FRT-1561
Rev. 1

FRT--1561 Rev. 1

DE82 012872

EX-VESSEL CORE CATCHER
DESIGN REQUIREMENTS AND
PRELIMINARY CONCEPTS EVALUATION

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Revised
June 14, 1974

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1.0 INTRODUCTION AND SUMMARY

As part of the overall study of the consequences of a hypothetical failure to scram following loss of pumping power, design requirements and preliminary concepts evaluation of an "ex-vessel core catcher (EVCC)" were performed. EVCC is the term applied to a class of devices whose primary objective is to provide a stable subcritical and coolable configuration within containment following a postulated accident in which it is assumed that core debris has penetrated the Reactor Vessel and Guard Vessel. Under these assumed conditions a set of functional requirements were developed for an EVCC and several concepts were evaluated. The studies were specifically directed toward the FFTF design considering the restraints imposed by the physical design and construction of the FFTF plant. Since the purpose of the evaluation was to assess the feasibility of various systems rather than to design an EVCC, the studies were not taken beyond the preliminary concept phase. The requirements and the results of the evaluation are presented in the subsequent sections of the report.

In performing the studies, the tacit assumption was made that some disruptive event had progressed to the stage where an EVCC was required. In fact, this assumption neglects that fact that the plant is designed with considerable margin under all steady state and transient conditions and has two independent and fast-acting shutdown systems, either of which is capable of terminating transients far more severe than those anticipated. Furthermore, after considerable studies have been performed, no initiators have been identified which could lead to such a postulated event. The studies performed on the EVCC also neglect many of the features inherent in the plant design which would prevent such postulated events from reaching a disruptive condition. Analyses have shown that even after a core disruptive event has been postulated to occur, the core fragments will generally be dispersed within the reactor vessel and can be cooled in place within the vessel. The heat removal capability of the plant under pony-motor flow and natural circulation is sufficiently redundant to remove the decay heat of the system adequately even under this extreme condition.

To perform the EVCC study, therefore, it was necessary to make a series of pessimistic assumptions which were contrary to what might be nominally expected. This was necessary in order to obtain a set of boundary conditions for evaluating the performance of the EVCC. However, even though the need for an EVCC must be predicated upon an extreme sequence of extremely unlikely and improbable events, the studies contained in this report were performed to establish whether an EVCC concept was even feasible. The results of these studies indicate that even under the most optimistic assumptions the feasibility of an EVCC is, at best, marginal. Areas which appear to offer the most problems are 1) assuring that the debris from the postulated meltdown can be directed into the proper position without causing recriticality, and 2) providing a heat removal system (i.e., coolant system) which in itself has a higher degree of reliability than the reactor system which has been designed and fabricated to the highest quality standards. The ability to design, fabricate, test, and inspect the EVCC system under the space restriction available and to assure that it will function properly after the months and years of non-functional service is questionable.

2.0 FUNCTIONAL REQUIREMENTS

The primary objective of providing a sub-critical and coolable debris configuration can be met by fulfilling the following functional requirements. These may be modified if subsequent analyses show that less conservative requirements would be adequate.

2.1 General Requirements

2.1.1 The Ex-Vessel Core Catcher (EVCC) shall be designed to terminate the consequences of a postulated core meltdown.

2.1.2 The EVCC shall be designed to prevent recriticality of the debris from the core meltdown.

This may be accomplished by the geometrical configuration of the design and/or by the inclusion of poisons. Problems to be overcome include the possibility of structural geometry changes (e.g., from debris or crust formation), assuring that poisons remain within the debris, and transient configurations (e.g., more fuel falling into a barely subcritical pool).

2.1.3 The EVCC shall be designed to assure that the molten debris penetrating the Guard Vessel drops into the Lower Cavity through the central five-foot diameter hole.

This may require sloping firebrick layer on the Reactor Cavity floor beneath the Guard Vessel to guide debris to the central hole.

2.1.4 Either the system shall be designed to be effective in the event that the core debris is stopped at the Reactor Cavity floor beneath the Guard Vessel, or the design shall assure that all debris which penetrates the Guard Vessel reaches the Lower Cavity.

2.1.5 The EVCC design shall be compatible with the limited accessibility of the Lower Cavity.

Access to the Lower Cavity for installation of the EVCC is limited vertically to the 5 foot diameter central opening which will eventually be covered by a 3/8 inch thick steel plate, and horizontally to the two blockouts 5 foot wide by 6 foot 9 inch high.

2.1.6 The EVCC design shall be compatible with the limited space available in the Reactor Containment Building.

Space for the EVCC cooling system equipment and ductwork is very limited because of the number of poured concrete floors already in place and the space allotted for other equipment. Space for EVCC equipment may be limited to the Lower Cavity and blockouts, with ties to the H & V Mobiltherm cooling systems. The Mobiltherm systems have a total capacity of 14,400 BTU/hour, about 70 to 80% of which is available to the cooling areas adjacent to the Lower Cavity.

2.1.7 The EVCC either shall operate on a different principle from the in-vessel core retention system, or shall be designed for more severe conditions than the in-vessel system can handle.

If the ex-vessel heat removal system were to operate on the same principle as the in-vessel system, then it could be postulated to fail by whatever mechanism failed the in-vessel retention system, unless the ex-vessel system could be designed for more severe conditions than could the in-vessel system. This leads to the preference of a sacrificial barrier concept over that of a catch grid submerged in a sodium pool. The sacrificial barrier absorbs decay heat and dilutes the molten pool, thereby reducing the heat load on the cooling system.

2.1.8 The sacrificial barrier material shall be as inexpensive as possible and available.

This makes depleted UO_2 undesirable as it could require an expenditure of approximately ten million dollars to fill the Lower Cavity with UO_2 in the form of bricks.

2.1.9 Any interaction between the sacrificial barrier material, molten fuel, stainless steel, and sodium shall result in low heat generation rates compared with the heat absorption capacity of the barrier.

Preliminary information received indicates that this requirement may not be met by basalt, the original reference barrier material, which appears to interact strongly with sodium. Therefore, depleted urania may be the preferred material although it is much more expensive (see Requirement 2.1.8). Thoria is another possibility. Glasses should not be ruled out, as their compositions may be tailored to avoid the interaction problem, and they can be inexpensive and easily fabricated. However, their use may require an extensive development program (see Requirement 2.1.14).

2.1.10 The EVCC shall not interfere with the safety of the plant design in level one and level two conditions.

Two critical areas in which the EVCC might give rise to unsafe level 1 and 2 conditions are: (a) the close confinement and lack of space and access for the EVCC installation with possible consequences of interface interference with other equipment in adjacent cells, (b) the possible connection of the cooling system with the Mobiltherm system which has a normal operational function, and (c) possible undesirable interactions between the EVCC monitoring and control systems with normal plant monitoring and control systems.

2.1.11 Power supplies and controls shall be provided as required for the system to function.

2.1.12 Provision shall be made for periodic testing of the system to assure that it is operational.

2.1.13 The EVCC system and its power supplies shall be designed as a Seismic Class I structure and shall be tornado hardened.

This is required because the system would be required to operate for some months following an HCDA. To meet this requirement, the diesel generators, for example, which are now Seismic Class II would require upgrading to Seismic Class I and would need to be tornado hardened. Alternatively, a battery supported Class IE power supply would be necessary. The Mobiltherm system is cooled by a Freon-type refrigeration machine with an outdoor radiator transmitting heat to the air by natural circulation. The radiator is probably the part of the system most susceptible to tornado damage.

2.1.14 Installation of the EVCC shall minimize cost and schedular impact on FFTF construction.

Note that this would rule against concepts requiring a lengthy development program.

2.2 Structural Requirements

2.2.1 The EVCC shall be designed to support its own weight plus that of the fuel and structure in and beneath the core, the sodium head, the impact from dropping debris, and the effects of possible fuel-sodium or steel-sodium interactions.

2.2.2 The Reactor Vessel and Guard Vessel shall not collapse following the postulated core meltdown.

The reactor primary coolant flow would be broken because of the drop in sodium level if the Guard Vessel were penetrated. Sodium in the Reactor Vessel, between the Reactor and Guard Vessels, and between the Guard Vessel and the Reactor Cavity floor can heat up to the sodium boiling point, as can the Reactor Vessel and Guard Vessel walls and supports, and sodium may leak through the Guard Vessel supports into the surrounding Reactor Cavity. High temperature mechanical property data for Type 304 stainless steel (Ref. 1) may be used for structural analysis of the vessels to determine if these conditions are acceptable. If not, the ex-vessel cooling concept may need to be designed to provide for the protection of the vessel supports.

2.2.3 The concrete walls of the Reactor Cavity and the Lower Cavity shall retain their structural capabilities following the postulated core melt-down.

The Reactor Cavity and Lower Cavity walls support the Reactor Vessel and Guard Vessel, respectively, and therefore this requirement follows from the previous requirement. In addition, the walls protect the equipment in the adjacent cells which may or may not be required after such an accident. The design temperature of the concrete is 250°F. However, it can probably be shown that this temperature limitation could be relaxed to perhaps 400°F or more without impairing the structural capability of the concrete.

2.2.4 Cavity walls shall be protected against contact with debris and sodium so that there shall be no gap at the top of the Lower Cavity through which the debris and/or sodium can reach the concrete walls of the cavity.

2.3 Cooling System Requirements

2.3.1 An auxiliary cooling system shall be used to remove decay heat from the fuel captured in the EVCC.

Under certain conditions, a completely passive core catcher could be adequate, with decay heat conducted into the containment substructures (Ref. 2). However, preliminary calculations showed that an auxiliary cooling system would be required for the FFTF core catcher.

2.3.2 The cooling system shall be designed to handle the maximum local heat flux which can occur at any location, and the maximum total heat load.

2.3.3 The system shall be capable of operating until the decay heat level drops to the point where heat losses to the surrounding compartments are sufficient to maintain temperatures at acceptable levels.

2.3.4 Either the primary EVCC cooling system shall not penetrate the Reactor Containment Building , or provision shall be made for isolating any portion of the system which may become radioactively contaminated.

2.3.5 Insulation shall be provided on piping and components to prevent excessive heat losses to the Reactor Containment Building.

If a natural circulation cooling system is used, insulation may also be needed to maintain the temperature difference between hot and cold legs in order to provide the buoyancy driving head.

2.3.6 Redundancy shall be provided in the core catcher cooling system.

The redundancy shall provide against common-mode failures (e.g., off-center molten pool locally contacting and melting through coolant tubes and wiping out all redundant cooling systems) unless such failures can be shown to be extremely unlikely.

2.3.7 The coolant shall not interact with the debris or sodium in the event of a local failure of the core catcher cooling system.

2.3.8 The cooling system shall be designed to prevent it from freezing.

It may be noted if NaK is used in the EVCC cooling system for example, that the H & V Mobiltherm system to which the NaK may exchange its heat operates at 65° to 45°F which is below the 66°F melting point of Na(56%)/K (44%).

2.3.9 Either a cleanup system for the cooling system must be provided or the cooling system must be designed to remain functional for the 20 year plant life without cleaning.

2.3.10 Contact between the cooling system fluid and air or water shall be prevented if such contact produces undesirable reactions.

2.3.11 If natural circulation cooling is used, the system shall provide an adequate driving head by arranged elevations and it may be necessary to design against the initiation of reverse flow.

2.3.12 The EVCC cooling system shall be instrumented to detect loss of coolant, plugging, temperatures, and possibly, radioactivity.

2.3.13 The EVCC cooling system shall be designed to permit appropriate periodic inspection of important components, to assure the integrity and capability of the system.

2.3.14 The EVCC cooling system shall be designed to permit appropriate functional testing to assure (1) the structural and leak tight integrity of its components (2) the operability and performance of the active components of the system and (3) the operability of the system as a whole and, under conditions as close to design as practical, the performance of the full operational sequence that brings the system into operation, including operation of applicable portions of the protection system, the transfer between normal and emergency power sources, and the operation of the associated ultimate heat sink.

