large Z ring supporting the vessel from the shield deck evolved from considerations of thermal gradient effects. For PSAR analysis, REXCO-H was used as a reference analysis of vessel damage. The more extensive code REXCO HEP will be used for the FSAR coupled with the "explosion" experiments being conducted at SRI. HEDL has not run REXCO themselves but used ANL to provide this service.

Several peripheral codes were described. CORDYNE is a simple lumped mass spring dash pot analysis to characterize the load transmission from the core support structure through the vessel to the supports.

BAM is a code reported in HEDL TME 71-50, which predicts the dynamic response of a pipe to a pressure transient of sufficient magnitude to cause the pipe yielding. HEDL has used the WHAM code to study pressure transmission effects in the primary cooling circuit. This is an elastic water hammer type code. Apparently at geometry changes negative pressures can result locally. This would result in cavitation and the code does not handle phase changes. This has led to rejection of this code. BLOWDOWN, a proprietary code from Westinghouse, is currently being utilized for evaluation of the response of piping and equipment to transient pressures in the vessel. HEDL has coded a lumped mass spring model called MOTION to predict the loads on the vessel supports. Analysis of the loads transmitted into the building has resulted in the elimination of structural columns which were inserted in the reactor cavity to carry these loads into the building foundation.

In informal discussions, information was provided on the pressure time history experiments conducted at SRI. The peak pressure measured in tests simulating 150 MW-sec was 1000 psi and in tests simulating 350 MW-sec it was 2000 psi. The scaled time duration of these pressures was in the range of 10 msec, for example, one typical case at 350 MW-sec gave 6 millisecond rise from 0 to 2000, 6 milliseconds at 2000, 4 millisecond drop to 1000 followed by a general trailing off. Since the pressure to yield a 36 inch pipe at 3/8 inch thick is about 300 psi, this appears to be a

critical area in the design, since several milliseconds on a thin wall pipe gives ample time for the pipe wall to respond. HEDL indicated that for the 150 MW sec reference accident, some pipe yielding was predicted for their design.

In answer to questions regarding critical areas in the primary piping system, indications were that the pump cover gas played a significant role, but that the connection between the main flow stream and the pump "reservoir" is a 3/4 inch wide annulus. The above mentioned problems together with the computer code WHAM have not provided a final analysis of system response. It is felt that pressure fronts traversing from the vessel outlet and inlet may come together at the IHX where some elements within it are sensitive to such loads.

For their assumed HCDA conditions, indications are that flow reversals were not sufficient to close the check valve on the primary inlet leg.

It was learned that Bechtel is using a finite element code, PIPEPLAST, to analyze local stresses at the outlet nozzles. HEDL is not code stamping the vessel for HCDA conditions. They have had some difficulty finding practical ways of communicating HCDA requirements on the vessel. In addition, they have transmitted information as imposed deformation on a reference design and will review the final design for the effect of any changes made by the vessel manufacturer.

Session III – Topics Discussed:

- 1 MELT III Modeling
- 2 VENUS Modeling, DRL Concerns, Disassembly Criteria
- 3 Fuel Dynamics Experimental Bases

Attendees

HEDL	GE
A Waltar	D Switick
W Partain	B Sarma
J Scott	

Summary of Discussions

1. MELT III, a new generation code, was discussed in comparison with MELT II which was used for the PSAR. The major code areas of change are
 - The steady-state thermal calculation includes detailed fuel restructuring - are trying to tightly couple with fuels people (Cox, Hanson) to have an accurate estimate of fuel failure based on their latest ideas (large effort in last five months with RDT concurrence)
 - The PECT code has been included as a subroutine to calculate transient cladding strain, fission gas (FG) induced fuel swelling, etc
 - Primary system sodium hydraulics are included with a detailed axial model of the entire fuel assembly, sodium condensation accounted for
 - A model for molten fuel expulsion into the sodium channel is included, models of released FG effects on the sodium channel hydraulics, heat transfer and neutron kinetics are incorporated
 - The ANL MFCI code (L. Smith) will be incorporated when it is available

The coolant hydraulics solution in MELT II is considered poor by HEDL, and they will do a much better job in MELT III. HEDL will also have SAS 2A available to them at Idaho Falls or Berkeley facilities.

2 DRL expressed concern in several areas of VENUS modeling for the TOP sequence. The key areas were

- Treatment of voids in reactor
- Homogeneous assumption vs. heterogeneous reactor
- Treatment of large single phase pressures

A basic problem in VENUS modeling of a TOP accident is the inherent homogeneous assumption (fuel, cladding and coolant are a homogenized fluid in each node), for a sodium-in condition, large single phase pressures are computed by VENUS when the cladding is still a strong, cold container. Thus, relative axial flow of fuel and/or sodium should be considered. ANL is working on this problem with VENUS. Both HEDL and ANL are attempting to bypass this problem by handling the motion of fuel and coolant within the original heterogeneous model, MELT or SAS.

3 Fuel Dynamics Experiments – J. Scott

Experiments designed to support the HEDL models of cladding deformation and rod failure showed that good correlations exist between PECT II predictions of differential thermal expansion in fresh fuel and PNL 17 cladding strain measurements.

Fission gas bubble coalescence during transients was verified by comparison of pre and post transient fuel photomicrographs. Examination of specimens exposed to transients show 2 to 6 percent volume swelling by bubble growth in the fuel grains. In order for coalescence to occur, temperatures in excess of the steady state irradiation temperatures must be attained. High pressure fission gas pores (~1.5 μm diameter) that exist on grain boundaries can be verified by photomicrographs of PNL 2.1 following irradiation at low power. J. Scott feels the results of the TREAT transient on PNL 2.1 are proof of this cladding loading mechanism.

