DR-1886

BAW-1365

MASTER

1000 MWe

L M F B R

Liquid Metal Fast Breeder Reactor

SAFETY ANALYSIS STUDIES

BABCOCK & WILCOX

Accident Analysis and Safety System Design Study

Final Report

Prepared for ARGONNE NATIONAL LABORATORY

United States Atomic Energy Commission Division of Technical Information

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1000-MWe LMFBR Accident Analysis
And
Safety System Design Study
— Final Report —

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BABCOCK & WILCOX
Power Generation Division
Nuclear Power Generation Department
P. O. Box 1260
Lynchburg, Virginia 24505

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1. INTRODUCTION

1.1. General

The increasing demand for electric power in the United States has emphasized the role that breeder reactors must play in the future fulfillment of the demand. Toward this end the AEC has established the LMFBR program to develop a safe, reliable, and economical LMFBR power plant for the utility industry. In accordance with this program, the AEC awarded a number of contracts to develop conceptual designs for a 1000-MWe LMFBR plant. Basic to these studies was the stipulation that these plants incorporate anticipated technology for the 1980s. Babcock & Wilcox (B&W) participated in the 1000-MWe LMFBR Follow-On Study and completed the work in 1968.

The results of the Follow-On Studies provided the necessary preliminary information required to examine, in a broad sense, many aspects that will determine the future course of events leading to the successful development of LMFBRs. Because of the wide scope of the studies, many safety-oriented problems that must be considered to provide detailed guidance for specific research and development and resolved to ensure that there is no undue risk to the public were not sufficiently evaluated. The obvious importance of continuing the safety-related work has been instrumental in B&W's decision to further evaluate the 1000-MWe LMFBR reference design in an Accident Analysis and Safety System Design Study. This study, which includes investigations of several high-priority, safety-related problem areas identified in Element 10 of the LMFBR program plan, involves the analysis of potential accidents that may determine the design basis for protective systems and safety features. Equally important, it includes the development of functional requirements and conceptual designs for several specified systems or features that could be influential in determining the overall safety of the plant. Following these investigations, research

and development programs were to be developed to provide the necessary safety technology.

The study was conducted on a cost-sharing basis under ANL contract No. 31-109-38-2339. Utility companies participated in the work under separate agreements with B&W.

1.2. Program Summary

1.2.1. Overall Program

The basic objective of the Accident Analysis and Safety System Design Study was to develop a better understanding of the influence of safety requirements on LMFBR plants of commercial size. To achieve this objective, the work was scheduled to be performed over a three-year period under a program definition phase and four time-oriented work phases. Figure 1-1 is a schematic representation of the sequencing of major activities and time phases.

In Phase I, component and system faults and malfunctions associated with the reference design developed by B&W during the Follow-On Study were identified and cataloged. Fault trees were prepared and used to help identify the protective systems and safety features required.

Phase II concerned the definition of initiating conditions based on the faults and malfunctions identified in Phase I and the subsequent analysis of potential reactor accidents to establish functional requirements for selected protective systems and safety features. In parallel, candidate systems or features having the potential to prevent or mitigate the consequences of accidents were identified and evaluated to provide guidance for the selection of reference designs at the end of Phase II.

In Phase III, conceptual designs were to be developed for the protective systems and safety features selected in Phase II. Additional safety analyses were also scheduled to verify the compatibility of the functional requirements and system designs.

During Phase IV, safety-related information needed for the design, licensing, construction, and safe operation of the reference pool-type LMFBR was to be identified. Appropriate research and development programs were to be specified, and, where meaningful, the

effects of nonattainment or partial attainment of research and development goals were to be assessed.

1.2.2. Current Status

The Accident Analysis and Safety System Design Study was initiated in June 1969, and the Phase I work was completed on schedule in October 1969. The fault trees and malfunction catalog developed during this phase are included in the Phase I report, BAW-1344.

Phase II work, initiated in parallel with the Phase I activities, proceeded in accordance with the work plan developed during the early months of the study. Phase II was scheduled to be completed in April 1971, but on October 15, 1970, B&W was advised that all 1000-MWe LMFBR Study contracts were being terminated for the convenience of the government. The directive specified that the termination was effective immediately and applied to all work under the contract with the following exception:

Babcock & Wilcox should prepare a final report, suitable for external distribution, to cover all of the work performed under the contract except that already reported in the Phase I report, BAW-1344.

In accordance with the directive, this report covers the work performed to date in Phase II of the study. In addition, the reports published as Technical Notes during Phase II are being reissued as Topical Reports. These reports, listed below, supplement the information given in this report and provide more detailed information on the work in the activities completed prior to contract termination.

BAW-1342: 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Technical Note — Accident Analysis Methods, November 1969.

BAW-1349: 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Technical Note — Candidate Secondary Containment Support Systems, March 1970.

BAW-1350: 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Technical Note — Initiating Conditions, Part I, Flow Abnormalities, April 1970.

BAW-1351: 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Technical Note — Candidate Emergency Heat Removal Systems, May 1970.

BAW-1352: 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Technical Note — Candidate Primary Containment Safety Features, May 1970.

BAW-1354: 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Technical Note — Candidate Protective Features, June 1970.

BAW-1355: 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Technical Note — Evaluation of Effects of Irradiation-Induced Metal Swelling, September 1970.

BAW-1356: 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Topical Report — Local Core Faults, December 1970.

1.3. Phase II Activities

Figure 1-2 shows the sequencing and relationship of the major activities and illustrates the overall approach planned for the Phase II work. In general, Phase II featured the performance of several accident analyses and related system design activities in parallel; it was scheduled to be completed over a 21-month period.

The first objective in the Accident Analysis series of activities was to define the conditions imposed on the core by localized system faults and malfunctions based on the fault trees and fault catalogs developed in Phase I. Three major categories of events were evaluated, including gross flow abnormalities such as flow coastdown, local flow disturbances that might occur because of fuel pin failure or assembly blockage, and malfunctions leading to reactivity insertions. The results obtained in these investigations are described in section 2.

For the events in which the core could be irreversibly damaged (either because of the severity of the fault or the postulated failure of the protective system), the analysis was continued in Activity 221, Accident Analysis. The basic objective of this evaluation was to define the course and the consequences of potential accidents extending over

the range from local conditions originating in one fuel assembly to severe hypothetical accidents involving core disassembly. The results were then used to guide in the selection of the design basis accident(s) in Activity 222 and to assist in the definition of functional requirements for the protective systems and safety features in the 230 series of activities. The accident analysis results and discussions of design basis accidents are presented in section 3.

Four different system areas that are influential in establishing the safety characteristics of the plant are included in the design portion of the study. As shown in Figure 1-2, these systems included protective system features, emergency decay heat cooling, primary containment safety features, and secondary containment safety systems or features. In each of these four areas, three major activities were scheduled to be performed primarily in series, proceeding from the identification of candidate systems or features in the 240 series to the establishment of functional requirements in the 230 series and the selection of reference concepts in the 250 series. Candidate systems were identified on the basis of input from the Follow-On Study work and from Phase I activities, and the functional requirements activities were planned to provide the connecting link between the accident analysis and system design portions of the study and to provide more definitive functional requirements prior to selection of the reference concepts. The results from all of the design-related activities are summarized in section 4, where the material on each system area is presented in one of the four major subsections.

The activities for evaluation of the effects of irradiation-induced metal swelling reported in section 5 are not shown in Figure 1-2 because this work was initiated about a year after the accident analysis study as an additional scope of work. It was undertaken as a short, "scoping" type of evaluation to better define the problems created by the swelling phenomenon, to identify potential solutions, to evaluate the relative merits of candidate changes to the reference design, and to qualitatively assess the effect of any suggested changes on the ongoing safety study. Section 5 gives a synopsis of the potential solutions considered and briefly describes the characteristics and features that might need to be reflected in an upgraded 1000-MWe reference design.

Figure 1-1. B&W-LMFBR Program Diagram

Figure 1-2. Activity Flow Diagram -
Phase II

* TECHNICAL NOTES WILL BE ISSUED DURING THE ACTIVITY

2. ACCIDENT-INITIATING CONDITIONS

Accident-initiating conditions may be generally defined as the conditions imposed on the reactor system because of the malfunction or failure of system components. A detailed review of B&W's 1000-MWe reference design was performed and reported in Phase I of the Design Study (BAW-1344) to identify fault conditions relevant to safety. From this review was developed a catalog of the faults that could potentially lead to serious accidents. This fault catalog then formed the basis for the detailed quantitative definition of accident-initiating conditions performed in Phase II.

Core-related accident-initiating conditions can be grouped into three major categories according to the manner in which they affect the core. The first category involves abnormal changes in the system that initially affect the whole core through thermal-hydraulic perturbations. These include abnormal changes in primary coolant flow, such as in a primary system pump coastdown, and abnormal changes in inlet temperature, such as loss of access to the heat sink as a result of secondary system failures. Analyses of accident-initiating conditions in this category are reported in section 2.1, Gross Flow Abnormalities.

The second category involves the malfunctions that initially affect the neutronic characteristics of the core. Included are such effects as malfunctions in the control systems and sudden changes in core composition or configuration, which result in reactivity perturbations. Analyses of this type of accident initiating conditions are reported in section 2.2, Reactivity Insertions. In the analyses of these first two categories the emphasis has been on the response of the reactor protection system to the reactor transients initiated by the abnormal thermal-hydraulic or reactivity perturbations. In addition, fairly extensive studies were made wherein the most sensitive and/or uncertain parameters were varied. These studies served to indicate

whether uncertainties in these parameters were significant and in need of better definition.

The third category of potential accident initiating conditions includes the faults that initially affect the core only on a local level. In general, these faults involve the introduction of a local abnormality in thermal-hydraulic conditions. Included are such effects as fuel pin failure that lead to coolant subchannel blockage or fission gas release and blockages of few or many subchannels or an entire assembly due to foreign material or objects. Analyses of these effects are reported in section 2.3, Local Flow Abnormalities. In these analyses the primary emphasis has been on the questions of (1) whether an initial abnormal condition propagates to involve a larger part of the assembly and (2) whether the reactor protection system can limit the consequences.

The scope of work for the Accident Analysis and Safety System Design Study specifies separate activities for the analysis of initiating conditions and the accidents that follow these conditions. Thus, a criterion for the separation of these analyses is required. The criterion used stated that the analyses of the transient effects up to the point of major damage or drastic changes in core conditions would be categorized as initiating conditions. Any further analyses would then be performed in the accident analysis activity. Specifically, this meant that for full-core accidents the initiating-condition analyses covered the range from the initial malfunction to the point of fuel failure or sodium voiding. For local accidents the initiating-condition analyses covered the range from the initial abnormality up to the point at which the potential for damage beyond a single assembly became significant. The analyses of accident conditions arising from some of the initiating conditions are reported in section 3, Accident Analysis and Candidate DBA Selection.

2.1. Gross Flow Abnormalities

2.1.1. General

A number of events can potentially disrupt coolant flow. This section considers only the events that can lead to gross or whole-core flow transients; local flow abnormalities are treated in section 2.3.

The importance of such events is immediately obvious: they have the potential of leading to inadequate cooling in large segments of the core or, indeed, in the whole core. Thus, such events could ultimately result in loss of cladding integrity, widespread fuel melting, coolant boiling, and very large, rapid reactivity insertions. For this reason it is important to predict the dynamic response of the reactor system to severe flow imbalances. Analyses should indicate not only the nature of the system's response but also the adequacy of protection.

Many definable flow abnormalities have little or no effect on the behavior of the core. This is especially true of some abnormalities in the intermediate or secondary coolant systems; these abnormalities will not be considered here.

This study seeks to define the conditions imposed on the core through flow abnormalities that arise from specific localized system faults. The conditions so defined may then be used as initial conditions for the accident analysis calculations. The ultimate end of all the calculations will be the selection of the design basis accident(s) for the engineered safety features.

The conditions arising from faults in the primary and secondary coolant systems can be analyzed in several ways. The approach used here emphasizes the analysis of identifiable accidents. In order to identify potential faults in the system, a detailed review of B&W's 1000-MWe reference design was made, and malfunction catalogs were constructed. From these catalogs numerous fault trees were drawn to describe the potential fault paths arising from the initial malfunction. From the fault trees, several primary loop abnormalities were identified and studied:

1. Complete loss of six primary pumps.
2. Complete loss of two primary pumps.
3. Double-ended break in a 28-inch primary pipe.

The primary system is coupled to the intermediate system through six intermediate heat exchangers (IHXs). Many identifiable intermediate and secondary loop malfunctions have the potential for disrupting the normal operation of the primary system. However, none of the malfunctions

leads to conditions more severe than the complete and instantaneous loss of access to the heat sink provided by the intermediate system. This malfunction is postulated rather than arising from a causal sequence of events. Since this hypothetical event bounds the severity of credible events, it is the only malfunction analyzed in connection with the intermediate and secondary systems.

For each of the events enumerated above (1-3), certain sensitivity studies were performed to assess the effects of uncertainties on the results. The details of the calculations are presented in BAW-1350; the calculations and results will only be summarized here.

2.1.2. Loss of Flow

One of the most serious malfunctions possible in a reactor core is the interruption of adequate cooling, which may arise from a number of faults, such as loss of pumping power, loss of system integrity (due to pipe or vessel rupture), or blockage of flow channels. A loss of pumping power is probable during the operating lifetime of the plant; thus, it is very important to be able to predict the behavior of the reactor system during loss of pumping power transients.

2.1.2.1. Flow Coastdown

The six B&W primary pumps are distributed among four nonvital busses, so that no more than two primary pumps will ever draw power from a single bus. This means that the failure of a single bus can affect no more than two primary pumps. The simultaneous failure of more than one bus is considered incredible for any event other than the loss of off-site power. In the event that off-site power is lost, all six primary pumps will coast down simultaneously. Since this flow transient is more severe than the transient arising from the loss of three to five pumps, and since the loss of six pumps or two pumps is the most probable failure mode, only six-pump and two-pump coastdowns were investigated. The six-pump coastdown bounds the severity of credible coastdown accidents.

The flow coastdown calculations were performed with the TART computer code.¹ The reactor is represented

by nine axial and seven radial regions: the seven central axial regions represent the active core, and the first and last axial regions represent the lower and upper axial blankets. Of the seven radial regions, one represents the hot pin, one represents the peak (or maximum powered pin), and one represents the radial blanket; the remaining regions represent the remainder of the active core.

The reactivity coefficients, nuclear parameters, and sodium worth distribution are as follows:

Doppler coefficient	$\left(T \frac{dk}{dT}\right)$	- 0.00533
Sodium density coefficient	$\left(\rho \frac{dk}{d\rho}\right)$	0.0253
Axial expansion coefficient	$\left(R \frac{\delta K}{\delta R}\right)$	- 0.532
Linear expansion coefficient	$\left(\frac{1}{L} \frac{\delta L}{\delta T}\right)$	0.0000104
β_{eff}		0.00364
$\ell^* (s)$		0.29×10^{-6}

Two pump inertias were considered in these investigations: the reference design inertia of 15,000 lb-ft², and an inertia of 6000 lb-ft². The flow transients were calculated using a digital simulation.

Two series of calculations were performed for the flow coastdown accident. The first used nonrestructured fuel pin models; later, many of these calculations were rerun using restructured fuel pin models.

The first (nonrestructured) pin model assumed no fuel densification effects and no central void. The fuel pin was

divided into five nodes of equal radius and nine axial segments. The seven central axial segments represented the core region, and the two end segments represented the upper and lower axial blankets. Axial conduction was neglected. The conductivity function used in this representation was

$$K_{\text{fuel}} = 1.7666 \times 10^{-4} + \frac{0.277778}{T_{ji} - 32}$$

where T is the temperature in the radial node j at a specific axial location i . This representation was found to disagree significantly with TAMPA² for end-of-cycle conditions; specifically, the model produced fuel temperatures higher than those predicted by TAMPA. As a result, a more sophisticated representation was adapted for end-of-cycle conditions.

The EOC pin was modeled using nine axial segments with ten radial nodes. Again, axial conduction was neglected. A central void of 0.003-foot radius was assumed. The conductivity function was taken from the TAMPA code. In this way the effect of fuel densification in the EOC pin was approximated. The results agreed quite well with the conditions predicted by TAMPA; the centerline temperatures agreed to within 50F.

The scram setpoints for the core instrumentation were 2621 MW for the power monitor (107% power) and 1139F for the bulk outlet temperature monitor. The bulk outlet temperature thermocouple was located just above the upper axial blanket outlet in the outlet plenum. A 5-second time constant was assumed for this thermocouple.

The scram delay time for the system (300 ms) is defined as the elapsed time between the detection of an out-of-limit condition and the first motion of the rods. The normal rod speed for shim motion is 15 in./minute; the scram speed of 0.7 g is backed up by a secondary motor-driven 30 in./second.

A number of parameters may influence the course of the coastdown transients. Because of uncertainties in many of these parameters it was necessary to investigate the influence of the

most sensitive or uncertain parameters on the response of the reactor. The following parameters were investigated:

1. Reactivity coefficients.
2. Fuel pin model.
3. Control system parameters particularly rod worths and delay time.
4. Partial failure of the protection system.
5. Pump inertia.

The reactivity coefficients, pump inertias, and fuel pin model were varied independently. In order to assess the relative importance of the various parameters, the transient was allowed to develop until terminated by a scram initiated by either the power monitor or the bulk outlet temperature monitor. This arrangement is equivalent to bypassing the first three levels of protection: (1) the pump electric power monitors, (2) the flow monitors, and (3) the power-to-flow ratio monitors. Any of these three detectors will produce a scram much earlier than either the power monitor or the bulk outlet temperature monitor. The use of this analytical scheme in no way implies that three simultaneous faults in the protection system would ever be expected; it is merely expedient from a calculational point of view because it allows enough time for the effect of each parameter to become evident.

The following values of the reactivity coefficients were used in the sensitivity studies with delayed scram: (1) rated values, (2) 0.5 Doppler with all other coefficients as rated, and (3) zero axial expansion coefficient with all other coefficients as rated, and (4) 1.5 times the sodium density coefficient with all other coefficients as rated. The calculations were performed for pump inertias of 15,000 and 6,000 lb-ft² with the nonrestructured fuel pin model. The power traces for the 15,000-lb-ft² pump inertia are shown in Figure 2-1.

The most important aspect of the results is that, in general, any "worsening" of the reactivity coefficients (that is, increasing of positive coefficients or decreasing of negative coefficients) leads to a power rise that is more rapid than normal. This power rise

in turn leads to an earlier-than-normal scram and a consequent lowering of the integrated power (and hence the maximum temperatures) for the transient. An example of this behavior is shown in Figure 2-2 for the 15,000-lb-ft² coastdown. One can see that increasing the sodium-density coefficient has the most pronounced effect on reactor dynamics.

For the 6000-lb-ft² inertia coastdown with the BOL pin, the maximum cladding temperatures continue to rise after scram. This effect is produced by the rapid decay of coolant flow for the 6000-lb-ft² pump inertia, so that after about 7 seconds the flow is essentially stagnant. Since the calculation does not include the pickup of flow from the pony motors, this is equivalent to decay heating in stagnant sodium. Use of the pony motors would reduce the cladding temperatures to an acceptable level.

Some pin damage may occur at a cladding temperature of 1300F, as it does in the steady state; however, the probability of such failures is reasonably low. Any failures are expected to occur principally in damaged or defective pins and are not expected to be violent.

Some of the calculations described above were repeated using the restructured fuel pin model and a hot pin representation in TART. Hot pin factors of 1.10 for power and 1.13 for channel enthalpy rise were applied to the peak pin in order to arrive at a consistent hot pin representation. As before, the scram was delayed until either the bulk outlet temperature monitor or the power monitor produced a scram signal.

As in the previous cases, no fuel melting occurs, although relatively high cladding temperatures exist. For a coastdown with a 6000-lb-ft² pump inertia, the cladding temperatures are somewhat high (~1400F). The effect of the decay cooling pony motors has been neglected, but if they were taken into account, the cladding temperatures would have been reduced substantially. The improved heat transfer characteristics of the restructured fuel pin model lead to early scram relative to the nonrestructured model.

The effect of varying certain of the protection system's parameters on the excess integrated energy in a flow coastdown transient were investigated. Here, excess integrated energy is

defined as that generated above the energy that would have been generated had the reactor remained in steady-state operation. The two parameters varied were the scram delay time and the total reactivity worth at scram. The base case was the restructured, 6000-lb-ft² flow coastdown. The scram delay times were varied from 150 to 500 ms. The total reactivity worths were reduced from rated values to 0.25 rated.

The excess integrated energy exhibits a weak dependence on the total reactivity worth available at scram over the range of the variables investigated and a strong dependence on scram delay time. As for terminating flow coastdown transients, we can thus conclude that there is little incentive to increase the scram reactivity, but there may be some incentive to reduce the scram delay.

Some failures may occur among damaged or defective pins, but in the absence of molten fuel these are not expected to be violent. If the protective system performs as designed, then no failures are expected. The scram mechanisms were deactivated to study the loss of two primary pumps. The maximum centerline fuel temperature rise was 150F, and the maximum cladding temperature rise was 152F. A new equilibrium power level of 2542 MWt was reached in about 3 seconds.

2.1.2.2. Pipe Break Accident

The accident considered here is a double-ended break in a 28-inch primary pipe. The break is assumed to occur at the inlet nozzle and to be complete in 0.01 second.

The model describing the flow transient following a double-ended break in a 28-inch primary pipe is quite simple; it rests upon the approximation that the flow rate following the break will eventually approach the asymptotic equilibrium flow for five pumps at a pressure differential characteristic of the core only. The double-ended break is assumed to occur at the inlet nozzle. The asymptotic equilibrium flow for five pumps and the core, exclusive of the inlet nozzle, is 20,000 lbm/s. The normal core flow with six pumps operating is 25,738 lbm/s. The nature of the transient between the two equilibrium

flow conditions is not known from this analysis; however, it is assumed to be parabolic.

The reactor was represented by seven radial regions and nine axial regions. The fuel pin model was the restructured model. All coefficients were assumed to be nominal, and the scram mechanisms were deactivated. The results of this event are quite similar to those for the loss of two pumps since the equilibrium flows are nearly equal. The power trace for this accident is presented in Figure 2-3. The equilibrium power, 2538 MWt, was reached in 1.5 seconds, and the maximum fuel and cladding temperatures were 2347 and 1295F. It is evident that this transient is acceptable from a safety standpoint even without a scram. No sensitivity studies were performed.

2.1.3. Loss of Heat Sink

Many malfunctions of the secondary system could affect the primary system. Since the only couplings between the primary and secondary systems are the intermediate heat exchangers, any secondary system malfunction will be felt by the primary system through the IHXs. The most severe condition that can be postulated is the complete and instantaneous loss of access to the heat sink provided by the secondary loop. Consequently, this is the only malfunction studied here.

Since this accident is hypothetical in the sense that it is nonmechanistic but bounds the severity of all mechanistic malfunctions, the investigations assumed the form of a parametric study. The TART computer code was used, and the loss of access to the secondary loop was simulated by setting the heat transfer coefficient in each IHX to zero. The reactor was divided into six thermal-hydraulic regions. The hot pin was not included, and the nonrestructured fuel pin model was used. The reactivity coefficients were set at their rated value except for the axial expansion coefficient. Calculations were made with and without axial expansion reactivity coefficients and with various degrees of coolant channeling in the vessel (defined by a channeling coefficient α equal to the ratio of effective mixing volume to total available volume). Two values of the axial expansion coefficient have been investigated—the nominal value and zero—and all calculations were terminated at 120 seconds real time. Values of α ranging from 0.25 to

1.0 were also considered. All protective functions were bypassed in order to determine the time available to shut down and activate the emergency decay heat removal system.

The effect of coolant channeling in the reactor inlet plenum is of some importance. Values of the channeling coefficient α vary from 1 (perfect mixing) to 0.25 (equivalent to perfect mixing in one-quarter of the available volume); the normal value of the axial expansion coefficient was used in these calculations. In every case, the power is slowly varying over the duration of the calculation. The power histories corresponding to various values of the channeling coefficient are shown in Figure 2-4. In only one case, $\alpha = 0.25$, does coolant boiling occur.

The fraction of molten fuel as a function of time after loss of the secondary loops is also a function of the channeling coefficient. For the case of perfect mixing, the molten fraction was not significant even at 120 seconds.

The nature of the power traces with and without axial expansion feedback indicates that feedback is significant in reducing the power rise in this type of accident.

From the various calculations, it is obvious that for a bulk outlet temperature scram set point of 1139F, scram will be produced before any fuel melting occurs, even for $\alpha = 0.25$. In addition, the cladding temperature rise will be minor if a bulk outlet scram is produced.

From these results, it can be seen that the time available to scram and activate the decay heat removal system is ample. This, in effect, removes secondary loop malfunctions from the category of potential DBAs.

2.1.4. Evaluation and Conclusion

The following abnormalities of the primary loop were investigated:

1. Complete loss of six primary pumps.
2. Complete loss of two primary pumps.
3. Double-ended break in a 28-inch primary pipe.

A number of other abnormalities, such as a hole in the inlet plenum shield tank, might have been studied, but in every case these events obviously give rise to less severe transients than do the events listed above. Hence, the events listed above provide an upper bound for the severity of identifiable flow abnormalities in the primary loop.

Similarly, in the secondary loop a number of malfunctions, such as turbine trip, steam generator failure, or the like, could influence conditions in the primary system; however, all of these events are less severe than the complete, instantaneous loss of access to the heat sink. As a consequence, instantaneous loss of the heat sink was the only event investigated under secondary loop abnormalities. Appropriate sensitivity studies were performed for each of the two broad categories investigated. These studies are discussed in detail in section 3.

The results of these calculations indicate that no fuel melting or cladding failure occurs for any transient so long as the protective system functions normally. As to flow abnormalities in the primary loop, three levels of protection may be bypassed before failure thresholds are approached. If one postulates such partial failure of the protection system and a rapid flow decay, then high cladding temperatures occur in the hot pin. These temperatures are probably sufficient to cause some cladding failure.

On the basis of these studies, it can be concluded that no irreversible damage results from identifiable flow abnormalities unless partial failure of the protection system is postulated. At least three levels of protection must fail (three simultaneous faults) before even limited cladding failure can occur. Thus, it is extremely improbable that flow abnormalities at full power will lead to gross core damage.

2.1.5. References

¹ M. G. Stevenson and B. E. Bingham, "TART — An LMFBR Transient Analysis Project," conference on the Effective Use of Computers in the Nuclear Industry, University of Tennessee, April 1969.

² R. J. Trost, TAMPA — Computer Program for Analyzing Reactor Fuel and Cladding, NPGD-TM-37, Babcock & Wilcox, Lynchburg, Virginia, April 1969.

Figure 2-1. Power Traces — Six-Pump Flow Coastdown
(15,000 lb/ft² Pump Inertia, No Central Void)

Figure 2-2. Cladding Temperature — Six-Pump Flow Coast-down (15,000 lb/ft² Pump Inertia, No Central Void)

Figure 2-3. Power Trace — Double-Ended Pipe Break
(Transient Time = 0.01 s)

Figure 2-4. Effect of Coolant Channeling With Axial Expansion Feedback

2.2. Reactivity Insertions

2.2.1. General

The term reactivity accident or reactivity excursion is somewhat ambiguous since, in general, the power level of the reactor is a function of the reactivity status and the reactivity rate of change; thus, any overpower accident is essentially a reactivity accident. However, it has seemed convenient to distinguish between accidents induced directly by the addition of reactivity and those in which the reactivity change does not occur directly but is induced through some other independent mechanism or arises as a feedback. Flow abnormalities are an example of the latter class of accidents in that the reactivity change is the result of feedback from flow conditions. Here the term reactivity accident will be used to describe the events that directly induce gross additions of reactivity that results in rapid changes in reactor power.

In all reactor designs, provisions must be made for changing the reactivity status rapidly. Generally, reactivity control systems are biased toward the removal of reactivity from the system. However, since such systems do exist, we must examine the consequences of rapid reactivity insertions. In addition to the maloperation of the reactivity control system, certain conditions may arise spontaneously or occur as a result of maloperation of other systems that could potentially result in large reactivity additions. Therefore, it is necessary to examine this group of events that could result in the addition of reactivity and to determine the consequence of each event.

The objective of these analyses is to define the conditions imposed on the core by a gross addition of reactivity to the system. The conditions so defined will be used as initial conditions for further accident analyses; the ultimate objective is to define the design basis accident(s) for the engineered safety features.

The technical approach here is very similar to that used in the study of flow abnormality accident-initiating conditions. That is, the fault catalog and fault trees generated in Phase I were examined to determine the fault conditions that may arise in the system and that lead to gross reactivity insertions.

A review of the Phase I fault trees and malfunction catalog revealed a number of events that could cause accidental reactivity insertions. Among these are some that are patently incredible; others, although credible, are of little significance to this study. A comprehensive list of such malfunctions or events is as follows:

1. Single rod withdrawal at rundown speed.
2. Continuous withdrawal of four regulating rods at shim speed.
3. Gas bubble (s) entrained in the coolant.
4. Loading accidents.
5. Moderator entrainment.
6. Sudden fuel movements.
7. Sudden drop in inlet temperature.

In addition, several arbitrary reactivity insertions and partially protected events were investigated. These calculations, designed to provide data for the design of the protection system, are summarized in this section; details are given in the topical report BAW-1360.

2.2.2. Rod Withdrawal

The point kinetics version of the TART¹ program was used to analyze the transient response of the reactor in all of the studies. The reactor was represented by seven thermal-hydraulic regions: one region represented the hot pin; three regions represented the first enrichment zone, including the peak or maximum-powered pin; one region each represented the second and third enrichment zones, and a single region represented the radial blanket. The power and sodium worth distribution were representative of the end-of-cycle (EOC) reactor, and values were taken from the B&W 1000-MWe Follow-On Study.

An approximate model for sodium voiding was used in the TART program. In the event that any initiating condition leads to sodium temperatures above 1700F, voiding is assumed to begin. The

sodium-sodium vapor interfaces are conservatively assumed to move from the point of voiding inception at a rate of 50 ft/s in both directions.

The EOC fuel pin was modeled using nine axial segments with 10 radial nodes. Again, axial conduction was neglected. It was assumed that the pin had a central void with a radius of 0.003-foot. The conductivity function was taken from the TAMP program.² In this way the effect of fuel densification in the EOC pin was approximated.

The protection system contains 25 rods; seven of these are safety rods and are thus out of the core during operation. The remaining rods are used as shim rods, which are inserted at various positions throughout the cycle. The distribution of rods among the five TART thermal-hydraulic regions was varied for these analyses depending on which point in time during the cycle produced the most unfavorable configuration for the accident in question.

The scram setpoints for the core instrumentation were 2621 MWt for the power monitor (107% power) and 1139F for the bulk outlet temperature monitor. The bulk outlet thermocouples were located just above the upper axial blanket outlet in the outlet plenum. A 5-second time constant was used for this thermocouple.

The scram delay time of 300 ms is defined as the elapsed time between the detection of an out-of-limits condition and the first motion of the rods. The normal rod speed for shim motion is 15 inches per minute; the scram acceleration of 0.7g is backed up by a secondary motor-driven scram at 30 inches per second.

During normal operation the reactivity control system maintains the position and velocity of all control rods as directed by the reactor protection and control systems and by the operator through manual commands. The reactivity control system is designed so that the number of potential faults is extremely limited; however, if they did occur, the system would provide a potential source of reactivity addition. Although there is little probability that these faults would occur, it is important to be able to predict the response of the reactor system as a result of these failures. A number of malfunctions were considered:

1. The withdrawal of a control rod at 30 in./second due to failure of a secondary drive motor.
2. The continuous withdrawal of four "ganged" shim rods at 15 in./minute at power.
3. The withdrawal of a control rod at 30 in./second from 1 watt with the power range nuclear instruments disabled.
4. The withdrawal of a control rod at 30 in./second from full power with the intermediate range rate instruments disabled.
5. The continuous withdrawal of four "ganged" shim rods at 15 in./minute from 1 watt with the intermediate range rate instruments disabled.

The withdrawal of a single rod can occur as a result of a malfunction in the control rod drive system which, because of its rather unique nature, is briefly described here.

The B&W control rod drive unit comprises two synchronous motors that operate through an epicyclic gear train comprising a combination ring gear and flywheel, three planet gears, a planet cage, and a sun gear. For convenience, the upper drive motor is called the primary motor and the lower one is called the secondary motor. Whenever the reactor is critical, both of the motors in a drive operate continuously while linked to the gear train. No clutches or mechanical disconnectors are used.

A noninterruptible vital bus provides power for the secondary drive motors. Power for the primary motors is obtained from two separate nonvital busses, either of which can power all primary motors. The vital and nonvital busses remain synchronized. The rod-hold electromagnetic latch and the primary motor brake for each rod receive power from the same feed as the primary motor for that rod. Setback, rundown, and control operations are accomplished by switching primary motor power to one of three motor windings corresponding to no-motion, plus 15 in./minute, or minus 15 in./minute. Scram is accomplished by interrupting primary motor power so that the secondary motor, acting through the gear train, drives the rod into the

core. Conversely, if the secondary motor failed, the primary motor would drive the rod out of the core. The secondary motor cannot fail through loss of power since it derives its power from a vital bus. However, faults could occur through bearing failure or flywheel seizure.

2.2.2.1. Single Rod Withdrawal

Since the rod of maximum worth (the central rod) is a safety rod, it is in the core only when the core is subcritical; hence, its ejection by secondary motor seizure is not possible. The rod used for this analysis is one of six in the first radial ring of control rods. A single such rod is conservatively assumed to be worth \$0.82. For automatic shim control at the beginning of life, such rods may be ganged and inserted as much as 75% of their length. In this study it was assumed that the secondary motor on one of these rods failed and that the ejection velocity was 30 inches/second. It should be noted that even for ganged rods no more than a single rod may be ejected. Even if a vital bus failed, which is incredible, no more than one rod would be ejected since each of the three ganged rods is on a separate vital bus. The event was analyzed with TART. The protection system was assumed fully operable. Drive failure was assumed to occur at time zero; the power rose sharply, crossing the 107% over-power trip at 0.14 second. It was conservatively assumed that the magnetic clutch on the drive unit did not function as designed and therefore did not disengage. The scram began after a 300 ms delay, and the power peaked at 3310 MWt, 0.481 second after the accident began. The maximum cladding temperature was 1186.4F (7.4F rise), and the maximum fuel temperature rise was 48.6F in the hot channel. The excess integrated energy was 188 MW-s. The power history corresponding to this sequence of events is illustrated in Figure 2-5.

Two parameters were investigated in connection with the withdrawal of a single rod at 30 in./second; (1) the reactivity coefficients and (2) the degree of insertion of the rod at the inception of the transient. The variation of rod worth was not investigated since this variable was adequately investigated in the series of arbitrary reactivity insertions that were studied.

The reactivity coefficients and the initial insertion length were varied independently to assess the relative importance of each. The protection system was assumed to be functional for each of the transients studied. The values of the reactivity coefficients used were (1) 0.5 nominal Doppler with all other coefficients normal and (2) 1.5 times the rated sodium density coefficient with all other coefficients as rated.

In one case it was assumed that the procedural controls on a maximum rod insertion of 75% were violated and that the rod was fully inserted when the secondary drive motor failed. The results of this transient are similar to those for the reference case, since the scram is produced at 2621 MWt in any event. However, since the fully inserted rod is initially positioned on a segment of the rod worth curve where the slope is small, the power overshoot is less severe than that of the reference case. For the fully inserted rod, the maximum power of 3251 MWt occurred 0.568 second after the initiating malfunction. The fuel did not melt, and the cladding temperature rise in the hot channel was only 1.1F. Therefore, it is reasonable to assume that the cladding did not fail.

The effects of varying feedback parameters on the single rod withdrawal transient are shown in the tabulation below, which displays the system temperatures corresponding to the three conditions of the feedback parameters. It is evident that in this case, as in the case of transients arising from flow abnormalities, (see section 2.1) that any "worsening" of the feedback parameters leads to a power rise that is more rapid than normal with a consequent earlier-than normal scram. Thus, for higher positive or lower negative feedback parameters, the energy yield of the excursion is reduced.