CONCEPT FEASIBILITY EVALUATION

3.0 MATTERS RELATING TO THE FEASIBILITY OF AN EVCC

A review of matters related to the feasibility of an EVCC indicate that two issues may have a significant impact on any concept selected. These are potential fuel/coolant interactions (FCI) and the behavior of the molten fuel following the postulated meltdown.

3.1 General Model for FCI in an LMFBR Environment*

Much of the work performed on the expansion and collapse of vapor bubbles has related to a bubble in an "infinite" sea of liquid (e.g., References 3 and 4). However, fuel-coolant interactions occur at the interface between two different liquids be they molten metals and water, molten UO_2 and sodium or water and liquified natural gas. The problem of vapor bubble collapse in the presence of a boundary has now been considered by Plesset and Chapman (Reference 5). The collapse of a bubble in contact with a wall is shown in Figure 3.1. During the sequence a notable phenomenon is the formation of a high velocity jet of coolant impacting the wall as the collapse progresses.

This has been incorporated into a model of fuel-coolant interactions by Buchanan and Dullforce (Reference 6). Their model is an attempt to overcome the problems associated with other models, many of which incorporate homogeneous nucleation in water is 290°C but "explosions" have been observed between water and tin with the tin initially below 290°C . Furthermore, water is very difficult to superheat, even with careful precautions, but FCI's still occur under industrial conditions. Finally the heat transfer rates required are "3 orders of magnitude greater than those that occur during boiling processes" (Reference 6). This implies that a large increase in surface area is necessary. The model is shown schematically in Figure 3.2.

a) A triggering mechanism brings the liquids into intimate contact and a vapor bubble is formed.

b) The bubble expands and collapses due to condensation in sub-cooled liquid. Due to density differences or perhaps freezing of hot fuel liquid (in our case UO_2) the collapse is assymetric

* Other models which are available may reach somewhat different conclusions than those here.

(after Plesset and Chapmen) and a jet of cold coolant is formed.

- c) The jet penetrates the fuel and, due to the high velocity, disintegrates.
- d) The small coolant droplets have a high surface area causing rapid heating of the coolant jet.
- e) When the jet reaches a high temperature it vaporizes to form a high pressure vapor bubble which expands, dispersing some of the fuel into the coolant as a spray. This vaporizes the cold liquid forming a bubble and the cycle repeats itself from b) above.

This cyclic behavior is in accord with experimental observation. This mechanism also provides temperature and pressure thresholds which can diminish the interaction or even prevent its initiation, both of which effects have been observed.

An initial study was commenced to extend the quantification of the above as in Reference 6 to UO_2 /Sodium conditions; however, more time was required to determine the various unknowns than was justified at this time. Therefore, the simpler ANL model of Fauske was used in the current assessment. (Section 3.2)

3.2 ANL Model for UO_2 /Na Vapor Explosions

This model has been proposed by Fauske (References 7 and 8) and may be stated as follows: "vapor explosions can occur on mixing a cold liquid with a hot liquid if the temperature of the cold liquid is raised to the temperature limit for spontaneous nucleation". The contact temperature, T_i , can be defined by

$$\frac{T_i - T_c}{T_H - T_i} = \left(\frac{(K\rho c)_H}{(K\rho c)_C} \right)^{\frac{1}{2}}$$

where K , ρ , and c are the thermal conductivity, density and specific heat respectively and subscripts H and C refer to the hot and cold liquids respectively. The contact temperature even for boiling sodium (assumed 1700°F) and molten UO_2 (5000°F) is only 2917°F compared to a spontaneous nucleation temperature of about 3720°F. This would indicate that an FCI could not occur (Figure 3.6).

However, Armstrong (References 9 and 10) has demonstrated that FCI's can be produced during small-scale out-of-pile tests. Fauske in Reference 8 details an explanation of these tests including entrainment, wetting and superheating and reasons why these conditions do not apply to an LMFBR environment. Until further work clarifying the impact of the various models on reactor environments can be undertaken the results of the above ANL work are applied to the appropriate reactor situations.

3.3 Fragmentation During FCI

Although an FCI may not result in a vapor explosion, substantial heat transfer is effected which may be violent and accompanied by substantial fragmentation of the hot fuel liquid. Early work in this field (Reference 11) proposed a fragmentation criterion: a molten metal should not fragment in a liquid if its solidification temperature is above the boiling transition temperature. The boiling transition temperature should be evaluated from Henry's correlation (Reference 12).

Defining a vapor explosion as occurring when "the vapor produced cannot be relieved quickly enough to prevent pressurization and the formation of shock waves" (Reference 8) then it is apparent from Figures 3.5 (Reference 13) for the M-series tests 1-3 that vapor explosions occur. However, it is also apparent that even with these relatively large quantities of UO_2 and sodium in a small rig that massive pressurization and explosions did not occur. It is anticipated that considerable, if not total, fragmentation will occur at the locations* most likely to be affected by a large FCI and that the maximum sustained pressure will be of the order of 500 psi with 1000 psi pulses of short duration (see Section 4). However, the energy content of these pulses is small due to the short duration.

*In the core basket region of the core support structure after melt-through of the core support plate or penetration to the assembly inlet nozzle by molten fuel and in the lower plenum following melt-through of the CSS lower dome.

3.4 Molten Steel/Coolant Interaction (MSCI)

A thermal interaction between molten steel and liquid sodium could result in generation of large pressure pulse if the condition of the two liquids were such that the cold liquid is raised to the temperature limit for spontaneous nucleation⁽²²⁾. When this condition is reached, vaporization may be rapid enough to produce shock waves. However, an evaluation made by Agrawal (Reference 23) shows that the molten steel-coolant interaction (MSCI) is not explosive for a wide range of conditions.

The figure below shows the excess temperature required above saturation temperature for liquid water and sodium to nucleate spontaneously⁽²²⁾. A rapid heating of cold liquid can result from a sudden contact with a hot liquid and the contact temperature, T_i , is given by the equation in Section 3.2.

Comparison Between Spontaneous Nucleation Temperatures and Sudden Contact Temperatures for Three Different Systems.

Agrawal (Reference 23) calculated the contact temperature for a number of molten steel temperatures ($T_{\text{hot}} = 2500^{\circ}\text{F}$, 4000°F , and 5000°F) and a value of 1466°F for the sodium temperature ($T_c = 1466^{\circ}\text{F}$). Results are shown in the above figure along with Fauske's results for $\text{UO}_2\text{-Na}$ and $\text{Al-H}_2\text{O}$ systems. It is seen that for the molten steel temperature (5000°F) approaching its boiling point ($\sim 5100^{\circ}\text{F}$ at atmospheric pressure), there exists a possibility of a violent thermal interaction. For molten steel temperature of up to 4000°F , the contact temperature is well below that required for spontaneous nucleation. It is further added that at these high temperatures of the molten steel, the contact temperature is not altered significantly due to change in the cold liquid (sodium) temperature. For example, the contact temperature for the steel temperature of 4000°F is calculated to be 3316°F and 3391°F for sodium temperatures of 1466°F and 1650°F , respectively. It is, therefore, concluded that the MSCI would not be of a postulated explosive type for steel temperatures of the order of 4000°F . Since it is doubtful that large quantities of liquid sodium could exist in that environment (i.e., the sodium would undoubtedly be in the vapor phase), the possibility of a MSCI is considered to be extremely unlikely.

Shape	Time	Velocity (m/sec)
<i>A</i>	0.63	7.7
<i>B</i>	0.885	19
<i>C</i>	0.986	42
<i>D</i>	1.013	65
<i>E</i>	1.033	100
<i>F</i>	1.048	125
<i>G</i>	1.066	129
<i>H</i>	1.082	129
<i>I</i>	1.098	128
<i>J</i>	1.119	128

FIGURE 3.1
COLLAPSE OF A BUBBLE ADJACENT
TO A BOUNDARY

FIGURE 3.2
FCI CYCLIC SEQUENCE
 (after Buchanan & Dullforce)

FIGURE 3.3

FIGURE 3.4
 RUN M2 6.8 lb UO₂-Mo-Cr
 7.1 lb Na AT 570°F

FIGURE 3.5
 RUN M3 3.0 lb UO_2 -Mo-Cr
 6.5 lb Na, AT 1160°F

FIGURE 3.6 Comparison between Spontaneous Nucleation Temperatures and Sudden Contact Temperatures for Two Different Systems.

4.0 FUEL BEHAVIOR DURING A POSTULATED MELTDOWN

This section attempts to describe in general terms the behavior of the fuel at various locations during the course of the meltdown and assess the likelihood of fuel/coolant interactions of destructive capability. The locations to be detailed are shown in Figure 4.1

4.1 Accident Sequence

4.1.1 In-Core

It is a prerequisite that the fuel channel must be continuously locally voided on the order of a second for clad melting to occur. Since clad melting must occur before gross fuel movement it will be assumed the molten fuel does not contact large quantities of liquid sodium when it initially moves. The sodium that may be available would be present as vapor or a film adhering to some neighboring pins. Due to the relatively small quantities and thicknesses and the presence of fission gases, contact between the two liquids would cause increased evaporation rates rather than a FCI.

As the fuel continues to slump it would repeatedly solidify and remelt as the pins collapse and drip. Any sodium retained in the lower portions of the pin structure of the assembly would be heated by increased radiation or spattering drips of steel or fuel and would have evaporated or be boiling before gross quantities of fuel can contact it.

It is concluded that the vapor explosions will not occur during the in-core part of the meltdown sequence.

4.1.2 Shield Blocks and Core Support Plate

If the accident progresses further so that virtually the whole of the assembly slumps it can be shown that even with flow in the outer subchannels, the fuel is boiling in the center of the assembly. Any sodium percolating around the periphery of the assembly or re-entering the assembly from the top would be vaporized by the fuel vapor or refluxing fuel. This would tend to pressurize the assembly and prevent sodium entry. Probably a quasi-steady state chugging cycle would be established in the upper structure of the assembly.

As the fuel forms a melting attack on the shield blocks or assembly walls, the sodium in contact with these items would tend to vaporize before breakthrough occurs. This presents a cushion of vapor (with attendant boiling sodium) to the fuel at melt-through. Similarly as the fuel melts through the lower shield block into the nozzle assembly drops of molten steel or fuel would fall through the vapor cushion into the sodium below. This will particulate the drop and may set up a chugging motion of the sodium interface within the nozzle at the level of the inlet flow cutouts. The debris would tend to wash out through the cutouts and fall into the CSS bottom plate although some would possibly clog the gaps between the nozzle assembly, liner, and receptacle.

4.1.3 Core Support Structure

Although some of the particulates may find their way through labyrinthine paths between the receptacle, liner, and nozzle into the CSS lower dome, the paths would tend to clog and most of the debris would fall between the receptacle tubes onto the core basket bottom plate. Such a debris bed at 0.55 porosity, is about 15 inches deep. However, the bottom of the windows is only 3.5 inches above the bottom plate and most of the debris is envisioned as washing through the 0.25 inch diameter holes in the screen. Normal flow direction does not exist due to sodium vaporization and blockages. The screen is 40% open and although the largest particles (< 2% total fines) in the bed may block the holes it is anticipated that the final leveled bed is not much in excess of 3.5 to 5 inches deep. This is because the heavier particles will settle into the bed and the finer particles will wash out. This represents a bed loading of $38-55 \text{ gm/cm}^2$. Furthermore the bed is penetrated by the 151 receptacle tubes which will help to remove some of the heat. This bed represents 23 to 33 percent of the core fuel in what is anticipated to be a stable configuration. If dryout and melt-through of the bottom plate or receptacles should occur then the lower dome should be able to retain this percentage of the core indefinitely either as a debris bed or a molten pool.

Some of the debris which washes through the screen would fall into the gap between the basket and the barrel. That which is carried through the barrel window will be caught by the geometry of the CSS (Figure 4.2). These "cups" can retain a further 8 percent of the core as a level bed with 0.55 porosity. This again represents a stable configuration with an average bed loading of $14.7 \text{ gm UO}_2/\text{cm}^2$ with a maximum loading of 44 gm/cm^2 .