PNL 2.1 was a single rod, stagnant Na experiment subjected to a transient characterized as 6200°F/second peak and 4300°F/second average energy addition rate. Failure occurred as a guillotine rupture of the rod at 60 percent of TREAT energy input (indicated by TC/s erratic behavior) with the upper part being ejected 9 inches and out of the peak TREAT flux. It was thus preserved for analysis essentially in its failure state. Photomicrographs of the failure region showed separated grains but no FG formation within the grains. Conclusion was that the grains were forcibly separated by pore gas before melting could occur. Cladding strain measurements match this picture by indicating the calculated differential expansion strains (1 percent) over much of the rod but with 7 percent strains near the failure zone and the outward rupture of the cladding.

PNL 2.11 is another potential example of this mechanism. It was supposed to experience two TREAT flat tops with operation at 4800°F for a length of time prior to a power burst. The first TREAT ramp was programmed to minimize differential expansion induced strains on the cladding. The rod failed very soon after attaining 4800°F on the flat top.

One HEDL experiment did not agree with the fuel dynamics models. The specimen in question was PNL 59.8, 36 inches long and irradiated in GETR to 80,000 MWd/Te. The rod had a well defined central void and rather gassy region. The fuel eventually compacted into a more reactive configuration.

F.2 Meeting Notes – ANL/GE – Discussions on FFTF HCDA Analysis

The subject meeting was held at ANL/Chicago on May 24, 25, and completed the review of the FFTF HCD analysis for FY 72 under PA 67. The primary GE objectives were

- 1 To determine current status and future schedule of analytic model development and analyses of the FTR HCD analyses

2. To identify key areas of analysis where experimental verification of analytical models is required or already exists.
3. To establish procedures for GE to initiate analyses with the ANL SAS-2A computer code. Also to determine the schedule for ANL to put the code on the Berkeley CDC 7600 computer.

The tentative agenda was essentially followed; changes were made to accommodate the availability of personnel.

Summary

ANL/RAS (G. Fischer's Accident Analysis Group) has primary responsibility for providing analyses of the FFTF unprotected loss-of-flow (LOF) excursion for inclusion in the FDSAR. HEDL has prime responsibility for the transient overpower, TOP excursion. ANL is also performing a TOP analysis, and the two organizations will meet later to compare results.

ANL will begin documentation of their LOF analyses by November 1972 and terminate effort by January 1, 1973. This schedule may be revised with changes in the FTR Project schedule.

The adopted ANL approach is to perform as detailed a mechanistic analysis as possible, allowing for "reasonable" conservatism. Analytical development in support of the FFTF HCD analyses is a continuing project at ANL. Current emphasis centers on fuel-cladding behavior after loss-of-cladding integrity in the LOF sequence. ANL would like to take credit for assumed large fuel expansions caused by fission gases (defined as frothing) to quickly shut down the nuclear transient and limit the energy release. The potential for molten clad and/or fuel to freeze in the outlet region of the fuel assemblies is a very real concern.

In the TOP sequence, development emphasis appears to be on describing the details of molten fuel-fission gas mixtures ejecting from the rods into flowing sodium. It is hoped that enough fuel will be ejected from the core to limit the energy release. ANL is attempting to maximize the non-coherency of fuel initial conditions due to differences in prior irradiation history. The fresh core TOP case study has not been completed at this date. ANL is concerned with the loss-of-fuel non-coherency and the axial fuel failure location in this case.

The tendency is to show that the LOF or TOP sequence is terminated by fuel expulsion from the assemblies before a classic disassembly condition is reached in large areas of the core. Thus, the analyses could be completed within the SAS computer system; similar to HEDL's approach to employ MELT III for the TOP calculations.

Revisions within the VENUS code now include the capability to treat the additional local pressures due to fission gases as well as a potential MFCL. Current analytic development is directed at the expected relative axial flow of the sodium and fuel. This could strongly influence the calculated energy release.

Based on the above types of analyses, ANL has reached a preliminary conclusion that the consequences of both the TOP and LOF incidents will be low compared to the quoted 150 MW-sec containment capability.

One obvious area of difficulty is that the often complex analytical models required to perform the above analyses do not have sufficient experimental evidence to support their use. Examples of sensitive modeling areas are molten fuel-cladding motion following cladding loss of integrity and fuel frothing due to entrained fission gases. Argonne realized this situation and is currently recommending both out- and in-pile experiments to provide further justification for fuel frothing, cladding movement, non-coherent effects, etc. A prime example is the continuance of the out-of-pile experiments of T. Weber at HEDL to demonstrate the fuel frothing mechanism.

Neither ANL nor HEDL is currently pursuing a failure propagation sequence for the FFTF HCDA.

At this time ANL and HEDL have not performed detailed analyses of the control material transient behavior for any of the sequences. It is GE's opinion that such analyses should be considered for the FTR/FSAR, especially in light of the very long time scale of the LOF analysis and the stated ANL/HEDL models for fission gas effects in solid fuel during TOP excursions.

The responsibility for analyses to support in-vessel cooling of the core debris rests with ANL. At this point ANL has not performed detailed calculations to support PAHR or to consider questions of recriticality and secondary MFCI's.

ANL was quite cooperative in making arrangements for GE to use the SAS-2A code at Chicago, and if necessary, at Berkeley next fiscal year. The current schedule calls for SAS-2A to be available to GE on the CDC 7600 early in July 1972.

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