<u>Case</u>	<u>Maximum temp rise, F</u>		<u>Excess integrated energy, MW-s</u>
	<u>Fuel centerline</u>	<u>Cladding</u>	
Nominal	48.6	7.4	187.9
1.5 x Na density	41.3	6.0	154.6
0.5 Doppler	40.0	5.0	147.6

2.2.2.2. Withdrawal of Four Rods at Shim Speed

The second event studied is the continuous withdrawal of four ganged rods, which can arise from operator error. It is usually possible to withdraw a maximum of three rods simultaneously; however, if a jumpered condition exists in a particular control switch, one additional rod could be withdrawn at the same rate, 15 in./minute. For this study the four rods were located in the first radial ring of control assemblies. These rods were further assumed to be fully inserted, in violation of procedural limits at the inception of failure.

The transient following the continuous withdrawal of four rods is somewhat different from the single-rod case. The power is slowly varying, although there is an inflection in the power trace due to the increasing slope of the rod worth curve. A scram was produced by the power monitor at 4.90 seconds. The maximum power of 2638 MWt occurred 5.2 seconds after initiation of rod withdrawal. The maximum cladding temperature was 1196F, and the maximum fuel temperature rise was 91.4F. The excess integrated energy for this transient was 398 MW-s. The power trace is shown in Figure 2-6.

The greater integrated energy associated with the slower four-rod withdrawal ramp is to be expected. This phenomenon demonstrates one of the principal axioms of protective system design—that the further one removes the scram setpoint from operating conditions, the more vulnerable one becomes to very slow insertions of reactivity. Although the integrated energy in this accident is relatively high, fuel melting did not occur, and the cladding temperatures were acceptable.

For the continuous withdrawal of four shim rods, the parameter investigated was the variation of reactivity feedback coefficients. The values used were the same as those in the previous case: (1) normal values, (2) 0.5 of the normal Doppler, and (3) 1.5 times the rated sodium density coefficient. Again, the protection system was assumed operable.

The behavior of the power and temperature is predictably similar to the previous case. Once again, the worst coefficients lead to the smallest transient energy generation. The results are summarized as follows:

<u>Case</u>	<u>Maximum temp rise, F</u>		<u>Excess integrated energy, MW-s</u>
	<u>Fuel centerline</u>	<u>Cladding</u>	
Nominal	91	24	398
1.5 × NA density	66	16	259
0.5 Doppler	59	15	238

It is very clear, from the results presented in connection with the rod withdrawal events, that so long as the protection system is fully operable the consequences of rod withdrawal transients are completely acceptable from a safety point of view. The principal question, then, becomes one of the probability of failure of the protection system. Although it is widely believed that protection system failures are extremely improbable, manufacturers and operators of nuclear reactors, as well as reactor regulatory bodies, have become increasingly interested in a class of limited failures which have come to be known as "common mode failures." A common mode failure is one that, through some common cause, disables all of the identical components of a particular type in the reactor protection system.

2.2.2.3. Partial Failure of Protection During Rod Withdrawal

The remainder of the analyses performed in connection with rod withdrawal events were directed toward predicting the consequences of common mode faults in the protection system. The performance of these analyses in no way implies that common mode faults are expected. The study had a dual purpose: (1) such analyses serve as qualitative indicators of the adequacy of protective systems based on the concept that functional diversity in instrumentation can provide sufficient margins of safety in transient conditions; and (2)

analyses of this type tend to place completely unprotected hypothetical accidents in sharper perspective and thus aid in the rational selection of DBAs.

These calculations were performed by assuming that the first trip setpoint reached produces no scram because all instruments of that particular type have been disabled due to some common cause. The transient then develops until it is terminated by some later scram originating from a signal of some different type of instrument.

2.2.2.4. Single Rod Withdrawal From Zero Power

Consider the malfunctions that cause rod withdrawal from 1 watt power. The levels of protection for these accidents are as follows:

1. Intermediate range nuclear rate setback
($10^{-6} \% \leq \text{power} \leq 10 \%$).
2. Power range nuclear level or rate.
3. Power-to-flow ratio.
4. Individual assembly outlet temperature
(level or rate).
5. Bulk outlet temperature.

For the calculation the reactor was assumed critical at a power level of 1 watt with shutdown coolant flow conditions. The intermediate range nuclear rate instrumentation was assumed disabled through systematic failure. Thus, the first effective level of protection was the power range overpower trip. This trip was set at the maximum value of 111% of full power. (The rated value for this trip setting is 107% of full power with the possibility of a maximum error of 4%.)

Consider the ejection of a single rod due to secondary motor malfunction. The rod was ejected at time zero at 30 in./second; after 1.157 seconds it was completely withdrawn from the core and \$0.82 of reactivity had been inserted. In the absence of the Doppler reactivity feedback at low power, the reactor quickly achieved a stable period of 0.6 second. The reactor power crossed

the overpower trip at 12.3 seconds. The fuel temperature was relatively constant over most of the duration of the transient. However, as the integrated power became significant, the temperature in the fuel rose rapidly, reaching a maximum of 1595F in 3.1 seconds. The integrated energy at scram was 1350 MW-s; before shutdown it was 3000 MW-s.

2.2.2.5. Four-Rod Withdrawal From Zero Power

The transient resulting in the continuous withdrawal of four shim rods at 15 in./minute from zero power is quite similar to the ejection transient. The major difference is the magnitude of available reactivity and the addition rate. In the rod ejection accident the maximum available reactivity is \$0.82; however, the four shim rods can eventually insert as much as \$2.5. Hence, there is a possibility for prompt criticality in this accident. The withdrawal rate of 15 in./minute limits the rate of reactivity insertion to acceptable levels.

Once again the intermediate range nuclear rate instruments were assumed disabled. The first level of protection is the power range overpower trip. At time zero the rods start out at 15 in./minute. The power level crosses the overpower trip at 55.6 seconds; the maximum power is 4400 MW, about 200 MW higher than in the previous case. The maximum power is greater because the faster rate of power rise at overpower trip leads to a slightly larger power overshoot. The integrated energy to shutdown is 2500 MW-s. This accident appears to be mild enough until we consider the reactivity history; at the time of scram a total of \$0.91 of reactivity had been inserted. Thus, we see that prompt criticality was narrowly averted. However, these results do show that even systematic failures in the protection system can be tolerated for these accident conditions.

2.2.2.6. Single-Rod Ejection From Full Power

In the case of the single-rod ejection from full power, the results are very different: It is assumed here that the rod

is ejected at 30 in./second. The levels of protection in this case are as follows:

1. Power range nuclear instruments (level or rate).
2. Power-to-flow ratio.
3. Individual assembly outlet temperature or rate.
4. Bulk outlet temperature.

In this case, if the power range instruments are assumed disabled, the first two trips are effectively bypassed, since the power to flow trip is dependent on the power range nuclear instruments. Further, the outlet thermocouples tend to be relatively slow, having a 5-second time constant and a scan dead time. The results for this case indicate that at the time the scram was produced, due to an excessive outlet coolant temperature increase rate, the power level had reached 27,000 MWt. The scram signal was produced 1.42 second after inception of the fault. By the time the control rods entered the core 325 ms later, the power level had reached 38,500 MWt and coolant boiling had begun throughout the first enrichment zone. Obviously, this accident is unacceptable. The most obvious way to reduce the consequences of the accident is to limit the rate at which the control assembly can be withdrawn. This can be done in a number of ways, but perhaps the most promising is to limit the size of the control rod drive primary motor. The motor was not sized during the 1000-MWe Follow-On Study. It appears that a 1-hp motor will be capable of inserting a 700-lb control assembly at a rate of 30 in./second but will only be able to withdraw the same rod at a rate of 9 in./second. With an ejection rate of 9 in./second, the accident once again becomes less severe; however, some limited number of fuel failures might occur before the reactor scram lowers the power level. For this case, the TART results show that only the hot channel fuel would have failed; thus, it might be expected that a relatively few pins throughout the central zone would have reached failure.

2.2.3. Gas Bubble Entrainment

Because of the rather large positive sodium density coefficient of reactivity, any event resulting in the removal of sodium from the core is potentially serious. Apart from void formation due to sodium, the event that leads to the most severe conditions is the entrainment of gas bubbles in the sodium coolant.

The argon cover gas is a potential source of gas that may be entrained in the primary coolant. Argon is somewhat soluble in sodium at the temperatures characteristic of the primary system; in addition, a large interfacial area exists between the primary sodium and the cover gas. It is therefore conceivable that a certain amount of argon can be dissolved in the primary system under certain circumstances.

Another potential source of gas in the primary system is the gaseous fission products released from the fuel vents. Certain of these fission products have a chemistry similar to that of argon and are soluble in hot sodium.

The concern over dissolution of gases in the primary system arises because the temperature differences in the system may cause some of the gas to precipitate from the coolant and be swept into the core region. Such an event would lead to momentary voiding of regions of the core and initiate a reactivity transient. The severity of the transient depends, of course, on the location of the voided channel or channels since the magnitude of sodium density feedback reactivity is spatially dependent.

There are two aspects to the gas-bubble problem: reactivity effects and thermal-hydraulic effects. The reactivity effects arise solely as a result of removing sodium from regions of the core in which entrained gas is flowing. An associated phenomenon occurs during bubble passage, however, and its effect is principally thermal-hydraulic. In effect, as the bubble passes a particular axial location, it removes cooling at that location. Depending on whether or not a sodium film is left on the pins, the cladding materials may experience a precipitous rise in temperature. After the bubble passes, the returning sodium is then exposed to elevated temperatures, which could

initiate coolant boiling. It was shown in these studies that the thermal-hydraulic aspects of the gas-bubble accident are of little significance compared to the reactivity aspects.

To cause any difficulty, a relatively large volume of gas must flow through some portion of the envelope of positive sodium worth in the core. The gas precipitated in the IHX is likely to be in the form of a very few small, discrete bubbles. In view of this, it appears that these small bubbles would have to be collected and coalesced in order to produce any significant effect. In the absence of a collection location, the probability of entrained gas leading to an accident is very low.

The objective of this analysis was to determine how much gas must flow in the central seven assemblies to produce incipient fuel failure in the unprotected reactor. It was assumed that a large bubble of undetermined size enters a primary pump and is broken up by the coolant flow. After breakup it was assumed that the smaller bubbles remain very closely packed in a random array. The void fraction for this foam is about 61% based on values in Perry's Chemical Engineering Handbook.³ This representation was used in all the analyses. It was further assumed that the velocity of bubble passage is the same as the steady-state coolant velocity in the core, 20 fps.

The passage of a 61% void foam through the seven central assemblies of the reactor introduces a net reactivity of \$0.58.

The resultant power trace is shown in Figure 2-7. The power rises quite rapidly as the gas flows into the core, but since the flow was postulated to be continuous, the power quickly reaches a near-equilibrium value of 5480 MWt in 0.2 second. Fuel failure will probably occur between 0.8 and 1.0 second after the beginning of the transient because of the molten fuel expansion mechanism. Fuel melting began 0.4 second into the transient; at this time approximately 8 ft³ of gas has flowed into the core.

Although 8 ft³ of gas could be entrained in the system over an extended period of time, it is extremely unlikely that that amount would collect at some point and be suddenly released into the primary system. Design features may be included in the primary design to disentrain the gas and/or to prevent its collection. In particular, the IHXs could be equipped with gas bleed lines to disentrain gases. In

addition, the primary pump outlets could be equipped with suitable electromagnetic flowmeters to detect the passage of significant volumes of gas into the inlet plenum.

However, the most important aspect of this accident can be seen by inspecting Figure 2-7. This trace assumes no scram; it may be seen that the reactor power passes the overpower trip at 0.03 second. If the protective system operates as designed, the transient may be terminated before fuel melting begins, even with a 300-ms scram delay time. Since there is no reason to believe that the protection system will fail to produce a scram, this accident, viewed realistically, will produce neither fuel melting nor cladding failure.

2.2.4. Other Initiating Events

A number of other initiating events were identified from the malfunction catalog or fault trees. These devices, however, were constructed without regard to probability or credibility. As a consequence, many of the events thus identified are quite improbable and may be eliminated on grounds of credibility. Several events fall into this category: (1) loading accidents, (2) moderator entrainment, (3) sudden fuel movements due to thermal ratcheting, and (4) sudden drops in the coolant inlet temperature. Credibility considerations are emphasized in discussing these events.

2.2.4.1. Loading Accidents

The classic loading accident is postulated based on the initial assumption that the core has been misloaded to the extent that the reactor is just critical at zero power. At this point the assembly of maximum worth is dropped into the location of maximum worth. Criticality during loading is assumed to arise as a result of (1) rods being inadvertently withdrawn and this condition remaining undetected, and/or (2) gross misloading of fuel.

In B&W's reference design it is physically impossible for the rods to be out of the core during refueling. The rods must be in the core with the drives disconnected before refueling can take place. Because of the weight of an individual rod, the rod cannot

be lifted from the core by hydraulic pressure even under full-flow conditions. With all rods in the reactor the system is subcritical by several percent.

A study was undertaken to determine whether the core could be misloaded so as to be just critical with all rods in the reactor. A criticality search for the reference core with all rods in was performed with the one-dimensional diffusion theory code FARED.⁴ The results indicate that a uniform enrichment greater than 14% fissile is required to achieve criticality with rods in. Since this enrichment is significantly greater than the maximum enrichment in the reference design, criticality with rods in is deemed impossible. Since criticality cannot even be approached during refueling in B&W's reference design, the loading accident, in the classic sense, is clearly impossible and was not considered further.

2.2.4.2. Moderator Entrainment

If a substance having substantially different moderating properties from the sodium coolant were to be introduced into the reactor primary system, a reactivity insertion could take place. This effect would be pronounced if the foreign substance were either hydrogen or carbon.

The introduction of hydrogenous material into the reactor results in two competing reactivity effects: a gain in reactivity due to decreased leakage in the lower energy groups, and a loss of reactivity due to the decrease in k_{∞} at low energy. The positive component predominates in general.

For the Fermi reactor⁵ it has been estimated that a substance with the hydrogen density of water flowing at 20 ft/s through the core would result in a reactivity ramp of \$800/s. A concentration of 0.01 vol % of hydrogenous material, such as oil, will cause a reactivity increase of several cents.

Further, the inleakage of oil to sodium systems is not entirely without precedent. In May 1959 a thermocouple well failure in the SRE (Sodium Reactor Experiment) led to the admittance of 2 to 10 gallons of Letralin to the primary sodium.⁶

Thus, it is seen that the exclusion of moderating material from the primary system should concern the designer of fast reactors. In B&W's reference design only two sources of moderator are associated with the primary system: the oil-filled eddy current couplings between the primary motors and their shafts, and the carbon-filled cans of the radial neutron shields. The immediate consequence of a multiple seal failure that releases oil is the leakage of oil onto the charge face of the reactor. From here there are two additional seals between the oil and the primary coolant. In any event, such leakage is most likely to be confined to the charge face. Thus, this mechanism does not seem very important as a potential source of introducing moderator to the primary coolant.

The solid graphite logs of the radial neutron shields are canned in stainless steel. If failure of several shield cans (an extremely improbable event) is postulated, it is unlikely that significant quantities of graphite would be released. In all likelihood the graphite would rapidly log with sodium, as it did at Hallam, and release a negligible amount of moderator to the coolant stream. Because of these considerations the rapid release of sufficient quantities of moderator to cause reactivity effects is deemed extremely remote. Slow leaks of these substances into the primary system have little effect on safety, and they can be detected rather easily. Hydrocarbon vapor is readily sensed in the argon cover gas, and carbon is easily cold-trapped.

2.2.4.3. Sudden Fuel Movements

Only the thermal ratcheting of fuel is of concern here. Other types of fuel movement, such as fuel slumping or melting, are consequences of initiating events and are not initiating events themselves. As such, these events properly belong in the category of accident analysis.

In pellet fuel pins it is known that thermal cycling will cause unrestrained pellets to move axially away from the midplane of the core. Substantial amounts of reactivity may be tied up in such motion, and if at some later time the axial voids in the fuel column collapse under the action of some shock, then this reactivity

may be reintroduced rather rapidly. It is not known now whether vipac fuel can be made to ratchet in a manner similar to pellet fuel.

In vipac fuel, where the fuel column is a continuous homogeneous medium, it is unlikely that enough radial cohesion is present in the pin to cause the mass to slip coherently through the tube. In particular, the forces generated axially are likely to be considerably reduced in a column of vipac fuel.

At this time it is felt that thermal ratcheting in vipac fuel is improbable at best. Even if thermal ratcheting were possible, the loss of reactivity would probably become apparent during operation owing to unexpected motion of the shim rods. In this event, safety would not be compromised since a reactivity inventory would betray the advent of a dangerous condition. Caution should be used, however, in interpreting reactivity accounting results since some small, unforeseen condition might introduce enough positive reactivity to mask the negative effects of thermal ratcheting.

2.2.4.4. Sudden Drop in Inlet Temperature

In principle, a sudden drop in the coolant inlet temperature could lead to a reactivity-induced transient. This effect arises because the fuel is momentarily overcooled, leading to a fuel temperature drop and a consequent positive Doppler feedback.

In B&W's reference design this accident has a low probability for several reasons:

1. The primary pump suction is taken from the large pool of sodium. The mass of the sodium in this pool is about 1.2×10^6 pounds, so that any temperature transients affecting the bulk temperature of this mass are necessarily slow because of the very large heat capacity.

2. There are no separate closed loops subject to isolation and sudden reactivation.

3. In an unbalanced condition, the intermediate system cannot remove enough heat to cause a rapid lowering of pool temperatures, even with imperfect mixing.

Since no mechanism leading to a sudden drop in inlet temperature can be defined, we do not believe that analysis of the event is warranted.

2.2.4.5. Arbitrary Reactivity Insertions

In a series of calculations the reactor was subjected to (1) unbounded ramps of varying rates and (2) total insertion times of varying rates. The objective was to determine the maximum ramp rate or combination of rate and insertion time that could be controlled to ensure that the power returned to the steady-state value before either coolant boiling or cladding failure occurred. The assumptions for this study are as follows:

1. The control system is operable.
2. A scram is included at the reference setpoints.
3. The cladding failure states are as given in BAW-1350, Appendix A.
4. There are seven thermal-hydraulic regions in the core.

The calculations used the TART code to study core behavior following ramped reactivity insertions. In all cases the core was protected and would scram at several trip points: 107% over-power, 1139F bulk outlet temperature, 1700F sodium void temperature, and 1.8 power-to-flow ratio. The core model was set up with seven regions of composition, one corresponding to the hot fuel pin and associated materials. The input parameters were chosen so that voiding, fuel melting, and other dangerous behavior would always appear in a pin.

Two major types of reactivity insertions were studied. In the first, calculations were made beginning with a reactivity insertion rate of \$0.50/s and increasing by \$0.50/s intervals until the cladding failed. Cladding rupture conditions were taken from Figure A-10 of Appendix A, BAW-1350. For an assumed fuel melting point of 4500F, the ramp rate that caused cladding failure was about \$5/s. The melting point has since been re-evaluated and determined to be near 6000F. The 4500F temperature is conservative, however, since it leads to earlier failure at lower reactivity rates. Failure occurred 0.486 second after the transient began.

An additional set of reactivity insertion calculations was made in which specific reactivities were inserted in short time intervals. The calculations began with \$0.25 intervals for each set of three insertion times. Neither trip points nor cladding rupture conditions were changed for these calculations. Cladding failure occurred when \$1.25 was inserted in 0.1 second. The time from the beginning of the reactivity insertion to the cladding rupture was 0.2 second.

2.2.5. Evaluation and Conclusion

In a well-designed system the possibility that serious accidents will arise from reactivity perturbations is extremely remote. The analyses performed in this activity showed that the conditions that arise from definable malfunctions will not lead to irreversible damage to the fuel, the core, or components unless at least partial failure of the protection system is postulated. The rod ejection and rod withdrawal analyses are particularly significant, since these malfunctions lead to a well-defined set of initiating conditions. Such malfunctions as gas bubble entrainment, loading incidents, moderator entrainment, fuel movement, and inlet temperature drops are less amenable to analysis since it is difficult to imagine circumstances under which they might arise and to specify their effect on the system. However, analyses have shown that the reference design can accommodate an unbounded reactivity ramp of up to \$5/second, or an insertion of \$1.25 in 0.1 second, without extensive fuel pin failure. No credible mechanism have been found through which any of these five malfunctions can lead to such a severe reactivity insertion. Despite this, such possibilities must be carefully investigated to ensure that they cannot occur with such severity. In particular, potential sources of noncondensable gas or moderating material should be considered as a factor in any design changes that might be undertaken. Further consideration of the mechanism of fuel ratcheting, particularly with particulate fuel, is also warranted, if for no other reason than that all potential reactivity changes during a core lifetime should be identified and estimated.

If the failure of the first level of protection is hypothesized, then reactivity incidents become more serious, although it is not likely that the consequences are unacceptably severe. However, as

discussed in section 4.2, consideration of such failures is valuable since a greater emphasis on diverse and redundant protection may be required.

2.2.6. References

- ¹ M. G. Stevenson and B. E. Bingham, "TART — An LMFBR Transient Analysis Project," Conference on the Effective Use of Computers in the Nuclear Industry, University of Tennessee, April 21-23, 1969.
- ² R. J. Trost, TAMPA — Computer Program for Analyzing Reactor Fuel and Cladding, NPGD-TM-37, Babcock & Wilcox, Lynchburg, Virginia, April 1969.
- ³ John H. Perry, Chemical Engineers Handbook, McGraw-Hill, New York (1950).
- ⁴ D. H. Roy, et al., FARED — One-Dimensional Fast Reactor Physics Design and Analysis Code, BAW-3867-9, Babcock & Wilcox, Lynchburg, Virginia, October 1969.
- ⁵ H. Soodak, "The Reactivity Effect of Hydrogen Addition in Fast Reactors," Proceedings of International Atomic Energy Agency Symposium on Physics of Fast and Intermediate Reactors, Vienna, Austria, August 1961.
- ⁶ Thompson and Beckerly, The Technology of Fast Reactor Safety, I, MIT Press (1969), p 639.

Figure 2-5. Initiating Conditions — One Rod Withdrawn at Rundown Speed

Figure 2-6. Initiating Conditions — Four Rods Withdrawn,
Each Worth \$0.76

Figure 2-7. Power Trace — Gas-Bubble Accident

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2.3. Local Flow Abnormalities

Two of the more important malfunctions that may occur within a reactor core are the failure of a single fuel pin and a flow blockage incident that affects an entire fuel assembly. These events must be evaluated with respect to their probability of leading to accident situations. A malfunction such as a fuel assembly inlet blockage must be shown to be either incredible, inconsequential, or adequately protected against. It is of interest to assess the types and extents of the pin failure and inlet blockage malfunctions that result in the initiation of accident conditions, if any.

2.3.1. Intra-Assembly Flow Abnormalities

The concern about the effects of single fuel pin failures is a direct result of the requirement for LMFBR operation with some small number of failed fuel pins. Unrelated pin failures (isolated, individual failures) would not ordinarily be considered as safety hazards. However, if a pin failure could induce additional failures, an accident condition might eventually ensue. The determination of the threats that pin failure propagation may pose for safe reactor operation is a key problem in the LMFBR safety evaluation. This determination of threat or risk involves the examination of the likelihood of failure propagation, the detectability of related failures, and the possible consequences of any resultant accident.

A number of modes of pin failure transport may be postulated, among which inadequate cooling is of prime importance. The inadequate cooling might result from pin enlargements or fuel pin debris within the coolant channels. Also, the transient release of fission product gases may result in a temporary local condition of inadequate cooling. These blockage and voiding effects of pin failure are considered to be the most likely pin failure consequences; therefore, the investigation has been concentrated in this area with the general objectives of quantitatively determining the effects of flow disturbances and assessing the detectability of the event and/or the likelihood of additional pin failures. The investigation of flow blockage effects also afforded the opportunity to assess the consequences of blockages resulting from

ex-core material. This is important because of the possibility that such a blockage may initiate several pin failures.

The following approach was used to evaluate the effects of partial flow blockages within an assembly: A model was developed (the BLOCKO computer program¹) to calculate the flow and temperature distributions within a number of adjacent, blocked channels. Several blockage types were examined: among these were geometries that might correspond to fuel debris in a channel, cladding debris (flat plate), and failed and expanded fuel pins. In addition to the temperatures around a blockage, the temperature distributions at the pin bundle outlet were determined, as well as the overall average reductions in assembly flow. These calculational results were useful for appraising the detectability of types and extents of flow blockage.

Although most credible blockages consisting of regular-shaped objects might be shown to preclude sodium boiling within the blockage, the effects of sodium boiling were considered. The flashing of small amounts of superheated sodium liquid was modeled. The vapor bubble growth in a pin bundle and the subsequent double liquid slug ejection were calculated.

The approach for evaluating the effects of transient fission gas release has been to determine the resulting flow and thermal transients in the affected channel. The effects of the fuel meat in retarding or "slowing up" the gas during its release have been neglected. Other very conservative assumptions have also been made in the model used to determine these flow and thermal effects.

Section 2.3.2 deals with the problem of flow blockages within a fuel pin bundle. Some aspects of molten fuel and sodium interaction, which might be postulated as a consequence of a local channel blockage, are treated in the Accident Analysis chapter under section 3.2.2. Section 2.3.3 discusses the effects of transient fission gas release from a failed fuel pin.

2.3.2. Intra-Assembly Blockages

The B&W reference design includes nearly 100,000 fuel pins, as do other typical LMFBR core designs. Therefore, since each fuel pin has two distinct coolant channels, the probability of a local

coolant channel blockage in an operating plant is significant. The effects of such a blockage may be of some consequence since the specific power values are about 200 W/g. Flow blockages may cause rapid temperature increases in the case of a sudden blockage with possible sodium boiling and/or fuel melting. Blockages that are formed more gradually might result in sustained high sodium coolant temperatures as well as sodium boiling, depending on the severity of the blockage. The problem of flow blockages is of considerable interest for several reasons.

The present state of the art of core protective instruments is such that nonviolent events occurring on the single-coolant-channel level are essentially undetectable. A blockage might conceivably form and lead to a serious fault on a larger scale before protective action can be initiated. Faults on a multiple-channel scale may also be included in the category of undetectable events, depending on the type of event.

Flow blockage is of concern in the problem of possible propagation of fuel pin failure. In the past, the blockage fault has been postulated as a possible mechanism for failure propagation. The question of detection plays a role in failure propagation since, as mentioned above, faults may progress to a multiple-channel scale without being detected.

It is convenient to group the credible blockage extents into two categories—single or few-channel blockages and many, adjacent-channel blockages. Single or few-channel blockages tend to promote elevated temperatures within the blockage zone. Many-channel blockages tend to promote elevated temperatures downstream from a blockage zone (assuming that a many-channel blockage tends toward a generally less severe area and hydraulic diameter reduction than that for a single-channel blockage). This is a direct result of the differences in crossflow and associated pressure distributions for the two cases.

2.3.2.1. Single- or Few-Channel Blockages

Both generalized and specific types of few-channel blockages have been examined using the BLOCKO flow blockage model. Although the computer program determines both the flow and

temperature distributions, the results are best presented in terms of the normalized flow per unit length through the blockage. The advantage of this method is the account of blockage length since the effect of a complete blockage of a channel is strongly dependent on the length of the blockage. (This length determines the energy addition to the sodium if we neglect transverse heat conduction out of the blockage.) The temperature rise (ΔT) of the sodium flowing through a blockage is inversely proportional to the blockage flow per unit length ($\dot{m}/L$); more specifically,

$$\Delta T \propto \frac{q'}{\dot{m}/L}$$

where q' is the local linear power rating. This relationship for the blockage may be considered an equality when normalized by using a similar relationship for the ordinary temperature rise through the core. The blockage temperature rise, linear power, and flow per unit length may then be considered to be normalized values. The BLOCKO results are shown in Figure 2-9. The blockage area reductions (A_o is the undisturbed unit channel flow area) are shown; the hydraulic diameter reductions can be considered using an effective flow area:

$$A_{\text{eff}} = A[D_e/(D_e)_o]^{5/7}$$

where $D_e/(D_e)_o$ is the ratio of the blocked channel's equivalent hydraulic diameter to that of the undisturbed channel. This equation is based on turbulent flow occurring within the blockage.

Minimum allowable limits for the normalized flow per unit length are shown in Figure 2-9. These limits correspond to 1400F coolant temperatures for a blockage centered about the core midplane, and limits corresponding to sodium boiling (1700F) for various axial locations of a blockage are also shown. The required blockage lengths may then be obtained from this figure for surpassing sodium bulk temperature limits. The effective flow areas that may be used in Figure 2-9 for different blockage types have been determined and are given in Figure 2-8.

The BLOCKO calculational model does not account for transverse heat conduction from the blockage to adjacent, unblocked channels. This transverse heat conduction may be important for a single-channel blockage. The British have published results of an analysis of coolant channel blockages and have shown that the sodium temperature in a completely blocked channel cannot exceed boiling even if the sodium is entirely stagnant.¹ This pertains to the case where sodium occupies the entire channel and the local specific power does not exceed 200 W/g (peak specific power of the B&W reference core is 170 W/g). In order to carry over the British results to the B&W reference core, we have used a very simplified model to approximate the maximum coolant temperatures for stagnant sodium in multiple-channel blockages.

The simplified model uses the integrated form of the heat flow equation in cylindrical, one-dimensional geometry. An assumption has been made to neglect the heat transfer through the fuel and consider that the sodium in idealized pie-shaped segments conducts the heat energy to the boundary. The equation for the radial temperature rise from the boundary to the blockage center becomes

$$\Delta T = \frac{V_{\text{fuel}}}{V_{\text{sodium}}} \frac{\ddot{q}_{\text{fuel}} r^2}{4K}$$

where $V_{\text{fuel}}/V_{\text{sodium}}$ = fuel-to-sodium volume ratio,

$\ddot{q}_{\text{fuel}}$ = volumetric heat generation rate,

K = sodium thermal conductivity,

r = approximate blockage radius, which depends on the number of channels blocked.

For the case where sodium occupies the entire channel at a local heat rating of 13.1 kW/ft, the equation above gives the following results for various numbers of blocked channels:

<u>Actual channels blocked</u>	<u>Blockage radius, in.</u>	<u>Radial temperature rise, F</u>
4	0.265	620
10	0.427	1610

The detailed British results give a 530F temperature rise through a blockage of four actual channels. For the reference core then, there is little probability that sodium boiling will occur downstream from a blockage of less than four actual channels where stagnant sodium occupies the blocked channels. Of course, sodium boiling within a blockage is not precluded, since the amount of sodium available for radial heat conduction might be diminished by the presence of either inert or fissile material in the channels.

2.3.2.2. Many Adjacent Channels Blocked

The case of many adjacent blocked channels would reasonably arise only under the following circumstances:

1. Extensively failed fuel.
2. Bowed pins (a postulated consequence of a molten fuel/sodium interaction).
3. Crud deposition in the core.

A number of cases involving very severe blockages 1.6 inches in axial length over as many as 19 adjacent channels were examined. The downstream temperatures increased generally with increasing numbers of blocked channels. However, the results show that even for 19 blocked channels, the downstream temperatures would not be expected to exceed 1400F. The assembly flow reduction for such a case (19 blocked channels) has been calculated as less than 0.4%.

A blockage type was examined corresponding to the geometry obtained when the fuel pin is expanded to the original pitch dimension. The number of blocked channels was fixed at 46, and the axial length varied up to 0.15 of the channel length (~0.30 of the active core length for the reference core). A maximum temperature of 1600F was calculated in the area downstream from the blockage for the

maximum blockage length examined. The flow reduction in the reference fuel assembly for this worst case was less than 3%.

Figure 2-10 shows the effects of a radially uniform blockage in every channel of a single assembly. Comparison of Figures 2-10 and 2-9 shows that the flow through a radially uniform blockage is less sensitive to area reductions than for the case of blockage of a few channels (note the difference in abscissa scales). This is because the bypass flow around a blockage of a few channels tends to "starve" the blocked volume itself. For a uniformly blocked pin assembly, however, there are no flow maldistribution effects (ideally) and the flow entering the assembly must pass through the blockage uniformly. The plot in Figure 2-10 pertains to the case of turbulent flow in the blocked and unblocked portions of the channels. This might pertain, in particular, to the case of crud deposition in an assembly.

2.3.2.3. Possible Effects of Sodium Boiling

A model was developed to calculate the effects of local pockets of sodium "flashing" to vapor within a pin bundle. The effects of vapor blanketing due to the presence of an extensive vapor bubble were determined for various amounts of superheated sodium liquid. The cases examined included superheated sodium liquid volumes of up to 10^{-3} ft³ at 2000F and one case of a volume equal to 3×10^{-4} ft³ and a temperature of 3000F, which might be indicative of the results of a molten fuel and sodium interaction. In the worst case examined, the vapor bubble lifetime did not exceed 80 ms. Although cladding temperatures in the vicinity of the bubble source might be expected to be quite high, the calculated fuel temperature increases were negligible. (The energy source for the cladding temperature excursion was taken to be the superheated liquid sodium.)

2.3.3. Fission Gas Release

Fission gas release is of some concern because it may be postulated that such a gas release mode might lead to additional pin failures. The postulated mechanisms of pin failure are (1) extensive gas blanketing accompanying the large, rapid gas releases; (2) localized

gas blanketing of the cladding opposite a persistent gas jet; and (3) localized flow reduction attendant to the simultaneous flow of sodium and fission product gases for low gas release rates. Recent experimental investigations at ANL² have indicated that both extensive and localized gas blanketing as described above may be of little concern. However, this leaves the possibility of adverse effects from very low gas release rates.

The gas release phenomenon has been examined from the standpoint of the effects of fuel pin gas inventories, cladding breach sizes, cladding breach axial locations, and the resulting flow transient on fluid, cladding, and fuel temperatures.

2.3.3.1. Internal Pin Pressure for a Defective Vent

Calculations were performed to determine the effects of fission gas released from fuel pins that fail in the presence of small quantities of this gas. Although the reference core design employs vented fuel pins, these pins will probably operate with some small positive gas pressure differential, which will be necessary to prevent sodium ingress through the porous plug during shutdown. To obtain an internal pressure of 25 psia during shutdown conditions, a fuel pin pressure of about 50 psia will be required. The pressure of 25 psia is the hydrostatic sodium pressure at the vent elevation. It has been postulated that a porous plug vent may become blocked by the deposition of volatile fission products on the inner face of the plug or in some other mechanism. A typical internal gas pressure may be as high as 135 psia for a blocked vent in the case where the vent flow area is reduced to 10% of normal.

2.3.3.2. Thermal Effects of Gas Releases

The most desirable method of determining the thermal consequences of fission gas would include an account of (1) the fission gas escape rate, (2) the transient effects of this gas flow on the sodium flow rate, and (3) the transient effects of sodium flow reductions on the coolant, cladding, and fuel temperatures in the channel. Since no model was available with which to examine all these effects, a

simplified evaluation was made. In this evaluation, the following assumptions were made:

1. The cladding defect occurs at the channel entrance, and the sodium flow rate instantaneously falls to the steady-state level corresponding to some constant fission gas flow rate from the defect.

2. After the cladding defect forms, the void fraction in the affected channel instantaneously assumes a value corresponding to that given by the steady flow of sodium and some constant fission gas flow rate from the defect (using the Lockhard-Martinelli correlation³).

3. No crossflow of fission gas to other adjacent coolant channels occurs; this gives the maximum magnitude and duration of coolant flow reduction in the channel.

4. No transverse heat losses from the channel occur due to either turbulent fluid mixing or convection by crossflow.

Based on these assumptions, a number of computer calculations were made, using the TART program,⁴ to determine the time required to raise the sodium coolant temperatures to 1700F for various levels of sodium flow and void fraction. The effect of increasing void fraction is, of course, to remove sodium from the channel and thereby decrease the capability for energy storage in the system considered (one channel and the associated pins). The result, shown in Figure 2-11, is represented by the "unsafe region" boundary. A plot on the figure shows the length of time that fission gas may flow for average gas flow rates corresponding to the fractional sodium flows shown on the abscissa.