4.1.4 Vessel Lower Head

The remaining debris, representing 59 to 69 percent of the core, would fall into the vessel lower plenum and form a bed in the lower head of the vessel. The parameters of the resultant bed are a maximum depth of 6.0 to 6.5 inches and a loading of about 70 gm/cm^2 . The time for the power to reach an acceptable level (~ 0.25 percent) is 55 to 110 hours after subcriticality. This indicates that the holdup times associated with the various phases of the melt-down and the percentage of fuel retained at each step is of utmost importance. Figure 4.3 indicates the relationship between bed depth, time for the power to decay to non-dryout levels and location of the core. For example, in order to demonstrate a stable bed with 40% of the core in the lower head a total holdup time of about 11 hours must be identified between the various melting processes and 60% core retention in the core and support structure. This represents a worst case analysis and applies to the center of the bed.

The time required for bed dryout and particle coalescence to be established is unknown. However, it must be assumed that a melting attack on the vessel is initiated although final melt-through may not occur.

4.1.5 Guard Vessel

If melt-through of the reactor vessel occurs, the detailed sequence leading to failure of the guard vessel is given in Section 4.2. In summary, the debris bed is deposited at the angle of the lower plate and conical section of the vessel due to changes in the flow hydraulics. Melt-through of the guard vessel is less likely than that of the reactor vessel and this probably

represents a stable termination, particularly if the anticipated bed retention occurs in the core support structure. Moreover FCI's are not anticipated in either the lower plenum or the guard vessel.

4.1.6 Ex-Vessel Core Catcher

Whether a thermally stable bed configuration is attained in the core catcher depends upon the time taken to reach the EVCC. However, even if a melting attack on the catcher bed occurs, this represents a stable thermal termination to the accident since the EVCC would be designed to handle a molten core. No FCI's are anticipated under these conditions, although vigorous sodium boiling will occur. (See Section 3)

4.1.7 Summary of Postulated Sequence

The above postulated accident sequence indicates that violent vapor explosions of destructive potential are not anticipated during the course of the accident. However, the nuclear behavior of debris beds, large and small, must also be evaluated to determine if the consequences of these geometries are acceptable (i.e., recriticality does not occur).

4.2 Need for A Catcher Cone

Based upon the available experimental evidence, it appears that if a debris bed is formed in the lower head of the reactor vessel, that the heat generation rate and bed loading at the center would exceed the debris bed loading curve (Figure 4.4) and that dryout would result. The bed loading at the bed center is about 90 gm/sq.cm., assuming no retention, and the generated heat flux at 10,000 seconds is about $(350 \pm 39) \times 10^3$ BTU/hr ft² (corresponding to $0.8 \pm 0.1\%$ power). This is far in excess of the permissible maximum heat flux for dryout (Reference 14). Thus it is possible that vapor blanketing and particle coalescence may occur followed by melt-through of the reactor vessel.

If this occurred the sodium would leave the vessel through the hole sweeping the debris bed out with it (Figure 4.5). As the sodium velocity decreases, due to increasing flow area, increasing areal extent and reduced static head as the guard vessel fills, the fuel particles will settle. When the

levels in the guard vessel and reactor vessel have equalized, the debris in the annulus and sloping surfaces will settle forming a debris bed in the region shown in Figure 4.5.

If the bed is assumed to contain the whole core, to have a uniform profile and to contain all the fuel the maximum bed depth is 5.56 inches. This represents a peak bed loading of $70.5 \text{ gm UO}_2/\text{cm}^2$. The heat generation rate depends upon the time at which the bed reaches this position but melt-through of the guard vessel is still possible. However, it is unlikely that all the fuel would be involved in such a configuration and the guard vessel would probably maintain its integrity.

If melt-through did occur, the above indicates that it would happen several feet away from the hole into the lower cavity. This shows the necessity for a conical device to direct the sodium/particle slurry into the EVCC. The core is shown in Figure 4.6 in relation to the guard vessel, vessel insulation and the lower cavity. The largest possible face angle is desired to minimize possible buildup problems on the cone. The cone is also extended up to the guard vessel skirt to prevent wash over behind the cone permitting attack on the skirt itself.

4.2.1 Candidate Materials

Although most of the fuel will be as particulates, proposed materials should be resistant to attack by molten fuel as far as possible. Since UO_2 melts at 3000°K , the choice of materials is limited. From Reference 15 potential materials are magnesium oxide (Mg O) and thorium oxide (Th O_2) with melting points of 3075°K and 3225°K , respectively. Thoria presents a fabrication problem with attendant cost implications. The magnesia, on the other hand, should be relatively inexpensive. Problems may be encountered with respect to erosion by the particles but a simple test program should identify any requirement for sheathing material in the vicinity of the hole. The removal of thermal insulation from the guard vessel would not represent a problem since the replacement materials are also insulators. Potential interactions between Mg O and UO_2 should be considered experimentally.

a

d

b

e

c

f

FIGURE 4.1
LOCATIONS AT WHICH
FCI POTENTIAL EXISTS

FIGURE 4.3

FIGURE 4.4
DEBRIS BED LOADING CURVE

FIGURE 4.5
DEBRIS REDISTRIBUTION AFTER VESSEL MELT-THROUGH

FIGURE 4.6
LOCATION OF CATCHER CONE

5.0 EX-VESSEL CORE CATCHER DESIGN CONCEPTS

Several EVCC design concepts are discussed in this section including completely passive, passive with natural circulation, and forced circulation systems. A completely passive system is one which does not utilize circulating coolant. Heat is removed by conduction into the ground or to the atmosphere. A passive system is one which utilizes naturally circulating coolant and requires no power supply. An active system uses forced circulation cooling requiring a power supply.

For purposes of the present discussion, it has been assumed that core debris has penetrated the bottom of the Reactor Vessel following a postulated core meltdown. It is also assumed that no secondary criticalities and no violent fuel-coolant interactions occur. As noted in Section 4, it is possible that the core debris could be retained and cooled in place in the Guard Vessel. However, if melt-through of the Guard Vessel did occur, it may happen several feet away from the center of the bottom plate and therefore a conical device is required on the Reactor Cavity floor beneath the Guard Vessel to direct the debris to the central hole into the lower cavity. The following sections discuss some of the consequences of a postulated meltdown.

Functional requirement 2.1.7 (Section 2) for EVCC states that the EVCC either shall operate on a different principle from any in-vessel core retention system, or shall be designed for more severe conditions than the in-vessel system can handle. Otherwise, the ex-vessel system could be postulated to fail by whatever mechanism is assumed to have failed the in-vessel retention system. This leads to the preference of a sacrificial barrier concept over that of a catch grid submerged in a sodium pool.

All of the concepts considered utilize sacrificial barriers to absorb the initial heat content and the large initial decay heat generation rate, and to dilute the molten pool, thereby reducing the heat load on the heat removal system. Basalt has been considered a leading contender for a sacrificial barrier material. However, recent information indicates that basalt may interact with sodium and generate a large amount of heat.

The uncertainties associated with basalt and various glasses make these materials questionable in application to EVCC's and ANL recommends that either depleted urania or thoria be considered for any near-term applications. The function of the sacrificial barrier is to absorb the decay heat and dilute the molten pool, thereby reducing the heat load on the heat removal system.*

5.1 Completely Passive System

5.1.1 Basalt Bed

A completely passive system with a basalt sacrificial barrier is described by Jansen and Stepnewski (Reference 2). The molten fuel was assumed to rapidly dissolve basalt to form a homogeneous molten solution. Upward and downward heat transfer coefficients are estimated which determine the degree of superheat in the pool and the fraction of the heat which flows downward. The pool is assumed to grow hemispherically, and equations are given which approximate the temperature gradients in the basalt underlying the pool. Two main cases are considered, with the pool either allowed to seek its own maximum size, or with its growth limited by the use of non-soluble insulation at an arbitrary boundary.

For the first case, Figures 5.1, 5.2, and 5.3 show the relation between molten pool radius, temperature and time. This result is not acceptable for FFTF, as the molten pool reaches a maximum radius of 12.78 ft. which somewhat exceeds the dimensions of the Lower Cavity. From Figure 5.4, it is seen that the concrete walls of the Lower Cavity would become hot enough to lose their structural capabilities, assuming that the thermal properties of concrete are not too different from basalt.

For the second case in which pool growth is limited by a nonsoluble insulation at the boundary, Figure 5.5 shows the insulation temperatures for various pool radii, Figure 5.6 shows the upward and downward heat flow, Figure 5.7 shows the transient temperature profiles through the insulation and backing concrete, and Figure 5.8 shows the effect of substituting a lower viscosity sacrificial material for basalt.

* ANL is in the process of calculating pool growth for various models. Preliminary results are given in Table 5.3.

Figure 5.7 shows a maximum concrete wall temperature of about 450°C which is at about the upper limit of what can be tolerated, and makes no allowance for the uncertainty in concrete thermal properties during long-term exposure to high temperatures. The 11-foot pool radius is also larger than permitted by the Lower Cavity dimensions. Allowing for 3 feet of insulation, the maximum pool radius would be 7 1/2 feet, resulting in higher temperatures than for the 11 foot radius as is shown in Figure 5.5.

The availability of a suitable nonsoluble insulation of low enough thermal conductivity is open to question. Reference 2 notes that at 1300°C observed corrosion rates in molten basalt for high magnesia ranged from 0.07 to 0.10 in/day and 0.013 in/day for mullite.* The corrosion rate of high alumina was decreased from 0.03 to 0.003 in/day as the Al_2O_3 content was increased from 90 to 99%. At 1580°C all specimens tested showed corrosion rates appreciably greater than 0.5 in/day except 98% MgO which was attacked at a rate of 0.5 in/day.

It would seem reasonable to separate the insulation and nonsolubility requirements on the insulation by having a nonsoluble liner such as tungsten separating the molten pool from the insulation.

Figure 5.6 shows somewhat more than 10^6 BTU/hr being transferred upward to the sodium during the first 200 hours and decreasing slowly thereafter, so that a heat removal system may still be required for the sodium above the EVCC even though the system was considered to be completely passive.

The assumption of rapid solution of the basalt in the molten fuel requires confirmation as does the estimated heat flow equations. Recent ANL analyses indicate higher heat fluxes in the sideways (as well as upward) direction than in the downward direction, resulting in a molten pool which is flat rather than hemispherical and which therefore has a larger heat flux when the sides of the pool reach the boundary than does the hemispherical model.

* Mullite has the composition $3Al_2O_3 \cdot 2SiO_2$

5.1.2 Depleted Urania Bed

A completely passive UO_2 barrier was suggested by Zivi (Reference 16). It is still assumed, however, that the decay heat not absorbed by the barrier is removed from the containment vessel by some other means.

The analysis considers that the rate of penetration of the barrier by the fuel mass would be very low because:

- 1) The molten fuel containing the heat-generating fission products would tend to float on top of the molten layer of the barrier because of the higher temperature and dissolved lighter elements in the fuel.
- 2) For the melting front to advance through the barrier, the heat required to melt the barrier UO_2 would have to be conducted through the previously melted portion of the barrier. The low conductivity of the barrier UO_2 makes this a slow process, relative to the case in which a dense heat source displaces the melt and continually moves into contact with fresh unmolten substrate material.
- 3) The large enthalpy change required to raise cavity temperature UO_2 through melting also makes the melt-through process a slow one.

Approximating the barrier by a semi-infinite slab initially at uniform temperature T_0 , Equation (1) gives the position of the melting front as a result of imposing a temperature T_s at the slab surface. Zivi obtained Equation (1) from Reference 17 by assuming that the thermal properties of the slab are not changed by melting

$$X = mt^{1/2} \quad (1)$$

where

X is the displacement of the melting front

t is the time after imposing surface temperature T_s
 m is a coefficient given implicitly by

$$\frac{T_s - T_m}{\text{erf } Z} - \frac{T_m - T_o}{1 - \text{erf } Z} \approx \frac{\lambda \rho m}{2} \frac{(\pi \alpha)^{1/2}}{k} \exp Z^2 \quad (2)$$

where

T_m = melting temperature of slab (2800°C)
 α = thermal diffusivity of slab ($5.3 \times 10^{-3} \text{ cm}^2/\text{sec}$)
 $Z = 1/2 m \alpha^{-1/2}$
 λ = latent heat of fusion (60 cal/g)
 ρ = density (9.4 g/cm^3)
 k = thermal conductivity ($5 \times 10^{-3} \text{ cal/cm}^\circ\text{C sec}$)

Taking the above numerical values and with $T_o = 20^\circ\text{C}$ and $T_s = 3300^\circ\text{C}$, Equation (2) yields $m = 0.02 \text{ cm sec}^{-1/2}$. The assigned value of T_s (3300°C) is an estimate of the boiling point of UO_2 , and hence the approximate maximum temperature which might be imposed by a molten mass of fuel. A somewhat higher value for T_o and small improvements in physical property values would not significantly affect the results.

With the value of $m = 0.02 \text{ cm sec}^{-1/2}$, Equation (1) indicates that melting front penetration of a depth of, say, 8 feet would require about 1720 days. Decay heat will have decreased to negligible levels, and it would be expected that upward heat flow would have reduced the assumed surface temperature by this time, so that a peak depth of penetration which is considerably less than the Lower Cavity depth can reasonably be expected. More detailed analysis would be required to check whether the concrete floor of the Lower Cavity reaches unacceptably high temperatures.* Reference 16 notes that events such as structures collapsing into molten fuel or explosions that produce hydrodynamic agitation could cause mixing of the molten fuel with the molten barrier beneath it, thereby increasing heat flow into the barrier.

* In addition to the degradation of structural integrity, unacceptably high concrete temperatures will liberate water vapor which could result in formation of free hydrogen if sodium/water contact occurred.

The model presents arguments against rapid downward penetration of the melt front, although experimental verification of the tendency of molten fuel and molten barrier to remain separated may be required in order to take credit for this effect. However, the model does not preclude relatively rapid sideways growth of the molten pool. Preliminary analyses by ANL indicate that the molten pool would reach the sides of the Lower Cavity rapidly enough and generating a large enough heat flux that a cooling system would be required to protect the concrete walls of the cavity. Therefore, the concept of a completely passive uranium barrier is not considered suitable for FFTF. Similar arguments would apply to a completely passive thorium barrier.

On the basis of the above discussions, it does not appear likely that a completely passive EVCC, utilizing basalt or similar material as the sacrificial barrier, is suitable for FFTF. However, to provide boundary conditions for possible further analyses of this concept, additional calculations were performed and are given in Appendix A. These results (Appendix A) are for a completely passive EVCC utilizing an insulating barrier. The results show that the concrete wall of the Lower Cavity reaches a maximum temperature of 851°F, and the maximum temperature remained above 800°F for about 100 days. Temperature distributions are presented which can be used to investigate whether the concrete will retain its structural capabilities.

A completely passive system utilizing a sacrificial bed rather than an insulating barrier would result in even higher concrete wall temperatures.

5.2 Passive System

The feasibility of a passive cooling system using naturally circulating coolant was investigated. It was found that a reasonable design concept could be developed from thermal-hydraulic considerations. However, space for the EVCC cooling system equipment and duct work is very limited because of the number of poured concrete floors already in place and the space allotted for other equipment. There does not appear to be any practical way of laying out the required duct work between the Lower

Cavity and ground level. Therefore, the concept described here probably is not feasible for FFTF.

The concept, as originally conceived, included a sacrificial barrier of basalt in the Lower Cavity. Based on current information this would now be changed to depleted urania or thoria.

A sketch of the core catcher concept is shown in Figures 5.9 and 5.10. The Lower Cavity is lined with a stainless steel inner Hot Liner and outer Cold Liner. There are 97 independent natural circulation NaK-22 primary loops in each of two systems. The loops of the second system alternate with the loops of the first system, and each is independently capable of handling the required heat load.

Heat is transferred from the primary loops through heat exchangers to secondary loops, which penetrate the outer containment at ground level and dump the heat to the atmosphere through auxiliary heat exchangers.

The hot legs of the primary loops are thermally bonded to the Hot Liner and the cold legs are thermally bonded to the Cold Liner. The liners do not serve a structural purpose but act as heat distributors (fins) for the coolant loops.

The primary loops are 3/4 inch 16 BWG stainless steel tubes (0.065 inch wall 0.620 inch ID). Each is assumed to have an equivalent length of 150 ft and a height of 15 ft over which the buoyancy head difference acts. The required coolant tube diameter is found by equating the buoyancy driving head with the frictional head losses:

$$D^5 = \frac{32 f L_f Q^2}{\pi^2 \Delta T^3 \rho^2 g C_p^2 L_e \beta n^2} \quad (3)$$

The friction factor is given as a function of Reynolds Number and relative roughness in a number of standard references (e.g., Reference 18), and the

Reynolds Number is given by

$$Re = \frac{4Q}{\pi \mu n D C_p \Delta T} \quad (4)$$

where

D = tube I.D., ft

f = Fanning friction factor

L_f = equivalent length of circuit, ft

Q = total heat load, Btu/hr

ΔT = mean temperature difference between hot and cold legs, °F

ρ = density, lb/ft³

g = acceleration of gravity, ft/hr²

C_p = heat capacity, Btu/lb-°F

L_e = vertical distance over which ΔT acts, ft

β = volumetric coefficient of expansion, °F⁻¹

n = number of parallel circuits

μ = viscosity, lb/ft-hr

The spacing between tubes on the hot liner is determined by the maximum heat flux anticipated at that location. For the present study, this was based on a preliminary analysis performed by ANL. The analysis assumes upward and sideward heat fluxes in the meltdown pool to be three times the downward heat flux, and shows the pool reaching the liner region near the top of the lower cavity after about 80,000 seconds. The maximum heat flux is estimated as 14,680 Btu/hr-ft². The present analysis conservatively assumes that the total sideward and downward heat flux at that time is being handled by one of the two cooling systems resulting in a heat removal capacity of 2.7×10^6 Btu/hr for the system. The limitation on the tube spacing is that no point on the hot liner should exceed the melting point of basalt (2000°F), so that the melting front will be stopped at the Hot Liner.

The primary hot leg temperature was assumed to be limited to 1100°F.

The primary cold leg temperature was limited to a maximum of 400°F as this leg controls the Lower Cavity concrete temperature. It should be noted that with the present concept, because of the limitation on cold leg temperature it is not conservative to have less hydraulic resistance in a primary or secondary loop than is required by the design. Less hydraulic resistance would result in hot and cold leg temperatures approaching each other, resulting in excessively high cold leg temperatures. Therefore, any overdesign should be accomplished by increased system capacity (e.g., closer tube spacing, or larger Auxiliary Heat Exchanger (AHX)) rather than by reducing loop hydraulic resistance. However, as an alternative to the present concept, an auxiliary cooling circuit could be used to maintain the Lower Cavity concrete temperature, and the primary cold leg would no longer have maximum temperature limitations.

The Intermediate Heat Exchangers are individual countercurrent Double Pipe Heat Exchangers (DPHE) 10 ft long, resulting in a uniform temperature difference of 29°F between primary and secondary loops.

The NaK-22 secondary loops are the same tube size as the primary loops. Each is assumed to have an equivalent length of 400 ft and a height of 70 ft over which the buoyancy head difference acts.

The Auxiliary Heat Exchanger (AHX) is a NaK-to-air natural circulation heat exchanger which discards the heat to the atmosphere. A sketch of the AHX is shown in Figure 5.11. The overall dimensions are 1 ft high, 14 ft wide, 8.5 ft long. It consists of a series of vertical parallel plates. Each secondary loop is bonded to a pair of plates and passes back and forth for ten passes. Loop channels alternate with empty channels for naturally circulating cooling air flow. The size was determined using Siegel and Norris' correlation for natural circulation heat transfer in vertical gaps (Reference 19).

The design data are summarized in Table 5.1.

The effect of varying certain design parameters was investigated. Figure 5.12 shows the effect of increasing the coolant tube diameter in order to decrease the number of tubes required. The figure is based on approximate calculations performed for scoping purposes; more exact computer calculations would be required for design values.

Figure 5.13 shows the effect of bonding the tubes to a better-conducting graphite liner instead of a stainless steel liner. In this figure, the effect is shown of varying the thermal bond conductance between the stainless steel tubes and the graphite liner.

Changes in molten pool model assumptions can result in higher heat fluxes at the liner, and so the effect of increasing heat flux is shown in Figure 5.14.

The 97 coolant tubes including insulation would require a duct about 2 1/2 feet in diameter for the hot legs, a slightly smaller duct for the cold legs, and an additional pair of similar ducts for the redundant system. Space limitations in the Reactor Containment Building probably would make it extremely difficult to install this system.

A similar natural circulation concept was analyzed with the goal of minimizing the space requirements in the Reactor Containment Building. This resulted in a single loop of approximately 3-inch OD pipe for each of the two redundant systems, with about 2-inches of insulation on the hot leg and somewhat less on the cold leg.

5.3 Active System

As has been noted, the natural circulation concept may not be feasible because of space limitations in the Reactor Containment Building. An alternative concept is linking the EVCC cooling system to the H&V Mobiltherm forced circulation cooling system. The Mobiltherm systems have a total capacity of 14,400,000 Btu/hr, about 70 to 80% of which is available to the cooling areas adjacent to the Lower Cavity. An EVCC design concept was

investigated by Sharbaugh (Reference 20) for the purpose of permitting a preliminary cost and construction schedule estimate. The concept met the requirement that all component parts must fit through the 5 foot diameter vertical access opening (Section 2, Requirement 2.1.5) into the Lower Cavity where the EVCC will be assembled in place.

The EVCC consists of a sacrificial bed of depleted UO_2 brick, bounded at the sides and bottom of the Lower Cavity by the heat removal circuits. Figures 5.15 and 5.16 show the EVCC in relation to the plant, and the details of the EVCC. The two circuits are filled with NaK which is pumped by EM pumps through NaK/Mobiltherm heat exchangers. There are two pumps and two heat exchangers per circuit located in the Lower Cavity blockouts. One quarter of the insulating floor is high density brick to transmit the weight of the UO_2 bed to the concrete, and is so placed as to protect the floor coolant piping. The space containing the piping is filled with lead for improved heat transfer. A conical refractory floor is installed on the Reactor Cavity floor beneath the Guard Vessel, sloped to guide debris to the central hole above the lower cavity. A review of fuel-coolant interactions concluded that vapor explosions are not likely to be violent and do not negate the feasibility of the concept.

5.3.1 Alternative Active System

An alternative active cooling system concept is sketched in Figure 5.17. This concept utilizes an insulating barrier rather than a sacrificial barrier. Debris falling through the central hole into the Lower Cavity is collected in a tungsten liner which may contain a fuel dispersal device, poison material, and some sacrificial barrier (urania) if required. The tungsten liner is supported by a layer of thoria brick, chosen because of its high melting point. This is surrounded by a bed of additional thoria or depleted urania, chosen for its high melting point and good insulating properties at high temperatures. The next layer is insulating firebrick, chosen for its good insulating characteristics for temperatures up to about 2600°F. Between the insulating firebrick and the walls of the lower cavity is ductwork through which the atmosphere of the adjacent cells is

circulated by blowers located in the blockouts. Most of the decay heat from the core debris enters the sodium above the EVCC as is the case for all of the systems investigated. The heat flowing horizontally and downward may be too high to be acceptable in a completely passive cooling system, but appears to be low enough to be handled by gas cooling as described with the heat load removed by the H & V system in the adjacent cells.

The main advantage of this insulated barrier system over the sacrificial barrier system is the simplification of the heat removal system. The disadvantages are a) the need for a tungsten liner which may be too large to fit through the vertical access hole, b) less capability for removal of upflowing heat if required, and c) assuring the integrity of the tungsten liner, e.g., against W-Fe interaction. All of the systems have the problem of preventing recriticality of the debris. With either a passive (natural circulation) or an active (forced circulation) cooling system, the heat flux and total heat load requirements at the boundary are the same. Table 5.2 (from Reference 21) gives the values for several configurations.