2.3.4. Full Assembly Blockage

The loss of adequate cooling to an entire assembly is a potentially severe fault. Among the more serious consequences if protective action is not taken are the possibilities of coolant boiling, fuel melting and molten fuel-sodium interaction. The events of cooling voiding and reentry on molten fuel cannot be well defined with the present methods of analysis. However, if a violent fuel-coolant interaction were to occur, the accident could potentially proceed along a number of fault paths involving propagation of damage to other assemblies.

The primary objective of the work reported here was to determine the characteristics of the coolant temperature transients following various degrees of flow reduction. This information was used to provide guidance for setting functional requirements for the protection system.

A single channel TART model was used in the analysis with the single channel representing an average pin in a peak assembly. Eleven axial segments were used and reactivity feedbacks were set to zero. Various degrees of blockage were simulated by ramping the flow to its final value at a rate of 600% per second.

Figure 2-12 (a) shows the rise in coolant outlet temperature as a function of time after flow blockages allowing 35% and 40% of the normal flow rate. Sodium boiling does not begin within 10 seconds following the flow reduction. When the coolant flow is reduced to 30%, however, boiling is initiated after about 3.2 seconds if no protective action is taken. For this case, the assembly outlet thermocouple would detect the coolant temperature rise and effect a scram at about 2.0 seconds. The outlet temperature for the protected and unprotected case is shown in Figure 2-12 (b).

Fuel temperatures rise slowly during the less severe flow reductions. For the case where flow is reduced to 35%, there is only a 400F rise in the peak fuel temperature, and no melting within 10 seconds. Further, extensive cladding failures are unlikely in the cases without voiding.

For a more severe blockage in which the flow was reduced to 2% in 0.1 second, boiling began at about 1.1 seconds. Even with the conservative assumption of no heat transfer from the pin after voiding, no fuel melting would occur if a scram were initiated at the time boiling began, although cladding failures would be expected.

2.3.5. Evaluation and Conclusions

2.3.5.1. Intra-Assembly Blockages

A single-channel blockage is generally not detectable by outlet temperature measurement because of crossflow and turbulent mixing downstream from the blockage. These effects may mask the presence of very high sodium temperatures within and

immediately downstream from a blockage. The boiling of stagnant sodium in a single blocked channel, however, is not likely because of radial heat conduction; it is possible, however, that some fuel melting could occur within adjacent pins for a severe blockage consisting of fissile material in a single channel.

Single-channel blockages of any type are not expected to lead to large amounts of superheated sodium which might flash to vapor. Even so, a model of the effects of localized pockets of stagnant sodium flashing to vapor has shown that the resultant fuel pin vapor blanketing is of such short duration as to be of little consequence as far as rapid propagation of fuel pin failures is concerned.

For a multichannel blockage of two to four adjacent channels, no boiling of stagnant sodium is likely. Also, no large volume of high-temperature sodium would be present downstream from such a blockage because of the high values of crossflow in evidence. Again, as in the single-channel case, the magnitude of the downstream crossflows lowers the blocked channel's sodium temperatures at the bundle outlet, making detection by temperature measurement unpractical.

A multichannel blockage of 19 or more channels could conceivably lead to appreciable amounts of high-temperature sodium in the channels downstream from a blockage. For the cases examined, the maximum temperature occurred within the upper axial blanket. The average flow reductions within the assembly for such a severe blockage would probably not be detectable by practical flow measurement means. A scheme using the measurement of temperature fluctuations at the assembly's outlet appears to be a possibility for detecting a blockage of this extent.

The possibility of radial propagation of fuel pin failures initiated by a single coolant channel blockage appears very remote for most cases. The most severe case of single-channel blockage, that of dense fissile material, appears to have some small but significant probability for involving additional channels and fuel pins; however, the probability of actually incurring such a blockage in the first place should be vanishingly small. From the blockage studies, it appears that for multiple, adjacent channel blockages, the probability for radial propagation of pin failures increases with the number of

blocked channels. The probability of failure propagation initiating from the first affected channel, however, should be small.

2.3.5.2. Fission Gas Release

In the event of transient gas release from a failed fuel pin, the accompanying coolant flow reduction in the affected channel is not severe enough to result in sodium boiling even in the event of a partially plugged vent. The cladding and fuel thermal transients in the affected channels may result in short-term cladding temperatures in the neighborhood of 1600F but negligible increases in center-line fuel temperatures. There is a low probability of additional fuel pin failures for the pins surrounding the affected channel. However, even if failures did occur, no further immediate propagation of fuel pin failures is indicated because of the absence of molten fuel in these pins and their low fission gas inventories.

2.3.6. References

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- ² B. M. Hoglund, Argonne National Laboratory, Personal Communication, January 1970.
- ³ R. W. Lockhart and R. C. Martinelli, "Proposed Correlation of Data for Isothermal Two-Phase, Two-Component Flow in Pipes," Chem. Engr. Progress, 45, No. 1, January 1949, pp 39-48.
- ⁴ J. H. Scott, et al., 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Methods, BAW-1342, Babcock & Wilcox, Lynchburg, Va., December 1970.

Figure 2-8. Local Blockage Configurations

Blockage Type		A/A_0	$De/(De)_0$	$(A/A_0)_{eff}$
Cylindrical Foreign Object		0.48	0.28	1/5
Single Failed and Expanded Fuel Pin		0.48	0.43	1/4
Multiple Failed and Expanded Pins		0.27	0.28	1/9
Small Cylindrical Passage		0.10	0.30	1/25
Inert Spherical Particles		0.095	<0.034	1/120

Figure 2-9. Normalized Flow per Unit Length Through Blockages With Various Characteristics

Figure 2-10. Normalized Mass Flow Through Radially Uniform Blockage (Occurs in Every Channel of a Single Assembly)

Figure 2-11. Length of Time Fission Gas May Flow at Constant Rate for Various Sodium Flow Reductions (Hot Pin)

Figure 2-12. Outlet Coolant Temperature Vs Time — Peak Power Assembly

a. Flow Blockage With
No Scram

b. Flow Reduced From
100 to 30% in 0.1 s

3. ACCIDENT ANALYSIS AND CANDIDATE DBA SELECTION

Once the initiating conditions for an accident have been defined, the analyses may be extended to follow the course of the accident. This extension must be related to the criteria for core and system damage and to the actions of the protective system in preventing or limiting damage. If full protective action is effected before severe damage is predicted, then the analysis need not be carried past the initiating condition phase. However, if it is hypothesized that protective action is not effected, then the consequences of the accident-initiating condition must be evaluated.

The analysis of the events following damage to the core is extremely difficult and full of uncertainties. Following either full-core or local disruptions the geometry and condition of core material cannot be well-defined. Thus, it is often necessary to make conservative assumptions and apply conservative arguments in order to lessen the effects of uncertainties.

Without protective action a full-core accident can only be terminated by fuel movement. Although there is a reasonably high probability that significant fuel movement may occur shortly after fuel failure, the classic approach is to assume that the core "disassembles" due to the vapor pressure of very high-temperature fuel. Following the termination of the excursion by fuel vapor pressure, a large fraction of the core may be in a molten or vaporized state. This large mass of high-energy-density fuel then has the potential for severely damaging the primary system, particularly if the energy in the fuel can be transferred rapidly to sodium. It is unusually difficult to define a sequence of events leading to or following disassembly. However, since the potential consequences cannot be easily dismissed, an analysis of a core disassembly accident and the resulting work energy are reported in section 3.1,

Core Disassembly Analysis. It should be pointed out that the present available methods of analysis have several limitations: (1) they do not include molten fuel/coolant interactions, (2) they do not account for the effect of core structure on the course of the accident, and (3) they do not properly integrate core and system response.

The last of these is particularly important in that the calculations of the core accident and of the system's response must be performed independently, with the only link between the two being a theoretical available work energy calculated from the core conditions following disassembly. The work energy is then used as input information for calculations predicting the system's structural response, as described in section 4.3, Primary Containment.

The fault paths that can be postulated in order to trace the possible consequences of a local abnormality are extremely complex. The undetected propagation of pin failures within an assembly may be expected to occur only under very limited conditions. However, it is difficult to show conclusively that it cannot occur and that at no point in a propagation sequence will molten fuel mix with sodium. Similarly, an undetected inlet flow blockage is very unlikely in a properly designed system; however, it is again difficult to show that the subsequent events do not in any case lead to the mixing of molten fuel and sodium. It is quite possible that, through careful analyses and experiments and with careful design, fuel pin failure propagation and localized molten fuel/sodium interactions can be ruled out as initiators of a serious accident. However, the potential consequences of these events are significant and should be considered. Analyses of accident conditions potentially arising from local abnormalities where protective action is not effected are reported in section 3.2, Local Accidents. The primary emphasis in these analyses has been on examining the effects of a hypothetical localized molten fuel/coolant interaction and, in particular, the response of the fuel assemblies.

The primary objective of the work in this activity was to define the consequences of the various accidents with regard to the inherent and engineered safety features and to the protective system and devices. The ultimate purpose, then, was to select a design basis accident or accidents so that the safety-related functional requirements for these

features and systems could be uniquely defined. Because of the early termination of this work, the DBA(s) was not selected; however, primary candidates for the DBA(s) and the considerations involved in its selection are discussed in section 3.3, Design Basis Accident Selection.

3.1. Core Disassembly Analysis

3.1.1. General

Several of the malfunctions discussed in sections 2.1 and 2.2 can potentially lead to serious consequences if protective action fails or is not effected prior to extensive fuel damage. If protective action is not taken, then a full-core accident can only be terminated by inherent negative reactivity effects. The Doppler effect serves to control the reactor power to some extent during a severe accident; however, it can never lower the power to a normal level unless the reactivity causing the accident is removed. The only effects that can conclusively terminate an unprotected accident, then, are those involving fuel movement. Several fuel movement effects may be identified as significant. Prior to any irreparable damage axial fuel pin expansion and assembly radial movements may add negative reactivity; however, these cannot be depended on to override large positive reactivities. Fuel "slumping" may add positive reactivity; however, it is more likely that it will be a predominantly negative effect in B&W's reference design, in which the pins are supported from the top. On the other hand, to depend on fuel slumping as a shutdown mechanism would be perhaps overly optimistic without further model development. During most severe accidents, extensive fuel melting and fuel failures probably would occur, with a reasonably high probability that molten fuel would be ejected into the sodium in assemblies not voided of sodium. If violent sodium "vapor explosions" did occur, then rapid sodium expulsion and the resulting large positive reactivities would tend to increase the energy release from a severe accident. However, fuel particle ejection with the voiding sodium and assembly radial movements would probably be an overriding negative effect, tending to terminate the accident. Again, further model development and analysis are necessary to prove this conclusively.

The remaining strong fuel movement effect is the classic core disassembly on fuel vapor pressure. If no other effect terminates

an unprotected accident, then fuel temperatures rise so that the fuel vapor pressure moves fuel away from the higher power density regions. Fuel temperatures must be quite high for this to occur, and the conventional disassembly analyses assume that the core has no structural integrity and behaves hydrodynamically. This assumption may not be conservative; however, this slight lack of conservatism is insignificant compared to the conservatism implied by assuming that the core can reach the disassembly condition.

Following a core disassembly on fuel vapor pressure, the core may contain a large fraction of molten and vaporized fuel. This fuel then has a large potential for doing damage to the system, particularly if it mixes with bulk sodium and a large vapor explosion occurs. The events following a disassembly are extremely difficult to predict; however, if the core can indeed go through a fuel vapor pressure disassembly, then the possibility of a nearly full-core vapor explosion cannot be easily dismissed.

The objectives of this investigation were to analyze a severe hypothetical core disassembly accident and to estimate the potential for doing work on the system. The analytical results could then be used as input information for analyses of system response.

The particular accident analyzed, perhaps a typical disassembly accident, was a flow coastdown without protection. The analysis of the initiating conditions for this accident is reported in section 3.1, Gross Flow Abnormalities. Two series of calculations were performed; the TART¹ code was used to analyze the accident through sodium voiding to the point at which a switch to the Weak Explosions module of the SAS/MARS² code was necessary. SAS/MARS was then used to calculate the disassembly until the excursion was terminated. The second series of calculations was similar to the first but included improvements in the thermal-hydraulic analysis and used two-dimensional Doppler, sodium void, and material worth distributions that were unavailable at the time of the first series of calculations. For both series 1 and series 2 calculations the flow coastdown with a 6000 lb-ft² pump inertia was used, and all calculations were performed for end-of-cycle conditions. Sensitivity studies were performed during both series of calculations to determine the influence of certain parameters on the energy release.

Due to the lack of adequate calculational methods, it was not possible to include molten fuel/sodium interactions occurring before or during disassembly. However, a check calculation was made in an effort to determine whether fuel movement due to these interactions could possibly increase the energy release during disassembly.

The theoretical work energy available from fuel expansion following disassembly was calculated for one reference case in the series 2 set. Estimates of available work energy from sodium expansion were made assuming that all the molten fuel mixed with sodium following disassembly. Calculations of available work from sodium expansion were also made with the BANGO code and are reported in section 4.3, Primary Containment.

3.1.2. Core Disassembly Analysis Results

3.1.2.1. Series 1 Calculations

One of the primary objectives in this part of the work was to determine the influence of multichannel analysis on the disassembly results. The TART calculations were performed with a two-dimensional representation in which the core was divided into a varying number of radial regions (or channels) corresponding to annular rings of fuel assemblies. An approximate voiding model was employed in which voiding was assumed to begin in a given channel whenever the coolant temperature in any segment of that channel exceeded an input trip temperature. Voiding was assumed to proceed at a constant rate of 50 fps in both the upward and downward directions. The primary parameter of interest for these calculations was the rate of reactivity insertion at prompt criticality, the point at which the switch was made from the TART calculation to the disassembly calculation provided by SAS/MARS. Table 3-1 gives the results for cases in which the core was represented by one, three, five and seven regions.

The core representations used for the TART calculations were retained for the SAS/MARS disassembly calculations. SAS/MARS was modified to accept input temperature distributions given by TART for the time at which the switch was made. Figures 3-1 and 3-2 show the power and reactivity traces from SAS/MARS for the

Table 3-1. Results of Coherence Calculations

	<u>Case 1</u>	<u>Case 2</u>	<u>Case 3</u>	<u>Case 4</u>
No. of regions used to represent core	7	5	3	1
Region equivalence scheme based on 7 regions ^(a)	1 ← 1 2 ← 2 3 ← 3 4 ← 4 5 ← 5 6 ← 6 7 ← 7	1 ← 1 2 ← 2, 3 3 ← 4, 5 4 ← 6 5 ← 7	1 ← 1, 2, 3 4, 5 2 ← 6 3 ← 7	1 ← 1, 2, 3, 4, 5, 6, 7
Time to inception of voiding, s	5.6608	5.6808	5.8608	7.3208
Fraction of core voided at prompt criticality	0.089	0.078	0.138	0.368
Time that individual channels begin to void, s				
1	5.6608	5.6808	5.8608	7.3208
2	5.7844	5.8029	(b)	--
3	5.7988	(b)	(b)	--
4	5.8144	(b)	--	--
5	(b)	(b)	--	--
6	(b)	--	--	--
7	(b)	--	--	--
Time to prompt criticality, s	5.8213	5.8323	5.8849	7.3419
Reactivity insertion rate at prompt criticality, \$/s	40	37	95	157

(a) The symbol ← denotes combined regions.

(b) Not voided at time of prompt criticality.

reference seven-region case. The energy generation during the disassembly phase for the one-, three-, and seven-region cases is tabulated below:

<u>No. of regions</u>	<u>dK/dT at prompt criticality, \$/s</u>	<u>Total energy generated, 10⁴ MW-s</u>
1	157	8.4
3	95	7.1
7	40	4.3

These studies indicate that the single channel analyses may overpredict the reactivity insertion rate due to sodium voiding at prompt criticality by a factor of four and consequently, in this case, overpredict the energy generation by as much as a factor of two. This effect is probably most pronounced—and hence, most important—in reactors like the B&W reference reactor, which have a large sodium void effect, since the reactivity effect arising from sodium expulsion has the strongest influence on the reactivity insertion rate. It should be noted here that the energy generation is not a linear function of the reactivity insertion rate at prompt criticality.

Figure 3-3 shows the variation of energy release with Doppler coefficient for two cases: discontinuities in the gradient of material worth at zone interfaces were (1) accounted for and (2) ignored. It may be seen that, in the presence of a large negative Doppler coefficient, discontinuity effects do not influence the energy generation very strongly; however, for small Dopplers the effects of discontinuities become governing.

The influence of the equation of state of UO₂ was investigated for three of these equations; the equations and the energy releases for the three cases are shown below with their sources.

Equation and case No.	Calculation	Energy generation, MW-s	Source
1	$P = 4.5 \times 10^{11} \exp\left(-\frac{5.154 \times 10^4}{T}\right)$	41,318	APDA-209
2	$P = 2.121 \times 10^{11} \exp\left(-\frac{3.714 \times 10^4}{T}\right)$	23,204	Menzies
3	$P = 7.6 \times 10^{11} \exp\left(-\frac{5.511 \times 10^4}{T}\right)$	43,318	Personal com- munication, D. H. McFarlane

Figure 3-4 shows the variation of energy generation with prompt neutron generation time with and without surface integrations. Decreasing the prompt neutron generation time in the presence of a large Doppler coefficient increases the number of peaks in the power trace. The maximum release occurs when the core disassembles near a power peak. Figure 3-5 shows the power distributions for which the energy generation from disassembly was studied. The results are tabulated below.

<u>Distribution</u>	<u>Energy generated, MW-s</u>
Flat	45,923
Nominal	43,318
Peaked	37,874

The effect of the material worth distribution on the energy release was also studied. The dependence is extremely weak since the worth distribution affects only the material dispersion reactivity. Once this reactivity comes into effect the transient is swiftly terminated, so that any parameter that serves only to increase the material dispersion feedback will have only a small effect on the integrated energy.

The results of the parameter study indicate that uncertainties in the Doppler coefficient have perhaps the greatest influence on the energy generation for a given disassembly initiating condition. By explicit multichannel calculations of the accident, this

study also indicates that the assumption of radial coherence significantly overpredicts the reactivity insertion rate at the time the reactor goes prompt critical. The effect of this, then, is to overpredict the energy generation during the disassembly phase of the accident.

3.1.2.2. Series 2 Calculations

This set of calculations used a ten-region TART model in which each ring of assemblies in the core was considered as a separate region. Two-dimensional Doppler, sodium void, and material worth distribution calculated with SCRAM were used. A more reasonable sodium void velocity of 200 cm/s was used. This voiding rate and the more detailed core representation resulted in an extremely asymmetric core voiding pattern and temperature distribution at the time of the switch from TART to SAS/MARS. For this reason, a method was developed to run the SAS/MARS disassembly calculation for the full core rather than using symmetry about the axial midplane as in previous calculations. Figure 3-6 is a schematic of the regional structure used. Benchmark calculations with a symmetric test case showed excellent agreement between the half- and full-core models. With the slower voiding rates the excursion did not reach prompt criticality before a switch from TART to SAS/MARS was necessitated. The switch was made when the average fuel temperature in the peak TART segment reached 6000F. For the reference case using the 200 cm/s voiding rate, the total reactivity insertion rate at this point was \$16/s, although the rate varied between \$10 and \$18/s depending on voiding patterns. The total energy added before the switch was 34,800 MW-s.

Two calculations were made with SAS/MARS employing different equations of state for UO₂. The equations, their sources, and the corresponding energy releases are shown below.

Case No.	Equation of state	Energy generation, MW-s	Source
1	$P = 2.121 \times 10^{11} \exp\left(-\frac{3.714 \times 10^4}{T}\right)$	21,053	Menzies
2	$P = 7.6 \times 10^{11} \exp\left(-\frac{5.511 \times 10^4}{T}\right)$	34,264	Personal communication, D. H. McFarlane

Figures 3-7, 3-8, and 3-9 show the power and reactivity traces for the two cases. Further results, including masses of molten fuel at the end of disassembly and the energy therein are shown in Table 3-2.

Table 3-2. Flow Coastdown Accident Without Protection Conditions at End of Disassembly

	<u>Case 1</u>	<u>Case 2</u>
Equation of state	Menzies	ANL
Total energy release, MW-s	21,053	34,264
Mass of molten fuel, kg	18,230	26,870
Energy in molten fuel, MW-s	8,363	17,492

The influence of the reactivity insertion rate due to sodium voiding at the initiation of the disassembly calculation was also investigated. The energy generation for the reactivity insertion rates considered is tabulated below:

<u>Case</u>	<u>dk/dT at prompt criticality, \$/s</u>	<u>Total energy generated, MW-s</u>
1	10	20,509
2	16	21,053
3	25	21,866

These results indicate that the energy generation is not very sensitive to the reactivity insertion rate for rates on the order of \$25/s and less.

The methods used in this analysis (TART, SAS/MARS) do not allow for the possibility of fuel failure and violent sodium-fuel interactions occurring before the start of disassembly due to fuel vapor pressure. These interactions, or vapor explosions, could

have several immediate consequences. The sodium voiding due to the vapor explosions could lead to large reactivity insertions and potentially much larger energy releases. The ejection of fuel particles with the voiding sodium could tend to lessen this effect; however, with the relatively large void worth-to-fuel worth ratio in a large LMFBR, it appears unlikely that fuel ejection would negate the sodium void reactivity.

On the other hand, it is possible that radial movement caused by high sodium vapor pressures could terminate the excursion. There is a corresponding danger that vapor explosions away from the center of the core would tend to compact the interior of the core and lead to an overall positive reactivity in some case. This possibility was checked with a SAS/MARS run in which the fuel vapor pressure was arbitrarily increased by a factor of 100 in region 11 (Figure 3-9). The result showed a decrease in energy release of about 10%. This indicates that even with high pressures in this region, in which fuel movement can potentially cause a positive reactivity in both directions radially, the overall effect is negative. Thus, it is possible that with improved methods of analysis, including radial structural dynamics, the effect of sodium vapor explosions occurring before disassembly would be to decrease the energy release.

3.1.3. Work Evaluation

3.1.3.1. Problem Discussion

Evaluating the available work energy is perhaps the most critical part of the analysis of any severe core disassembly accident. After termination of the nuclear excursion, the reactor core contains a large mass of molten and vaporized fuel and steel. Both the expansion of the fuel and of the sodium following mixing with the fuel could potentially contribute to the work done on the system. The most realistic way to estimate the actual damage work is direct calculation of the dynamic effects on the system of the physical forces due to the expansion of fuel and/or sodium simultaneously with the calculation of the dynamics of the expansion. However, there are no methods available for accomplishing this, and the conventional one is to calculate a theoretical expansion and obtain the available work energy. This work energy is then used as input to a calculational code which evaluates the system's response (see section 4.3).

3.1.3.2. Fuel Expansion Work Energy

If sodium were completely out of the core and no sodium-fuel mixing were to occur during or following disassembly, then the fuel-liquid-vapor mixture would expand against the system's inertial and structural restraints. The conventional way to evaluate the available work energy is to assume an isentropic expansion following the constant volume heating during the disassembly. This evaluation has been made for the flow coastdown disassembly using Menzies' equation of state as reported in section 3.1.2.2 above. The most complete available tabulated information for UO_2 properties is that of Booth,³ which was used for the work calculations. Since Booth's saturation pressure as a function of temperature does not agree with that given by the Menzies equation of state used in the disassembly calculation, a normalization was made so that the expansion was calculated from a temperature at which the Booth vapor pressure agreed with the Menzies vapor pressure for the final conditions given by the MARS run. Figure 3-10 shows the results for the available work energy as a function of temperature for an expansion down to 2.2 atmospheres. The saturated vapor pressure as a function of temperature for both the Menzies and Booth data are also given. This work energy curve was used to evaluate the work energy from each region used in the MARS calculation. The total work energy, assuming that each region expanded independently, was 600 MW-s, corresponding to an average value of 33 J/g. The work energy for the expansion of the fuel in the voided part of the core was 250 MW-s.

The total internal energy in the fuel above 3100K was calculated to be 9600 MW-s using the Booth data, a value that agrees fairly closely with a value of 8400 using Meyer's information.⁴ It should be pointed out that the actual total energy in fuel above 3100K as calculated by MARS was considerably lower (6200) due to a low value of specific heat above 3100K. However, these results for the work energy are conservative since the expansions were calculated using the higher internal energy. Further, if a higher specific heat above 3100K had been used in the MARS runs, then more nuclear energy would have been generated, but the excursion would have terminated at lower

pressures. Thus, the work energy calculated with the Booth data would have been lower.

The available work energy due to fuel expansion is not insignificant in itself; however, it seems unlikely that the fuel can expand without extensive mixing with sodium, particularly if considerable sodium remains in the core during disassembly. Thus, the uncertainty involved in the fuel expansion work calculations is relatively insignificant compared to the magnitude and uncertainties of determining the work energy due to the expansion of sodium following extensive mixing.

3.1.3.3. Sodium Expansion Work

With the methods of analysis available now, it is not possible to determine mechanistically the sodium-fuel mixing patterns that may occur during and after a core disassembly accident. It is likely that any sodium remaining in the core during the disassembly will mix with molten fuel. Further, the molten fuel in the voided part of the core will probably mix with sodium at some point, and, in one sense, this mixing may be desirable in order to cool the fuel before a melt-through of the core and reactor vessels occurs. On the other hand, the expanding sodium potentially could do much more damage to the system than the expanding fuel could if extensive mixing did not occur.

There are several sources of information on the available work energy from the sodium expansion following the interaction between fragmented molten fuel and sodium. The Hicks and Menzies model⁵ assumes that the fuel-sodium mixture reaches instantaneous thermal equilibrium at constant volume and that the sodium then expands while remaining in thermal equilibrium with the fuel. The results from this model, for an expansion to 1 atm, show a maximum work energy of about 300 Joules per gram of fuel at a sodium-fuel mass ratio of 0.10 for initial fuel and sodium temperatures of 3450 and 1150K, respectively. The B&W computer code BANGO, developed during the 1000-MWe Follow-On Study, is similar to the Hicks-Menzies model but includes a time-dependent calculation of the expansion based on the inertial constraint provided by the bulk sodium above the core. BANGO

calculations (section 4.3) for a sodium-fuel mass ratio of 0.096 and initial fuel and sodium temperatures of 5000 and 1000F, respectively, gave a work energy of about 175 J/g for several cases involving varying fractions of the core. More detailed models using finite heat transfer rates tend to yield lower work energy values. Results from the BNWL code SOCOOL-II show a work energy of about 135 J/g and 180 J/g for fuel particle diameters of 500 and 100 microns, respectively, with a sodium-fuel mass ratio of 0.088 and initial fuel and sodium temperatures of 3000 and 672K, respectively.⁶ The BNWL results show a relatively weak dependence on initial fuel temperature with a work energy of about 170 J/g for a particle diameter of 500 microns and an initial fuel temperature of 5000K. B&W's MFCDYN code (see section 3.2) seems to show fairly good agreement with the BNWL results for a reference case using a particle diameter of about 290 microns, although MFCDYN was not used for a case representing full-core-length mixing.

The disassembly calculation using Menzies' equation of state resulted in a total molten fuel mass of 18,230 kg. If this entire mass of molten fuel were to fragment and mix with an optimum mass of sodium, the resulting total theoretical work energy would be about 2500 to 3200 MW-s for work energies in the range of 135 to 175 Joules per gram of fuel. If only the fuel in the unvoided regions of the core were to mix and a work energy of 175 J/gm were assumed, then the work energy for sodium expansion would be about 2040 MW-s.

A BANGO calculation which considered the condensation of sodium vapor on the surrounding subcooled sodium gave a work energy of 88 J/g. This, then, represents a total theoretical work energy of about 1600 MW-s if all the fuel mixed with sodium (again with a sodium-fuel ratio of 0.096). Further, BANGO calculations assuming low heat transfer from the fuel to the sodium after thermal equilibrium was reached show a reduction in work by as much as a factor of three. Thus, there is the possibility that, with improved methods, the total work energy could be calculated to be in the range of 1000-2000 MW-s even with the pessimistic assumption of full-core mixing.

3.1.4. Evaluation and Conclusions

For the flow coastdown without protection analyzed here, the voiding of the core and the resulting reactivity insertion results in a relatively mild core disassembly. However, a large fraction of the core is molten at the end of the excursion. The available work energy from the expansion of the fuel is not negligible (600 MW-s) but is relatively insignificant compared with the potential work energy due to sodium expansion (2500 to 3200 MW-s) if all the molten fuel were to mix with sodium following disassembly. This large work energy has the potential for severely damaging the primary system. However, it seems very unlikely that the entire mass of molten fuel in the core at the end of disassembly can mix with sodium in a rapid, coherent manner with all the fuel fragmenting into small particles. It seems reasonable that fuel expansion in the voided regions of the core would contribute to the available work and that mixing would occur primarily in the regions containing sodium. Some mixing would probably also occur at fuel-sodium boundaries, but it does seem likely that, with additional experiments on molten fuel-sodium interactions and improved analytical methods, the hypothetical situation of complete mixing could be demonstrated to be very improbable.

In addition to the problem of available work energy evaluation, the current methods of analysis are limited in their ability to predict sodium voiding patterns and rates realistically and to calculate the effects of molten fuel-sodium interactions occurring before and during disassembly. These can drastically alter the course of the accident, and, in particular, it seems likely that the latter could terminate the excursion before disassembly due to fuel vapor pressure occurs. Again, improved methods must be developed to demonstrate this.

3.1.5. References

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- ⁴ R. A. Meyer, et al., Fast Reactor Meltdown Accidents Using Bethe-Tait Analysis, GEAP-4809, General Electric Co. (1967).
- ⁵ E. P. Hicks and D. C. Menzies, Theoretical Studies on the Fast Reactor Maximum Accident, ANL-7120, Argonne National Laboratory, Oct. 1965.
- ⁶ B. Wolfe, et al., Fast Flux Test Facility Periodic Technical Report, BNWL-1394, Battelle, August 1970.

Figure 3-1. Power Trace — Seven Core Regions (ANL
Equation of State, 50 fps Sodium Voiding)

Figure 3-2. Reactivity Traces — Seven Core Regions (ANL Equation of State, 50 fps Sodium Voiding)

Figure 3-3. Energy Yield Vs Doppler — Seven Core Regions

Figure 3-4. Variation of Energy Generation With Prompt Neutron Lifetime

Figure 3-5. Power Distributions

Figure 3-6. 29-Region WEAK EXPLOSIONS Model

Figure 3-7. Power Trace — 200 cm/s Sodium Voiding,
Initial Power = 3.7×10^5 MW

Figure 3-8. Reactivity Traces — Menzies Equation of State,
200 cm/s Sodium Voiding

Figure 3-9. Reactivity Traces — ANL Equation of State,
200 cm/s Sodium Voiding

Figure 3-10. Available Work and Pressure

3.2. Local Accidents

3.2.1. General

The two general types of localized accidents considered in this study involve the postulated violent interaction of molten fuel and sodium coolant. These accident studies are essentially extensions of the initiating conditions discussed in the previous chapter—intra-assembly and entire-fuel-assembly blockages.

The violent interaction of molten fuel with sodium coolant may occur under certain conditions. These conditions involve or result in the fragmentation of the molten fuel, which may produce a very rapid energy transfer to the neighboring coolant. A sufficiently rapid energy addition to a constrained volume of sodium liquid can result in extremely high fluid pressures and significant generation of sodium vapor. A high-pressure sodium vapor phase has the capability of performing potentially damaging mechanical work on the core structures.

The study of initiating conditions in the area of intra-assembly blockages has shown that the incidence of molten fuel formation on a local level is not likely but cannot be precluded. Should a limited amount of molten fuel come into contact with sodium coolant within the core, an accident situation may result as a consequence of an accompanying pressure pulse and limited vapor generation. A small-scale fuel-sodium interaction would be expected to result in a short-duration flow disturbance but more importantly, it may jeopardize the integrity of the fuel assembly wrapper can. Since the wrapper can of the reference core is essentially a flow boundary for one assembly, damage to this boundary could lead to a significant flow maldistribution within the assembly. A severe flow maldistribution might be postulated to lead to a more serious accident situation where more than one assembly could suffer damage if the accident were to progress undetected.

An incident of the flow blockage of an entire assembly was introduced in section 2.3 as an initiating condition. Although passive safeguard devices and the core protection system might reasonably prevent an accident initiated by a credible type of flow blockage (see section 4.1), some consideration must be given to the possible consequences of such an accident. This type of accident, i.e., flow blockage of an entire

assembly, may be assumed to be characterized by (1) a severe flow reduction in the assembly followed by (2) a sodium coolant temperature rise to a superheated condition, (3) sodium boiling and ejection of liquid sodium from the assembly, (4) possible fuel melting and cladding failure with (5) molten fuel and sodium interaction upon the return of liquid sodium. Considerable uncertainty exists regarding the course of events following the liquid slug ejection, such as the possibility of multiple slug ejections on further boiling, the times for cladding failure, and whether or not significant amounts of sodium would return to the proximity of the molten fuel. Assuming that an interaction of molten fuel and liquid sodium could occur on this scale, however, the accident might mechanically affect a fairly large number of assemblies.

The progression of this accident to the point where a number of fuel assemblies are involved may involve nuclear as well as mechanical effects. Immediate or delayed voiding in affected assemblies should be examined for this case. Even though the reactivity addition might be inconsequential, this accident may involve sufficient energy release from molten fuel and coolant interaction that some safeguard requirements are delineated. This accident might, then, be the DBA or one of several DBAs selected for the design. This aspect is treated in detail in section 3.3.

The objective in the study of the consequences of localized molten fuel and sodium interaction has been to determine the possible significance of this event with respect to the integrity of the fuel assembly wrapper can. For the case where the wrapper can would reasonably be expected to fail (due to a molten fuel-coolant interaction resulting from a postulated blockage of an entire fuel assembly), the objective was to determine the probable extent of fuel assembly displacement and crushing (if any).

A model was developed (MFCDYN) to calculate the pressure-time history and the work done in a molten fuel-sodium interaction event.¹ The results from these calculations have been used to determine the possible effect on the integrity of the fuel assembly wrapper can. The energy absorption capability of the wrapper can was determined and compared with the energies available from a molten fuel-coolant interaction.

A model was also developed for the determination of the response of the fuel assemblies in proximity to a local core pressure disturbance. The response of interest included wrapper can elastic and plastic deformations as well as net fuel assembly displacements in the radial direction. Unfortunately, the work was terminated before a meaningful appraisal of fuel-coolant interaction could be made utilizing the fuel-coolant model along with the structural response model.

3.2.2. Molten Fuel-Sodium Interaction

The available work from a mixture of molten fuel and sodium has been determined using the MFCDYN model. The MFCDYN computer program treats the heat transfer variation with time starting with a mixture of fragmented fuel and liquid-phase sodium at two arbitrary (input) temperatures. The program treats the effects of the unheated sodium coolant acting as the constraint for the sodium-fuel mixture; this constraint is either acoustic or inertial in nature. The effect of the wrapper can is not included in the model. It has been assumed that the wrapper can is a perfectly rigid body as far as its effect on the expanding fluid volume, pressure, and work are concerned. The following values of pertinent program parameters were used:

1. Initial fuel temperature—6200F (this value was employed to account for the additional energy in the fuel latent heat of fusion, so that the actual energy corresponds to completely melted fuel at 5000F.
2. Initial sodium temperature—1050F.
3. Distance to free surface (or constant pressure reservoir)—4.17 ft.
4. Area for mixture expansion—0.0867 ft² (the fuel assembly coolant flow area or one-half the flow area "seen" by a mixture expanding upward and downward.
5. Heat transfer coefficient between sodium and fuel in the mixture—20,000 Btu/h-ft²°F.
6. Typical fragmented fuel particle radius—0.000457 ft (139 microns).
7. Volume of fission gas in the mixture volume—0.0 ft³.