TABLE 5.1

EX-VESSEL CORE CATCHER COOLING SYSTEM

Lower cavity filled with basalt

Inner hot liner (1-inch thick stainless steel shell) acts as fin for primary system hot legs.

Outer cold liner acts as fin for primary system cold legs.

For redundancy, 2 sets of primary loops, secondary loops, and auxiliary heat exchangers (AHX), each operating by natural circulation.

Primary Loops (Each Set)

Coolant	NaK-22
No. of Tubes	97
Stainless steel tubes	3/4 in. OD
	16 gage
	0.065 in. wall
	0.620 in. ID
Equivalent Length:	150 ft.
Elevation Difference	15 ft.
T_{1H} :	1100°F
T_{1C} :	400°F (Limitation: Lower Cavity Concrete Wall Temperature)

IHX

Double Pipe Countercurrent Heat Exchangers	
Length:	10 ft.
$T_1 - T_2 =$	29 F

Secondary Loops (Each Set)

Coolant	NaK-22
No. of Tubes:	97
Stainless Steel Tubes	3/4 in. OD
	16 gage
	0.065 in. wall
	0.620 in. ID
Equivalent Length:	400 ft.
Elevation Difference:	70 ft.
T_{2H} :	1071 F
T_{2C} :	371 F

AHX

Overall Dimensions:	Height 1'
	Length 8.5'
	Width 14'
Ambient Air Temperature:	100°F
Capacity:	2.7×10^6 Btu/hr

* TABLE 5.2

Summary of Fuel Pool Growth in Sacrificial Beds

Case	Limiting Diam. Dm, m.	Time to Reach Dm, 10 ³ sec	Max. Radial Heat at Dm		Depth of Penetration at tm = 2.3 days, cm	Max. Downward Heat at 2.3 day	
			Flux, cal/sec-cm ²	Total Load 10 ³ -cal/sec		Flux cal/sec-cm ²	Total Load 10 ³ cal/sec
Sacrificial Material: UO ₂ , 9.5 g/cm ³							
1	3	40 (11 hr)	2.6	54	25	0.24	17
2	5.5	7200 (>2.3 d)	<0.8	<35	21	0.14	20
Sacrificial Material: UO ₂ , 5 g/cm ³							
3	3	15 (4.2 hr)	3.5	46	48	0.25	18
4	5.5	>200 (>2.3d)	<0.6	<18	36	0.13	29
Sacrificial Material: ThO ₂ , 9 g/cm ³							
5	3	55 (15.3 hr)	1.6	35	23	0.20	15
6	5.5	>200 (>2.3d)	<0.65	<26	20	0.14	17
Sacrificial Material: ThO ₂ , 5 g/cm ³							
7	3	25 (7hr)	1.9	33	44	0.22	16
8	5.5	>200 (>23d)	<0.5	<15	34	0.12	23.5
Sacrificial Material: Basalt 2.6 g/cm ³							
9	3	5 (1.4 hr)	5.4	171	170	0.38	26.8
10	5.5	60 (23 hr)	0.9	106	100	0.18	43

* From Reference 21

TABLE 5.3
PRELIMINARY RESULTS OF ANL POOL GROWTH STUDIES

<u>Material</u>	<u>Assumption of debris/barrier solubility</u>	<u>Time to Reach Wall</u>	<u>Max. heat flux, BTU/hr-ft²</u>
Basalt	Soluble	14 hr	12,000
Basalt	Insoluble	1.4 hr	73,000
UO ₂ Powder	Soluble	> 2.3 days	small
UO ₂ Powder	Insoluble	3 hr	46,000

FIGURE 5.1 Temperatures in molten basalt pool vs pool radius.

FIGURE 5.2 Temperatures in molten basalt pool vs time.

FIGURE 5.3 Radius of molten pool vs time.

FIGURE 5.4 Temperature profiles in basalt beneath molten pool.

FIGURE 5.5 Comparison of pool stopped by insulation with pool continuing to grow.

FIGURE 5.6 Heat transferred from molten pool.

FIGURE 5.7 Transient temperature profiles beneath insulation around hemispherical molten pool.

FIGURE 5.8 Comparison of sacrificial materials.

TABLE
Viscosity of Sacrificial Materials (in poise)

T °C	Basalt	Olivine-Basalt	Fayalite
1100	13 950	7015	21.9
1200	2 675	298	11.9
1400	178	1.7	4.4
1600	21	0.03	2.0
2000	0.9	0.00007	0.6

SCALE: 1/4" = 1'-0"
REACTOR GUARD VESSEL SUPPORT SEE DWG
H-4-14773

FIGURE 5.9 EX-VESSEL CORE CATCHER

SECTION

SCALE: 1/4" = 1'-0"

FIGURE 5.11 AUXILIARY HEAT EXCHANGER

ELEVATION

FIGURE 5.12 TUBE SPACING VS. DIAMETER

FIGURE 5.13 EFFECT OF BOND
CONDUCTANCE BETWEEN STAINLESS
STEEL TUBE AND GRAPHITE LINER
ON TUBE SPACING

L,
in.

FIGURE 5.14 VARIATION OF TUBE
SPACING WITH HEAT
FLUX

FIGURE 5.15 FX-VESSEL CORE CATCHER CONCEPT

FIGURE 5.16 DESIGN CONCEPT

FIGURE 5.17 ALTERNATE CORE CATCHER CONCEPT

6.0 GUARD VESSEL STRUCTURAL BEHAVIOR FOLLOWING POSTULATED MELTDOWN

In addition to consideration relevant to the EVCC system directly, other conditions which might result from a postulated core meltdown must also be addressed. One area of interest is the structural integrity of the guard vessel and supporting structures.

Again, for purposes of performing the study, it has been assumed that core debris has penetrated the bottom of the Reactor Vessel and the Guard Vessel and has dropped into an Ex-Vessel Core Catcher (EVCC) in the Lower Cavity. The reactor primary circuit pony motor or natural circulation coolant flow has been lost because of the drop in sodium level below the reactor outlet nozzles when the Guard Vessel was penetrated. Most of the decay heat may be transferred into the sodium above the EVCC rather than into the sacrificial barrier or the EVCC cooling system. As a result, sodium in and below the Reactor Vessel (RV) and Guard Vessel (GV) can heat up to the sodium boiling point and remain at that temperature for a number of months (assuming that there is no means of removing sufficient heat to lower the temperature). The question then arises as to whether the Reactor and Guard Vessels will collapse under these conditions.

Appendix B documents a number of cases which have been considered. The heat losses from the Reactor Vessel to the Reactor Cavity and the Head Compartment are estimated to be at least 862,620 Btu/hr for a RV temperature of 1620°F and 488,120 Btu/hr for a RV temperature of 1080°F. Heat flow through the melt-through hole in the GV is estimated to be given by:

$$Q(\text{Btu/hr}) = 244D(\text{ft})^{1.96} \Delta T(^{\circ}\text{F})^{1.32}$$

The GV shell at the sodium boiling point of 1725°F has a rupture time of 26,000 hr. The temperature will begin to drop below 1725°F after 1667 hr, so the shell is not expected to rupture due to creep.

If there is good sodium circulation through the melt-through hole in the GV bottom head, the skirt will not rupture. Hole sizes required to provide adequate circulation are tabulated below for several of the cases evaluated in Appendix B.

Case	% heat to Na beneath GV	% heat to Na in RV	Temperature of skirt, °F	Required Hole Diameter, ft
3-	100	0	1725	1.28
4-	100	0	1620	0.316
5-	55	0	1725	0.535
6-	30	50	1725	0.475

If the insulation of the GV bottom head and on the interior of the skirt deteriorates uniformly, then the skirt is not expected to rupture even with no sodium circulation through the hole.

7.0 SUMMARY

Functional requirements have been developed for an EVCC which might possibly be installed in the FFTF. Several preliminary concepts have been evaluated including a completely passive system, a passive system with natural circulation cooling and an active system with forced convective cooling.

If an EVCC is to be installed in FFTF, an active system as described in Section 5.3 would have the highest probability of meeting the functional requirements. The system consists of a sacrificial barrier of depleted uranium or thorium brick, bounded at the sides and bottom of the Lower Cavity by the heat removal circuits. The two circuits are filled with NaK which is pumped through NaK/Mobiltherm heat exchangers.

The system shall be designed to meet the functional requirements listed in Section 2. Outstanding problems include: a) preventing recriticality, b) assuring that the debris drops into the Lower Cavity through the central hole, c) determining the magnitude of the maximum fuel-coolant interaction for which the system should be designed, d) assuring that the Reactor and Guard Vessels, supports and cavity concrete walls retain their structural capabilities, and e) determining the thermal loads for which the cooling system should be designed.

Items a) and b) are severe design problems since they are quite dependent on the postulated sequence of debris relocation. Item d) is a principal problem because most of the decay heat may flow into the sodium above the core catcher rather than into the sacrificial barrier and the core catcher cooling system. The remaining items may have significant scheduler impact if it is determined that any experimental verification of analytical solutions is required.

The feasibility of the EVCC depends upon whether solutions can be found to the aforementioned problems. Installation appears to be feasible although difficult, and might significantly affect plant schedule.

8.0 REFERENCES

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APPENDIX A

EX-VESSEL CORE CATCHER

COMPLETE PASSIVE HEAT REMOVAL SYSTEM

INSULATING BARRIER

EX-VESSEL CORE CATCHER
COMPLETE PASSIVE HEAT REMOVAL SYSTEM
INSULATING BARRIER

INTRODUCTION

A completely passive heat removal system for an Ex-Vessel Core Catcher is one which does not require circulating coolant. Heat is removed by conduction into the ground, to the environment, or to adjacent cells which are cooled by existing ventilating systems.

Jansen and Stepnewski (Reference A.1) described a completely passive system with a basalt sacrificial bed surrounded by nonsoluble insulation and their results are discussed in Reference A.2. However, because of possible interactions between basalt and sodium, only depleted urania or thoria are currently considered for any near-term applications as bed materials.

Zivi (Reference A.3) suggested a completely passive urania bed and noted that if the molten fuel floated on top of the molten bed, then the rate of advance of the melting front through the bed would be a very slow process. Several weaknesses in the model are

- a) Events such as structures collapsing into molten fuel or explosions that produce hydrodynamic agitation could cause mixing of the molten fuel with the molten bed beneath it.
- b) The model does not preclude relatively rapid sideways growth of the molten pool.
- c) The analysis considered only the location of the melting front as the criterion for containment but did not consider the distribution of temperatures in the surrounding structure which were below the melting point of urania but still high enough to affect the structural capabilities of the plant.

However, it is apparent from the analysis that if the molten fuel can be prevented from mixing with the bed material, then the temperatures in the structure surrounding the bed will be lower than for the case of mixing. This suggests using a nonsoluble nonmelting liner to separate the fuel from the bed material, and using the bed as an insulating rather than as a sacrificial melting bed. If the analysis of such a system shows acceptable temperatures in the surrounding structure, it would still be necessary to demonstrate that a candidate liner material, e.g., tungsten or graphite, would perform properly. If the analysis shows unacceptably high structural temperatures, then the temperatures would also be too high for the case of the same bed material used as a sacrificial melting bed instead of an insulating barrier.

Upward heat flow from the molten pool into the sodium above the pool is higher with an insulating barrier than with a sacrificial bed. However, most of the heat flows upward for either design and the heat load on the overhead sodium is not much greater with the insulating barrier than with the sacrificial bed.

Disadvantages of the completely passive insulating barrier system are

- a) the need for a tungsten*liner which may be too large to fit through the vertical access hole to the Lower Cavity or which cannot be assembled in situ.
- b) less capability than with the active cooling system for removal of upflowing heat if required.
- c) assuring the integrity of the tungsten liner, e.g., against iron-tungsten interaction.