The MFCDYN calculations for the work done by the expanding sodium-fuel mixture include the work done during the initial phase, when the rapidly heated sodium suffers an increase in pressure. The expanding system does work during the entire expansion process, of course, but for these purposes, only the work was considered that was performed with the mixture pressure in excess of a specific amount. Figure 3-11 shows the results.

For comparison purposes, the results of Hicks and Menzies² are also shown in Figure 3-11 as dashed lines. They have calculated the work done by a mixture of fuel and sodium in expanding to various final pressures. It was assumed that fuel at 5750F and sodium liquid at 1600F are brought to thermal equilibrium at constant volume and then allowed to expand adiabatically to some final pressure (with the fuel and sodium in thermal equilibrium during the expansion process). The assumptions made for this calculation included the following: (1) the sodium and fuel liquids are incompressible and have negligible specific volumes compared to the vapor phase; (2) the sodium vapor behaves as a perfect gas; and (3) the specific and latent heats are constant.

3.2.3. Core Response

The fuel assembly structures must limit the consequences of localized molten fuel-sodium interactions. The basic fault propagation mode considered here involves inadequate cooling in a localized area followed by fuel melting, molten fuel-sodium mixing, and the conversion of thermal energy in the fuel to mechanical energy in the form of sodium expansion. The ability of the fuel assembly structures to dissipate or direct the mechanical work potential from the last event is highly dependent upon the rate at which the event takes place, which, in turn, depends on the temperatures, properties, and volumes of the fuel and sodium involved.

The primary approach employed has been to establish a limit for the amount of structural damage that can be tolerated and determine whether the amount of molten fuel required to produce this damage can credibly be available at the site of a local cooling failure.

If such an energy release should be sufficient to cause failure of the fuel assembly can within which it originates, the adjacent

fuel assemblies could be displaced and/or crushed. This could lead to fuel movement in the core and significant flow reduction within the affected assemblies. The reactivity effects caused by the fuel movement may be of little consequence; however, the flow reduction could result in local coolant boiling and progressive flow starvation. Since this is a potential accident propagation mechanism, it is of particular importance to determine (1) the rupture pressure of the fuel assembly can and (2) the pressure at which nearby fuel assemblies would be deformed to an extent that could interfere with their functions.

3.2.3.1. Fuel Assembly Response to Internal Pressurization

Two damage criteria were determined for a hexagonal fuel assembly wrapper can subjected to internal loading. These damage criteria may be expressed in terms of internal pressure required (1) to initiate plastic deformations in the can wall and (2) to form plastic hinges at the corners of the hexagon.

The graphs of these pressures as a function of fluence are shown in Figure 3-12. The abscissa values were obtained by considering the change in material yield strength with irradiation. The lower curve shows the pressures corresponding to the onset of plastic deformation, and the upper curve shows the pressures at which the fuel assembly duct can be expected to approximate a circular shape (with plastic hinge formation).

The maximum possible pressure that can be sustained by the fuel assembly duct was calculated to be approximately 2000 psi with an unirradiated ultimate strength of 54,000 psi.

The energy absorption capability of the fuel assembly duct is affected by the mechanical property of ductility, which changes with neutron irradiation. Given a sufficient irradiation exposure, the wrapper can material may suffer a considerable loss in ductility to the extent that the only usable deformation occurs in the elastic region (nil ductility). For a material condition of nil ductility and high yield strength, the results showed that the reference core duct could absorb approximately one ft-lb of energy per inch of axial length. The equation used to calculate this value is as follows:

$$E_e = (S_y)^2 \frac{TL}{60E}$$

where E_e = elastic strain energy in bending, ft-lb,
 T = wall thickness, in.,
 L = length of one duct flat, in.,
 E = Young's modulus of elasticity, psi,
 S_y = yield strength, psi.

The upper-limit energy-absorbing capacity for the duct was established by assuming that its material would accept the deformations required to permit it to transform from hexagonal to round then calculating the energy-absorbing capacity of the equivalent round configuration. This energy was calculated using the following equation:

$$E_p = \pi S_y T B \frac{D}{12}$$

where E_p = plastic strain energy per axial inch of duct, ft-lb,
 S_y = yield strength, psi,
 T = wall thickness, in.,
 B = plastic elongation limit, in./in.,
 D = duct equivalent diameter, in.

Figure 3-13 is a graph of plastic energy absorption capability as a function of fluence up to 10^{22} nvt. The abscissa values were obtained by considering the changes in material yield strength and ductility with irradiation at a temperature of 1100F.³ Since there are little data concerning the relationships of fluence, yield strength, and ductility at fluence levels above this point, the energy dissipating capabilities of the duct are given in Figure 3-14 for various material yield strengths and ductilities. The minimum total elongation at a fluence of 10^{22} nvt for 304 SS at 1100F is approximately 13.1% as determined from reference 7. At these same conditions, the yield strength is approximately 30,000 psi.

3.2.3.2. Allowable Quantity of Interacting Fuel

The amount of interacting fuel that would be required for a loss of wrapper can integrity has been determined as a function of the energy-absorption capability of the wrapper can. The important considerations in this evaluation were (1) the energy-absorption capability per unit wrapper can length and the required fluid pressure, (2) the axial length of the deformation, (3) the work energy capability of the heated sodium, and (4) the deposition of this work energy, i.e., whether it is utilized in deforming the structure or increasing the kinetic energy of the unheated surrounding fluid.

The required fluid pressure for the loss of wrapper can integrity may range from approximately 80 to 2000 psi. It is reasonable to expect that a maximum fluid pressure of less than 80 psi would cause no large permanent deformations of the wrapper can. In such a case all of the thermal energy of the fuel that is converted to work would appear as a change in the kinetic energy of the unheated coolant and also in frictional losses. As mentioned in section 3.2.3.1, the fluid pressure required to cause a perfectly circular wrapper can to fail is approximately 2000 psi for unirradiated 304 SS at 1100F. It has been assumed that one-third of the total available work energy is directed to deformation of the wrapper can.

Figure 3-15 shows the fuel masses required to deform a 66-inch length of a reference core wrapper can to the point at which the can's integrity is in jeopardy. The maximum energies for a given pressure limit were used as given in Figure 3-11, i.e., the worst sodium-to-fuel mass ratio was assumed for every pressure of interest.

3.2.3.3. Response of Fuel Assemblies Surrounding a Local Core Pressure Disturbance

The motions and deformations of interacting, externally loaded assemblies were analyzed by developing and solving the differential equations for predicting their responses to a forcing function resulting from a pressure rupture of a single assembly can. The equations were developed by equating the forces acting at each mass point of a discrete mass system that represents the core geometry in

idealized form. Utilizing a digital simulator computer code, these equations were solved for the forces and displacements associated with the motion of each mass point at specific instances of time. The program provides for calculating the effects of elastic and linearly varying inelastic structural deformation, clearances between structures, and fluid inertia and friction on the motion of each mass point.

Calculations were made using the multiple fuel assembly model to determine the structural effects of variations in applied impulse. The variation in applied loading time ranged up to 4.5 ms, whereas the peak loading on the first assembly ranged up to 150,000 pounds. The loading variation with time was taken to be triangular, with the peak load occurring at one-half the load duration time. The value of 150,000 pounds would correspond to a uniform fluid pressure of 630 psi over a 66-inch length of one flat of a hexagonal can.

The results of the calculations show that one row of fuel assemblies is likely to suffer crushing and bending failure if the impulse applied to each assembly of the row exceeds 37.5 lb-s. However, this was obtained with the assumption that failure occurs once the yield stress of the material is exceeded. In this case, one row consists of six fuel assemblies surrounding the site where the impulse-generating force originated.

Figure 3-16 shows how the displacement of the center of mass of one of these assemblies varies with applied impulse. This figure shows that the displacement is a nonlinear and discontinuous function of impulse. The nonlinearity occurs because the impulse is calculated and applied to the assembly in incremental time steps rather than being calculated and applied to the structure as a total quantity, and because the forces resisting motion are nonlinear. The discontinuity arises because it was assumed in formulating the calculational model that structural and fluid forces associated with the motion of an individual fuel assembly cease whenever the displacement that causes rupture is exceeded. As shown by the figure, the discontinuity begins with bending failure of the assembly and ends when the mass of the failed assembly contacts an adjacent unfailed assembly.

3.2.4. Evaluation and Conclusions — Local Accidents

A model of the molten fuel-coolant interaction processes was developed to determine the resulting pressure-time variations and the available work from such an event. Unfortunately, the results given by the computer program developed for this purpose (MFCDYN) have not been sufficiently substantiated to allow their presentation. A parametric study was planned for investigating the importance of various parameters in the interaction processes. Some limited number of results, however, were used to determine the work energy per unit fuel mass in a mixture of molten fuel and sodium.

Even though the probability of a molten fuel-sodium interaction would be very low for the case of a local fault within a pin bundle, the effects of such an event have been examined with respect to the affected fuel assembly. The amount of molten fuel required for the loss of wrapper can integrity has been examined as a function of the axial extent of deformation and the energy-absorption capability of a unit length of wrapper can. Although considerable uncertainties are present in the analysis, it is reasonable to conclude that an excess of the fuel and fertile material in one pin would be required (when in the molten state and mixed with sodium) to cause a wrapper can to fail with a neutron fluence of 10^{22} nvt or less. Under these conditions then, the loss of integrity of a wrapper can resulting from a localized blockage and subsequent fuel-coolant interaction would be very improbable. It is not reasonable to postulate that a very severe blockage would occupy the length of a single coolant channel (or smaller lengths of numerous adjacent channels) required to form this amount of molten fuel.

A model was developed to evaluate the effects of a localized pressure disturbance on the fuel assemblies surrounding a breached fuel assembly. The structural distortions and possible reactivity insertions from fuel movement were of interest. Unfortunately, the MFCDYN fuel-coolant interaction model was not sufficiently developed to provide input to this structural model. Consequently, the results obtained were limited to a study of the effects of variations in applied impulse to the affected assemblies. Under the conditions assumed, it appears that a small impulse would damage the six assemblies surrounding the disturbance

in the event that the wrapper can material possessed negligible ductility. Results from the model indicated, however, that the reactivity insertions from radial fuel movement would, in most cases, be insignificant.

3.2.5. References

- ¹ R. W. Moore, et al., 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Local Core Faults, BAW-1362, Babcock & Wilcox, Lynchburg, Va., Dec. 1970.
- ² E. P. Hicks and D. C. Menzies, "Theoretical Studies on the Fast Reactor Maximum Accident," ANL-7120, Argonne National Laboratory, Oct. 1965.
- ³ R. S. Moen, Fast Flux Test Facility Materials Design Data, BNWL-891, Battelle Memorial Institute, Oct. 1968.

Figure 3-11. MFCDYN Results Vs Hicks/Menzies Results for Work Done by Fuel-Sodium Mixture

Figure 3-12. Pressure Range for Plastic Deformation of Fuel Assembly Duct Vs Fluence

Figure 3-13. Effects of Neutron Fluence on Energy Absorption Capability for Plastic Deformation of Unit Length of Fuel Assembly Wrapper Can

Figure 3-14. Wrapper Can Energy, Absorption Capability for Unit Length — Effects of Material Yield Strength and Ductility

Figure 3-15. MFCDYN Results Vs Hicks/Menzies Results for Mass of Interacting Fuel Required for Loss of Integrity of the Assembly Wrapper Can

Figure 3-16. Displacement Vs Impulse — Center of Mass,
First Assembly

3.3. Design Basis Accident Selection

3.3.1. General

3.3.1.1. Introduction

A power reactor design provides three levels of safety for the public. First, the operational systems are designed, inspected, and operated so that the plant is inherently safe for normally anticipated transients and malfunctions. In general, this first level attempts to avoid faults that could lead to accidents and to provide reasonable tolerance for transient conditions and minor faults. At this level the interest of the owner in obtaining reliable, trouble-free operation nearly coincides with the public interest.

At the second level, protection prevents or mitigates accidents that could be caused by serious faults in the operational systems. Specifically, protection avoids undue risk to the public by preventing unacceptable damage to the reactor fuel. Careful analysis is applied to ensure that adequate protection is afforded for all credible accidents—those accidents that might "reasonably" be expected to occur. The owner benefits from protection because it preserves his investment in the reactor fuel, but the requirements invoked in the interest of public safety go well beyond the owner's purely economic interests.

The third level involves the application of engineered safety features, particularly the containment of radioactivity. At this level, accidents are assumed to occur despite the inherent safety provided in the operational and protective systems. Accidents may occur either because protection fails or because the accident conditions are so hypothetical that they would not be handled by the protection provided. The need for this third level of public safety rests on uncertainty. The probability of single and multiple faults, the description of the sequence of events that would occur, and the presence of the human element all represent areas of some uncertainty. Additionally, there are no firm criteria for accident probability (or probability-risk) limits. The lack of exact precision in these areas requires some additional assurance of safety beyond the relatively logical scope of protection. A design basis accident (DBA) is defined as follows:¹

An accident whose consequences are such as to set the basis for one or more design parameters for the containment and/or consequence-limiting safety systems. Such an accident may be selected because of the nature of the accident itself or because of considerations of the failure of one or more of the protective systems. In general, more than one accident may be required to set the basis for all of the design parameters for the containment and the consequence-limiting safety systems.

Thus, DBAs are not selected on the basis of whether they are credible or hypothetical but rather, with the objective of giving additional assurance of public safety beyond that provided by the operational and protection systems.

3.3.1.2. Problem

The most conservative safety approach to DBA selection is to choose the maximum hypothetical accident (MHA)—the assumed accident with the greatest release of energy and/or radioactivity. If this approach is taken, then the engineered safety features must preserve the public safety for any conceivable accident. This may not be possible. Even if it is, the required design measures may involve severe economic penalty that could destroy the LMFBR's ability to compete with other power sources.

For any severe accident, the amount of energy and radioactivity released may be uncertain because of the difficulty of treating ill-defined material conditions and geometries in the calculations. As discussed in section 4, this same difficulty encumbers description of the capabilities of the engineered safety features. To accommodate these uncertainties, it is necessary to introduce conservatism in the calculations. The conservatism, which is necessarily greatest for the MHA, further accentuates the demands placed on the engineered safety features.

The MHA approach presents another problem. MHAs are defined with little consideration for reality. The most ingenious pessimistic imagination may be expected to invent the worst, or maximum, hypothetical accident. No credit is taken (and therefore, little stimulus is offered) for diverse, redundant safety measures at the first two levels. The MHA approach might be defended in case of

some strong, overriding consideration, such as virtual ignorance of the consequences of credible initiating conditions. The MHA might also be selected in the unlikely event that minimal containment could withstand the consequences at least until a more agile imagination defined a worse MHA. But for a large power reactor, it appears nearly certain that a more practical approach to DBA selection is necessary.

At the other extreme of possible DBA choices, one may select an accident with little energy and/or radioactivity release. Such a choice might qualitatively satisfy the obligation of going beyond the limits of protection and could lead to an easily-satisfied set of requirements for the engineered safety features. The risk is that the uncertainty in the safety afforded by the first two levels may not be fully absorbed. Further, it is possible that the inherent integrity of the system and practical engineered safety features could handle a more severe accident; this approach could fail to take credit for such a capability.

For light water reactors, the double-ended primary pipe rupture is accepted as the containment DBA in the United States. This acceptance is the result of more than a decade of inter-relationships between designers, operators, and regulatory bodies. The consideration of other possible DBAs for a water reactor is largely a matter of comparison with the double-ended rupture. The large amount of accident-free operating experience being accumulated with light water reactors adds considerable force to the argument that this DBA selection is consistent with the public safety.

The 1000-MWe LMFBR has neither an accepted DBA nor a well-defined starting point for evaluating candidates. EBR-II and Fermi, although prototypal in many ways, do not provide the sort of precedent for DBA selection that has developed for water reactors. Both EBR-II and Fermi adopted near-MHAs for their DBAs. In a 1000-MWe LMFBR this approach could place a tremendous burden on the engineered safety features for many reasons, including the following:

1. The large plant contains much more fuel, which would increase the severity of a given accident.

2. The large plant uses plutonium instead of uranium fuel, and the much lower permissible leakage for plutonium would require better containment for the same accident.

3. The large plant has a positive sodium void reactivity coefficient in much of the core, unlike EBR-II and Fermi, so that given initiating conditions could lead to a more severe accident.

4. The oxide fuel in the large plant has a higher melting temperature than the metallic fuel in the early plants, which increases the possibility of a molten fuel-sodium vapor explosion.

5. The softer neutron energy spectrum of the large plant leads to calculational uncertainties because a wide range of energy must be considered, and cross sections (especially in the Doppler region) are not accurately known).

6. Competitive economic restraints on containment of the large plant may be more severe than those for EBR-II and Fermi.

Similarly, the limits of protection for the 1000-MWe LMFBR are not clearly defined. As discussed in section 4.1, functional requirements for protecting against local core accidents have not been defined, and the ability to protect against these local accidents has not been established. Furthermore, even as the limits of local accident protection become defined, they will initially be relatively uncertain.

Simply stated, the problem of DBA selection is uncertainty in identifying the candidate DBAs, in defining the consequences of the candidates, and in showing that the consequences can be handled by engineered safety features. These uncertainties exist largely because of the lack of established precedents and adequate methods of analysis.

3.3.1.3. Approach

Activity 222 in Phase II was responsible for the selection and analysis of the DBAs. In this activity we hoped first to establish a logical philosophy for DBA selection. This philosophy would be based on assessing the DBAs selected by other reactor designers,

considering the ability to protect against the initiating conditions discussed in section 2, estimating the consequences of potential accidents based on the accident analyses of Activity 221, and the feasibility of handling these accidents with the candidate engineered safety features identified in Activity 240.

Once the preliminary philosophy for DBA selection was defined, it was to be applied in selecting the DBAs from the accidents analyzed in Activity 221. Since a single accident might not determine all of the functional requirements for the engineered safety features (primary containment cover structure temperature and pressure versus time, etc.), the accident determining each requirement was to be identified by analysis. The work plan further provided for any necessary reconsideration and modification of the Activity 222 work in Phase III.

At the time the contract was cancelled, none of the results needed from other activities were available except the initiating condition analyses for whole-core accidents. A general survey of DBAs selected by other reactor designers was completed, and some progress was made toward establishing a DBA selection philosophy and reducing the number of candidate DBAs. The work that was completed is described below.

3.3.2. DBA Survey

3.3.2.1. Before 1960

In a literature survey we reviewed the DBAs chosen by other reactor designers. Bell and Culver published a 1960 survey of maximum accidents for some 21 reactors of diverse types.² The term "design basis accident" was not in use at that time; selected limiting accidents were either the MHA or the MCA (maximum credible accident). The authors found that "various reactor designers do not all have the same concept of the maximum credible accident." Even for a single reactor type, opinions on credibility differed. They also found "...an equal uncertainty by reactor designers as to the consequences of a given maximum credible or hypothetical accident." For example, some MCAs resulted in fuel failure, some did not.

Most of the pre-1960 designers defined their MCA as a loss of coolant resulting from a primary pipe rupture. Vessel rupture, although potentially more hazardous, was excluded, apparently because of confidence in the ASME code. Credit was generally taken for core cooling following the accident. However, most of the reactors surveyed had containment vessels capable of containing the radioactivity released from a complete core meltdown, with the containment design based on an MHA. None of the reactors surveyed are as large as most modern power plants; the largest was Fermi at 857 MWt.

3.3.2.2. Light Water Reactors

Since the 1960 survey the light water reactor (LWR) has emerged as the dominant type of power reactor in the United States. The LWR safety philosophy proceeded smoothly from the 1960 base, in that the double-ended primary pipe rupture with supplementary core cooling is the present DBA for LWRs.

Several factors contributed to the relatively straightforward evolution of this DBA since 1960. Most important, perhaps, is that the continued purchase of LWRs by the utilities lent urgency and realism to the need for agreement. The satisfactory safety record of early LWRs (including naval reactors) contributed to the licensing of additional plants, which in turn provided additional operating experience. As plant size increased, so did the quality of designs, analyses, and components. Competition among manufacturers contributed further to the development of a well-reasoned safety philosophy for LWRs.

3.3.2.3. Fast Reactors

The fast reactor safety philosophy in the United States has developed very differently than that for LWRs, partly because early experience was less trouble-free and partly because far fewer fast reactors have been built. EBR-II and Fermi both provided containment for a large-scale core meltdown, although it was considered incredible for both reactors. The prior major meltdown in the EBR-I core, technical uncertainties and the relative lack of economic pressure

undoubtedly provided sufficient reason for an MHA approach, but these two reactors provide the only significant LMFBR safety precedent.

During the 1960s American progress in LMFBRs was primarily limited to studies. In the early and mid-1960s major attention was devoted to two effects that would become significant only if a serious accident took place: secondary criticality and the positive sodium void coefficient. In 1967, 1000-MWe LMFBR Follow-On Study contracts were placed with five corporations. By the time of the 1968 Argonne conference,³ near-unanimous agreement was evident that cores should not be severely distorted or poisoned in an attempt to cope with these effects. The possibilities of fuel failure propagation and localized violent interactions between molten fuel and sodium coolant began to emerge as the largest safety problems.

The DBA philosophies and selections developed by the five contractors on the Follow-On Studies were varied and somewhat inconclusive. Atomics International⁴ planned to contain a primary flow coastdown without protection that resulted in 2200 MW-s available work energy, but they indicated a preference for an alternate approach emphasizing protection against accidents if research and development work could substantiate it. Babcock & Wilcox⁵ did not select a specific DBA but established the goal of a DBA involving some limited part of the core. Combustion Engineering⁶ designed the primary containment to withstand an available work energy of up to 1000 MW-s resulting from a hypothetical accident. General Electric⁷ offered a "conservative" approach, in which limits were placed on reactivity coefficients and containment was provided for an estimated 2000 MW-s of available work energy from a hypothetical accident. The preferred "advanced" approach, also offered by GE, took credit for protection and considered a minimal DBA involving seven fuel assemblies. Westinghouse⁸ identified two MHAs: (1) complete core voiding, meltdown, and secondary criticality; and (2) an open-vessel sodium fire. Either was assumed to provide secondary containment design bases of 30 psig and 500F. For future work, the emphasis was to be shifted from explosion containment to the prevention of fuel failure propagation. In general, one may conclude that the broad scope of the Follow-On Studies prevented the detailed accident analyses that would be necessary for a plant to be licensed;

however, there appears to be a definite tendency toward placing primary emphasis on protection rather than on the containment of severe hypothetical accidents.

The Fast Flux Test Facility (FFTF) will be the first large sodium-cooled fast reactor constructed in this country since Fermi; it will strongly influence the licensing of future LMFBRs. The FFTF approach to accident analysis has proceeded from highly conservative analyses⁹ to an increasing emphasis on more realism in defining accident conditions.

European countries have been quite active in the design and construction of prototype LMFBRs.¹⁰ Britain, France, and Germany profess emphasis on protection against credible accidents of limited magnitude, but all of the new prototypes rated at about 300 MWe will have containments designed for hypothetical accidents. The Russian approach has been variously reported as stressing the more probable accidents but with little emphasis on containment.

3.3.2.4. Recent Developments

With the sudden surge of LWR orders in the past few years, the regulatory agencies have been faced with the prospect of relatively large numbers of power reactors operating in well-populated areas. The result has been a closer scrutiny of reactor safety, although it is not yet clear whether any shift in safety criteria will result. Increasing opposition from public critics of nuclear power has further emphasized the need for careful estimates of accident risks.

In this regard, the "probabilistic" approach to reactor safety has become increasingly important. Perhaps first proposed by Siddall¹¹ in 1959, the concept has been strongly advocated by Farmer¹² and by Wolfe.¹³ This approach attacks the subjective aspects of licensing criteria and proposes that requirements be quantitative rather than qualitative. Ideally, one could define the probability of occurrence of a given accident, then multiply by the risk encountered if that accident occurred. The summed expected risk for all possible accidents could then be required to be below some specified limit.

In principle, this approach may be desirable, although it may not be practical because of the difficulty of obtaining

adequate data on component failure rates. Further, it removes none of the calculational uncertainties in the analysis of the accident consequences. Despite these problems, the approach offers the possibility for a consistent technique to realistically evaluate the effectiveness of the protection system and the engineered safeguards. It is likely that the LWR will lead the LMFBR in the adoption of any probabilistic analyses because of the greater data base. However, the LMFBR may gain a greater relative benefit due to the current difficulties in arriving at a consensus LMFBR safety philosophy.

3.3.3. Candidate Design Basis Accidents

At the time this study was terminated, no DBAs had been selected; however, three primary candidates had been defined. These were (1) the flow coastdown without protection, (2) partially protected reactivity accidents, and (3) a partially protected assembly flow blockage. It should be pointed out that if primary containment integrity can be ensured for a core-related DBA, then out-of-core accidents, such as large sodium fires, may be chosen to provide a partial design basis for the secondary containment.

3.3.3.1. Flow Coastdown Without Protection

This accident, although perhaps not the maximum hypothetical accident, certainly seems to be the most severe hypothetical accident resulting from a credible fault in the reactor system if protective action is not effected. However, although the initiating event of loss of power to the pumps may occur with some frequency, it is extremely unlikely that this will lead to a severe accident with B&W's reference design. Since the same electric power that drives the primary pumps is also required to hold the control rods out of the core, the individual control rods will scram immediately if the power to the pumps is lost.

Despite its hypothetical nature, the accident involving flow coastdown without protection received primary attention as a DBA candidate during the accident analysis activity. Although the initial core transient may be described with some confidence, there is considerable uncertainty involved in carrying the analysis through to

determine the system's response. The conservatism necessary to remove some of this uncertainty leads to results that imply that the integrity of the primary containment might be violated unless considerable effort and expense is taken to add additional protective features to the primary containment. On the other hand, with improved methods of analysis there remains the distinct possibility that even this severe hypothetical accident could be shown to be tolerable with further work.

3.3.3.2. Partially Protected Reactivity Accidents

Accidents in which credit is taken for some but not all of the protection are reasonable candidates for the DBA. This approach offers the definite advantage of providing incentive for redundant and diverse protection. A consistent and realistic hypothesis that can be made on this basis is a common-mode fault that disables the normal first level of protection. This, of course, represents a serious double-fault condition, since the accident must be initiated by an independent fault, and since the redundancy of the protection would prevent the defeat of a scram by a simple, single fault. Accordingly, the assumptions are more severe than those normally applied in analyzing protected accidents.

The most severe type of accident that can follow from this hypothesis is that of reactivity insertions at full power with the nuclear instruments disabled, since the only backup scram signals now provided in the B&W reference design are based on slow-responding thermal-hydraulic measurements. The results from analyses of rod ejection and withdrawal accidents with the first level of protection defeated are reported in section 2.2.2 of this report. Owing to the early termination of this work, the results are inconclusive. The single rod ejection at 30 in./s from full power, with the power range nuclear instruments disabled, appears to be a serious accident, although this initiating condition is incredible if not physically impossible. From these results, however, the four-shim-rod-withdrawal accident from full power, with the same instrument disability, would no doubt have resulted in as serious consequences from a more likely initiating condition. This accident would probably have caused fuel melting and perhaps fuel failures leading to violent fuel-coolant interactions. The present

methods of analysis are not capable of analyzing the subsequent events with any confidence; however, it is reasonable to expect that with adequate methods the accident could be shown to result in less available work energy than the flow coastdown without protection. However, it may prove undesirable for any partially protected event to lead to a serious accident. Therefore, had analyses shown the partially protected rod withdrawal to have serious consequences, in all probability the protection system would have been redesigned to reduce the severity of the accident. This could have been accomplished by providing a diverse, redundant instrument system, such as incore flux monitors. Such a system would provide adequate protection for the core.

3.3.3.3. Partially Protected Assembly Flow Blockage

A potentially serious, although perhaps incredible, initiating event is that of assembly inlet flow blockage. The normal first level of protection in this case would be the blocked assembly outlet thermocouple(s). If this protection is hypothesized to be lost through a common mode failure, then the consequences may be serious. However, it is extremely unlikely that the accident can in any way place the integrity of the primary containment in jeopardy.

Although serious consequences do not necessarily follow an undetected assembly blockage, it is difficult to show that a sequence of events leading through coolant boiling and fuel melting, to coolant re-entry on molten fuel and a resulting vapor explosion could not occur. However, two means of detection in the B&W reference will protect against subsequent propagation of damage or energy release. The assembly outlet thermocouples should detect any flow reduction in adjacent assemblies, and the power instruments should detect any significant reactivity effects.

At the time work was terminated in this activity, four primary questions remained to be answered before a flow blockage leading to a vapor explosion could be selected as a primary system DBA:

1. Can a single-assembly vapor explosion lead to significant reactivity effects so that further damage occurs before a scram is effected?

2. Is there any possibility that a local flow reduction in adjacent assemblies might not be detected?

3. If a scram is effected, is there any way to prevent control rods from getting into the core?

4. If no further damage occurs, what are the consequences of the single-assembly vapor explosion as far as determining functional requirements for the primary system components?

The maximum available work energy for the single-assembly vapor explosion is about 10 to 20 MW-s; thus, it is unlikely that any damage would be done other than in the immediate vicinity. Further, it appears likely that a scram could be effected before extensive propagation of damage. Therefore, it is felt that if these questions were answered, then this accident could be shown to be tolerable.

3.3.4. Evaluation and Conclusions

The three candidate DBAs described in section 3.3.3 appear to represent the best available choices at this time. The unprotected flow coastdown is quite severe, and its continued analysis would advance our understanding of the more severe disruptive accidents. The partially protected reactivity accidents represent a middle ground where the situation is quite well understood (at least to the point of major core damage), and where the penalty for a specifically postulated loss of protection can be determined relatively accurately. The partially protected local accident focuses attention on the major question of LMFBR safety—fuel failure propagation. Although analyses of local accidents might be the most uncertain of the three, perhaps the greatest understanding of any potential hazards of the LMFBR can be gained there.

It is our hope that removal of the uncertainties cited above can lead to an LMFBR DBA generally comparable to the accepted LWR DBA: no gross fuel damage or core disassembly and relatively light containment stresses. The primary obstacle appears to be the unresolved possibility that local fuel damage might propagate uncontrollably.

Additional research and development is needed to define credibility, sequence, and magnitude for a number of pertinent events.

Better methods are needed to describe severe accidents and their effects on engineered safety features. Perhaps most important, a general consensus must be reached on the requirements for the licensing of commercial LMFBRs.

3.3.5. References

- ¹ LMFBR Program Plan, Volume 1, WASH-1101, USAEC, Aug. 1968.
- ² C. G. Bell and H. N. Culver, "Comparison of Maximum Credible Accidents Postulated for U.S. Power Reactors," Nuclear Safety, 1, No. 4, June 1960, p 24.
- ³ Proceedings of International Conference on Sodium Technology and Large, Fast Reactor Design, Nov. 7-9, 1968, ANL-7520, Argonne National Laboratory.
- ⁴ ANL-7520, part II, p 203.
- ⁵ 1000-MWe LMFBR Follow-On Study, Task II and III, Final Report, BAW-1328, Babcock & Wilcox, Lynchburg, Va., Feb. 1969.
- ⁶ 1000-MWe LMFBR Follow-On Study, Task II and III Report, CEND-337, June 1968.
- ⁷ ANL-7520, part II, p 345.
- ⁸ 1000-MWe LMFBR Follow-On Study, Task II and III Report, WARD-2000-97, April 1969.
- ⁹ D. E. Simpson, et al., Preliminary Analysis of Postulated Maximum Accidents for the FFTF, BNWL-760, Battelle, Nov. 1968.
- ¹⁰ Report of Travel — United States LMFBR Plant Systems and Components Team, March 24-April 3, 1969, USAEC, DRDT.
- ¹¹ E. Siddall, "Statistical Analysis of Reactor Safety Standards," Nucleonics, 17, No. 2, Feb. 1959, p 64.
- ¹² F. R. Farmer, et al., An Appreciation of Fast Reactor Safety, Safeguards Division AHSB, United Kingdom Atomic Energy Authority, Risley, England (1970).
- ¹³ B. Wolfe, "Fast Reactor Safety," Nuclear Safety, 10, No. 1, Jan.-Feb. 1969, p 19.

4. CANDIDATE SAFETY FEATURES

Most of the safety features discussed in this chapter are restricted to the contents of the reactor building of B&W's 1000-MWe reference design. Although these features are specifically intended to protect the public from exposure to unacceptable levels of radiation, in many instances these features also serve to protect the plant investment and to promote operational reliability. These secondary benefits are the result of incorporating considerations that are common to good engineering practice when all of the conditions for safety are satisfied.

Generally speaking, the public can be protected by preventing the onset of an event that would be responsible for discharging contamination or, once the event is underway, confining the effects to a given volume, or by a combination of the preceding. In the reference design, protective safety features are applied to detecting initiating conditions in the core to prevent an accident, or once having detected an abnormality, utilizing this information to adjust power levels or to operate the engineered safety features.

The principal barriers to the disbursement of radioactive materials are the primary and secondary containment. The primary containment is designed to prevent the migration of radioactivity to unauthorized portions of the secondary containment during normal operation. In the event of an accident in the core, the incorporated passive safety features of the primary containment are intended to act to limit the effects to within its boundaries. These effects are generally manifested as either kinetic energy or potential energy (pressure). Should the primary containment fail to perform exactly as it was intended, the secondary containment would have to contain the overflow and thus eliminate any hazard to the public. Both passive and active safety features are incorporated in the secondary containment to ensure its performance.

In addition to the safety features that are related to both primary and secondary containments, a further consideration was separated from those above because of its unusual characteristics; this deals with preventing containment breaching by molten fuel. Systems dealing with this aspect are included in the emergency decay heat removal system.

The Phase II objectives for the safety feature functions described above were (1) to determine the abnormal conditions that could be expected within the plant, (2) to determine the features required to prevent or mitigate these accidents, (3) to delineate the functional requirements of these features, and (4) to define a set of safety features to be included in the reference design that are compatible with the normal and abnormal plant conditions.

Section 4.1 describes our efforts to define the protection required for credible accidents and discusses the selection of protective features and functional requirements. The accident analyses pertinent to protection were reported in sections 2 and 3.

Section 4.2 deals with treating core debris (resulting from a disassembled core) in such a manner as to prevent melt-through of the secondary containment. Initial core conditions are postulated and candidate features were developed and analyzed.

Section 4.3 covers the efforts expended on primary containment safety features and shows the emphasis placed on assessing the conditions created at the containment boundary by the potentially destructive elements of large core accidents.

For assumed accidents, the reactions of the primary tank and cover structure have been calculated, and an attempt was made to assess the effects of internal structures on the course of the accident and the final conditions.

Section 4.4 covers the safety features associated with the secondary containment. This section deals with the mechanisms that bring about the abnormal conditions that require features to prevent the compromise of containment integrity. The effects of sodium fires and plutonium release from the core are discussed.

4.1. Protection

4.1.1. General

4.1.1.1. Introduction

Protection must provide for the public safety by automatically preventing or suppressing conditions that could cause the acceptable fuel damage limits to be exceeded. The scope of protection is customarily limited to a set of accidents somewhat more severe than might reasonably be expected to occur, so that the primary emphasis is on credible rather than hypothetical accidents.

Protection may be passive, in the form of passive safety devices mainly intended to prevent accident initiation, or it may be active. The heart of active protection is the reactor protection system, which monitors plant conditions, compares these conditions with predetermined safety limits, and controls actuators as necessary to reduce core power and/or to activate supplementary cooling for fuel protection. The power is reduced by the reactivity control system, and the supplementary cooling is accomplished by the decay heat removal systems. Definition of the protection required for our 1000-MWe LMFBR requires the following:

1. Identification of the credible accidents for which protection must be provided.
2. Analytical descriptions of the accidents.
3. Identification of protective features that
(a) might cope with those accidents, and
(b) might reasonably be expected to be available for use by 1980.
4. Preliminary screening of candidate features by assessing their feasibility and effectiveness.
5. Evaluation of remaining candidate features by incorporating them in accident analyses.
6. Formulation of quantitative functional requirements for protection.
7. Assembly of candidate features into practical system and device designs.
8. Final evaluation of these designs using accident analyses.

During the 1000-MWe Follow-On Study, designs for protective systems and devices were specified. These designs were based primarily on engineering judgment and the results of a few pertinent accident analyses, which were performed especially in the area of accidents initiating within the reactor core.

In the Accident Analysis and Safety System Design Study, the opportunity was offered to retrace the ground briefly covered in the Follow-On Study with considerably more rigor. In addition to the larger safety effort permitted in the Accident Analysis and Safety System Design Study contract, we could take advantage of intervening progress in the state of the art of accident analyses. Whereas the design described in the Follow-On Study must be regarded as strictly conceptual, the Accident Analysis and Safety System Design Study was intended to lead to well-founded protective system and device designs. The intention was to prove that the traditional objectives of reactor protection can be attained in a 1000-MWe LMFBR.

The objectives of Phase II were to prepare a well-founded set of functional requirements and to identify a set of protective systems and devices with a high probability of meeting those requirements. Design refinements and proof of performance (via accident analyses) were then to be accomplished in Phase III.

To meet the objectives of Phase II, three work activities were defined. The first, Activity 241, was intended to identify candidate protective features. As an aid in this identification, this activity included preliminary estimation of required protective actions and functional requirements. The major effort in Activity 241, however, was devoted to a literature search for instruments used in other fast reactors or under development. This activity was completed and the work described in a topical report, BAW-1354.¹

Activity 231 was intended to establish functional requirements for protection. This work relates closely to the initiating condition and accident analysis activities described in sections 2 and 3. In effect, Activity 231 provided for liaison between design and analysis, with the further tasks of defining the range of credible accidents to be considered and establishing quantitative protection criteria. Activity 231 was near completion for most accidents, but we did not complete

the planned work in two areas: local core accidents and backup protection (diversity) for reactivity transients.

Activity 251 was intended to culminate the Phase II work by selecting a single set of protective systems and devices for detailed analysis and refinements in Phase III, using the results of Activities 241 and 231. No work was done on Activity 251.

4. 1. 1. 2. Approach

At the conclusion of the Follow-On Study it seemed likely that satisfactory first-level protection could be provided for whole-core accidents, whether reactivity insertion or flow reduction. The LMFBR appeared little different from PWRs with respect to these bulk accidents. Conversely, we realized that considerable work would be required for accidents that might initiate within the reactor core. At one end of the spectrum, it was not established whether protection must be provided for local accidents (it is not provided in PWRs). At the other end, it was not clear whether local accident protection, if needed, could satisfy the traditional protection requirement of preventing fuel damage. Hence, Activity 241, selection of candidate protective features, began with considerable confidence that time-proven methods could protect against bulk accidents, and with great uncertainty regarding the type of local accident protection that might be required. Thus, we tried to identify instruments that might help to protect against any type of local accident, not only at their inception but also at later stages.

Activity 241 included a review of the Follow-On Study designs for other protective features along with the literature search for instruments. In this activity we also attempted to spell out qualitative functional requirements for protection.

Activity 231 was intended to adapt initiating condition and accident analyses to the dual tasks of defining practical functional requirements and selecting candidate features that could meet the requirements. As bounding credible accidents of different types were defined, analyses quantitatively tested the effectiveness of different protective features. A prime example of this approach was the demonstration that flow guards and thermocouples can protect against single-assembly flow blockages; a practical set of quantitative parameters was established during the analyses.

As Activity 231 progressed through the evaluation of candidate protective features for all the various credible accidents, a set of functional requirements for protection would be generated. Simultaneously, effective features would be delineated. Activity 251 was then responsible for assembling these features into a consistent set of protective systems and devices. Problems caused by the system interrelations of these features would then be identified and resolved. Design details could be filled in, and a design with a high probability of success would be formulated for detailed analysis in Phase III.

4.1.2. Whole Core Accidents

Whole core accidents involve conditions where the core as a whole receives insufficient cooling relative to its heat generation rate. If not prevented or mitigated, such conditions can lead to fuel damage. They involve either coolant flow reductions (reduced heat removal) or reactivity insertions (increased heat generation).

Protection against whole core accidents traditionally comprises a power reduction to a level where sufficient cooling is provided by either the operating systems or the emergency decay heat removal systems (the latter serving a protective function). As anticipated in the Follow-On Study, the 1000-MWe LMFBR requirements for whole-core protection can be solved by the same means employed in PWRs.

Primary flow reductions can be sensed by flowmeters and pump power monitors; because of the safety margin designed into the reactor, the core outlet coolant temperature can initiate timely backup protection for credible flow-reduction accidents. Signals from these sensors can initiate control rod insertion to prevent fuel damage. The analyses supporting these conclusions are presented in section 2.2 and in the topical report, BAW-1350.²

Reactivity insertions and the resultant power increases are sensed by the out-of-core nuclear instruments, with protective power reduction again accomplished by control rod insertion. Section 2.3 and topical report BAW-1360³ present the analyses that demonstrate the ability to prevent fuel damage from credible reactivity insertions.

The analytical work on flow-reduction accidents further demonstrates that faults in the intermediate loops or the turbine island

do not endanger the core. Finally, although the evaluation of earthquake effects was beyond the scope of our work, it is presumed that protection against earthquakes can be afforded by seismograph scram, as for PWRs.

One may conclude that, for protection against whole core accidents, our 1000-MWe LMFBR design is at least as safe as a PWR. Major factors include the larger coolant margin to boiling, the temperature-stabilizing effect of the large mass of sodium in the primary tank, and the lower reactivity worth in a control rod. Conversely, credible accidents in the LMFBR do not proceed to the point where potentially detrimental differences such as positive coolant density reactivity coefficient, smaller delayed neutron fraction, and shorter neutron lifetime become significant.

Functional requirements for protection against whole core accidents have not been written. However, they could be prepared without further analysis or design effort from the Follow-On Study CSDD and the accident analyses cited above. The prevention of fuel damage by credible whole-core accidents is clearly a feasible objective.

Shortly before contract cancellation we began to examine the effects of a common-mode fault that disables the first level of protection for an accident. Protection systems are not now required to withstand such a serious fault condition, but DRL has recently requested PWR suppliers to analyze the consequences. Our work in this area progressed only to the point of permitting the following estimates: (1) for flow reduction accidents flow and temperature measurements appear to provide sufficient diversity of protection; (2) for reactivity transients it may be necessary to supplement the nuclear instruments with other fast-response protection (possibly incore neutron detectors) to prevent fuel damage. The PWR may have a safety advantage over the LMFBR in this respect because a relatively rapid pressure scram is available.

4.1.3. Local Accidents

With respect to protection, local accidents may be divided into two categories, depending on whether they influence an entire fuel assembly or a portion of an assembly. These two categories are discussed separately.

4. 1. 3. 1. Entire Fuel Assembly

Credibility considerations dictate that the principal local accident affecting an entire fuel assembly is an inlet flow blockage. The possibility of a wide span of initiating conditions, with speed ranging up to instantaneous and severity up to total blockage, cannot logically be excluded from consideration at this time.

Engineering judgment suggests that at least the most rapid and severe blockages should be rendered incredible by passive safety devices. Active protection should be required to consider only the blockages that can be handled by a practical set of protection-grade instruments. Finally, the capabilities of the active protection should comfortably overlap the range covered by the passive safety devices to dispel doubts that unprotected intermediate blockages could lead to credible fuel damage.

Temporarily adopting the requirements that single-assembly flow blockage leads to no fuel damage, we judged that redundant thermocouples at the outlets of the individual fuel assemblies are the only reasonable candidate protection instruments. As discussed in BAW-1354,¹ other candidate instruments appear to be impractical, lacking in sensitivity, not applicable to all fuel assemblies, too slow, and/or otherwise unsuitable.

BAW-1354 presents a conceptual design for a flow guard system that prevents foreign objects with dimensions larger than a fraction of an inch from blocking coolant flow to the fuel. Briefly, the concept employs multiple strained openings to a sub-inlet plenum from which the fuel assemblies receive flow through multiple inlets. It is estimated that detailed design and calculations would lead to the conclusion that passive safety devices could provide acceptable protection for sudden individual assembly flow reductions of more than about 60%.

TART runs described in section 2. 1. 4 show that for the peak power assembly, rapid flow reductions of 65% or less would not lead to coolant boiling for at least ten seconds. A hand calculation based on values presented in the Follow-On core CSDD, BAW-1328, Volume 2, approximately agrees with this result.⁴ Ten seconds

would provide abundant time for the outlet thermocouples to sense the increased outlet temperature and initiate protective action. (Note: The onset of sodium boiling is a conservative fuel failure threshold, since fuel or cladding failure would not necessarily ensue from limited boiling.)

Additional TART runs explored the limit of a temperature rate-of-change signal from the outlet thermocouples. It was concluded that a rapid flow reduction of 70% for the peak power assembly could be protected against by a system with the following parameters: thermocouple time constant (63% response to step input) of 5 seconds; scram setpoint of 30F/s; scanning frequency of eight times per second or less; scram logic delay of 25 ms; and a delay time of 150 ms for inserting control rods. These values are all considered attainable in practice. However, a major problem exists in this application of outlet thermocouples. In our Follow-On Study design the thermocouples must be located about 18 inches above the top of the blanket fuel. Although we allowed for the sodium transit delay time for this distance—250 ms at 30% flow—we did not account for signal dilution by such effects as transverse heat transfer or turbulence. These effects could perhaps be calculated, with difficulty, but one may assume that the results would be unfavorable. A redesign of the core outlet geometry, which is regarded as possible, would be necessary to maintain the ability to protect against rapid 70% reductions (and it could probably extend the active protection capability to near 75% flow reduction).

To summarize the protection capabilities for rapid flow blockage, passive protection may prevent flow reductions greater than about 60%, and active protection based on outlet thermocouples can handle flow reductions up to 70%. As discussed in BAW-1354,¹ protection against slow blockages up to 100% can be provided by the thermocouples with the aid of shutdown testing. However, the overall effectiveness of the thermocouples depends on redesign of the core outlet geometry. With this stipulation, it is concluded that protection can be provided to prevent fuel damage by credible single-assembly flow blockages. As discussed later, however, the need for such protection may be averted if we can show that significant blockages are inherently incredible.

We briefly considered the ramifications of providing backup protection instrumentation in the event of a common-mode fault of all the outlet thermocouples in a fuel assembly. In this case, fuel damage in the blocked assembly would seem inevitable. Further, the selection of a backup instrument would depend on detailed analyses of damage propagation, which have not been completed. One might speculate that the variety of possible propagation modes might require a corresponding variety of instruments, which could be very difficult to implement.

4. 1. 3. 2. Part of a Fuel Assembly

Our topical report covering the analysis of these accidents, BAW-1362,⁵ was still in preparation when the contract was cancelled. Accordingly, there was only a limited opportunity for assessing the protection aspects of accidents initiating within a fuel assembly. Only a few comments can be presented here.

During a 30-year lifetime, several hundred miles of fuel pin would be used in our 1000-MWe LMFBR. A complete absence of cladding failures, or a requirement to immediately shut down to replace minor failures, would be unbelievable. Some local pin failures must be tolerated. Further, the LMFBR would not be a viable power source if these minor failures inevitably propagated to cause extensive fuel damage.

BAW-1362 indicates that the relatively minor local faults considered in our analysis do not cause extensive damage and do not appear to be capable of propagation. As more severe faults are considered, the level of credibility decreases and the detectability increases. Although we have not been able to establish that all credible local faults could be protected against without providing specific protection instruments, that would appear to be a possible and highly desirable objective for further work. In this respect it may be particularly significant to recognize that protection is concerned only with credible accidents; containment will be provided for more severe but less likely accidents. Thus, either protection or containment could preserve the public safety for relatively unlikely local accidents, and the choice tends toward an economic tradeoff: the cost of protection versus the cost of contained accidents.

4. 1. 4. Candidate Features

Based on our work in the Follow-On Study and the Accident Analysis and Safety System Study, the following protective features appear most attractive. These features are described more fully in the Follow-On Study CSDDs, BAW-1328, Volumes 2 and 3,⁴ and in the topical report, BAW-1354.¹

4. 1. 4. 1. Instruments for Whole-Core Accidents

Out-of-core nuclear instruments, core differential pressure and bulk outlet temperature sensors, and pump power monitors are the most important.

4. 1. 4. 2. Instruments for Local Accidents

Individual assembly outlet thermocouples appear to be the most useful and practical. Numerous candidates described in BAW-1354 might have to be considered for applications where the thermocouples are unsuitable.

4. 1. 4. 3. Logic Circuits

Assuming that active protection for local accidents is required, digital logic appears necessary. Minicomputers appear well-suited for this application. If local accident protection is not required, digital or analog/relay logic would be satisfactory.

4. 1. 4. 4. Reference Reactivity Control System

The system described in the Follow-On Study, employing 25 control rods, is considered well-suited for protection. Although not necessary for safety, the scram-setback-rundown set of graduated protective actions has operational advantages.

4. 1. 4. 5. Alternate Shutdown System

We have identified no requirement for an alternate shutdown system that could not be better fulfilled by the reference system. Even if severe core misalignment prevented insertion of the reference system rods (and we have found no credible mechanism for that), it is likely that minor changes to the reference system design could ensure the insertion of adequate negative reactivity.

4. 1. 4. 6. Normal Decay Heat Removal

The Follow-On Study design included redundant NaK loops with air heat exchangers. Although this system appears satisfactory, some benefits could result from investigating natural (instead of forced) NaK circulation. The blowers in the NaK/air heat exchangers, however, could be eliminated only if very large exchangers were used. Rejection of decay heat through the intermediate loops and steam generators, combined with primary natural circulation, is a possible way to eliminate dependence on electrical power for emergency cooling.

4. 1. 4. 7. Passive Safety Devices

Apart from the normal features of the operating systems, the only passive safety device requiring evaluation is the flow guard system mentioned in section 4. 1. 3. 1. Possibly this system could be designed to prevent any rapid blockage that might lead to fuel damage.

4. 1. 5. Evaluations and Conclusions

4. 1. 5. 1. Whole-Core Accident Protection

With respect to accidents originating outside the reactor core, it appears quite clear that the fundamental objective of reactor protection can be met: conditions that could result in exceeding acceptable fuel damage limits can be automatically prevented or suppressed. Within the detail level of our Follow-On Study design, the credible accidents have been defined and analyzed, and practical protective features capable of handling the accidents have been identified. Many of the protective features require research and development work but have a high probability of success.

Detail problems would be expected to occur as the plant design proceeded past the Follow-On Study level. Similarly, refinement and analysis of the protective system designs would disclose problems. For example, we have paid little attention to sodium leak detection because no major LMFBR feasibility question is expected in that area and because a detailed plant design would be needed for detector definition. We do not believe that remaining problems of this sort cast fundamental doubt on the feasibility of the LMFBR.

Partially protected accidents combining a common-mode fault of the first level of protection with an accident-initiating event should be explored further. Even if the possible trend toward including such accidents in protection system design criteria does not materialize, such accidents are leading candidates for selection as design basis accidents. As noted previously, the LMFBR may have difficulty with the partially protected reactivity insertion unless incore neutron detectors are used for backup protection. The consequences of common-mode faults involving control rod insertion may also be significant.

4.1.5.2. Local Accident Protection

Unlike whole-core accidents, the situation regarding protection against local accidents is not at all clear. Thousands of instruments might give at least partial protection (limited fuel damage) against the conditions that have not yet been proved incredible. But the economics—capital cost and availability—of a reactor burdened with such protection could be unsatisfactory.

We have shown that flow guards and outlet thermocouples can probably prevent any fuel damage resulting from inlet flow blockage to a fuel assembly. If only 2-of-3 coincidence were used for these thermocouples, 864 of them would be needed (3×288). Thus, even minimal protection against local accidents might require protection far more complex than that in any existing central station reactor. It is hoped that extensive analysis of a more detailed plant design could prove, to the satisfaction of licensing and regulatory agencies, that credible flow blockages are restricted to a range that requires no active protection. If valid, this approach would also obviate the difficult task of providing backup protection.

Accidents initiating within a fuel assembly—initiation and propagation—present an even more serious problem. If such accidents are deemed credible and require protection, an awesome array of complex protective instruments might be needed. The basic research and development needed to establish the credibility (or, hopefully, incredibility) of these accidents is incomplete. For instance, we cannot state with certainty that credible local accidents will not lead to at least some fuel damage via a molten fuel/sodium vapor explosion.

It is in this area of local accidents that one cannot now identify the protection requirements of the large LMFBR. Pending further research and development work on local accidents, it is very difficult to estimate the effect of protection on the LMFBR concept. Certainly the LMFBR is now at a disadvantage with respect to light water reactors, where research and development work on essentially similar problems is proceeding in parallel with the construction and operation of large power reactors.

4.1.5.3. Conclusions

It appears feasible to protect the large LMFBR against the same accidents considered in the licensing evaluation of the protection of a large light water reactor. Uncertainties regarding the credibility of initiation and propagation of local core accidents prohibit assessment of this area of protection.

4.1.6. References

- ¹ R. C. Wilson, 1000-MWe LMFBR Accident Analysis and Safety System Design Study, Topical Report — Candidate Protective Features, BAW-1354, Babcock & Wilcox, Lynchburg, Va., June 1970.
- ² 1000-MWe LMFBR Accident Analysis and Safety System Design Study, Topical Report — Initiating Conditions, Part I, Flow Abnormalities, BAW-1350, Babcock & Wilcox, Lynchburg, Va., April 1970.
- ³ J. H. Scott, 1000-MWe LMFBR Accident Analysis and Safety System Design Study, Topical Report — Reactivity Insertions, BAW-1360, Babcock & Wilcox, Lynchburg, Va., October 1970.
- ⁴ 1000-MWe LMFBR Follow-On Study, Task II and III Final Report, BAW-1328, Vol 2, Babcock & Wilcox, Lynchburg, Va., March 1969.
- ⁵ R. W. Moore, et al., 1000-MWe LMFBR Accident Analysis and Safety System Design Study, Topical Report — Local Core Faults, BAW-1362, Babcock & Wilcox, Lynchburg, Va., February 1971.

4.2. Emergency Decay Heat Removal System (DHRS)

4.2.1. General

The general design criteria for the 1000-MWe reference design stipulate that systems and components essential to the prevention of accidents that could affect the public's health and safety or to the mitigation of their consequences shall be identified and then designed, fabricated, and erected to quality standards reflecting the importance of the safety function to be performed. Owing to the vital nature of these requirements, the reference concept must be examined more closely. Although the design of the post-DBA DHRS did incorporate some of the basic features known to be required at the time, subsequent considerations indicate that certain deficiencies still exist. Therefore, we have re-examined the reference design in an attempt to correct these deficiencies based on additional assumed requirements.

The primary area of interest in which the reference design was deficient includes the considerations given to meltdown accommodations for the fuel. Two pertinent questions may be asked: Can the core debris be cooled and contained indefinitely? Or will the decay heat cause it to melt through every containment and allow the release of radioactivity? A related problem is the possibility of secondary criticality as the core melts and slumps. Further examination of these problems should provide some insight into the provisions required to contain fuel melt.

The overall objective of Phase II was threefold: the first was to identify and develop emergency DHRS that could remove fission product decay heat from a portion or all of a disassembled core and transfer it to the ultimate sink, to the extent required for an initial screening of questionable systems (Activity 242). The second objective was to determine the functional requirements for the DHRS (Activity 232) based on the decay heat from the fission products and the stored energy resulting from an initiating accident (as analyzed in activity series 21X). The third objective was to select a reference system from the candidate systems (Activity 252) that survived the preliminary screening in conjunction with the functional requirements determined from the second objective.

The first objective was realized and the candidate systems that were identified and developed were presented in the topical report BAW-1351.¹ Preliminary calculations to meet the second objective led to inconclusive results owing to uncertainties in predicting the final disposition of the crumpled core following the initial excursion and disassembly. At this point, further work in this activity was deferred pending instructions from ANL on proposed modifications to the work scope. Hence, work still remains to be done on realizing the second objective and on the selection of a reference system from the candidate systems.

In achieving the first objective (Activity 242), the study was primarily directed toward defining an emergency DHRS that could remove decay heat from part or all of a disassembled core and transfer it to the ultimate sink. To perform this task, it was necessary (1) to establish a range of accident conditions as a primary step in the design of the emergency cooling system, (2) to establish major assumptions, and (3) to survey what others have done regarding the objective.

The accident conditions that were chosen as a basis for the design of the emergency cooling systems were (1) a hypothetical accident involving the meltdown of seven fuel assemblies and (2) an extreme case involving a total core meltdown. The following major assumptions were established:

1. An accident limited to seven fuel assemblies will not destroy the capabilities of the normal DHRS.
2. Meltdown accommodations for seven fuel assemblies must be provided inside the core vessel.
3. Meltdown accommodations for more than seven fuel assemblies will be provided outside the primary tank.
4. Under conditions requiring use of the outside meltdown accommodations, primary sodium will be present.
5. Pans or catchers made from an appropriate material will be used to accommodate molten fuel outside the primary tank.

Based on these assumptions, the possibility of containing molten fuel was examined along with various ways of cooling it. This investigation

covered (a) meltdown pan features and requirements to determine the possibility of containing molten fuel from a hypothetical seven-fuel-assembly meltdown inside the core vessel, and (b) a sodium-cooled pan located outside the primary tank to accommodate the entire molten core as a last-ditch cooling system. Based on the premises above, candidate emergency DHRs were developed.

In achieving the second objective (Activity 232), the program of work required the assumption of two failure modes: simple meltdown and sodium vapor explosion. The case of a simple meltdown was investigated under Activity 242. Hence, in this activity, the main effort involved scoping investigations on ways to contain and cool the molten fuel resulting from sodium vapor explosions. This involved qualitative and quantitative analyses of the following:

1. Cooling requirements for damaged and re-located core materials.
2. Size and geometry of debris.
3. Assessment of available modes of cooling.
4. Identification of potential configurations that may result in secondary criticality.

4.2.2. Post-DBA Cooling Survey

The emergency DHRs of fast reactors (EBR-II, Fermi, PFR, and the proposed LMFBRs) were surveyed to determine new requirements, ideas, and design features that might be applicable. In general, there is little information available. As an example, none of the older fast reactor designs (EBR-II, Fermi, etc.) incorporate post-DBA cooling systems based on removing heat from large quantities of molten fuel. Among the later designs, the PFR has provisions for the use of sea water as a last-ditch means of cooling the primary tank complex. Some of the 1000-MWe studies mention cooling coils attached to the cavity liner surrounding the reactor vessel. One conceptual design had several layers of natural-convection-cooled meltdown trays below the core support structure. Details of how or why these systems are expected to work were not available. The FFTF has considered the effect of a blast on the reactor vessel, and some work has been done on meltdown, but it has not been decided whether an external cooling system is required.

4.2.3. Seven-Fuel-Assembly Meltdown

A meltdown of seven fuel assemblies was selected as the design criterion for the meltdown pan inside the core vessel. The selection was arbitrary but seemed reasonable on these bases: (1) if not more than seven fuel assemblies melted, then the normal DHRS probably would not be damaged; (2) the removal of heat from the seven-fuel-assembly accident did not appear to be formidable; and (3) if more than seven fuel assemblies melted, then the whole core would probably be endangered. Based on this reasoning, it was assumed that for a hypothetical meltdown and seven fuel assemblies, the normal decay heat removal capability will not be incapacitated; therefore, accommodations will be provided inside the core vessel.

The possibility of containing molten fuel resulting from a hypothetical meltdown of seven fuel assemblies was examined along with various ways of cooling the fuel. This investigation involved exploratory heat transfer calculations using simplified models to determine meltdown pan features and requirements.

A steady-state analysis was made as a preliminary step in the investigation. The core material was assumed to be collected in the pan in a spread-out configuration, forming a layer of debris on top of the steel pan. Conservative assumptions and conditions were also made for the decay heat generation rate and the initial temperatures of the materials involved. Various heat sink and heat transfer mechanisms were considered. The results of this effort indicated that the heat absorbed by the heat sinks (i.e., conduction downward through the pan and radiation at the top of the debris) was greater than the decay heat generated by the fuel assemblies for a decay time of one hour after shutdown. Therefore, it was concluded that core debris up to seven fuel assemblies would not melt through the bottom of the pan.

A transient-temperature analysis was also made using the TIGER heat transfer computer code. In addition to the assumptions made for the steady-state analysis, it was assumed that the pony motors were in operation and were capable of providing 5% of the total flow rate. This investigation indicated that after 3 hours, the temperature profile of the pan was about 920F and just starting to arrive at steady-state

conditions. The peak temperature in the steel pan was about 1600F. Thus, it appeared that forced-convection cooling with 5% of the total flow rate was sufficient to preclude melt-through. The details of the model setup and the results are reported in BAW-1351.¹

A more sophisticated transient analysis was performed (subsequent to that reported in BAW-1351) using the LION code,² which is an expanded version of TIGER. This was done to determine the effects of using different surface heat transfer mechanisms, namely, natural convection, forced convection, and radiation. The surface heat transfer coefficients were also allowed to vary as a function of ΔT , fluid flow rate, and time, where applicable (the TIGER code is not capable of performing this function). In no case did melt-through occur.

4.2.4. Total Core Meltdown

For accidents resulting in a meltdown of more than seven fuel assemblies, it would be difficult to ensure that heat removal equipment inside the primary tank would survive the disturbance. Hence, the meltdown and cooling accommodations for the total core have been placed outside the primary tank as a last-ditch cooling system.

The design approach was to use natural circulation of the primary sodium to cool the molten fuel. The conceptual layout of the post-DBA primary DHRS with the external meltdown pan and primary emergency coolant loop is shown in Figure 4-1. The concept provided a sodium-cooled pan with a large heat transfer area below the primary tank. The meltdown pan had sufficient thickness and thermal conductance to prevent melt-through as long as the subcritical accumulation was surrounded by the sodium coolant.

An analysis was performed using an approach similar to that for the seven-fuel-assembly meltdown transient case. This study was useful in obtaining a better understanding of the magnitude of the potential melt-through problem and in defining ways to prevent melt-through or cooling of the molten fuel debris from a total core meltdown. The simplified, one-dimensional model consisted of three slabs. The lower layer represented the meltdown pan 40 feet in diameter and 1 foot thick, the middle layer represented the molten fuel of uniform thickness,

and the upper layer represented the molten structural debris. The model was set up so that an analysis could be made to determine the effects of varying the decay time, the surface heat transfer mechanisms, and the heat transfer coefficients of the upper and lower surfaces.

A series of heat transfer conditions was analyzed using the TIGER computer code. Heat transfer coefficients at the upper and lower surfaces were varied for each case. In two cases, the core debris was cooled by natural convection of sodium, depending on the ΔT , while transferring heat to an isothermal sink. Other cases included the effect of radiation by assigning cooling coefficients to simulate the heat transfer mechanism desired, and varying the decay time at which significant quantities of core material could reach the pan.

The results of the calculations are given in detail in BAW-1351. For all cases, the results showed that natural convection cooling at the external surfaces is a very effective way to retard or prevent melt-through. Even with low heat transfer coefficients and delayed natural convection cooling, it seemed likely that the pan would not fail.

4.2.5. Candidate Features

In the reference Follow-On design, the emergency DHRS removes decay heat after a DBA. A review of the reference design indicated that apparent deficiencies exist in the system, and some improvements could be obtained by alternate arrangements. Some of the deficiencies associated with the design are as follows:

1. There is no provision, except perhaps the reactor pit liner, for protecting the coils after a DBA.
2. The system has added complications because the normal and post-DBA cooling are combined.
3. There is no post-DBA cooling under the reactor.
4. The entire system cannot be operated periodically to ensure its operability when the need arises.
5. There is no insulation between the reactor vessel and the concrete shielding. Therefore, a considerable amount of heat must be removed to cool the concrete during normal operation.

To arrive at candidate systems that rectified these deficiencies and considered more desirable arrangements, a number of candidate functional requirements were identified. The system combinations that resulted are shown in Figure 4-2. The coordinate post-DBA system shown was divided into these three subsystems:

1. Primary DHR subsystem—covered features that were needed to remove heat from the disassembled core and transfer it to the primary sodium.
2. Secondary DHR subsystem—covered features that were needed to remove heat from the primary sodium and transfer it to the secondary heat transfer fluid.
3. Heat sink subsystem—covered features considered necessary to transfer heat to the final heat sink.

The investigation of the alternate systems covered the following areas:

1. Natural circulation of sodium within the primary tank.
2. Volume of the primary decay heat loop to ensure that the core is not uncovered in case of a breach in the primary tank.
3. Specific locations of heat sinks external to the primary tank.
4. Heat exchanger concepts to strive for reliability and low estimated capital cost.
5. Heat transfer fluids that are compatible with sodium and the heat sink fluid.
6. Heat rejection loops using natural circulation, forced flow, or a combination of both.
7. Systems operating as a standby unit or removing heat continuously from the biological shield under normal conditions.
8. Insulation requirements for the primary tank cavity during normal operating conditions.

In the Na-NaK-air concept, the results indicated that natural circulation of the NaK in the secondary subsystem may be feasible but that forced air flow may be essential to avoid excessively large NaK-to-air heat exchangers. In the second concept, (Na-organic coolant-water), biphenyl ($C_{12}H_{10}$) was considered as a possible alternate in

lieu of NaK because its physical and chemical properties appeared to be suitable as a heat transfer agent for cooling the sodium and transferring the heat to cooling water. It is possible that this system could be used to cool the biological shield during normal operation.

4.2.6. Core Meltdown Based on Flow Coastdown Accident

In the preceding investigations, the functional requirements for the emergency DHRs were assumed in lieu of a DBA to arrive at candidate systems and features. Under this activity, we attempted to establish the functional requirements in conjunction with the accident analysis study. The initiating accident was based on a flow coastdown with no protection.

Since it is not now possible to prove or disprove with certainty the mechanism of failure of the core, the second mode of failure (as discussed in section 4.2.1.3) was assumed for this investigation. Compared to a simple meltdown, this mode is more likely to occur for big accidents, and the situation is more complicated since the disposition of the crumpled core is unknown. Such an explosion might tend to scatter molten core materials to different regions of the primary tank, where localized melt-through could occur. An examination of the reference design revealed the following potential locations:

1. Within the original core envelope.
2. On top of the upper radial neutron shield.
3. On top of the shield tank transition plate and lower radial neutron shield.
4. In the rotating fuel storage drums (if the shield tank transition plate has been breached or melted through with the fuel debris).
5. In the meltdown pan inside the core vessel.
6. On the bottom head of the core vessel (if fuel melts through the meltdown pan).
7. On the bottom head of the primary tank (if fuel melts through all containment inside the primary tank).

Based on present knowledge, it is difficult to predict where the debris will be located. Therefore, the following assumptions

were made for the quantity and distribution of the fuel debris following the coastdown accident:

1. Forty percent of the core was expelled upward. This seemed to be a reasonable assumption to begin with since (a) the results from the MARS run indicated that 80% of the core was either molten or vapor, with pressure ranging from 1 to 258 atm; and (b) it would be unlikely to assume that all 80% of the molten core was expelled upward only.

2. The molten fuel debris collected on the shield tank transition plate and on top of the lower neutron shields. This assumption is justified since it is not likely that molten fuel would collect on top of the upper neutron shield.

3. The other 40% of the molten core melted through the grid plate and fell into the meltdown pan inside the core vessel.

4.2.6.1. Shield Tank Transition Plate Melt-Through Study

A transient heat transfer analysis was performed on the shield tank transition plate with a molten fuel debris layer above it to determine the possibility of melt-through. This particular location was studied in detail because subsequent melting of the plate would allow molten fuel to fall into the fuel storage drum and could cause secondary criticality. (See Figure 4-3, which follows section 4.3.)

The melt-through model consisted of two slabs with the molten fuel layer on top of a 1-inch stainless steel plate. The model did not account for any core structural steel debris. Natural convection cooling was provided at the top and bottom surfaces using variable heat transfer coefficients as a function of ΔT . Conservative assumptions were then made as follows:

1. Fuel debris about 1.08 inches thick was used based on the volume of 107 fuel assemblies (40% of the core) spread over the entire cross-sectional area of the shield tank. This is probably the maximum thickness of the fuel debris over the plate, since some of the fuel would fall down through the voided core and into the meltdown pan.

2. Heat flow was in the vertical direction only. Since we are concerned only with the condition of the plate, a one-dimensional flow assumption would represent the worst situation for the plate.

The transient calculations were performed using the LION computer code, which is capable of varying thermal properties as a function of temperature and can extensively vary surface heat coefficients as a function of ΔT , fluid flow rate, or time, whichever is desired.

The transient results showed that the plate was well cooled and that no melt-through occurred. The maximum temperature in the steel close to the fuel interface was 2000F, which is below the melting temperature of the steel. Equilibrium was achieved in approximately 3 hours.

To determine the effects of varying the thickness of the fuel debris, it was assumed that all 80% of the molten core was expelled upward and accumulated at the plate. A similar run was made with LION using a thickness of 2.17 inches. The results indicated that no melt-through occurred (up to 3 hours), since the plate was well cooled at the bottom. However, the calculated temperatures in the fuel debris layer exceeded the boiling point (~6200F).

4.2.6.2. Meltdown Pan Melt-Through Study

Two major points were considered in this analysis: How much of the molten core will find its way into the pan? How long can the pan contain the molten core? Regarding the first point, a potential hazard resulting in secondary criticality could exist if a large pileup of core material collected in the pan. For the coastdown accident, it is estimated that from 40 to 60% of the core may fall into the pan.

For second point, the sequence of events is not understood as well. The time required to melt through the pan is highly dependent on the model chosen for core meltdown and grid plate failure. In analyzing the transient temperature response of the pan, a simple model of three layers was set up:

1. The bottom layer represented the steel meltdown pan, 2 inches thick and 12.75 feet in diameter, with natural convection cooling of sodium at the bottom.

2. The middle layer represented the molten fuel debris (~60% of the core) with internal generation of decay heat.
3. The top layer represented the structural steel debris, consisting of cladding materials and the grid plate, with natural-convection cooling of sodium at the top.

Using the LION computer code,² the transient temperature results showed that violent boiling occurred in the fuel debris; this led to many uncertainties about core behavior during this period. After one hour, one-third of the pan had reached temperatures above the melting point. It is reasonable to expect the pan to lose its structural integrity as it gets thinner because of the heavy weight of the fuel and structures. Granting that the pan could melt through, it can be seen that the core vessel would be subjected to the same hazardous conditions as would the meltdown pan.

4.2.6.3. Criticality Investigation

Of the locations examined, the fuel storage drums and the meltdown pan inside the core vessel had the greatest potential for reassembly accidents. For the fuel storage drums, an axial FARAD calculation was made using a very conservative set of assumed conditions that gave a maximum fissile enrichment and minimum neutron leakage. The results indicated that a critical assembly could exist if extreme amounts of molten fuel were added to a fully loaded storage drum containing only fresh zone-3 assemblies. A few possible solutions for avoiding secondary criticality are as follows:

1. Apply administrative restrictions to the location of highly enriched assemblies within the storage drum.
2. Use wider spacing between assemblies in the storage drums.
3. Increase the poison and metal fraction within the storage drum.

For the meltdown pan, secondary criticality could also be achieved if a large pileup of core material collected in the pan. A critical height of 4.78 inches was calculated—it produced a k_{eff} of 1. Since 60% of the

core material would produce a height greater than 4.78 inches in the pan, secondary criticality could be achieved.

4.2.7. Evaluations and Conclusions

Five problem areas were considered to be major at the outset of this activity:

1. Defining the transport mechanism of core debris from its initial location to its final destination.
2. Determining the core debris configuration at the location where it is to be cooled,
3. Determining the mechanisms and systems for cooling core debris for seven fuel assemblies and for a whole core.
4. Developing the systems for rejecting heat to the ultimate sink.
5. Determining the functional requirements for the emergency DHRS based on a flow coast-down accident.

Although it was recognized that items 1 and 2 are of major significance to the development of satisfactory candidate systems, these points could only be treated lightly because of a lack of quantitative information. The majority of the effort for this phase was focused on items 3, 4, and 5.