MODEL

A sketch of the system is shown in Figure A.1a. The debris falling through the central hole into the Lower Cavity is collected in a cylindrical tungsten liner which may contain a fuel dispersal device, poison material, and some sacrificial uranium if required. The diameter of the liner should be

* or similar high temperature material

sufficiently large to avoid recriticality of uniformly dispersed fuel, to prevent fuel boiling, to avoid melting the adjacent thoria layer, and to keep the heat flux into the sodium above the fuel below the critical heat flux. A 7 ft diameter liner, about the same as used by ANL for initial pool diameter (Reference A.4) appears to meet these requirements as is discussed later.

The tungsten liner is supported by a layer of thoria brick, chosen because of its high melting point (Reference A.4) gives the following melting points: Mixed oxide, 20% Pu O₂: 2770°C, UO₂: 2850°C, Th O₂: 3270°C). This is surrounded by a bed of additional thoria or depleted urania, chosen for its high melting point and good insulating properties (low thermal conductivity and low thermal diffusivity) at high temperatures. Zirconia which has a melting point of 2700°C (Reference A.6) may be another suitable candidate material based on thermal conductivities shown in Figure A.2 (Reference A.5).

High density urania or thoria is assumed. Although porous material has lower thermal conductivity, urania densifies at temperatures above about 2500°F and therefore the initially low conductivity would be expected to increase during operation of the EVCC.

The next layer is insulating firebrick, chosen for its good insulating characteristics for temperatures up to about 2600°F. A thickness of 27 inches was chosen for the initial calculations. This resulted in a lower peak firebrick temperature than the permitted value, so a somewhat greater thickness should be acceptable.

A two-dimensional cylindrical geometry was used in the analysis with axial symmetry assumed. The average concrete wall thickness to the adjacent cells was taken as 14 ft. The heat transfer coefficient at the surfaces of the adjacent cells was taken as 1 Btu/hr-ft²°F to the cell atmosphere which was maintained at 120°F. The overhead sodium was assumed to be at the boiling point of 1725°F. Initial temperatures were taken as 120°F.

The following physical property values were assumed.

Material	Capacitance, $C_p \rho$		Conductivity, k	
	Btu/ft ³ °F	(Cal/cm ³ -°C)	Btu/hr-ft-°F	(Cal/sec-cm°C)
UO ₂ or Th O ₂	56.1	(.8986)	1.44	(.005953)
Firebrick	8.45	(.1354)	0.2917	(.001206)
Concrete and Bedrock	36.	(.5766)	1.0	(.004134)

The calculations were performed using a general heat transfer computer code, GHTX. The node numbering system is shown in Figure A.1a and A.1b. Time steps of 27.778 hr. were used and the calculations were carried out for 13,889 hr (1.59 years). The heat capacity of the fuel debris nodes (nodes 21 and 22) was neglected in the calculations.

The entire core generating all of the decay heat was assumed to reach the tungsten liner 10^4 seconds after shutdown, which was taken as time zero for the computer calculations. The decay heat was taken from Figure 6 of Reference A.7, using the upper bound of the uncertainty band (about 13% above the nominal values).

Code limitations prevented the desired heat transfer relations for the molten pool from being programmed directly. Estimated fuel temperature transients were used as input instead, and the accuracy of the fit is discussed in the next section.

HEAT TRANSFER IN MOLTEN FUEL

1. Sodium Critical Heat Flux

The model assumes that the temperature at the top of the debris bed is close to the sodium boiling point as sketched in Figure A.3. This requires that the heat flux into the sodium be below the critical heat flux. Reference A.8

predicts 1.9×10^6 Btu/hr-ft² for the critical heat flux for pool boiling of saturated sodium at 1 atm. Other models referenced in Reference A.8 predict critical heat fluxes ranging from 0.5×10^6 to 3.5×10^6 . A conservative value of 0.5×10^6 was assumed. This assumes communication between the sodium above the core catcher and the sodium in the Reactor Vessel sufficient to prevent dryout.

If the entire core generating all of the decay heat entered the EVCC 10^4 seconds after shutdown, the decay heat would be 0.9% of operating power. If all the heat were flowing upward uniformly from the 7 ft. diameter pool of molten fuel, the heat flux will be 0.319×10^6 Btu/hr-ft². This is less than the critical heat flux of 0.5×10^6 Btu/hr-ft²-°F and therefore, transition from nucleate to film boiling is not expected.

2. Heat Transfer Through Stainless Steel

3000 kg of fuel are assumed to be accompanied by 2000 kg of stainless steel (Reference A.1). This results in a 4 in layer of fuel with a 3.31 in. layer of stainless steel floating on it as sketched in Figure A.3. The stainless steel is assumed to consist of an upper frozen layer and a lower molten layer. Since the solid stainless steel is more dense than the liquid this configuration is unstable. Solid particles forming at the surface would tend to sink in the liquid, enhancing natural circulation. However, it is conservative to assume that the solid layer remains on top of the liquid. The top of the solid stainless steel layer will be close to the sodium boiling point, 1725°F, and the solid-liquid stainless steel interface will be at the stainless steel melting point, 2600°F.

Heat transfer through the molten stainless steel layer is given by Globe and Dropkin's correlation (Reference A.9) for heat transfer between horizontal plates heated from below:

$$Nu = 0.069 Ra^{1/3} Pr^{0.074} \quad (1)$$

Physical property data for molten stainless steel are sparse and are

reviewed in Reference A.10.

Assuming the following values in Equation (1) for a 205 cm diameter pool

$$\begin{aligned}\rho &= 7.208 \text{ g/cm}^3 \\ C_p &= 0.19 \text{ Cal/g-}^\circ\text{C} \\ \beta &= 19.8 \times 10^{-6} \text{ }^\circ\text{C}^{-1} \\ \mu &= 0.01 \text{ cm}^2/\text{sec.} \\ k &= 0.07854 \text{ cal/cm-sec}^\circ\text{C}\end{aligned}$$

results in

$$\Delta T_{4-3} = 513.9 (F_u Y)^{0.75} \text{ }^\circ\text{F} \quad (2)$$

where Y is percentage of operating power (400 Mw) and F_u is fraction of heat flowing upward, and ΔT_{4-3} is the temperature difference between locations 4 and 3 in Figure A.3.

Equation (2) is independent of the thickness of the molten stainless steel layer. For thin liquid layers where conduction yields lower ΔT 's than Equation (2), it is assumed that the conduction equation applies. This occurs for values of $F_u Y < 0.183$, for which the temperature difference through all of the stainless steel (solid plus any liquid) is

$$\Delta T_{4-2} = 5579 F_u Y \text{ }^\circ\text{F} \quad (3)$$

Thus the temperature at the fuel-SS interface (T_4 , $^\circ\text{F}$) is

$$T_4 = 2600 + 513.9 (F_u Y)^{0.75} \quad F_u Y > 0.183 \quad (4)$$

$$T_4 = 1725 + 5579 (F_u Y) \quad F_u Y < 0.183 \quad (5)$$

3. Heat Transfer Through Fuel

The fuel is assumed to consist of an upper solid crust and a lower molten region as sketched in Figure A.3. Since the solid fuel is more dense than the molten fuel it would tend to sink, and therefore the assumed configuration is conservative.

The top of the fuel crust is at temperature T_4 given by Equations (4) or (5). The fuel solid-liquid interface is at the fuel melting point, 5018°F (Reference A.4). The thickness of the fuel crust is given by

$$T_5 - T_4 = \frac{L_{6-4}^2 - L_{6-5}^2}{2k} \quad q = \frac{Q_u}{2k A L_{6-4}} (L_{6-4}^2 - L_{6-5}^2) \quad (6)$$

Substituting the values

$$L_{7-4} = 10 \text{ cm (total fuel thickness)}$$

$$L_{6-4} = F_u L_{7-4}$$

$$T_5 = 5018^\circ\text{F}$$

$$A = \frac{\pi}{4} (205)^2 \text{ cm}^2$$

$$k = 0.005 \text{ cal/cm-sec} - ^\circ\text{C}$$

expressing total upflowing heat, Q_u , in terms of Y, percentage of operating power, and solving for L_{6-5} cm

$$L_{6-5} = \left[100F_u^2 - 1.919 \times 10^{-3} \frac{(5018 - T_4)}{Y} \right]^{1/2} \quad (7)$$

Upward heat transfer through the molten fuel was calculated using Kulacki and Goldstein's correlation (Reference A.11) which was obtained for the case of upper and lower surfaces at the same temperature.

$$Nu_u = 0.756 \left[\frac{g\beta L_{7-5}^5 \rho q}{64\alpha\mu k} \right]^{0.25} \quad (8)$$

Substituting physical property values from Reference A.4

$$\rho = 8.7 \text{ g/cm}^3$$

$$\beta = 10^{-4} \text{ } ^\circ\text{C}^{-1}$$

$$\mu = 0.05 \text{ poise}$$

$$k = 0.005 \text{ cal/sec-cm- } ^\circ\text{C}$$

$$C_p = 0.12 \text{ cal/g- } ^\circ\text{C}$$

and noting that

$$N_u = \frac{h_u L_{7-5}}{k}$$

results in

$$h_u = \frac{Q_u}{A \Delta T_{6-5}} = \frac{q L_{6-5}}{\Delta T_{6-5}} = 0.03883 (L_{7-5} q)^{0.25} \text{ cal/cm}^2 \text{ sec-}^\circ\text{C} \quad (9)$$

Also

$$L_{7-6} = (1-F_u) L_{7-4} = 10(1-F_u) \text{ cm} \quad (10)$$

Expressing q in terms of Y

$$q = Y \frac{\frac{\%}{(400)} \frac{\text{w}}{(10^6)} \frac{\text{cal/w-hr}}{(860.01)}}{\frac{(100) \frac{\pi}{4} (205)^2 (10) (3600)}{\text{cm}^2 \text{ cm sec/hr}}} = 2.8951 Y \text{ cal/sec-cm}^3$$

and noting that

$$L_{7-5} = L_{7-6} + L_{6-5} = 10(1-F_u) + L_{6-5} \quad (11)$$

results in

$$\Delta T_{6-5} = 102.88 Y^{0.75} \frac{L_{6-5}}{\frac{0.25}{L_{7-5}}} \quad ^\circ\text{F} \quad (12)$$

Where L_{6-5} and L_{7-5} are given by Equations (7) and (11), respectively.
Since $T_5 = 5018^\circ\text{F}$,

$$T_6 = 5018 + 102.88 Y^{0.75} \frac{L_{6-5}}{\frac{0.25}{L_{7-5}}} \quad ^\circ\text{F} \quad (13)$$

When the fuel has completely solidified ($T_6 < 5018^\circ\text{F}$) the temperature is given by

$$\Delta T_{6-4} = \frac{1_{6-4}^2}{2k} = 52112 F_u^2 Y \quad ^\circ\text{F} \quad (14)$$

4. Fuel Boiling and Thoria Melting

Equation (13) may be used to confirm that there is no fuel boiling and no thoria melting. Taking decay heat $Y = 0.9\%$ at time zero (10^4 seconds after shutdown) and conservatively assuming all heat flowing upward ($F_u = 1$),

Equation 4:	$T_4 = 3075^\circ\text{F}$
Equation 7:	$L_{6-5} = 9.791 \text{ cm}$
Equation 11:	$L_{7-5} = 9.791 \text{ cm}$
Equation 13:	$T_6 = 5544^\circ\text{F}$

The maximum fuel temperature of 5544°F is less than the fuel boiling point of 5702°F or the thoria melting point of 5918°F (Reference A.4) and therefore no fuel boiling or thoria melting is expected.

5. Fuel Temperature Transients

The fuel temperature transient was estimated and used as input. The output included the sideward and downward heat flow (node 8) from the pool (nodes 21 and 22 in Figure A.1a) to the neighboring nodes (nodes 31, 32, and 23), from which the upward heat flow was obtained by subtracting from the known total heat generation. Maximum fuel temperatures were then calculated for various times during the transient using Equations (4), (5), (7), (10), (11), (12), (13), and (14). Comparison of the calculated fuel temperatures with the input fuel temperatures permitted better estimations of the fuel temperature to be used as input to the next run. Sideward and downward differences between maximum fuel temperature and liner temperature were conservatively neglected, since the overall thermal resistance through the fuel is much smaller than that through the insulation.