For item 3 it can be reasonably concluded that the mechanisms and systems required for cooling the core debris depend on the character and magnitude of the accident. Thus, for the seven-fuel-assembly meltdown, it appeared that melt-through could be precluded if sufficient flow (approximately 55% of the total) is provided by the pony motors on the primary pumps. For the total core meltdown, natural convection cooling provided at the upper surface of the debris and the bottom surface of the pan appeared to be very effective in preventing melt-through. Although the models used in the transient analyses were simplified, the calculations were conservative in that the assumptions employed were chosen to represent the worst situation that might be expected.

In item 4, candidate systems for postaccident decay heat removal were identified and developed based on the total core meltdown. Some of the important features for rejecting the heat to the ultimate sink are summarized as follows:

1. Circulation by natural convection in both the PDHRS and the SDHRS.
2. Separation of the post-DBA system from the normal DHRS.
3. Relocation of the heat exchange equipment to remove the heat from the primary systems to better protect them from core disturbances.
4. Incorporation of redundant and offset primary coolant channels to the heat exchangers located within the biological shields to ensure that blocking or shearing will not interfere with heat removal.
5. Combination of the post-DBA heat removal system with a normal operating heat removal function (shield cooling) to ensure availability.
6. Incorporation of bottom cooling for the primary tank to prevent breaching of the secondary containment by core debris.

In problem area 5, we attempted to establish the conditions to determine the functional requirements based on a flow coast-down accident. The transient heat transfer analyses indicated that the problem of whether or not the core debris could be cooled adequately and contained was very sensitive to the amount and the geometry of the debris and to the available modes of cooling. Hence, further work on cooling and containment problems would be of little value until more detailed information is available on the meltdown and behavior of the core. The following conclusions can be drawn:

1. The shield tank transition plate melt-through study proved that no melt-through occurred even for the worst conditions assumed. For debris depths greater than 1 inch, the extremely high temperatures calculated in the debris layer indicated that the debris could vaporize.
2. The meltdown pan melt-through study led to inconclusive results owing to uncertainties in describing boiling phenomena and liquid-phase transfer processes in the fuel debris layer.
3. The criticality calculations indicated that a potential danger could exist in the meltdown pan and the fuel storage drums, depending on the amount of core debris accumulating at those locations.

In order to complete the work on problem area 5, the following recommendations are made:

1. More information is needed on the mechanism of failure of the core, core redistribution, and the size and geometry of the relocated core.
2. Better thermophysical properties of the materials involved are needed over the temperature range experienced.
3. Thermally induced natural convection must be considered in all liquid-phase transfer processes (molten fuel debris layer) where adverse temperature gradients could occur, i.e., where the upper surface is cooler than the interior.
4. Some design adjustments to the storage drum and meltdown pan may be required to prevent secondary criticality from occurring for large accidents.

4.2.8. References

- ¹ 1000-MWe LMFBR Accident Analysis and Safety System Design Study, Topical Report — Candidate Emergency Heat Removal Systems, BAW-1351, Babcock & Wilcox, Lynchburg, Va., May 1970.
- ² J. R. Schmid, et al., LION — Temperature Distributions for Arbitrary Shape and Complicated Boundary Conditions, KAPL-M-6532, General Electric Co., Knolls Atomic Power Laboratory, July 1966.

Figure 4-1. Emergency Decay Heat Removal Concept for Total Core Meltdown

Figure 4-2. Candidate Systems and Possible Alternates for Emergency Decay Heat Removal

4.3. Primary Containment

4.3.1. Introduction

The principal function of the containment system for the 1000-MWe LMFBR is to contain all radioactive materials, including plutonium, that are of sufficient quantity to compromise the health and safety of the public and the operator. In B&W's conceptual design^{1,2} this objective is met by providing a double containment system in which the primary containment is emphasized and the secondary containment functions as the ultimate consequence-limiting barrier. As designed, therefore, the primary containment must contain the primary reactor system during all normal, abnormal, and credible accident conditions without permitting significant leakage to the secondary containment, and, under DBA conditions, it must limit leakage to levels that the secondary containment can retain safely.

In studying the primary containment and its safety features, an understanding of the system's response to core accidents must be established. The accidents considered may generate four possibly destructive elements: (1) shock waves, (2) fluid momentum, (3) a vapor bubble, and (4) a residual overpressure condition. Depending on the magnitude and course of the accident, each of these elements may have the potential to cause damage to the primary containment that leads to excessive leakage. Part of the objective of this work was to determine which elements are important and to identify and determine the effect of safety features that could be used to reduce the consequences in terms of damage to the system.

The primary containment study comprised two major parts: first, the accident conditions had to be established, and second, the response of the system to these conditions had to be evaluated. There are no completely satisfactory methods available now to accomplish either of these objectives, although several groups involved in the LMFBR effort are making progress. Therefore, conservatism has to be used in place of absolute knowledge or methods.

The problem of establishing the accident conditions is aggravated by the unpredictable way in which very high energy-density fuel can interact with the surrounding sodium coolant. For example,

very rapid fuel fragmentation and energy transfer to the sodium could create severe shock waves. On the other hand, a relatively slow interaction could concentrate more energy into a high-pressure vapor bubble. The molten fuel-coolant interaction phenomenon is still not understood adequately, and final determination of the accident conditions is contingent on the development of an accurate model.

The primary containment response to accident conditions is better understood, although difficult analysis problems were also encountered here. Several methods are available for treating portions of the problem, but two-dimensional calculational methods are still required to follow the course of the accidents accurately and account for energy-dissipation in internal structures. Again, progress is being made in the development of these methods but, in the interim judicious use of the available methods can provide fair approximations of the system response.

B&W's conceptual design for the primary containment, as presented in references 1 and 2, forms the basis for this study. The system is shown in Figure 4-3, and complete conceptual descriptions are given in the referenced reports. The major components of this containment are the reactor cover structure and the steel liner on the biological shield. The study has concentrated on determining potential damage to these components and on determining the effect of internal structures, including the reactor vessel. The major study objectives are outlined in section 4.3.2 along with a summary of the study approach. Brief descriptions of the accident conditions that were derived from a literature survey and served as a basis for the analytical work are given in section 4.3.3.

4.3.2. Study Objectives and Approach

The overall objective of the Phase II study of the primary containment was to determine the functional requirements and to select the structural and safety features required to maintain the integrity of the containment under the conditions created by potential core accidents. In order to attain this objective, several interim objectives has to be met:

1. Describe the accident conditions in enough detail so that subsequent events and conditions could be determined by further analysis.
2. Determine by analysis the effect of reference core internal structures on the course of the accident and the revisions that could be made to these structures in order to mitigate damage to the primary containment.
3. Determine by analysis the conditions at the primary tank and the cover structure and the reactions of these reference components to these conditions.
4. Determine the allowable reactions of the primary tank and cover structure and, by comparing these with the reaction results, determine whether alternate or additional safety features are required.
5. Identify alternate or additional candidate features required to fulfill or better fulfill requirements.

The first efforts in the Phase II portion of the study of primary containment included a study of potential accident conditions and a study to determine the capability of the reference design safety features to meet projected requirements. The possible destructive elements of accidents were identified. Emphasis was given to a study of the shock wave phenomenon, and underwater explosion-type calculations were employed to analyze the effects of shock waves. The results were published, along with suggested candidate safety features to alleviate the conditions of accidents, in a topical report.³

It became apparent that the method of equating the energy evolved in an accident to an equivalent TNT detonation and evaluating the system response to such an explosion was not necessarily proper because a large portion of the energy in a TNT explosion would be manifested as a shock wave and, in the reference system (which contains a large pool of sodium) much of this energy would be dissipated through the waste-heat process during passage of the shock wave through the sodium. This process would probably only raise the bulk sodium temperature a few degrees while giving acceptable or manageable conditions at the primary containment boundaries. The fact that these TNT explosions concentrate much of the accident energy in the shock wave also

reduces the energy in other potentially destructive elements of the accident.

Large core accidents probably do not behave like a TNT explosion. Many situations arise in which the energy deposited in the fuel could be converted to mechanical work energy by vapor expansion or sodium mixing and expansion much more slowly than in the TNT-type explosions. In an attempt to reflect this situation, the REXCO-H computer program⁴ was obtained from ANL and put into service. Since this program has a two-dimensional hydrodynamics calculation capability, it was possible to describe the system geometry and initial fluid conditions resulting from a power transient and subsequently observe the system's response and behavior. This method was limited, however, by being restricted to small material movements owing to the mathematical formulations involved. It did serve to provide a realistic way to treat the shock wave phenomena.

Perhaps the most potentially severe problem investigated was the sodium slug impact problem. The core was assumed to melt and mix with the available sodium, forming a high-pressure region which served to accelerate the sodium above it upward. Since this sodium slug would impact on the underside of the cover structure, imparting a considerable impulse to it, it was necessary to evaluate this impact problem to ensure the integrity of the cover structure. The BANGO computer program⁵ was available for use to calculate the slug velocity in one dimension, but it assumed completely rigid structures. The ASPRIN computer program⁶ was obtained and used extensively to evaluate the vapor region expansion and the slug velocity and impact in a quasi-two-dimensional manner. This program had geometric limitations and required an independent calculation of the mechanical work potential of the accident by means of an isothermal expansion of the core-sodium mixture. The method was not entirely satisfactory since the expansion of the vapor to obtain the mechanical work ability cannot realistically be done independently of structural and geometric considerations. An effort was made, however, to demonstrate the conservatism of the ASPRIN calculations. BANGO was used to evaluate the proper work potential of the accident, and REXCO-H was designated for use to

provide a true two-dimensional treatment of the sodium slug-cover structure impact problem.

4.3.3. Accident Conditions

Large accidents have been postulated for the LMFBR primarily because of these design characteristics:

1. The fuel in the core is not arranged in the most reactive configuration; therefore, inadvertent movement of the fuel can lead to an increase in reactivity. For example, bowing of fuel due to thermal effects may cause a power excursion resulting in overheating of the coolant, molten fuel in the pins, and cladding failures.

2. The void coefficient of reactivity in sodium is positive, at least in the central region of the core. Loss of effective coolant flow due to pump failures or mechanical blockage of the coolant flow channels may, therefore, lead to overheating and, again, molten fuel or cladding failures.

3. Molten fuel may be dispersed into the sodium in the coolant channels during an excursion; depending on how the fuel comes into contact with the sodium, a rapid energy release may result.

Resultant accidents in the 1000-MWe LMFBR core may have the potential for creating four possibly destructive elements: shock waves, fluid momentum, a high-pressure vapor bubble, and a residual overpressure condition. The magnitude and the destructive potential of each element depends on the total accident energy released, on the rate of release, and on the partition of energy. Each element, as it is used as a basis for the primary containment study, is described briefly in the following paragraphs:

4.3.3.1. Shock Waves

If explosive accidents are considered credible in the reference LMFBR, or if such accidents are defined as DBAs, then the first resultant disturbance to be considered in assessing the reactions of the primary containment is the shock wave effect. As the result of the explosive process, the initial mass of material will be converted into a gas at a high temperature and pressure. The pressure

wave in the reacting explosive material arrives at the boundary of the surrounding medium, and the pressure immediately begins to be relieved by an intense pressure wave and outward motion of the medium. Once initiated, the disturbance is propagated radially outward at supersonic velocity as a steep-fronted wave of compression described as the "shock wave." As the wave expands radially outward, its energy is rapidly dissipated. Beyond the close-in effects, there follows a range of pressures in which the shock wave is sufficient to vaporize the material behind the shock to a gas. Below the pressure that causes this irreversible vaporization, the absorption of energy in solids or liquids through the "waste heat" process is very rapid. This rapid decay in pressure continues until it falls to the dynamic crushing strength of the material. At this pressure, the waste heat mechanism becomes small, the wave becomes elastic, and pressure decays roughly as $1/R$, where R is the distance from the center of the blast.

In addition to the waste heat process and the pressure decay in the large volume of sodium, material density changes also may have a considerable mitigating effect on the shock wave pressure. In the case of a gas space, an enormous drop in pressure will occur as the shock wave enters the rarified medium. Several-hundred-fold reduction in pressure is possible because of the acoustic mismatch.

4.3.3.2. Fluid Momentum

Due to the shock wave's high peak pressure, the wave is capable of accelerating the sodium coolant and components or fragments of structural materials submerged in the primary coolant toward the boundaries of the primary containment. Both the fluid momentum and the possible missiles must be considered as possible destructive mechanisms.

In the radial direction, the peak pressure in the shock wave at the primary tank wall could be so high that there is little hope of containing the explosion within the tank. In other words, in large explosions the tank will probably fail because of the shock wave unless much of the original energy is dissipated in internal structures designed for that purpose. If instantaneous failure of the primary tank occurs, then the radial biological shield liner and the concrete shield

will be subjected to the kinetic energy of an impacting mass. This impacting mass could be a composite mixture of primary coolant and steel (from the tank's wall) which is accelerated across the annulus between the primary tank and the liner with an initial velocity equal to the particle velocity of the primary coolant. This composite mass will produce a dynamic pressure on impact against the liner and, unless the kinetic energy is absorbed by intervening structures or crushable materials, could damage the liner or the biological shield severely enough to prevent their functioning.

In the vertical direction, another pressure loading against the bottom surface of the cover structure will follow the pressure increase in the cover gas caused by the shock wave. This loading is caused by the sodium spray thrown upward from the primary coolant-cover gas interface. (One effect of the shock wave striking the free surface of the primary coolant is the production of a reflected expansion—tension—wave.) Therefore, the upward velocity of the sodium particle is the sum of the upward velocities of the sodium behind the incident and reflected waves. It should be noted that the velocity of the sodium particles at the free surface varies as a function of their radial distance from a vertical centerline through the center of the explosion. This causes a variation in the time that the sodium spray impacts against the bottom surface of the cover structure.

In the reference design, all of the core components, reactor internal components, and their respective supporting structures are submerged in the primary coolant. The shock wave's high peak pressure is capable of accelerating these components or fragments of the structural materials toward the boundary of the primary containment. The destructive potential of these possible missiles depends on several factors: size, shape, orientation, and location. In calculating missile damage to the primary containment, each of these factors must be considered along with the effects of gravity and deceleration in the coolant.

4.3.3.3. Vapor Bubble Expansion

Any accident that leads to severe overpower conditions in the reactor core will cause an accumulated energy deposition within the fuel. If the fuel reaches high enough temperatures, then

molten and even vaporized fuel will result. The fuel vapor itself will form a high-pressure region that may expand and do work on the system. Also, it is possible (and indeed probable) that high-pressure sodium vapor will result from mixing and the transfer of heat energy from the fuel to any remaining sodium in the vicinity. In any event, this mass of gaseous products is formed in a high-temperature, high-pressure gas bubble occupying an initial volume approximately equal to the volume of core materials destroyed during the accident's initiating transient period. Due to the high temperature and pressure within the bubble, the bubble expands very rapidly. As this expansion occurs, large amounts of primary coolant are being accelerated from the regions adjacent to the bubble's boundary.

In studying the effects on primary containment, the primary coolant being accelerated from the regions adjacent to the bubble and upward toward the bottom of the reactor cover structure is of utmost importance. The reason is that if the amount of kinetic energy associated with the primary coolant moving upward in the form of a sodium slug is large, then the slug could inflict extensive structural damage on the cover structure upon impact, unless adequate safety features are provided. Depending on the energy, the impact could also or alternately force radioactive sodium through seals, create missiles of cover-structure-installed components, or even lift the complete cover structure.

In considering the expansion of the vapor bubble and the acceleration of the sodium slug, the effect of radial constraints and intervening structures becomes important. As the surrounding sodium is accelerated in all directions, core support structures will resist the growth, and energy may be used or vented by deformation or failure of the structures. On the other hand, strong resistance by the structures may redirect more energy to accelerate the sodium slug.

In expanding upward, the vapor bubble expansion and the acceleration of the sodium slug will be affected by structures between the core and the cover structure. Provided these structures are not destroyed by earlier accident consequences, such as the shock wave, they may break up the bubble or divert the slug. Such a result could be very important in relieving the final impact conditions on the cover structure.

4. 3. 3. 4. Residual Overpressure

For core accidents that result in a high-temperature, high-pressure vapor bubble, the expansion of this bubble will increase the normal pressure in the primary containment after the dynamic conditions have subsided. The amount of pressure increase depends on the amount of leakage that occurs during the accident and on the resultant damage to the primary containment that will permit leakage afterwards.

In considering this overpressure problem, the design of the containment system and the consequences of the dynamic accident conditions become extremely important. For example, failure of the primary tank in the reference design will provide a large additional volume to relieve the pressure. In addition, it may be possible to incorporate safety relief valves to vent pressure into other safe areas.

4. 3. 4. Reactor Cover Structure Analysis

The gas-tight reference cover structure is a stainless steel, cylinder with a depth of 10 feet and a maximum diameter of 59 feet. The assembly sets on and seals to the top flange of the primary tank and extends radially outward beyond it. Holddown studs anchor the assembly to the concrete of the radial biological shield outboard of the sealing surfaces. The main body of the cover structure extends down into the primary tank opening for approximately 4 feet and forms an interface with the cover gas over the primary sodium. The cover structure contains 6 feet of serpentine aggregate with an 8-inch covering layer of steel shot for shielding.

For normal reactor operating conditions, the cover structure and its associated seals must limit the leakage of fission gases from the primary tank to the environment of the secondary containment. In combination with the holddown features and internal structures, they must also withstand the dynamic and post-accident conditions created by the DBA(s) without permitting excessive leakage or damage that could lead to a second hazardous condition.

Relative vertical motion between sections of the cover structure or between the cover structure and the primary tank sealing surfaces has been judged to be the major accident consequence that

could lead to the release of radioactive material to the secondary containment. Only two of the possible destructive elements of large core accidents appear to be capable of causing such relative motion: a sodium slug impacting on the bottom surface of the cover structure, and the residual pressure remaining in the containment after the dynamic conditions have subsided. Of these, the problems of relieving a final pressure that is capable of lifting the cover structure are small compared with the problems of dissipating the energy in a sodium slug. Therefore, we have emphasized the study of the sodium slug.

In order to determine the allowable limit for cover structure motion, the bending stresses in the intermediate and decay heat removal systems' piping were calculated as a function of cover structure movement. Based on a maximum allowable stress of 6250 psi, movements exceeding 4-5/8 inches could lead to rupture of this piping. Since this rupture could result in a severe sodium fire in the air environment of the reference secondary containment, the preliminary maximum cover structure motion was set at the 4-5/8 inches. It was recognized that the piping design could be altered to permit more movement and that localized inert gas protection could be improved to minimize the fire hazard.

The ASPRIN computer program⁶ was the principal tool used to investigate the sodium slug impact problem. No particular vapor bubble composition was assumed to start this investigation; rather, an arbitrary work potential was input. The vapor bubble was assumed to form in the central region of the core with an internal pressure of 600 atmospheres and a mechanical work potential in the range of 500 to 2000 MW-s when expanded isothermally to one atmosphere. Two reactor conditions were assumed to exist when the vapor bubble formed, and the analysis was executed for each. The first condition assumed that the primary coolant was at its normal level (18 inches below the cover structure), and the second assumed that a flow coast down had occurred and that the primary coolant was at the normal shutdown level (8 feet 3 inches below the cover structure).

The first series of ASPRIN scoping computer runs was made to determine the vertical movement of the core internal structures and the cover structure resulting from an assumed accident with 1000

MW-s of work energy. Based on the reference design, it was shown that the core vessel and its supporting structure would probably be driven downward more than 2 feet and that the core vessel would actually impact on the primary tank. Similarly, the calculations indicated that the cover structure would be lifted more than the allowable limit established (4-5/8 inches).

To reduce the amount of downward movement of the core internal structures, it was necessary to simulate the six tension support members in the ASPRIN code using various thicknesses of the flow divider wall as input. The code then calculated the elongation occurring in the support members as if it were elongation of the flow divider wall. The results of the analysis permitted us to adjust the size of the support members so that downward movement of the core vessel was acceptable.

To assess the requirements for cover structure holddown to reduce vertical movement to the established limit, a preliminary holddown stud design was conceived. Using the 1000-MW-s accident and ASPRIN computer runs for both accident cases (normal operating sodium level and the flow coastdown sodium level) the number, the diameter, and the working length of the studs were varied until acceptable cover structure movements were determined. From this analysis, 100 studs having a working length of 5 feet and a diameter of 6-1/4 inches were shown to limit the movement of the cover structure to approximately 4 inches for the first case (normal operating sodium level). For the case of the flow coastdown sodium level, 126 studs having a working length of 5 feet and a diameter of 8-1/4 inches would limit the movement to approximately 3.1 inches.

To assess the effect of accident size on the extent of cover structure movement, additional ASPRIN calculations were performed with the other accident work energies in the selected range as input. The sodium level used was the flow coastdown level (8 feet 3 inches below the cover structure), and the slug mass (33,505 slugs) used is based on the slug being the size of the primary tank. The tank's reference thickness of 1-1/2 inches was also input, and the 126 holddown studs 8-1/4 inches in diameter were used. The results are tabulated as follows:

Accident energy, MW-s	Na velocity at time of impact, ft/s	Na slug kinetic energy, MW-s (ft-lb $\times 10^8$)	Vertical movement of cover structure, in.
500	92.29	193.5 (1.43)	1.9
1000	123.47	346.37 (2.55)	3.1
1500	147.44	494.67 (3.65)	3.9
2000	165.53	627.48 (4.63)	4.6

These results show all cover structure movements within the allowable limit and indicate that the cover structure could be restrained using the conceptual stud design, provided the actual accident-caused vapor bubble approximates the bubble assumed here. It should be noted that radial restraint or relief cannot be directly accounted for in the ASPRIN program and the above movements are based only on the input as noted.

An effort was made to establish the conservatism of the ASPRIN calculations and to relate the assumed work energy to the core accident. Although this work was not completed, some preliminary results were obtained.

A calculation was performed using the REXCO-H computer program;⁴ a sodium slug having a uniform axial velocity was allowed to impact on the cover structure. The two-dimensional aspect of the problem was accounted for, and the impacting sodium was allowed to move radially. This resulted in a much smaller impulse delivered to the cover structure than was calculated using the radially restricted ASPRIN model. The total cover structure motion was not obtained, however, because of the excessive computer time requirement and the necessary mesh renormalization required by REXCO-H when gross movements are involved. The calculation did tend to verify the conservatism of the ASPRIN cover structure motion calculations.

The BANGO program⁵ was used to calculate the accident work energy. BANGO contains a mixing model for fuel and sodium and considers one-dimensional slug motion during expansion of the fuel-sodium two-phase mixture. Heat transfer from the fuel particles to the sodium is considered, as well as condensation heat transfer from the bubble. The work done on the sodium slug and on the argon cover

gas can be obtained. For a mixture of 30% of the core with a fuel temperature of 5000F and a sodium temperature of 1000F, the resulting work energy obtained due to expansion of the mixture down to system pressure was 452 MW-s. The bubble was then expanded isothermally down to 1 atmosphere, and an additional work energy of 662 MW-s was found. Thus, the total work energy possible, neglecting work done on structures, was 1114 MW-s. The slug's kinetic energy at impact was 300 MW-s, whereas ASPRIN predicted 461 MW-s for the same work potential accident.

BANGO was used as above to find the work energy potential of bubbles composed of mixtures of fuel and sodium. In each case, the fuel temperature was assumed to be 5000F and the sodium temperature 1000F. The mixture always contained a weight ratio of fuel to sodium the same as that of the undisturbed core, which is about 10.5. The fuel and sodium were allowed to remain at thermal equilibrium during expansion, and no credit was taken for condensation heat transfer from the mixture. The results were as follows:

<u>Portion of core mixed, %</u>	<u>Work potential, MW-s</u>
30	1114
60	2312
90	3341

Thus, the assumed range of work potentials in the ASPRIN calculations from 500 to 2000 MW-s might correspond realistically to accidents involving up to about 50% of the core. Obviously, further work would be needed to verify these results.

4.3.5. Influence of Internal Structures

The reference internal structures that are of primary interest to the study of primary containment are the primary tank, the tension support members, the flow divider tank, and the lower section of the shield tank. With the exception of the flow divider tank, these are shown and indicated in Figure 4-3; the flow divider tank can be

discerned as an upper extension of the core vessel, parallel and just inboard to the lower section of the shield tank. Each of these components serves a specific function during normal operation, in addition to having an influence on the amount of primary containment damage that can result from an accident.

During normal reactor operation the six tubular tension support members support and position the reactor core, the grid plate, the internals support bridge, the core vessel, the upper and lower radial neutron shields, and their shield tanks. As a result of an accident, forces will be exerted downward on the grid plate, causing elongation in the tension support members. An important limitation on this elongation is that the core vessel must not impact on the primary tank.

Using the ASPRIN program, a series of scoping runs was made to determine the necessary diameter and wall thickness of the tension support members to limit elongation. The vapor bubble assumed had an internal pressure of 600 atmospheres and a mechanical work potential of 1000-MW-s. The limit on downward movement was established as 2 feet, based on the reference clearance between the core vessel and the primary tank. It was necessary to simulate the six tension support members in the ASPRIN code using various flow divider wall thicknesses as input. The code then calculated the elongation of the support members as if it were elongation in the flow divider wall. The results indicated that six support members with a diameter of 30 inches and a wall thickness of 4-5/8 inches would limit downward movement to less than 1-1/2 feet. Due to the contract cancellation, the elongation caused by the 1500- and 2000-MW-s work energy accidents were not investigated. It should be noted that, as the tension support members are strengthened to limit elongation, more of the accident energy will be directed to accelerate a sodium slug. In addition, the large amount of downward movement, seemingly acceptable above, also causes downward core movement. If the core is intact to the point that safety rod position is important, this motion could create another safety problem. Neither of the latter problems has been investigated in detail.

To determine the effect of radially restraining vapor bubble expansion on the properties of the sodium slug at the time of

impact with the cover structure, the thickness of the flow divider tank was varied and the resultant conditions calculated by ASPRIN. Four thicknesses were used: 0.030, 0.125, 1.50, and 4.25 inches. The 1.50-inch thickness is the reference design. The ASPRIN results showed that, as the thickness is increased, radial expansion of the bubble is inhibited and more of the accident energy is directed to accelerate the sodium slug. For the 1000 MW-s accident case, using the normal sodium operating level (18 inches below the cover structure) and the 100 holddown studs 6-1/4 inches in diameter, ASPRIN calculations gave the following slug properties and cover structure movements:

Wall thickness, in.	Sodium slug at time of impact		Vertical movement of cover structure, in.
	Velocity, ft/s	Kinetic energy, MW-s	
0.030	71.1	17.03	0.131
0.125	117.89	50.26	0.577
1.50	151.2	78.22	2.53
4.25	175.4	103.5	3.81

Again, these results show all cover structure movements to be within the allowable limit. As calculated by ASPRIN, the results also clearly show the effect of varying the thickness of the radial structures. The results given here and those given previously (section 4.3.4), for cases where the accident energy was varied, indicate that the cover structure could be held by means of the conceptual stud design, provided the actual accident-caused vapor bubble approximates the bubble assumed here. However, the results also highlight the importance of design features that must be established before final calculations to determine cover structure holddown can be performed.

To check the effect of multiple radial intervening structures on the primary tank's reaction to an accident, a method for calculating sequential failures was employed. This was necessary because the ASPRIN program will not handle sequential failures and, due to their geometric configuration and their location in the reference design, failure of both the flow divider and the shield tanks is required before the vapor bubble pressure can act against the primary tank wall.

Using the tank thicknesses given in the reference design and an initial vapor bubble pressure of 600 atmospheres with 1000-MW-s of potential work energy, a series of ASPRIN program runs was made. With the flow divider tank input as the vessel, the program was run until the tank failed, and then the work energy used in failing the tank was subtracted from the original 1000 MW-s. The pressure in the bubble at the time step just before failure of the flow divider tank and the new energy were then input again, and the program was run until failure of the shield tank occurred. Again the energy and pressure were adjusted and the program run to calculate the response of the primary tank.

The results showed that the primary tank expanded from 52.25 feet to 53.9 feet in diameter without rupturing and that primary tank impact on the biological shield liner would not occur. Comparing this result with a similar ASPRIN calculation in which the radial resistance of intervening structures to vapor bubble expansion was not considered (see section 4.3.6) shows that expansion of the primary tank is noticeably reduced. Further studies to examine the effects of various tank thicknesses and energy levels were not completed, but this single result again clearly indicates the importance of the design of internal structures. At this time, use of ASPRIN for the analysis above and the accuracy of the results have not been evaluated, but two general trends appear when this result and the results of the wall thickness investigation are interpreted:

1. Weak radial structures will permit a high-pressure vapor bubble to expand radially and, while leading to more primary tank expansion, their failure will relieve pressure and thus reduce the amount of energy available to accelerate a sodium slug.
2. Strong radial structures will resist the radial expansion of a vapor bubble, and more of the accident energy will be available to accelerate a sodium slug. Primary tank expansion will be reduced, however.

4.3.6. Biological Shield Liner Analysis

The steel liner on the biological shield forms a major portion of the reference design's primary containment boundary. It is

fabricated of 3/4-inch-thick carbon steel and forms a sodium-leak-tight boundary between the primary tank cavity and the biological shield concrete. As shown in the arrangement of the reference design (Figure 4-3), the primary tank is suspended inboard from the liner leaving an approximate one-foot gap. This gas annulus permits the use of a circulating argon gas system, which reduces the heat load in the biological concrete and allows sufficient space for periodic inspection of the exterior surface of the primary tank. In addition, the volume of the annulus is sized to prevent uncovering of the reactor core in the event of a major primary tank failure. Of greatest importance to this study is the mitigating effect this gas will have on the shock waves generated by core accidents and the availability of the annulus for the installation of energy barriers (if they are required to reduce liner damage caused by any of the accident conditions).

Section 4.3.3.1 includes a discussion of the shock wave phenomenon, and the effect of the gas annulus between the primary tank and the biological shield liner on shock waves has been analyzed. This work is covered extensively in reference 3. The major conclusion drawn was that failure of the biological shield liner due to shock waves generated in any of the accidents considered is not anticipated in the reference design. This conclusion was based on the results of TNT-explosion calculations (considered very conservative for this analysis), which showed that the combined effects of the large pool of sodium and the gas annulus drastically reduced the energy in the shock waves.

This discussion emphasizes the study of radial vapor bubble expansion and the possible impact of the primary tank on the biological shield liner. As a base accident case, it was assumed that an accident had occurred in the central region of the core, forming a vapor bubble with an internal pressure of 600 atmospheres and having a work energy potential equal to 1000 MW-s when expanded isothermally to one atmosphere. The ASPRIN computer program was used extensively.

To determine the structural movement of the primary tank, two conditions were assumed for the reactor internals. First, it was assumed that a shock wave had extensively damaged internal structures, such as the flow divider and radial neutron shield tanks,

while doing no damage to the primary tank. Thus, the internal structures provide no structural resistance to the expansion of the vapor bubble. Second, it was assumed that the shock wave did not damage these structures and that they are capable of resisting the expansion of the vapor bubble. In the radial direction, for example, this means that the flow divider and the shield tanks must fail before the vapor bubble pressure acts against the primary tank wall.

Using the first assumed reactor internal condition, the ASPRIN results showed that the primary tank did not rupture, although the wall and bottom head did expand to impact against the biological shield liner. In order to estimate the effectiveness of energy-absorbing material to absorb energy and reduce the impact force, graphs were prepared from data obtained in these first calculations to show internal pressure on the primary tank wall, the tank's radial and downward motion, its velocity and its kinetic energy as a function of time.

For subsequent evaluations, a minimum gap of 5.75 inches between the primary tank and the energy-absorbing material was selected to provide an adequate cooling annulus during normal reactor operation. This allows for the installation of 4-3/4 inches of material and permits the reference primary tank to expand from an original 52.25 feet outside diameter to 53.2 feet before impacting with the energy-absorbing material. From the base accident case results, the time of impact is 10.3 ms, the velocity of the tank wall is about 56 fps, and the kinetic energy associated with the rapid radial expansion at the time of impact is about 12.8×10^7 ft-lb. A further assumption was made that the force would be applied uniformly to the energy-absorbing material, and that the surface area of the material would only be 44,450 square feet, which corresponds to the surface area of a 20-foot length of the primary tank.

Following a literature search^{7, 8} and a series of scoping calculations, an energy-absorbing material with a crushing strength of 600 psi or 86,400 lb/ft² was selected. The crushing strength of its impact area was calculated to be approximately 38.4×10^8 lb. To absorb the kinetic energy of the primary tank wall (12.8×10^7 ft-lb), this material will deform permanently to a depth of approximately 0.40 inch or

about 8% of its original thickness. Based on our investigation of energy-absorbing materials that could be used in this application, the allowable crushing will be up to 75% of the original thickness, or 3.56 inches. Thus, the selected energy absorber is still capable of resisting the pressure that remains in the primary tank after the kinetic energy has been absorbed.

Using the same set of assumed reactor conditions, calculations similar to those above have also been completed for accidents with assumed work energy releases of 500, 1500 and 2000 MW-s. The results, tabulated below, show that even without considering energy absorption by intervening structures, the energy-absorbing material of the type and thickness investigated can be used successfully to dissipate radial the kinetic energy of the primary tank.

Accident energy, MW-s	Radial velocity of primary tank wall at time of impact w/energy absorber, fps	Primary tank kinetic energy at time of impact, ft-lb	Amount of energy absorber crushed, in.
500	35.26	5.06×10^7	0.16
1000	56.175	12.8×10^7	0.40
1500	70.535	20.25×10^8	0.63
2000	79.68	25.84×10^8	0.81

To evaluate the effects of the downward movement of the primary tank's bottom head and its impact into a crushable energy absorber, the gap of 5.75 inches between the primary tank and the material was again selected. This allows for the installation of 8-3/4 inches of material and gives a uniform radial and axial cooling annulus for normal operation. Again based on the base accident case results, the time of impact, the tank's velocity, and its kinetic energy were established.

Following a series of scoping calculations, an energy absorber with a crushing strength of 900 psi or 12.96×10^4 lb/ft² was selected, and the crushing strength of its impact area was calculated to be approximately 6.0×10^8 lb. To absorb the kinetic energy of the primary tank's bottom head (1.58×10^8 ft-lb), this material will deform

permanently to a depth of approximately 3.31 inches or about 38% of its original thickness. Again, this is within the allowable 75%, and the selected energy absorber is still capable of resisting the remaining pressure in the primary tank.

The results of subsequent calculations for accidents with assumed work energies of 500, 1500, and 2000 MW-s are tabulated below. It is shown that energy-absorbing material of the type and thickness investigated can be used to dissipate the kinetic energy of the primary tank's bottom head. In the 2000-MW-s case, crushing extends to 5.4 inches or about 62% of the original thickness. The maximum permissible crushing in this case is 6.56 inches.

Accident energy, MW-s	Primary tank bottom head at time of impact with energy absorber		Amount of energy absorber crushed, in.
	Axial velocity, ft/s	Kinetic energy, ft-lb $\times 10^7$	
500	77.97	8.85	1.77
1000	105.06	16.06	3.21
1500	121.24	21.39	4.27
2000	136.24	27.0	5.4

4.3.7. Candidate Safety Features

As previously discussed, accidents in the 1000-MWe LMFBR core may have the potential to create four possibly destructive elements: (1) the shock wave, (2) the fluid momentum due to the passage of the shock wave, (3) the gas bubble pushing on the fluid at high temperature and pressure, and (4) the final pressure in the containment after the dynamic conditions have subsided. Each of these elements has been investigated in an effort to identify the safety features that are necessary to maintain the integrity of the primary containment. The ultimate purpose is to protect against leakage from the primary containment that could subsequently endanger the plant operators or the general public.

4.3.7.1. Shock Waves Protection

Based on the results of our scoping calculations and on an evaluation of the literature on the shock wave phenomenon, we have concluded that three features, already provided in the reference design, are the primary candidate methods to dissipate shock wave energy. These features are (1) the large volume of sodium in the primary tank, (2) the gas space between the primary tank and the biological shield liner, and (3) the cover gas space between the sodium pool and the cover structure. Our work has shown that, because of the mitigating effect of these features, shock waves are not likely to cause major damage to either the shield liner or the cover structure.

If larger core accidents or shock wave energies are subsequently calculated, some additional protection for the biological shield liner may become necessary. Two candidate methods have been considered: energy-absorbing material close to the core and energy-absorbing material in the annular space between the primary tank and the liner. The second of these appears to be the best solution because of design, inspection, and economic considerations. It is possible that any requirement for shock wave protection will be automatically fulfilled by the application of energy-absorbing material to dissipate the energy of primary tank impact on the shield liner.

4.3.7.2. Fluid Momentum

Using the methods of Monson and Stuyter⁷ and Cole,⁹ the velocities of shock-wave-accelerated fluid and missiles were calculated to assess damage to the primary containment. Based on these results and on information from the literature survey on missile protection in similar designs, missile protection does not appear to be required for the reference B&W design. However, since there is a small probability that missiles and combined loads could endanger the shield liner, it is suggested that positive protection could be provided by adding a missile barrier in the annular space between the primary tank and the liner. This could be the same baffle design suggested to deflect the impacting mass³ or the energy absorber, if that is required. No additional protection for the cover structure is considered necessary.

4.3.7.3. Sodium Slug Protection

As discussed in section 4.3.3.3, a core accident may produce a high-pressure vapor bubble that will expand and accelerate a volume of sodium toward the cover structure. With sufficient kinetic energy, this sodium will impact against the bottom surface of the cover structure (sodium slug); based on our evaluation, this is potentially the most dangerous element of large core accidents.

With regard to candidate safety features, the following discussion is based on the conservative results of the ASPRIN work and the requirement that the cover structure movement must be limited so that the secondary sodium piping will not rupture. In our reference design this limit is 4-5/8 inches; it may be increased by attention to the piping design but there is an incentive to minimize the movements because of the increased chance of seal leakage and the possible problems caused by the cover structure reseating.

To limit the cover structure movements, the holddown stud system specified in the reference design and adjusted in this study appears to be adequate for accidents with work energies up to 1000 MW-s. This limit may be increased when the effect of intervening structures is taken into account, but our evaluation indicates the slug impact should be minimized if possible. For this reason, further analysis is recommended, and the use of one or a combination of the following features should be considered:

1. A core holddown design that will absorb energy and split the vapor bubble or the forming sodium slug.
2. A shaped slug diverter placed under the cover structure to divert the slug over the shield tank wall into the cover gas space.
3. Energy-absorber material placed in the pool of sodium in the path of the slug to prevent the slug from striking the more vulnerable center section of the cover structure.
4. Energy-absorber material on the bottom surface of the cover structure.

4.3.7.4. Residual Overpressure

If the reactor cover structure is retained in place against the dynamic accident conditions, and limited movement has been shown to be a requirement in this study, then an overpressure condition could be created in the primary containment (see section 4.3.3.4). The magnitude of the residual pressure is, of course, dependent on the accident and on the amount of leakage that would occur during its course. No major study effort was devoted to determining these values because of the contract cancellation, but the reference design features were reviewed in an attempt to determine the possible problems.

In the reference design, the design pressure of the primary tank is 30 psig and the design pressure of the reactor cover structure is 100 psig. One reason for these choices was to ensure that the primary tank would fail before the cover structure failed. Our study has shown that the cover structure can be retained against accident effects that would either rupture the primary tank (shock waves) or expand the tank against the biological shield (vapor bubble expansion). In either case the additional volume in the gas space around the primary tank would reduce the final pressure in the containment by almost 30%. Therefore, the philosophy used in selecting the design pressures for the primary tank and cover structure has been confirmed to be good.

In addition to the gas volume between the primary tank and the shield, approximately 3000 ft³ of volume is also available in the radioactive gas waste disposal system. Small vent lines, with pressure-regulating valves set at the normal gas pressure (5 inches of water), connect these tanks to the primary containment. A resultant 36% reduction in final pressure is possible if this volume is still accessible after the accident. It was also important, therefore, to consider this feature in more detail in later studies.

4.3.8. Evaluation of Study Results

The first objective of this study of the LMFBR primary containment was to describe accident conditions in sufficient detail so that subsequent events and conditions could be determined by further analysis. We found that there are no completely satisfactory methods

available to accomplish this objective successfully. Especially important is the requirement for a good description of the molten fuel-coolant interaction phenomenon, so that an accurate partition of energy can be made.

Because of resultant deficiencies in accident descriptions, it was necessary to assume accident conditions in order to attempt to analyze the primary containment's response and to determine the effect of internal structures. This area of study is better understood, although difficult problems were encountered here also. Several methods are available to treat portions of the problem, but none is completely satisfactory for application to the B&W reference pot-type system.

To evaluate the effects of shock waves, accident energies in equivalent pounds of TNT were assumed. Underwater explosion calculational methods were used, and no major requirements for safety features were found to be necessary in the B&W design. We feel that the methods used to calculate shock waves effects are conservative, and that it is unrealistic to assume that such a large portion of accident energy is manifested as a shock wave.

To evaluate the other possibly destructive elements of large accidents, our major tool was the ASPRIN computer program. By careful selection of input data, ASPRIN was used to assess the problems created by the sodium slug impacting on the cover structure. These results appear to give some assurance that holddown of the cover structure can be successfully accomplished against accidents in the range of 1000 MW-s. By judicious use of ASPRIN we were also able to study the effect of radial core structure failure on the slug impact energy. The accuracy of these results is questionable, but they clearly indicated that weak radial structures will reduce the slug energy and minimize the slug impact problem. Careful study of these structures, in combination with the vapor bubble expansion study, is definitely necessary in case large accidents and a major sodium slug are a problem.

In addition to using the ASPRIN program, the two-dimensional REXCO-H program was used to study the radial relief of the sodium slug as it hits the cover structure. Due to the excessive computer time required, it was only possible to show that a much smaller slug impact was delivered to the cover structure when radial relief was

accounted for, but this result is very important. The calculation also tends to verify the conservatism of the ASPRIN cover structure movement calculations.

The BANGO computer program was used to calculate the accident work energy. For various mixtures of fuel and sodium, the work energy potential of vapor bubbles was calculated and the slug kinetic energy at impact with the cover structure was determined. These results also tend to confirm the conservatism of the ASPRIN calculations.

4.3.9. References

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- ⁶ G. L. Fox, ASPRIN — A Computer Code for Predicting Reactor Vessel Response to Hypothetical Maximum Accidents on Fast Reactors, BNWL-1166, Battelle.
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- ⁸ Evaluation and Design of the Machinery Dome for the Enrico Fermi Atomic Power Plant, APDA-303, Atomic Power Development Associates.
- ⁹ R. H. Cole, Underwater Explosions, Princeton University Press, Princeton, N.J. (1948).

Figure 4-3. Section Showing Primary Containment

LEGEND

- 1 Biological shield
- 2 Mechanical jack
- 3 Reactor cover structure
- 4 Tension support member
- 5 Rotating storage drum drive mechanism
- 6 Radial neutron shield assembly (upper)
- 7 Refueling port
- 8 Radial neutron shield assembly (lower)
- 9 Grid plate
- 10 Control rod drive assembly (25 total)
- 11 Shield tank
- 13 Core holddown assembly
- 14 Core
- 15 Core vessel
- 16 Meltdown pan
- 17 Internals support bridge
- 18 Rotating storage drum
- 19 Primary tank
- 20 Biological shield liner

4.4. Secondary Containment Support Systems

4.4.1. General

The secondary containment is very important in nuclear plant safety because it constitutes the last barrier between the reactor system and the public. Its integrity must be maintained under all credible conditions to prevent the uncontrolled release of radioactive materials to the environment. Eventually, perhaps even the controlled release of radioactivity may be prohibited, but for this study it was restricted to one-tenth of the release criteria listed in ICRP Publication 2.

The design of the secondary containment and its associated safety features depends on the type of plant, its operating requirements, accident considerations, siting criteria, economics, and precedent. This study was not unduly influenced by precedent or economics, although these factors were considered. The type of plant and the operating requirements were based on the B&W reference design presented in the 1000-MWe Follow-On Study reports. Two hypothetical sites were considered—one extremely conservative and the other typical. Thus, the accident conditions and their effects on the secondary containment were the ultimate independent variables in the study. The accident conditions assumed were unrealistically severe in some cases. However, they were chosen to evaluate the containment design under adverse conditions to determine the safety features required.

The secondary containment design for an LMFBR is complicated by the presence of large amounts of plutonium in the reactor and by the combustible nature of the sodium coolant. The operating requirement of unencumbered access to the containment during operation leads to the selection of an air atmosphere. The presence of both air and sodium naturally requires that consideration be given to sodium fires. Therefore, the principal problems were deemed to be (a) the release of radioactive materials to the environment under reactor accident conditions, (b) the consequences of large sodium fires, and (c) simultaneous occurrence of both of these.

The objectives of this section of the Phase II portion of the safety contract were to identify and review candidate safety support

systems, to determine their functional requirements, and to select safety support systems. The identification and review portion was accomplished and a topical report prepared and published as BAW-1349. The selection and functional requirement portions were being performed at the time the contract was cancelled and are therefore not complete. The extent to which the individual tasks were completed is noted in subsequent sections.

To initiate the investigation, a literature survey on reactor containments was made to determine the minimum practical leak rate to be expected from the reference design containment building. Candidate safety support systems were identified for consideration. Their functions were determined in a general way as to their applicability to mitigating accident conditions. These safety features were presented in BAW-1349.

In lieu of well defined DBAs, a range of accident conditions were postulated in the initial stages of the study. The resulting consequences were then evaluated in terms of radioactive material release and pressure and temperature conditions in the secondary containment. It became evident that the reference design containment would not perform its required function when subjected to some of the most severe accident conditions. Therefore, safety support systems would be required for some conditions. For example, the most severe uncontrolled sodium fire would surely lead to the destruction of the containment building. Also, the uncontrolled, airborne release of large fractions of the core plutonium inventory to the secondary containment could not be tolerated with the reference design. It should be noted, however, that the credibility of these events was neither established nor disproved. This is essentially the remaining task, i.e., to establish the maximum credible accident conditions; the required safety support systems could be selected, and their functional requirements could be established. It was planned to devote considerable effort to the evaluation of the accident conditions and to the consequences thereof. Models would be developed and obtained when possible to describe the sodium fire phenomena (both pool and spray fires). Means of reducing the consequences would be investigated. Plutonium and fission product release and transport would be investigated in an effort to determine the need or lack of need

for incorporating such safety systems as filters and double-walled containment. Again, the selection and the functional requirements of safety systems would ultimately depend on the severity of the accident conditions. When possible, conservatism would be used in place of credibility to obtain results. The following sections describe the effort in some detail.

4.4.2. Sodium Fires

4.4.2.1. Sodium Expulsion

To consider the hazards associated with sodium fires in the air-filled secondary containment building, it was necessary to determine the amount of radioactivity associated with the sodium, as well as the total amount of sodium that might be present. It was assumed that the secondary sodium piping could be armored and protected so that secondary sodium spillage need not constitute a fire hazard. Primary sodium was considered to be the only sodium that would participate in accidental fires. Therefore, it remained to define the quantity and geometry of the primary sodium that could be expelled into the secondary containment during large core accidents.

The sodium expulsion analysis is described in detail in BAW-1349. Briefly, the analysis was based on the hypothesis that the explosive core accident would cause a slug of sodium to be accelerated upward until it impacted on the underside of the cover structure. High pressures would be generated, and sodium would flow through any available leak paths existing in the cover structure. The leak paths were assumed to include the control rod drive penetrations, rotating shaft penetrations, and the annuli around the rotating plugs. The sodium slug was assumed to have an impact velocity of 50 to 200 fps. Force balance equations based on classical laws of motion were written to describe the impact, which was assumed to be inelastic. The restraining forces of the cover structure holddown bolts were considered. These equations were solved and the pressure drops across the cover structure were obtained for the range of impact velocities. The sodium flows through the structure were subsequently obtained.

Several situations were investigated by varying key parameters, such as leak path area, impact velocity, and

holddown bolt properties; two cases were selected for detailed analysis. These cases provided input for the sodium spray fire investigations presented in section 4.4.2.2. They differed only in impact velocity: the first assumed an impact velocity of 100 fps and the second 200 fps.

The cross-sectional leakage area was taken as 10.2 ft², including areas around the large rotating plug, the intermediate plug, the small plug, two refueling ports, and 25 control rod drives. The cross-sectional area of the holddown bolts was 5 ft², equivalent to 100 3-inch-diameter bolts. (The holddown bolts were subsequently upgraded during the primary containment accident studies due to excessive cover structure motion.)

Figures 4-4 and 4-5 show the results of the two calculations discussed above. In both cases, the leakage flow rate was determined as well as the integrated total leakage. The case 1 results show a total leakage of 1490 lb, and case 2 shows 4425 lb. The flow rate curves were used in the spray fire analysis to obtain the sodium ejection velocity from which the sodium particle size distributions were obtained.

4.4.2.2. Sodium Spray Fires

To investigate the consequences of the sodium-air reaction that would occur following the entry of sodium into the secondary containment, it was necessary to describe the geometry of the sodium. The energy release rate from the sodium-air reaction depends largely on the surface area of the sodium exposed to the air. The total energy release depends on the net amount of reacting sodium. It was necessary to evaluate both of these parameters to determine the pressures and temperatures to which the secondary containment might be exposed. The conditions thus generated would establish the necessity for including safety support systems.

It was necessary to develop an analytical model for calculating the spray fire phenomena. Although a great deal of experimental work was being done in this area at the time of our effort, no general results were available that could adequately be used for spray fire analysis. Consequently, it was considered that the method adopted herein was suitable for the required preliminary calculations.

Future work should include a comparison with experimental data to confirm the validity of the technique.

As mentioned previously, the leakage path through the cover structure is actually a composite of the leakage paths that could exist. Therefore, the geometry of the emerging sodium cannot be precisely defined. It was necessary to assume that the leak paths behave as spray cones comprising conical annuli of sodium. Under these conditions the sheets of sodium tend to break into droplets. Reference 1 suggests that a maximum drop size can exist for a given relative velocity between the liquid and the gas. A drop that is larger than this critical maximum will shatter into smaller drops.

Further, it was assumed that the droplet size distribution below the maximum size follows a relationship called the upper limit equation.² The upper limit equation has been applied to a variety of experimental data on sprays and fits the data accurately.

Based on these two assumptions it was possible to describe the particle size distribution for a given expulsion velocity. Using the mass flow rate information given in the previous section, the expulsion velocity as a function of time was obtained. By suitably averaging over small time increments, the emerging sodium was thus characterized by a collection of sodium increments, each increment having an average initial velocity and hence a characteristic particle size distribution.

The burning model assumed for each sodium droplet was as follows: Each particle has a known initial velocity and size. The momentum of a particle carries it up into the air, and the particle burns at a rate proportional to its surface area. The forces on a particle change as the velocity and size of the particle change. Each particle rises to some maximum height and falls back through the air. The burning of each particle ends whenever it burns itself up or comes into contact with the floor. Each initial particle was accounted for by a group-averaging calculation. The amount of sodium burned during flight was calculated for each particle group and then summed to yield the total amount burned as a spray for the accident. The energy released through this mechanism was calculated according to the amount of sodium burned, and the release was assumed to be instantaneous.

Sodium burning calculations were performed for the two sodium expulsion accidents described in section 4.4.2.1 as cases 1 and 2.

In case 1, a total of 1490 lb of sodium was expelled into the secondary containment; 397 lb of this burned. This results in an energy release of about 1.2×10^6 Btu. The air temperature and pressure rose to maximum values of 185F and 19.2 psia. This caused the wall liner to heat up to a maximum temperature of about 120F.

Case 2 was based on a sodium slug impact velocity of 200 fps, which resulted in the expulsion of 4427 lb of sodium into the secondary containment. Of this amount, about 2850 lb burned, releasing 6.1×10^6 Btu of heat energy. This resulted in a maximum air temperature of 626F, a pressure of 32.3 psia, and a maximum wall liner temperature of 283F.

It was felt that case 2 represented an upper bound on the sodium spray fire accident. The conditions thus created were never fully explored to reveal their effects on the containment function. Future work would include a detailed analysis on the effect of heating the wall liner to ensure its integrity.

4.4.2.3. Sodium Pool Fires

Sodium pool fires have been investigated experimentally and analytically for some time, notably at Atomics International.⁵ Accidents in which large amounts of sodium were allowed to burn on the floor of the secondary containment building were investigated early in the contract work. However, later sodium expulsion work showed that such large amounts of sodium could not be expelled from the primary to the secondary containment. Since it has been tacitly assumed that a secondary sodium pipe break could not result in pool fires, no mechanism could be found for the spillage of large amounts of sodium within the secondary containment building. This conclusion resulted in a de-emphasis of the sodium pool fire investigations; hence, the subject is de-emphasized here. Only a brief accounting follows, but details are given in BAW-1349.

The work on pool fire analysis utilized the FIRE code, which is a modified version of the SOFIRE 1-B code developed at Atomics International.³ The most uncertainty in the pool fire analyses existed for situations where moderately small amounts of sodium (several thousand pounds) were spilled on the building floor or the upper surface of the cover structure. A pool less than one inch deep resulted. These conditions lead to rapid heat transfer from the hot sodium to the much cooler floor. A severe temperature drop occurred in the sodium—occasionally even to the freezing point. This indicated that much slower burning would result than for sprays or for deep pool fires. This result was part of the basis for assuming that droplets that contact the floor no longer contribute energy in the spray fire calculations.

The analyses were based on a burning model in which the burning rate is proportional to the surface area of the exposed sodium. Thus, geometry was important again, and several situations were investigated.

The sensitivity studies pointed out at least three parameters worthy of further study in attempting to reduce the severity of large sodium pool fires: the exposed surface area of the sodium pool, the initial pressure within the containment, and the initial oxygen concentration. The exposed surface area is a maneuverable parameter since drainage systems and floor gratings may both be employed. These would restrict the surface area and the rate at which oxygen arrives at the sodium surface. The initial pressure and oxygen concentration are restricted parameters if unencumbered access to the building is required. BAW-1349 gives detailed results of the calculations for different values of these parameters.

4.4.3. Radioactive Dispersion Considerations

4.4.3.1. Environmental Studies

In general, environmental safety studies are conducted to assess the consequences of accident situations relative to the exposure of plant personnel and the general public to radioactive materials. The DBA should provide the specific accident conditions that must be accommodated. No DBA was selected for this study; hence,

no maximum accident conditions were established. Specifically, the amounts of plutonium and fission products that would be introduced into the secondary containment during a DBA were never established. In lieu of a direct analytical technique for describing the behavior of the radioactive materials after a core disturbance, a somewhat reversed approach was taken. It was decided to determine just how much radioactive material could be tolerated within the secondary containment without leading to excessive exposure of the public. It was hoped that this would lead to some preliminary conclusions on the accident magnitudes that might be tolerable. Obviously, if one could tolerate more radioactive material in the secondary containment than existed in the core, then a whole core meltdown could be acceptable from a radioactivity release point of view. Likewise, if some fraction of the core plutonium and fission product inventory could be tolerated within the secondary containment, then an accident that would release no more than this fraction of material would be acceptable. If the results showed that minimal amounts of radioactivity could be tolerated, then the work burden would shift toward evaluating safety support systems that would increase the toleration limits and toward reducing the release fractions.

The maximum permissible body burdens for sodium-24 and the isotopes of plutonium-239, -240, -241 and -242 were determined by using one-tenth of the values listed in ICRP Publication 2, Table I. The one-tenth reduction factor was included to account for nonoccupational exposure. The 2-hour external gamma dose selected was 25 Rem at the exclusion area boundary, a value recommended for site selection in 10 CFR 100. A site, with its associated physical dimensions, was selected and three exclusion area radii—600, 1100 and 1609 meters—were considered. Two independent sets of meteorological conditions were assumed. One resulted in very poor atmospheric dispersion and the other in good dispersion. Using the standard calculational techniques (described in detail in BAW-1349), we calculated the leakage source from the containment building that would result in the maximum permissible body burdens for persons at the exclusion boundary. The maximum amounts of sodium-24 and plutonium in a state available for leakage (airborne) were thus determined and compared with the total amounts present in the primary system.

It was determined that plutonium-241 was the limiting plutonium isotope. It was assumed that any release from the core was homogeneous in that each plutonium isotope would be released in an amount corresponding to its relative amount in the undisturbed core. Therefore, since the tolerable amount of plutonium-241, expressed as a fraction of the original core inventory, was smaller than that for the other isotopes, it controlled the tolerable release fraction of isotopes from the core.

Table 4-1 shows the maximum fractions of core materials which, if available for leakage in an airborne state within the secondary containment, would lead to the accumulation of one maximum permissible body burden at the given exclusion area radius in a 24-hour period.

It was seen that, for the worst site meteorological conditions (condition F) and the smallest exclusion area radius (600 m), the core plutonium inventory that could be tolerated in a state available for leakage in the secondary containment was restricted to 0.015%. This is a very small amount, and it pointed out the need for detailed investigation of the core release mechanisms and the transport mechanisms that could get the plutonium from the core region to the secondary containment in an airborne state. This work was started and is discussed in section 4.4.3.2.

It should be pointed out that these were very conservative numbers. It may be that only the 2-hour accumulation is important, in which case the values in Table 4-1 can be increased by a factor of 12, yielding 0.18% for the worst case. (The equations are linear.) A complete study of the leakage phenomena would include agglomeration and settling out and plating out of the material, thus reducing the leakage.

The external gamma dose from sodium-24 was not a problem. The release of 0.133% of primary sodium (~3325 lb) could be tolerated under the worst conditions above. It is likely that the resulting sodium fire in such a case, rather than the radiation exposure, would provide the limiting considerations.

Table 4-1. Maximum Tolerable Fractions of Core Plutonium Inventory and Primary Coolant Sodium Available for Leakage

	Fraction, %		
	600 m from source	1100 m from source	1609 m from source
<u>Condition F</u>			
Plutonium	0.015	0.026	0.031
Primary Na	0.133	0.240	0.311
<u>Condition A</u>			
Plutonium	0.351	0.495	0.593
Primary Na	3.510	4.969	5.980

4.4.3.2. Plutonium Dispersion

In this section the main effort was directed toward understanding the mechanisms and conditions under which plutonium could be transported from the core to the secondary containment. Such an understanding would lead to the definition of practical functional requirements that neither economically penalize the design nor overlook important issues. The major assumptions for the plutonium transport investigation were as follows:

1. The existence of a fuel sodium interaction.
2. The amount of fuel entering into such a reaction.
3. The fragmentation and size distribution of the fuel.
4. That the consequences of such a reaction should result in the formation of a sodium vapor bubble.

The approach used for the plutonium transport problem was to assume that a sizable breach in the primary containment was required for a significant amount of plutonium to enter the secondary containment. This situation could occur after the impact of a large slug of sodium. It was further hypothesized that the sodium breaching the

primary containment would be relatively free of plutonium and that the major portion of plutonium would follow through the breach in the form of entrained fuel in the argon and sodium vapor of the cover gas.

It then became evident that fruitful results might be achieved by showing that (1) the bubble, together with the fuel aerosol, would condense in the liquid before it mixes with the cover gas; or (2) a significant amount of fuel is removed from both the bubble and the argon cover gas (should it find its way there).

Figure 4-6 shows the major areas involved in the plutonium transport problem. Preliminary investigations have been performed in areas 3 through 12 (as indicated on the figure).

Vapor Bubble Study (Formation, Expansion, and Condensation) — This portion of the investigation involved the thermodynamic aspects of a sodium vapor bubble with entrained fuel and steel following a disassembly accident. The three main areas investigated were those associated with items 3, 5, 6, and 7 in Figure 4-6, i.e.,

1. Establishing the equilibrium temperature and pertinent thermodynamic conditions between the fuel, steel, and sodium.
2. Establishing the equilibrium conditions when the pressure in the sodium bubble is reduced by expansion to the pressure in the argon cover gas space.
3. Estimating the time required to condense the expanded bubble to determine whether the bubble still exists before it vents into the cover gas.

The following simplifying assumptions were made to develop the model of the sodium vapor bubble:

1. Following a disassembly accident, the sodium, fuel, and steel mixed until thermal equilibrium was achieved. The sodium bubble expanded along the saturated liquid sodium curve up to the critical point. If thermal equilibrium was not reached when the liquid sodium reached the critical volume, additional energy was added at constant volume.

2. A thermal decoupling between the materials was assumed after thermal equilibrium (i.e., no heat transfer between fuel, steel, and sodium), followed by an isentropic expansion of the sodium bubble until the pressure was in equilibrium with the argon gas pressure.

3. Booth's data⁴ were used for the thermodynamic properties of the sodium and fuel.*

Aerosol Fuel Transport — This study was conducted in parallel with the vapor bubble study described above. The areas investigated here were those associated with items 10, 11, and 12 in Figure 4-6. It was assumed that the bubble would rise and release some of the fuel aerosol to the argon cover gas. From there the aerosol could escape to the secondary containment through openings in the cover plug. A model was developed to estimate the amount of fuel aerosol that could (a) be carried up to the cover gas while suspended in the bubble, and (b) escape through the cover into the secondary containment.

The mechanism for transport of the fuel aerosol up into the cover gas is the drag on the aerosol particles exerted by the upward motion of the vapor bubble. The drag in the radial direction during the bubble expansion is neglected. It is assumed instead that after the bubble expansion is over, the aerosol is dispersed uniformly throughout the bubble. At this time, buoyancy forces the bubble upward and gives it the upward velocity that carries some but not all of the aerosol with it.

Vapor Bubble Study Analysis — The equilibrium temperature for the formation of the bubble was determined from a trial-and-error solution to the following equation:

$$Q_{\text{fuel}} + Q_{\text{steel}} + Q_{\text{Na}} = 0.$$

In this investigation the output of MARS was used to establish the initial fuel temperature (8650R), the mass of fuel

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*See reference 4; best sodium as defined in NDC-R/180.

involved (208 moles), and the mass of sodium involved (177 moles). The mass of steel was taken to be the amount present in the core.

After the equilibrium temperature was obtained, it was assumed that the system expanded isentropically (with no heat transfer from the fuel to the sodium vapor). The expansion of the sodium was calculated on a trial-and-error basis using Booth's data,⁴ by first assuming a final temperature T_2 . At this selected temperature, the pressure (P_2) and the entropy of the saturated liquid (S_f) and saturated vapor (S_g) were found directly, and the vapor quality (x) was determined. The specific internal energy and specific enthalpy were calculated based on the quality previously determined. The total volume of the sodium after expansion was also calculated. This volume was derived by solving three simultaneous equations involving the final equilibrium pressures of the sodium and the argon, their final volumes, and the p-v relationship for isentropic compression of the argon gas. The work done during the isentropic expansion of the sodium bubble was calculated as the decrease in internal energy from the state of thermal equilibrium to the expanded state.

To determine whether the sodium vapor bubble could condense before it mixed with the cover gas, the following relationships were derived. The same assumption was made—that no heat was transferred from the fuel particles to the sodium. The governing equation in differential form is written as follows:

$$dQ = q_c dt \quad (a)$$

where $Q = m_{Na}(xh_{fg})$ = total heat available from condensation, Btu,
 $q_c = h_c AT$ = condensing heat transfer rate.

The variables in equation a are the time t , the vapor quality x , and the heat transfer area A , which is expressed as a function of x . For a sphere, the heat transfer area is πd^2 , where $d = (\frac{6}{\pi} V)^{1/3}$, and the volume (V) can be expressed as $V = vw$; the specific volume in turn is $v = v_f + xV_{fg}$. Therefore, the heat transfer area is

$$A = \pi \left[\frac{6}{\pi} (v_f + xV_{fg})w \right]^{2/3}.$$

Substituting into equation a and simplifying, we obtain

$$dt = \frac{Cd_x}{(v_f + xv_{fg})^{2/3}}$$

where

$$C = \frac{(w)^{1/3} h_{fg}}{8.79 h_c \Delta T} = \text{constant, assuming a constant condensing heat transfer coefficient.}$$

Upon integrating,

$$t = \frac{3C}{v_{fg}} (v_f + x_2 v_{fg})^{1/3}.$$

Aerosol Fuel Transport Analysis — The amount of fuel transported as an aerosol in the bubble was determined by developing a model of the system. The starting point for the model was the completion of the bubble-growth phase. At this point in time, the following conditions were assumed:

1. The bubble pressure and the argon pressure are equal and the bubble radius ceases to increase ($dr/dt = 0$).
2. Some sodium vapor and fuel aerosol are present in the bubble.
3. The net axial velocities of the vapor bubble (v) and the aerosol (u) are zero.
4. The particle distribution for the aerosol that has mixed with the sodium is described by a relationship given in ANL-7688.⁶

The bubble rises to the surface due to buoyancy. For a rising bubble whose radial growth is negligible during the rise, the velocity, $v = 2$ gt. When the top of the bubble breaks through the sodium surface and reaches the cover, it is assumed that the resistance offered by the sodium above the bubble becomes zero. Thereafter, the bubble mixes immediately with the argon cover gas, and the resulting cover gas volume is assumed to contain all the aerosols that were present in the bubble at the end of the calculation.

The aerosol velocity (u) is related to the gas bubble velocity (v) through a nonlinear differential equation:

$$\frac{du}{dt} = -g + C_D \frac{A}{m} \frac{\rho_g}{2} (v - u)^2$$

where

A = cross-sectional area of the assumed spherical aerosol particle = $\frac{\pi}{4} d^2$,

d = particle diameter,

ρ_g = density of the gas in the bubble.

The drag coefficient was obtained from an experimental curve of drag versus Reynolds number for spheres. The bubble position is determined by integrating the velocity to give $z = gt^2$. From this relationship the time required by the bubble to rise can be calculated. It is assumed that any aerosol that falls to the bottom of the gas bubble mixes with the sodium at that interface and is removed from the bubble.

The objective of the following part of the analysis was to determine the amount of fuel that enters the secondary containment during depressurization of the cover gas space. The geometry for the leakage of the fuel aerosol through the cover is shown in Figure 4-7. An analytical model was devised to study this problem and was programmed using MIMIC.

The pressure drop in this relation is affected by the critical flow phenomenon. When the downstream-to-upstream pressure ratio is below the critical pressure ratio (P_{rc}) the effective pressure drop is constant. Under this condition the downstream pressure acts as the product of the critical pressure ratio and the upstream pressure. The effective ΔP becomes $P - P_{rc}(P)$ or $(1 - P_{rc})P$. When the upstream pressure decreases until the pressure ratio is greater than the P_{rc} , the effective ΔP is actually the ΔP across the cover plug. It is assumed that the secondary containment pressure is constant at one atmosphere.

Vapor Bubble Study Results — The equilibrium temperature of the bubble was 5080F, the melting point of the fuel. While the steel remained as a liquid, some of the fuel solidified. The two-phase fuel mixture was approximately 24% saturated liquid. The thermal equilibrium conditions are shown in Figure 4-8. Figure 4-9

shows the path of the sodium from its initial liquid state to a vapor state at T_{equil} and followed by an isentropic expansion to a two-phase mixture. After the isentropic expansion, the sodium bubble was a two-phase mixture of saturated liquid and vapor with a vapor quality of 49%. The work done by the isentropic expansion was 1900 MW-s. On the assumption that the condensing coefficient for sodium vapor is equal to approximately 10,000 Btu/h-ft²-°F, the time to condense is approximately 0.4 second; this is within an order of magnitude of the time to rise.

Aerosol Transport Study Results — The results of the fuel transport study using the model previously discussed and the particle size distribution in ANL-7688 were not encouraging. A total of 32% of the original fuel reached the argon cover gas. The calculated time for the bubble to rise was approximately 0.7 second, and the terminal velocity of the bubble was 40 fps. To determine the amount of fuel leaving the cover gas and entering the secondary containment, a preliminary run was made assuming that all the core fuel (56,400 lb) was dispersed in the cover gas in two different-sized particles. These particle sizes were taken as 200 (d_1) and 50 (d_2) microns. Half of the dispersed fuel particles were one size and the other half were the other size. The fraction of fuel entering the secondary containment was approximately 77%.

4.4.4. Candidate Features

A number of candidate systems were developed as a result of the work described in the previous sections. These systems are the outgrowth of concerns that stem from major accident assumptions, and it is the purpose of these systems to mitigate the effects of the accidents; however, other systems and features that also work toward this end are included in the investigations of primary containment safety features (section 4.3).

4.4.4.1. Oxygen Depletion Systems

These systems limit the amount of oxygen available for reaction with sodium. They cover a range of schemes having different capabilities, and they must be matched with the functional requirements.

System A: Partially Inert Atmosphere — This system provides a machinery dome type of protection. It requires a massive cover that would form an envelope over the possible leak paths. In addition, it requires an inert gas system, a special cooling system for the inert gas, and a purging system. In the Accident Analysis and Safety System Design Study the machinery dome concept is considered as part of the primary containment.

System B: Reduced Containment Pressure — This system requires a steam jet exhauster to maintain the entire containment pressure at approximately 9 psia. The studies show that an appreciable reduction in the maximum temperature and pressure of the containment can be attained. The system is rather simple and is being planned for use in some light-water reactor plants.

System C: Totally Inert Containment — This system will require a gas purging provision and makeup storage. It will not require a continuous purge (as with an air-filled containment), nor will it require the attendant valves and instrumentation.

4.4.4.2. Containment Filter System

This system would be used to clean up the containment atmosphere after a fire. The filtration system would operate in conjunction with the containment's coolers. The filter system would contain roughing filters, absolute filters, and charcoal filters in parallel with the air coolers. These filters would be valved off during normal operation and would be heavily protected against missiles, as are the coolers themselves.

Because of the time required to clean up the containment, it is highly unlikely that this system could be given credit for reducing the maximum body burden dose outside the containment.

4.4.4.3. Containment Atmosphere Salt Addition System

This system was proposed on the basis that it would (1) add additional heat sink capacity to the containment, (2) serve as a possible fire extinguishing agent, and (3) serve as a possible aid in agglomerating atmospheric particles. The system has a number of possible arrangements; two are discussed below:

System A — In this arrangement, bins located in the hemispherical portion of the containment contain specially treated NaCl to prevent caking. These bins would be open to the containment atmosphere during a fire to allow a gravity discharge at some preselected rate. The bins would be opened electrically on a signal originating from a manual or automatic source.

System B — This system is essentially the same as system A except that in place of gravity feed, the bins have blowers to discharge the salt to the atmosphere. Calculations were performed in conjunction with the spray fires analysis, which showed that the salt addition system could reduce the spray fire consequences considerably. For the case 2 spray fire described in section 4.4.2.2, the salt addition would reduce the maximum temperature of the wall liner from 283F for no salt addition to 197F. It was assumed that the salt reaches an equilibrium temperature with the containment atmosphere during its trip to the floor. If this is a poor assumption, then greater control of the salt discharge may be required.

4.4.4.4. Double Containment System

Zero-Leakage System — This system would yield an essentially zero leakage from the containment after a sodium fire. It is proposed as a possibility only if it is not possible to prevent a greater-than-allowable concentration of plutonium in the secondary containment when a 0.1% per day leakage is assumed.

This system consists of a double liner with a capability of "pump back" to the containment after an accident. During normal operation the space between the liners is exhausted to a stack along with the purge air from the containment. The space between the liners is always kept below atmospheric pressure with a steam jet exhauster.

After a fire is detected, the containment is isolated, and the mechanical vacuum-compressor pumps are started. These pumps discharge back into the containment, maintaining the below-atmospheric condition between the liners.

Stack Release System — This system would permit a larger amount of radioactive release inside the secondary containment than would a single-barrier containment. The system would be identical to the zero-leakage system but would not require the pump-back capability, including inspection and reliability of this feature. Releasing through a stack lessens the dose rate of the release from distance, dilution, and mixing phenomena.

In general, systems of this type will add materially to the capital cost of the plant. An economic trade-off would be required between these systems and others, such as a partially inert or fully inert containment, before a selection could be made.