Input fuel temperatures for the last two runs (Runs 4 and 5) are shown in Figure A.4. The deviations between the input fuel temperature and that calculated by the above procedure are given in Figure A.5. From Figure A.5 it is seen that the deviations for Run 4 tend to be negative (non-conservative). Those for Run 5 tend to be positive (conservative) and of smaller magnitude than those of Run 4. Since the difference in maximum concrete temperature between Runs 4 and 5 does not exceed 14°F as shown in Figure A.6, Run 5 is accepted as the final iteration.

RESULTS

Transient fuel temperature is plotted in Figure A.4. The fuel solidifies after about 40 days.

The maximum concrete temperature occurs on the Lower Cavity centerline (node 51) which reaches a peak temperature of 851°F at 2778 hours. The temperature remains over 800°F for about 100 days. The transient temperature at this location is plotted in Figure A.6.

The maximum firebrick temperature (node 4) is 2102°F. This is less than the permitted value of about 2600°F, and therefore a somewhat thicker firebrick layer could have been used than the assumed 27 inches.

Temperature profiles at selected times are shown in Figures A.7 through A.11. These can be used to investigate whether the concrete will retain its structural capabilities.

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WESTINGHOUSE ELECTRIC CORPORATION

SEE FIGURE 1b FOR NODE NUMBERING OF COMPLETE SYSTEM

SKETCH SHEET

FORM 28577

FIGURE 11b NODE ARRANGEMENT FOR COMPUTER CALCULATIONS

WESTINGHOUSE ELECTRIC CORPORATION

Abb. 16 Wärmeleitfähigkeit einiger hochschmelzender Werkstoffe

FIGURE A.2 THERMAL CONDUCTIVITY OF VARIOUS HIGH MELTING POINT MATERIALS (REFERENCE A.5)

FIGURE A.3 MOLTEN FUEL POOL
WESTINGHOUSE ELECTRIC CORPORATION

FIGURE A.5 DEVIATION BETWEEN INPUT AND
CALCULATED FUEL TEMPERATURES

10 X 10 TO THE CENTIMETER 46 1510
MADE IN U.S.A.
KEUFEL & ESSER CO.

FIGURE A.6 MAXIMUM CONCRETE TEMPERATURE

NODE 51 - CAVITY CENTERLINE

TRANSIENT DISTRIBUTION. TIME= 1.6667E+03.

1725.0	120.0	0.0	2101.7	1627.4	120.0	120.0	106.0	120.0	120.0
120.0	120.0	1368.3	567.5	258.1	142.1	122.7	120.0	120.0	120.0
4345.0	4345.0	1884.7	780.3	409.0	231.7	128.8	120.0	120.0	120.0
3413.7	3184.3	1917.2	709.5	415.0	240.2	129.2	120.0	120.0	120.0
2310.8	2139.0	1550.0	557.2	344.4	228.8	126.0	120.1	120.0	120.0
782.7	718.1	493.3	323.3	236.8	173.0	125.9	120.3	120.0	120.0
385.6	359.9	278.3	217.0	173.8	142.3	123.2	120.2	120.0	120.0
208.3	200.2	175.2	155.7	139.7	127.5	121.2	120.1	120.0	120.0
131.8	130.8	127.6	125.0	122.8	120.9	120.2	120.0	120.0	120.0
121.0	120.9	120.6	120.4	120.2	120.1	120.0	120.0	120.0	120.0
120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0
120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0

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FIGURE A.7 TEMPERATURE DISTRIBUTION AT 1667 HOURS

TRANSIENT DISTRIBUTION. TIME= 2.7778E+03

1725.0	120.0	0.0	1957.1	1694.1	120.0	120.0	78.2	120.0
120.0	120.0	1314.2	561.3	214.4	150.8	124.0	120.0	120.0
<u>3579.0</u>	<u>3579.0</u>	1714.7	779.1	446.0	274.2	133.2	120.0	120.0
2906.0	2731.8	1766.3	734.0	458.7	293.8	134.5	120.0	120.0
2125.1	1989.2	1524.7	621.8	413.1	294.3	131.8	120.0	120.0
<u>850.6</u>	<u>790.6</u>	<u>579.5</u>	<u>438.4</u>	312.8	235.5	137.7	121.6	120.0
484.1	456.1	365.6	291.6	232.9	185.4	133.5	121.6	120.0
286.8	275.3	238.8	207.0	177.4	151.9	127.9	121.1	120.0
156.5	154.2	146.9	140.0	133.1	126.7	121.9	120.3	120.0
125.3	125.0	124.0	123.0	121.9	120.9	120.3	120.1	120.0
120.2	120.2	120.1	120.1	120.1	120.0	120.0	120.0	120.0
120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0

FIGURE A.8 TEMPERATURE DISTRIBUTION AT 2778 HOURS

TRANSIENT DISTRIBUTION. TIME= 4.4445E+03

1725.0	120.0	0.0	1661.6	1452.2	120.0	120.0	63.3	120.0	120.0
120.0	120.0	122.3	530.6	208.5	151.2	124.2	120.0	120.0	120.0
2974.0	2974.0	1478.2	706.1	424.6	275.8	133.8	120.0	120.0	120.0
2418.8	2280.7	1511.3	672.3	442.6	293.2	135.7	120.0	120.0	120.0
1793.7	1686.5	1310.0	589.9	420.3	308.6	135.2	121.0	120.0	120.0
793.6	744.5	571.3	426.9	342.8	272.0	149.7	124.2	120.2	120.0
504.9	476.6	397.4	330.2	273.9	226.1	148.6	125.2	120.2	120.0
331.3	320.0	283.5	250.3	217.0	186.7	142.0	124.7	120.2	120.0
180.5	183.5	170.3	103.2	152.3	141.5	128.5	122.2	120.1	120.0
135.7	135.0	132.7	130.3	127.5	124.8	122.2	120.6	120.0	120.0
121.0	120.9	120.8	120.6	120.4	120.3	120.1	120.0	120.0	120.0
120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0	120.0

FIGURE A.9 TEMPERATURE DISTRIBUTION AT 4445 HOURS

TRANSIENT DISTRIBUTION. TIME= 7.7778E+03

1725.0	120.0	0.0	1335.9	1185.8	120.0	120.0	49.3	120.0
120.0	120.0	1117.9	492.1	196.8	146.5	123.6	120.0	120.0
2381.8	2381.8	1225.8	605.7	374.1	251.6	131.8	120.0	120.0
1933.1	1827.1	1234.2	574.2	389.5	272.3	133.8	120.0	120.0
1437.1	1355.2	1072.8	510.2	375.7	293.4	135.2	121.7	120.1
678.4	640.7	507.1	393.1	325.4	267.4	155.1	127.6	120.6
459.0	440.1	378.1	324.4	278.5	238.6	159.5	130.7	120.9
330.7	321.6	291.8	263.9	235.6	219.0	156.2	131.4	121.0
208.3	205.4	195.5	185.4	173.9	162.2	141.0	128.1	120.9
152.3	151.4	148.1	144.6	140.4	135.8	129.2	124.1	120.5
123.8	123.6	123.3	122.8	122.3	121.7	121.2	120.6	120.1
120.1	120.1	120.1	120.1	120.1	120.1	120.0	120.0	120.0

FIGURE A.10 TEMPERATURE DISTRIBUTION AT 7778 HOURS

TRANSIENT DISTRIBUTION. TIME= 1.3889E+04

1725.3	120.3	3.0	1088.9	987.3	120.0	120.0	39.8	120.0
120.0	120.0	1041.3	462.2	187.0	141.9	123.0	120.0	120.0
1941.0	1941.0	1037.0	526.2	330.4	227.7	129.7	120.0	120.0
1572.7	1490.0	1026.2	493.4	341.1	244.3	131.4	120.0	120.0
1169.7	1105.8	884.8	436.8	326.3	253.0	133.1	121.9	120.2
569.2	539.6	434.5	343.9	289.6	242.6	151.9	120.7	121.2
397.7	382.9	334.2	291.8	255.2	223.2	158.2	133.1	121.9
299.4	292.2	268.8	246.8	224.3	202.9	157.8	135.1	122.4
207.0	204.7	190.6	188.3	178.8	168.9	147.8	133.7	122.6
161.8	160.9	157.9	154.6	150.6	146.2	137.3	129.9	122.2
128.8	128.6	128.1	127.4	126.7	125.8	124.4	122.9	120.8
120.7	120.7	120.6	120.6	120.5	120.4	120.4	120.3	120.1

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FIGURE A.11 TEMPERATURE DISTRIBUTION AT 13,389 HOURS

APPENDIX B

GUARD VESSEL STRUCTURAL BEHAVIOR

FOLLOWING MELT-THROUGH

GUARD VESSEL STRUCTURAL BEHAVIOR FOLLOWING MELT-THROUGH

Core debris is assumed to penetrate the Reactor Vessel (RV) and Guard Vessel (GV) and drop into an Ex-Vessel Core Catcher (EVCC) in the Lower Cavity. Primary coolant flow is lost because of the drop in sodium level. It is assumed that Reactor Cavity ventilation continues to operate after the accident, but ductwork between the Head Compartment and vessel components may no longer be intact.

Most of the decay heat can flow into the sodium above the EVCC. The sodium in and below the RV and GV can be heated to its boiling point in about a day. Sodium will begin to boil and will condense on structural surfaces. As these surfaces in the RV approach the sodium boiling point, the region of sodium condensation will work its way along the outlet pipes and eventually reach the IHX where the condensed sodium can flow back to the RV through the inlet pipes by gravity.

DECAY HEAT FLOW INTO SODIUM

Decay heat versus time is shown in Figure B.1 (Figure 6 of Reference B.1). The fraction of this heat which flows into the sodium as opposed to that which enters the EVCC may be roughly estimated as follows. Based on ANL's correlation for debris bed cooling, it has been estimated that approximately 50% of the core melting down would be coolable in-vessel, if recriticality does not occur. Therefore, a meltdown which penetrates the GV could contain 50-100% of the core. The melted fuel may be from a region of the core where the decay heat generation rate is higher than for the unmelted fuel (i.e., the heat generation in the melted fuel can be greater than the average value). However, the escape of volatile fission products will reduce the heat generation rate in the melted fuel, so the net effect may be approximately the core average heat generation rate in the melted fuel.

Preliminary ANL calculations (Reference B.2) show a typical cylindrical molten pool of 8.9 ft radius by 2.5 ft deep on the sacrificial barrier, with upward and sideward heat fluxes three times the downward heat flux. (This, again, assumes that recriticality does not occur). This results

in 53% of the heat from the molten pool flowing upward into the sodium above the EVCC.

If all of the decay heat is assumed to be generated in the molten pool, then about 53% will flow upward into the sodium.

If 50% of the decay heat is assumed to be generated in the molten pool, then about 26% will flow upward into the sodium. This will be added to the 50% from the fuel remaining in the RV to give a total of 76% of the heat generation appearing in the RV sodium.

HEAT LOSSES FROM REACTOR VESSEL

Reference B.3 shows the heat load on the reactor cavity gas cooling system during normal operation. Items No. 1 through 19 of Reference B.3, the heat losses from the RV to the reactor cavity, were scaled up for the case of RV sodium temperature of 1620°F. All of the thermal resistance was assumed to be in the insulation, and the variation of thermal conductivity with temperature was accounted for in an approximate manner by using the variation with temperature of the thermal conductivities of similar materials in performing the scaling. The resulting heat loss was 585,420 Btu/hr for the RV temperature of 1620°F. A similar calculation for a RV temperature of 1080°F resulted in a heat loss of 308,120 Btu/hr.