4.4.4.5. Drainage Systems

This system would preclude the availability of large areas of sodium surface for burning. The system comprises grated floors, floor drains, and drain tanks to accept the molten sodium. The grated floor will help to shut off the oxygen.

As previously discussed in section 4.4.2.3, no mechanism could be found that would yield large pool fires. However, the analysis on the spray fires did show that some of the sodium would reach the floor and would be available for pool burning if no provisions were made for drainage. The value of the grated floor is questionable as a fire retardant, but it should enhance drainage.

4.4.4.6. Miscellaneous Features

Containment Liner Insulation — In an air-filled containment where there is a possibility of fire, it may be necessary to insulate. Pressurized water reactors have several coats of a special paint to provide the extra temperature required for the liner. Certain manufacturers offer a silicon resin that is resistant to as much as 2000F flame for a short time without losing flexibility. This material has a thermal conductivity of 0.09 Btu-ft/h-ft²-°F, and it is claimed that its final oxidation state is silica. Its apparent ability to bond all materials makes it a good candidate for insulation. Its ability to cure as a rubbery compound might be beneficial to reduce containment leakage further.

Spray Baffles — This study has shown that sodium sprays constitute the major source of energy release in the event of a fire. Therefore, it is necessary to suppress a spray configuration. One possible method is the use of baffles. It should not be difficult to provide baffles to prevent large sprays of sodium from forming in the secondary containment.

4.4.5. Evaluations and Conclusions

The vital role of the secondary containment in the protection of the public has long been a source of serious concern to those who must determine the adequacy of its performance. In PWRs the concern is failure of integrity due to pressure and missiles. For the reference design, the concern is for failure of integrity due to temperature and missiles. In both types of plants the prevention of an inadvertent release of radioactive material is the motivating force for maintaining integrity. In both types of plants it is relatively easy to eliminate failure due to missiles by providing adequate shields.

To determine the adequacy of the secondary containment for the reference design, it was necessary to establish the limiting conditions that would (1) destroy the containment due to high temperatures resulting from a sodium fire and (2) be required to prevent excessive leakage of radioactive material from the containment.

The spray fire analysis presented here appears to be ultraconservative. Such assumptions as the flow area through the cover plug, the impacting velocities of the sodium, and the particle distribution of sodium—all of which influenced the final temperature of the atmosphere—were known to be excessive, but no basis for reducing the conservatism was available. Other assumptions, such as values for burning rates and energy liberation, were based on meager laboratory findings but also seemed to be conservative when compared with results of the different researchers. Therefore, we are confident that the conditions resulting from a fire that was predicted by our analysis are more severe than they probably would be in reality.

In addition to establishing a confidence level in assessing the post-accident conditions, certain improvements could further decrease the severity of the situation. These features include

(1) spray baffles, which will guarantee a minimum amount of sodium atomization; (2) drain tanks to eliminate pool burning fires; (3) gratings to minimize air access to the draining sodium; and (4) a salt-addition system, which will simultaneously provide a heat sink and a fire retardant.

In summation, we believe that with the incorporation of certain safety features the integrity of the secondary containment can be assured. The problem of plutonium release from the secondary containment was not resolved. Although many researchers have been investigating various aspects of plutonium and plutonium aerosols, there is a dearth of information on the transport of plutonium through the sodium and the conditions required to start the chain of events.

The absence of data made it mandatory that we set up guide lines on how much plutonium could be tolerated in the containment (see section 4.4.3.1). We felt justified in drawing this conclusion because we could now relate the plutonium concentration to accidents involving various amounts of the core.

The two approaches taken to eliminate the plutonium from the primary tank cover gas are also discussed in section 4.4.3.2. The approach where the vapor bubble is condensed before it breaks into the cover gas appears to hold the most promise. Experimental results indicating that the condensation coefficient for sodium vapor is 10,800 Btu/h-ft² lends credence to the possibility. However, an overall coefficient of 10,000 would be required before condensation would be assured. In addition, the possibility of validly applying Taylor's theorem of instability to the large bubble should increase the heat transfer area significantly and decrease the requirement for such a high overall coefficient.

Before a judgment can be made concerning the safety features required to prevent the inadvertent release of plutonium, more work is required to predict the transport mechanisms of plutonium from the core through the containment. However, if one is willing to accept the economic penalties, a double containment with "pump-back" capability should satisfy the functional requirement of near-zero release of plutonium from the reactor building.

4.4.6. References

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- ² R. A. Mugele and H. D. Evans, "Droplet Size Distribution in Sprays," Ind. & Engr. Chem., 43, No. 46, p 1317.
- ³ R. P. Johnson, SOFIRE 1-B User Package, Engr. Dept. No. TI-707-14001, Atomics International, Feb. 1969.
- ⁴ D. L. Booth, The Thermodynamic Properties of UO₂ and Sodium, TRG-1871, UKAEA (1966).
- ⁵ Annual Technical Progress Report, AEC Unclassified Programs, Fiscal Year 1968, AI-AEC-12721, Atomics International, Jan. 1969.
- ⁶ R. B. Duffield, et al., Reactor Development Program Progress Report, April-May 1970, ANL-7688, Argonne National Laboratory, July 1970.

Figure 4-4. Sodium Expulsion, Case 1

Figure 4-5. Sodium Expulsion, Case 2

Figure 4-6. Major Considerations Relative to Plutonium Transport

Figure 4-7. Cover Plug Flow Path

Figure 4-8. Pressure-Volume Diagram of Sodium

Figure 4-9. Temperature-Entropy Diagram of Sodium
(Booth's Data)

5. EVALUATION OF IRRADIATION-INDUCED METAL SWELLING

5.1. Introduction

The contract scope of work for the activities described in the preceding sections specified that the study should be based on the B&W 1000-MWe LMFBR Follow-On Study reference design.¹ However, since it and all other Follow-On Study designs, was developed before the swelling of structural materials was recognized as a problem, this design does not contain provisions to accommodate the irradiation-induced swelling now anticipated. In anticipation that the reference design might have to be changed significantly to accommodate the swelling and that the changes might have a significant effect on the safety characteristics of the plant, an additional scoping-type evaluation was authorized to evaluate the metal-swelling problem. This evaluation included tasks to better define the problems, to identify potential solutions, to evaluate the relative merits of each candidate, and to assess the effects of the "composite solution" on the reference design and on the ongoing Accident Analysis and Safety System Design Study.

The detailed results obtained in this study are presented and discussed in BAW-1355.² The evaluation of potential solutions is summarized in section 5.2, and section 5.3 describes a revised design that reflects the tentative modifications to mitigate the effects of irradiation-induced metal swelling.

5.2. Evaluation of Potential Solutions

In order to meet stringent thermal conditions, to permit the various mechanical operations involved with reactor control and fuel handling, and to prevent adverse changes in reactivity, LMFBR cores must be designed to provide structural and geometric stability during the entire life of the core. Many forces, including nonuniform heat generations,

nonuniform temperatures and corresponding stresses and strains, act to alter the physical geometry or alignment of the fuel assemblies and related core structures. However, by 1967 there was reasonable assurance that the forces anticipated at that time could be controlled by precautionary constraints on fuel pin and reactor structure design without creating major obstacles to fast reactor economic performance. Then, late in 1967, the UKAEA reported that gross swelling of austenitic stainless steels was being observed in cladding on mixed-oxide fuel pins irradiated in the Dounreay Fast Reactor.³ As more data became available, correlations relating the volumetric swelling to the known variables of fluence and temperature were developed. When utilized to extrapolate from the range of experimental data to target operating conditions of 100,000 MWd/t in the temperature range of 800 to 1200F established for commercial LMFBR plants, these correlations indicated that the volumetric change in the structural materials might be 25 to 50% or higher in some of the stainless steel materials. If confirmed, this degree of swelling would produce growth of the fuel assemblies in the range of 1/4 to 1/2 inch across flats and up to 1 to 2 inches in length. In addition, the inherent flux gradient that becomes progressively steeper from the center to the peripheral regions of the core would cause variations in both fluence and temperature from one side of the assembly to the other. These conditions in turn would cause differential growth across the assembly and could lead to extremely large bowing and distortion of the assemblies.

Obviously, the radial growth would have to be accommodated in a manner that would prevent significant radial displacements, since center-to-center distances and locations would need to be maintained to satisfy structural and geometric stability criteria and meet fuel handling and reactivity control system requirements. At the same time, fuel assembly bowing and distortion must be controlled, but the task is significantly more difficult than before because clearances must be provided in the active core region to accommodate the increase in the size of the assemblies.

5.2.1. Metal Swelling Correlations

Two irradiation-induced metal swelling correlations were selected from the four considered for the evaluation of the B&W reference design:

1. PNL-Westinghouse 20% Cold-Worked Type 311 SS —

$$100 \frac{\Delta V}{V} = 1.01 \times 10^{-36} (\phi t)^{1.69} \left(\exp - \frac{7800}{RT} - 5480 \exp - \frac{25,300}{RT} \right),$$

where

$$100 \frac{\Delta V}{V} = \text{volume change, \%},$$

$$\phi t = \text{fluence, n/cm}^2 \text{ (E > 0.1 MeV)},$$

$$T = \text{temperature, K.}$$

2. ANL Data —

$$\text{Volumetric growth} = C_1 (\text{fluence})^{C_2}$$

where fluence is based on the total neutron flux and constants C_1 and C_2 are a function of temperature as shown in Table 5-1.

Table 5-1. Coefficients for ANL Growth Equation

<u>Temperature, C</u>	<u>C₁</u>	<u>C₂</u>
400	1.06467×10^{-22}	0.97688
450	1.42326×10^{-23}	1.01913
500	5.57175×10^{-27}	1.16567
525	3.64843×10^{-30}	1.30001
550	6.44568×10^{-35}	1.50056
575	4.62410×10^{-36}	1.54396
600	3.28210×10^{-37}	1.58970

Figure 5-1 is a plot of linear growth (percent) versus distance along the channel for the reference design based on the PNL 20% cold-worked equation. These linear growths are representative

of assemblies that have been exposed for three years at end-of-cycle flux and temperature conditions. Radial zone 1 is the innermost core assembly, and radial zone 12 is the outermost. Zone 13 is typical of the blanket assemblies. The peak growth occurs just above the core midplane owing to the combined influence of fluence and temperature. The incentive for fluence reduction as a means of reducing growth is illustrated by the 30% reduction in fluence in zone 1, which reduces the linear growth by about 45%.

Figure 5-2 shows linear growth (percent, after three years' exposure) as a function of core radius for the PNL 20% cold-worked equation. Layer 1 is just above the core midplane, and layers 7 and -7 are the upper and lower edges of the active core. As in Figure 5-1, the advantage of reducing fluence can readily be seen. Similar but slightly small linear growth values were obtained using the ANL correlation.

5.2.2. Potential Solutions

It was obvious that the structural material growth predicted in the reference design by the equation in the preceding section was intolerable. Aside from major concern about material properties, there was extreme doubt that feasible methods could be devised to accommodate the large dilation and distortion of the core components. Consequently, studies were initiated to evaluate the effect of potential changes in the design parameters that could be altered to reduce the magnitude of the metal swelling. In parallel, studies were performed to determine ways to accommodate the residual growth that would remain after the reference fluence and temperatures had been reduced. These studies are summarized briefly in the following paragraphs and described in more detail in BAW-1355.²

5.2.2.1. Reduction of Fluence and Fluence Gradients

The work had two objectives: first, to reduce the neutron fluence in the core to minimize bulk metal swelling. Two options are plainly visible—a reduction in the flux level and a reduction in the fuel residence time. A third option, softening the neutron spectrum, applies to the high-energy fluence. This option was not emphasized

in the limited time available because previous experience has shown that softening the neutron spectrum, for example with BeO, has an adverse effect on breeding. The second objective was to flatten flux distributions in the core to reduce the deflections caused by differential swelling. These deflections are particularly severe near the core edge where the flux gradient is large. The methods investigated are summarized below:

1. Reduction in fuel residence time.
2. Reduction in fuel residence time and volume (burnup held constant).
3. Use of an annular core concept.
4. Use of reflectors.
5. Variation in the relative enrichments of the radial zones.
6. Use of axial zone loading for the core.
7. Use of a thin annular core.

To the extent that the scoping evaluation was performed, it appeared that a combination of items 1, 2, and 5 offered the greatest potential for mitigating the problem; the combination that was used is described in section 5.3.

5.2.2.2. Reduction of Temperature

Since the magnitude of metal swelling is sensitive to temperature, reducing the temperatures in the core offers potential for reducing the dilation and distortion of core components. Either the inlet temperature or the temperature rise through the core can be changed to achieve the objective, but any changes will also affect many other aspects of the plant, including the steam cycles utilized. Consequently, limited steam cycle studies were performed to determine the economic penalties associated with reducing the primary coolant temperature. Consideration was given to several steam cycles to cover solutions ranging from the use of temperature reductions alone to minor reductions in temperature in combination with reductions in fluence.

In general, the results indicated that the volumetric metal swelling could be reduced to a maximum of 15% by reducing the core midplane temperature from 950 to 750F. If a 1000 psi steam

cycle were employed at these temperature conditions, power cost would be increased on the order of 0.5 mill/kWh. However, an average core sodium temperature of 850 to 900F appeared to be a desirable target value if satisfactory control of swelling were to be realized through a concurrent reduction in fluence. We evaluated three steam cycles that are potentially compatible with this required temperature reduction, including the selected 1800 psi/900F/900F cycle discussed in section 5.3. The reference 2400 psi/950F cycle would also be compatible provided the reheat requirement was eliminated.

5.2.2.3. Core Arrangement and Evaluation

In the early stages of the study several mechanical devices or features were considered briefly, as described in BAW-1355,² prior to selection of the core clamp device as the reference concept. A parametric study for the reference concept was then conducted using the reference fuel assembly as a basis. Basically, two computer codes, CRASIB⁴ and DEFLECT,⁵ were utilized in the study; the effects of variation in the following parameters were evaluated:

1. Length of the fuel assembly and relocation of intermediate support for various lengths.
2. Size of the fuel assembly wrapper can.
3. Inlet temperature variation.
4. Section modulus change along the length.
5. Mass flow rate.
6. Combined effects of good features from these parameters, in successive stages.

The following results were obtained for each variation noted above:

1. Maximum stress.
2. Maximum strain.
3. Maximum restrained deflection.
4. Restraint loads at all supports.
5. Reactivity change at the end of cycle.
6. Maximum unrestrained free deflection at the end of cycle.

Most of the analysis involved a typical fuel assembly near the core-radial blanket interface, which probably experiences the most severe conditions due to maximum temperature and flux gradients at this location. Some of the general conclusions from this investigation can be summarized briefly as follows:

1. The variation of the length of the fuel can and a feasible relocation of supports does not change the stresses and strains significantly. Restraint loads are reduced with the increase in the length. The longer, unsupported fuel can span entails a possibility of a positive reactivity change due to the sinusoidal deflection pattern.

2. Maximum stress, strain and restraint loads are reduced with the smaller flexible can size. The practical limitations (relocation of control rods) may not allow a large variation of the can size without several design changes; hence, for the present optimization, the can size is considered to be slightly smaller than that in the reference design.

3. Inlet temperature variation (same core ΔT conditions) has a significant effect on the results. The maximum stress, strain, deflection, and restraint loads decrease with the decrease in the inlet temperature. This is mainly due to the reduction in the swelling effects.

4. The practical application of the variation of section modulus along the length of the fuel can appears to be limited. The expected reduction in strain does not seem to be significant.

5. The increased mass flow rate reduces the radial temperature gradient and lowers the average temperature along the length of the fuel can. This two-fold effect significantly reduces the maximum stress, strain, deflection, and restraint loads.

5.3. Design Modification

The studies described in the preceding section indicated that the maximum volumetric swelling at the core midplane could be reduced to approximately 15% (4.9% linear) by modifying selected design parameters and conditions and selecting 20% cold-worked type 316 stainless steel for the structural material. These changes allowed a core clamp

device to be employed to locate the full assemblies positively and minimize bowing toward the center of the core without imposing excessive economic or performance penalties. The revised design is described briefly in the subsequent paragraphs along with the analytical results obtained in the evaluations.

5.3.1. Description

Fundamentally, the modified design is similar to the reference design. The differences lie primarily with the reduction of burnup, fuel residence time, and core inlet and outlet temperatures, together with the incorporation of a core clamp.

Since the scope of this study did not permit reoptimization of the modified design, the number of pins per assembly (331) and the core arrangement of 288 fuel assemblies, 25 control rod positions, and 138 radial blanket assemblies were unchanged. Similarly, the height of the core was left at the reference 34.7 inches, but the pin diameter was changed to 0.250 inch with a ligament of 0.055 inch to provide the desired burnup and fuel residence time. The key parameters for the reference design and those for the proposed modified design are compared in Table 5-2.

Figure 5-3 shows a conceptual design for a core clamp and its relationship to the core holddown assembly and the lower radial neutron shield. Since this design was carried only to the point of establishing feasibility, further effort will be required to establish an optimum arrangement with regard to increases in the core holddown assembly diameter, clamping pad locations on the ducts, and the overall length of the fuel assembly. This core clamp arrangement would be used on each outboard radial blanket element (72 places). Figure 5-4 is a plan view of the radial pattern. Each beam is radially loaded in the direction shown, so that the proper clamping load at the inboard elements could be attained.

The core clamp is attached to the core holddown assembly by a skirt (see Figure 5-3). Cams and plungers are mounted on the skirt for loading the beam springs, which are located in each outboard blanket element between the bearing pads. As the core holddown assembly is jacked down on the fuel assemblies, the core barrel actuates the

plunger through upward motion of the cam. The plunger deflects the spring, which puts a load into the blanket element at two planes through spiders. The clamp is unlocked through the plunger release mechanism, which forces the cam to the "down" position during the lifting of the cores holddown assembly, thus withdrawing the plunger and removing the load from the beam spring. The plunger release mechanism, therefore, requires downward and upward motion during the raising operation of the core holddown assembly. A trapezoidal plate section was chosen for the beam spring section as being the most desirable shape from the standpoint of maximizing the deflections for a limited stress level. The larger the deflections (within reasonable stress levels) the less effect system mechanical tolerances will have on load-producing capability.

The unloading sequence of the core clamp is the reverse of loading. No fuel movement occurs during these operations since the clamp only operates during the axial loading of the fuel assembly compression spring.

Table 5-3 shows the core design changes that were made to reduce the fluence by 30% and limit the midcore volumetric swelling to approximately 15%. The physics parameters and volume fractions of the modified core are listed in Table 5-4, and other related data are presented in BAW-1355.²

5.3.2. Design Evaluation

5.3.2.1. Volumetric Swelling

Growth calculations were made using both the ANL correlation and the PNL 20% cold-worked correlation. Figure 5-5 is a plot of the axial growth profile. The two correlations predict about the same peak growth in the innermost assembly of radial core zone 1 (18.5% for ANL and 18% for PNL). As in the reference design analysis, the ANL correlation predicts larger growths in the low-temperature, low-flux regions of the core.

5.3.2.2. Stress and Deflection

Figures 5-6 through 5-9 show deflections, restraint loads, and maximum stresses for the revised reference designs. A significant reduction in the maximum deflection and the

associated positive reactivity near the end of the fuel cycle is obvious from Figures 5-6 and 5-7. The axial distribution of deflection of the reference and modified fuel assemblies shown in Figure 5-7 indicates that, near the end of the fuel cycle, the reference fuel assembly would bow inward over the entire active core length. For the modified fuel assembly only about half of the active core would bow inward. The large reduction in the restraint loads at the intermediate support is obvious from Figure 5-8.

The stresses for the modified fuel assembly shown in Figure 5-9 increased over those for the reference design because, in reducing the neutron fluence level near the core centerline, the level and the gradients increased considerably near the interface of the core and radial blanket. Some modifications, such as rotation at half the fuel cycle, changing the section modulus of the wrapper can along the axial length, or raising the actual core from the reference base, may reduce the maximum stress significantly. The necessity of increasing the fuel assembly length is obvious from Figure 5-8, which shows the large reduction achieved in the restraint load by increasing the fuel assembly length from 100 to 121 inches.

5.3.2.3. Economics

The NAPS code was used in conducting the economic evaluation study to determine the effects of metal swelling for the modified reference design. The results indicate that the fuel costs were 0.98 mill/kWh compared with 0.75 mill/kWh for the reference design, and that total power generation costs increased from 3.47 to 3.80 mill/kWh. However, about 0.13 of the 0.33 mill/kWh was associated with modifications that were not directly related to the metal swelling problem, including the change from a twisted-ribbon to a wire type spacer. Furthermore, the penalty associated with modifying the design to accommodate metal swelling would be further reduced if the target 100,000 MWd/t burnup specified for the reference design could not be achieved through future research and development.

In addition, the decrease of both temperature and fuel burnup provides a design that is closer to present-day technology, thus reducing total research and development expenditures.

5.3.2.4. Safety Aspects

The phenomenon of metal growth under high-temperature irradiation presents several problems in fast breeder technology; thus, some departure from traditional design practices may be required. In particular, the core design, which has been modified to accommodate metal swelling, may no longer represent the optimum core from the points of view of economics and safety. Therefore, it was mandatory that the impact of core modification on reactor safety be carefully evaluated. However, the task is exceedingly complex, and the effort in this task was limited to a qualitative survey and limited analysis because of the constraints of time and funds available for this scoping study. The results are discussed in detail in BAW-1355 and summarized briefly in the following paragraphs.

Design Characteristics — The modification of the core will obviously affect the design characteristics of the system in a number of ways. Both operating margins and safety related parameters may be affected. For example, in considering operating margins, two principal parameters will be affected by changes in the core design: temperatures and burnup. The reactor thermal power and linear heat rate have been increased, and the pin size has been decreased by the changes described in section 5.3.1. These changes combine to produce a net increase in the steady-state fuel temperatures. This implies that the operating margin to fuel melting has been decreased. On the other hand, the coolant outlet temperature has been dropped 50F and the inlet temperature has been dropped 100F. This means that the cladding temperatures will be lower in the steady state. The coolant temperatures are lower, so the margin to coolant boiling has been increased.

The question of fuel pin failure states enters into all these considerations. The smaller pin and the increased heat rate mean that, for a given energy generation (MW-s), a larger fraction of the pin radius will be molten. This effect is partially offset by the increase in the ratio of cladding thickness to pin diameter. Therefore, it is not clear to what extent (if any) the failure states will be affected.

Although the effect of the changes in temperature is uncertain, there is little doubt that the reduction in burnup

improves the margin to failure. If pin performance as a function of lifetime could be accurately predicted, we could design the pin to have a certain margin to failure at its effective end of life. Ideally, this margin would be the same for any burnup, but the required accuracy cannot be calculated at present. Therefore, if it is assumed that the reference pin had some margin to failure at 100,000 MWd/t average burnup, we must conclude that the smaller pin, with its greater cladding thickness-to-diameter ratio, will have an increased margin to failure at the reduced burnup of 80,000 MWd/t.

In the case of safety-related values, the core design changes affect a number of parameters, including the Doppler coefficient, the sodium density coefficient, and the radial expansion characteristics. The Doppler coefficient of reactivity remains essentially unchanged by the design modifications because its magnitude is largely governed by the neutron spectrum. The addition of sodium in the modified core tends to soften the spectrum, which should increase the magnitude of the Doppler coefficient; however, this effect is offset by the higher enrichment and greater radial leakage in the modified core, both of which tend to harden the spectrum. As a result, the spectrum and hence the Doppler coefficient remain virtually unchanged. This means that there will be little difference in the response of the modified and reference cores in accidents that lead to rapid fuel heating, such as rapid reactivity insertions. For slower transients the excursion will be controlled by the slower feedback mechanisms, since the prompt feedback will be very similar.

Although the Doppler coefficient was not affected significantly, the large volume of sodium in the modified core causes the coolant void coefficient to be somewhat larger than the reference void coefficient—0.0219 and 0.0183, respectively. Coupled with the fact that the Doppler coefficient remains relatively unchanged, this increase means that flow transients and slow reactivity transients will be somewhat more severe in the modified core.

Probably the largest change in safety-related parameters is caused by the anticipated change in the radial expansion coefficient due to the core clamping scheme selected for the modified core. The reference radial expansion coefficient was calculated on the

basis of a uniform radial core expansion. The true expansion, however, is anything but uniform; it is a function of the transient type, power level, and power gradient (radial position). Hence, the bow is not only non-uniform axially, but its degree may also be a function of radial position. The core clamp and bearing pad arrangement selected for the modified core produces a sinusoidal bow within the core. The midplane deflection, which is initially outward, changes with life, so that the long-term net deflection at the end of life is positive. This effect is very slow and should cause only minimal problems. The transient bowing behavior of this arrangement, however, may have safety implications. The transient bowing phenomenon is a complex function of thermalhydraulic design and transient type. Since the magnitude of the deflection at any particular axial location is dependent on the thermal gradient at that location, the transient bow is strongly dependent on the time sequence of thermal events in the core. Thus, the response is likely to vary with transient type.

Outward bow is accomplished by establishing in the clamping area a thermal gradient of a magnitude similar to the core thermal gradient. In a reactivity transient there is a time lag between the establishment of the gradient in the core and that in the assembly extensions. The momentarily larger gradient in the core will cause a transient inward deflection and, hence, a positive reactivity feedback. Approximately 0.25 to 0.5 second later, the same gradient will be established in the upper portions of the assembly and the member will begin to bow outward, leading to a net negative reactivity effect.

In flow-loss situations the effect is reversed. The loss of flow initially causes the outlet temperature to rise, which will momentarily increase the gradient in the upper portions of the assembly, causing an outward deflection. As the flow coasts down the peak coolant temperature shifts upstream, and as this peak enters the upper core regions the bow will be inward. Thus, the reactivity effect is likely to be prompt negative and delayed positive in flow transients.

Accident Analysis — In surveying the characteristics of the modified design under potential accident conditions, the pertinent malfunction catalogs and fault trees developed in Phase I were reviewed to determine the effects of core design changes on the basic

faults and malfunctions, and two broad categories of initiating conditions were evaluated. The energy generation in a disassembly accident was also estimated. The results from the fault and malfunction survey are documented in BAW-1344,⁶ and the initiating conditions and disassembly investigations are summarized briefly below.

As in the Phase II work on the reference design, the accident-initiating conditions tend to fall into two broad categories: (1) those arising from flow abnormalities and (2) those arising from reactivity insertions. As expected, because the Doppler coefficient remains essentially unchanged and the sodium density coefficient is increased, the vulnerability of this modified design to flow-induced transients is increased. In order to compare the responses of the modified and reference cores, calculations were performed to simulate a flow ramp-down. The radial expansion coefficient was not included in the calculations. The temperature traces for these studies are presented in Figure 5-10. It will be seen that slightly higher coolant temperatures are incurred in the modified core; however, the difference is less than 1%.

In the case of reactivity-induced transients, the general response is as follows:

1. For slow reactivity transients in which there is some significant heat transfer from the fuel, the behavior of the modified core is similar to its behavior flow transients—it displays a slight vulnerability to very slow transients.
2. Since the Doppler feedback controls rapid reactivity-induced transients, there is no significant difference in the responses of the reference and modified cores.
3. Although it is not known exactly how redesigning the core will affect damage propagation, the effect is expected to be small.

The decrease in pin pitch at a constant pitch-to-diameter ratio will decrease the crossflow for a given pressure gradient. This may lower propagation thresholds somewhat. The thicker can walls and larger assembly gaps should act to inhibit assembly propagation.

In the evaluation of potential disassembly accidents, the magnitude of the energy generation can be estimated from previous sensitivity studies. Figure 5-11 shows the energy generation as a function of Doppler coefficient. This curve is representative of the reference core with a density coefficient of 0.0258. The initiating accident was a flow coastdown from power. The effective ramp rate at prompt criticality was \$37/s; this rate was governed by the rate of sodium expulsion from the core. The lower value of the sodium density coefficient in the modified core (0.0219 versus 0.258) will reduce the ramp rate and, hence, the energy generation, at prompt critical.

The curve in Figure 5-11 indicates that, for a sodium density coefficient of 0.0258 and a Doppler coefficient of -0.0070 , the energy generation will be 3.3×10^4 MW-s. Therefore, for $dk/d\rho = 0.0219$ and $T(dk/dt) = -0.0070$, we can predict with confidence that the energy generation will be less than 3.3×10^4 MW-s.

5.4. Evaluation and Conclusions

In general, the survey indicated that several fairly significant changes would be required in the reference plant parameters to reduce the growth of the structural materials to the point that design features could be employed to safely accommodate the residual dilation and deflection of the fuel assemblies, control rod guide tubes, and other related structures if the degree of swelling calculated from the current correlations were confirmed. Even if the degree of swelling currently predicted is reduced several fold, as expected, when experimental data are obtained at conditions closer to those in the reference design (or structural materials that resist irradiation-induced swelling better than those used in the reference design are developed), an open-core arrangement and a core restraint system similar to the one defined in the evaluation would probably be required. However, subject to reservations about the safety characteristics noted below, it appears that the conceptual changes defined in the evaluation could be incorporated into the reference design without imposing excessively severe limitations on overall plant reliability or economic performance.

The scoping evaluation also indicates that the potential changes would affect the safety characteristics of the plant in a number of ways,

but with the exception of the radial expansion behavior, the safety characteristics of the modified design are not expected to be significantly different from the reference design. However, the radial expansion characteristics of the core have changed to the extent that positive reactivity is inserted during life, and a limited transient analysis indicated that a prompt positive reactivity feedback may be obtained in certain transients due to the requirement for an open-core arrangement. In other transients, it is conceivable that prompt negative but delayed positive feedback could occur. In either case, the response could adversely alter the course and consequences of potential accident situations, but additional evaluations would be required to determine the significance of the potential movements.

Table 5-2. Key Plant Parameters — Reference and Modified Designs

Parameter	Reference design	Modified design
Reactor power, MWt	2450	2580
Avg discharge burnup, MWd/t	100,000	80,000
Fuel cycle life, yr	3	2
Flux > 0.1 MeV, n/cm ² -s (EOL)	3.78×10^{15}	4.36×10^{15}
Flux, total, n/cm ² -s (EOL)	6.80×10^{15}	7.77×10^{15}
Fluence > 0.1 MeV, n/cm ²	3.5×10^{23}	2.3×10^{23}
Bulk core inlet temp, F	800	700
Bulk core outlet temp, F	1100	1050
Steam cycle, psig/F/F	2400/950/950	1800/900/900
Plant capacity factor	0.86	0.85
Core		
Height, in.	34.7	34.7
Pin OD, in.	0.280	0.250
Pin pitch, in.	0.337	0.305
Cladding material	SS 304	SS 316
Spacer type	Tw ribbon	Wirewound
No. of pins per S/A	331	331
No. of fuel S/A	288	288
No. of control S/A	25	25
Pitch of S/A, in.	6.500	6.323
Can material	SS 304	SS 316
Can thickness, in.	0.128	0.140
Volume fractions (1/3 rods in core)		
Fuel	0.442	0.390
Steel	0.165	0.181
Sodium	0.381	0.418
Control	0.012	0.012
Fissile loading, kg	2770	2411
Avg enrichment, %	11.6	12.9
Core avg linear heat rate, kW/ft (BOL/EOL)	8.32/7.75	8.94/8.36
Nuclear power peaking (BOL/EOL)	1.48/1.50	1.52/1.39
Breeding ratio, EOC	1.35	1.28
ΔK void (EOL), $\Delta K/\Delta p$ (core + axial blanket)	0.0183	0.0219
Doppler effect, $-T(dK/dt)$	0.0075	0.0076

Table 5-3. Selected Procedure for Reducing Peak Fluence

Procedure	Reduction of fluence, %
1. Reduce fuel residence time:	20
Cycle time — 3.4 to 2.7 yr	
Burnup — 100,000 to 80,000 MWd/t	
2. Reduce fuel residence time and volume:	6
Cycle time — 2.7 to 2.0 yr	
Pin OD — 0.280 to 0.250 in.	
Burnup — constant at 80,000 MWd/t	
3. Optimize zone enrichment ratios:	4
Subject to constraint on peak/average power	
Total	30

Table 5-4. Description and Volume Fractions for Modified Core

	Core	Radial blanket	Axial blanket	Radial refl'r	Axial refl'r
Outer diameter, in.	113.7	136.5	113.7	160.2	113.7
Height, in.	34.7	42.7	14.0	42.7	11.8
Volume, liters	5784	3139	2331	3857	1967
No. of fuel assemblies	288	138	288	--	--
Zone 1	96	--	--	--	--
Zone 2	84	--	--	--	--
Zone 3	108	--	--	--	--
Fuel pin OD, in.	0.250	0.551	0.250	--	--
Fuel pin pitch, in.	0.305	0.581	0.305	--	--
Cladding thickness, in.	0.010	0.017	0.010	--	--
Assembly pitch, in.	6.323	6.323	6.323	--	--
Can thickness, in.	0.140	0.140	0.140	--	--
Sodium gap, in.	0.200	0.200	0.200	--	--
Fuel density, % TD	85	90	85	--	--
Fissile loading, kg Pu	2411	--	--	--	--
Initial enrichment					
Zone 1	0.1112	--	--	--	--
Zone 2	0.1233	--	--	--	--
Zone 3	0.1498	--	--	--	--
Avg	0.1292	--	--	--	--
Fuel residence time, EFPD	620	--	--	--	--
Avg disch burnup, MWd/t	80,000	--	--	--	--
Power, MWt	2580	--	--	--	--

Volume Fractions

	Fuel	Sodium	Steel	Tantalum
Core zone 1	0.3731	0.4320	0.1774	0.0175
Core zone 2	0.3954	0.4130	0.1819	0.0098
Core zone 3	0.4013	0.4079	0.1831	0.0077
Radial blanket	0.5884	0.2433	0.1683	--
Axial blanket	0.3898	0.3900	0.1850	0.0352
Radial refl	--	0.5000	0.5000	--
Axial refl	--	0.3560	0.1798	--

5.5. References

- ¹ 1000-MWe LMFBR Follow-On Study — Task II and III — Final Report, BAW-1328, Babcock & Wilcox, Lynchburg, Va., February 1969.
- ² 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Topical Report — Effects of Irradiation-Induced Metal Swelling on the Reference Design, BAW-1355, Babcock & Wilcox, Lynchburg, Va., November 1970.
- ³ C. Cawthorne and E. J. Fulton, "Voids in Irradiated Stainless Steel," Nature, 216, November 1967, pp 575-576.
- ⁴ W. H. Sutherland, "Creep Analysis of Statically Indeterminate Beams," Computer Code, Preliminary CRASIB — Workshop Reference, Battelle-Northwest, March 1970.
- ⁵ DEFLECT — Modification of Code ELBOW,⁷ Babcock & Wilcox (1969).
- ⁶ 1000-MWe LMFBR Accident Analysis and Safety System Design Study — Phase I — Fault Trees and Malfunction Catalog, BAW-1344, Babcock & Wilcox, Lynchburg, Va., December 1969.
- ⁷ ELBOW — Fuel Element Bowing Code, Argonne Code Center Reference, Material Abstract 229, Argonne National Laboratory.

Figure 5-1. Axial Linear Growth — PNL Correlation

Figure 5-2. Radial Linear Growth - PNL Correlation

Figure 5-3. Core Clamp Assembly Dwg (163340E)

Figure 5-4. Radial Loading Sequence

LEGEND

- FUEL ASSEMBLY - 286 REQ'D.
- CONTROL ROD - 25 REQ'D.
(S - SAFETY ROD POSITION)
- BLANKET ASSEMBLY - 138 REQ'D.

Figure 5-5. Volumetric Growth — Modified Reference Design

Figure 5-6. History of Deflection at Core Midplane for Modified and Reference Fuel Assemblies

Figure 5-7. Axial Distribution of Deflection for Modified and Reference Fuel Assemblies

Figure 5-8. Time History of Restraint Load at Intermediate Support for Modified and Reference Fuel Assemblies

Figure 5-9. Time History of Maximum Stress for Modified and Reference Fuel Assemblies

Figure 5-10. Maximum Coolant and Fuel Temperature Vs Time — Flow Transient

Figure 5-11. Energy Generation Vs Doppler Coefficient