Heat losses from the RV to the Head Compartment during normal operation are given in Reference B.4. This cannot be used directly to determine the heat losses following an HCDA because the ductwork between the Head Compartment and the vessel components may no longer be intact. However, H. Mitchell (Reference B.5) has calculated that of the 360,000 Btu/hr entering the Head Compartment during normal operation, 180,000 Btu/hr goes to the air in the ducts and 180,000 Btu/hr goes to the air in the compartment. If air flow is lost in the ducts following an HCDA, the heat flow to the air in the compartment will be greater than 180,000 Btu/hr, and therefore it is conservative to assume a heat loss of 180,000 Btu/hr to the Head Compartment. This is for the case of 1080°F sodium in the vessel and 80°F air in the compartment. Scaling up to the case of 1620°F

in the vessel results in a heat loss of 277,200 Btu/hr to the Head Compartment.

The total heat loss from the RV is

RV temperature, °F	Heat Loss, Btu/hr		
	to vessel cavity	to head compartment	total
1080	308,120	180,000	488,120
1620	585,420	277,200	862,620

HEAT FLOW THROUGH HOLE IN GUARD VESSEL

Since the bottom of the Guard Vessel is insulated, an appreciable temperature difference can exist between the sodium below the GV and that within the vessels unless there is good sodium circulation through the hole left in the GV by the melt-through. As a rough estimate of the natural circulation heat transfer through the hole, it is assumed to be the same as that for the cooling of a hot plate facing up at the bottom of a large pool (Reference B.6, Equation 2.39):

$$Nu = 0.0785 Ra^{0.32} \quad (1)$$

Substituting sodium physical properties at 1600°F results in

$$Q = 244 D^{1.96} \Delta T^{1.32} \quad (2)$$

where

Q = heat flow, Btu/hr

D = hole diameter, ft

ΔT = temperature difference, °F

Equation 2 is plotted in Figure B.2.

This is combined with the results of the preceding section on heat losses from the RV to give the temperature of the sodium beneath the GV versus heat flow for various hole diameters, as is shown in Figure B.3. Figure B.4 is a cross-plot of the same data.

STRESSES IN GUARD VESSEL

Creep-rupture data for annealed Type 304 stainless steel are given in Figure 4 of Reference B.7. Figure B.5 of the present memo is a cross-plot of these data.

Reference B.8 gives GV stresses for various conditions. Figure B.6 shows the geometry. The present concern is for long-term creep-rupture effects and therefore only the effects of internal sodium pressure and dead weight will be considered; OBE and DBE seismic loads and the thermal loads at sodium spill conditions will be ignored. The conditions at locations 4, 5, and 6 on Figure B.6 will also be ignored as the bottom head of the GV is already assumed to have failed.

1- Guard Vessel Shell

The maximum stress in the lower GV shell (Location 7) with the GV filled with 250°F sodium is 1.1 ksi (tension on inside surface, compression on outside surface [Reference B.8, page A-34]). It would be expected to be less for the present case with the GV bottom head penetrated because of the reduced internal sodium pressure and the balancing of pressure across the bottom head. However, it will be conservatively assumed to be unchanged.

Assuming a maximum sodium head of 30 feet to the outlet nozzle and one atmosphere overhead pressure, the maximum sodium pressure is 1.65 atmospheres and the corresponding boiling point is 1723°F.

From Figure B.5, for 1.1 ksi at 1725°F, the rupture time (t_R) is $\frac{2.6 \times 10^4}{10^4}$ hr. From the section Heat Losses from Reactor Vessel or from Figure B.3, for 1620°F sodium in the vessels, the heat loss (Q_L) is 862,620 Btu/hr.

If the reactor operating power is 400 MW then the decay heat (Q_D) is $13.65 \times 10^6 Y$ Btu/hr, where Y is percentage of operating power. If all of the decay heat is going to the vessel, then the decay heat level which equals the heat losses is given by

$$13.65 \times 10^6 Y = 862,620$$

$$Y = 0.0631\%$$

From the upper end of the uncertainty band in Figure B.1, this corresponds to a decay time (t_D) of 6×10^6 sec or 1667 hr.

For the vessel sodium temperature to drop to 1200°F, for example, and neglecting the time delay caused by the heat capacity of the sodium and structure, from Figure 3, $Q_L = 5.7 \times 10^5$ Btu/hr. $Y = 5.7 \times 10^5 / 13.65 \times 10^6 = 0.0418$. From Figure B.1, $t_D = 1.2 \times 10^7$ sec = 3333 hr.

Even this latter value is well below the rupture time of 26,000 hr and therefore the GV shell is not expected to fail as a result of creep.

2- Guard Vessel Skirt, Good Sodium Circulation

The maximum stresses in the GV skirt including geometrical imperfections (Location 3) for the cases considered in Reference B.8 (page A-23) are longitudinal compression of

- 1.1 ksi (Condition 1, GV empty)
- 4.2 ksi (Condition 2, GV full)

For the present case with the GV bottom head penetrated, the longitudinal compression would be the 1.1 ksi given in Condition 1 because the skirt would not be supporting the weight of the sodium. However, there would be a hoop stress resulting from internal sodium pressure of

$$\sigma = \frac{pd}{2t} = \frac{(30)(46.2)(266.125)}{2(.75)(1000)(144)} = \underline{1.71 \text{ ksi}}$$

This appears to be the highest principal stress and will be used in the creep-rupture analysis.

From Figure B.5, for 1.7 ksi and 1725°F, $t_R = 2500$ hr.

As in the preceding section, the sodium is assumed to remain at 1725°F for 1667 hr. This would use up $1667/2500 = 0.667$ of the life of the skirt, assuming it to be at the same temperature as the sodium. The temperature then drops to 1200°F after 3333 hr which will not appreciably increase the fraction of the skirt life used up. Therefore, the skirt would not be expected to rupture under these conditions of good sodium circulation through the hole in the GV bottom head.

3- Acceptable Hole Size, If All Heat to Sodium Beneath GV, and Skirt at 1725°F

Figure B.5:@ 1725°F and 1.7 ksi, $t_R = 2500$ hr

Allowing 10% for fraction of life lost during time that temperature is decreasing, permissible time at 1725°F is $t_R^1 = (0.9) (2500) = 2250$ hr

Figure B.1:@ 2250 hr, $Y = 0.053\%$

$$Q = 13.65 \times 10^6 \times 0.053 = 723,450 \text{ Btu/hr}$$

Figure B.4: $D^{-1} = 0.78 \text{ ft}^{-1}$

$$D = \underline{1.28 \text{ ft}}$$

4- Acceptable Hole Size, If All Heat to Sodium Beneath GV, and Skirt at 1620°F

Figure B.5:@ 1620°F and 1.7 ksi, $t_R = 1.65 \times 10^4$ hr

$$t_R^1 = (.9) (16,500) = 14,850 \text{ hr}$$

Figure B.1: $Y = 0.0095$

$$Q = 13.65 \times 10^6 \times .0095 = 1.297 \times 10^5 \text{ Btu/hr}$$

Figure B.4: $D^{-1} \approx 3.16 \text{ ft}^{-1}$
 $D = \underline{0.316 \text{ ft}}$

5- Acceptable Hole Size, If entire Core Melts Down into EVCC, 55% of
Heat Flows Up into Sodium Above EVCC, and Skirt at 1725°F

As in Case 3, total $Q = 723,450 \text{ Btu/hr}$

Heat flow into sodium $Q = (.55) (723,450) = 397,900 \text{ Btu/hr}$

Figure B.4: $D^{-1} = 1.87 \text{ ft}^{-1}$
 $D = \underline{0.535 \text{ ft}}$

6- Acceptable Hole Size, If 50% of Heat to Sodium in RV, Additional 30%
to Sodium Above EVCC, and Skirt at 1725°F

As in Case 3, total $Q = 723,450 \text{ Btu/hr}$

Heat flow from RV = $(.8) (723,450) = 578,760 \text{ Btu/hr}$

Heat flow through hole = $(.3) (723,450) = 217,035 \text{ Btu/hr}$

Figure B.3 $D = \infty$ curve, for $Q = 578,760 \text{ Btu/hr}$

Reactor Vessel $T = 1211^\circ\text{F}$

$\Delta T = 1725 - 1211 = 514^\circ\text{F}$ through hole

Figure B.2: for $\Delta T = 514^\circ\text{F}$ and $Q = 217,035 \text{ Btu/hr}$
 $D = \underline{0.475 \text{ ft}}$

7- No Circulation, Insulation Deteriorates Uniformly, and All Heat to
Sodium Beneath GV

In the previous cases, the internal insulation on the skirt was assumed to have deteriorated completely in the presence of sodium, allowing the skirt to reach the sodium temperature. However, the insulation on the GV bottom head was assumed to remain effective. For the present case, it will be assumed that both the internal insulation on the skirt and the insulation on the GV bottom head deteriorate equally in the presence of sodium. No sodium circulation is considered after sodium boiling ceases.

The insulation is assumed to be 9 inches of Kaylo on the GV bottom head and internal to the skirt, and 2 inches of Kaylo external to the skirt. The external insulation thermal conductivity was taken as 0.68 Btu-in/hr-ft²-°F corresponding to a mean temperature of 800°F, and the internal insulation thermal conductivity (K_i , Btu-in/hr-ft²-°F) was allowed to vary. Assuming all of the thermal resistance to be in the insulation, sodium temperature 1725°F beneath the GV, and Reactor Cavity temperature 120°F, the skirt temperature (T_S , °F) is given by

$$\frac{(0.68)}{2} (T_S - 120) = \frac{K_i}{9} (1725 - T_S)$$

or

$$T_S = \frac{40.8 + 191.7K_i}{0.34 + 0.1111K_i} \quad (3)$$

For a total area of the GV bottom head plate and cone estimated as 444 ft², the thermal resistance through the bottom head insulation and the GV and RV walls is approximately

$$R = \sum \frac{L}{KA} = \frac{\text{insul. in}}{K_i (444)} + \frac{\text{walls in}}{(144) (444)} + \frac{(2.5 + 2.75)}{(144) (444)}$$

$$= \frac{\frac{\text{Btu-in}}{\text{hr-ft}^2\text{-F}} \text{ ft}^2}{K_i} + \frac{\frac{\text{Btu-in}}{\text{hr-ft}^2\text{-F}} \text{ ft}^2}{\text{Btu}} + \frac{8.211 \times 10^{-5} \text{ hr-°F}}{\text{Btu}}$$

$$= \frac{0.0203}{K_i} + 8.211 \times 10^{-5} \text{ hr-°F/Btu} \quad (4)$$

Sample calculation: Let $K_i = 50$ Btu-in/hr-ft²-°F

Equation (3): $T_S = 1633^\circ\text{F}$

Equation (4): $R = 4.88 \times 10^{-4} \text{ hr-°F/Btu}$

Heat flow into RV

$$\begin{aligned} Q &= \frac{1725 - T_{RV}}{R} \\ &= \frac{1725 - T_{RV}}{4.88 \times 10^{-4}} \end{aligned} \quad (5)$$

Solve Equation (5) simultaneously with T versus Q curve for $D = \infty$ in Figure B.3. Intersection is at $Q = 7.00 \times 10^5$ Btu/hr.

$$Y = \frac{Q}{13.65 \times 10^6} = \frac{7.00 \times 10^5}{13.65 \times 10^6} = 0.0513\%$$

Figure B.1: $t_D = 8.5 \times 10^6 \text{ sec} = 2361 \text{ hr}$

Figure B.5: For $T_S = 1633^\circ\text{F}$ and 1.7 ksi, $t_R = 1.34 \times 10^4 \text{ hr}$

Similar calculations made for other assumed values of K_1 and results plotted in Figure B.7. Allowing 10% for fraction of life lost during time that temperature is decreasing, $t_R^1 = 0.9 t_R$ is greater than t_D over the entire range.

Therefore, the skirt is not expected to fail in creep, even with no sodium circulation through the hole, if the insulation in contact with sodium deteriorates uniformly.

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- B.4 FCC-1047, August 20, 1973, Letter from A. E. Cottam (ARD) to D. W. Holligan (Bechtel), "Head Compartment Heat Loads, Contract AT(45-1)-2171, Task 1". Attached report: FCC-1001, July 1973. H. A. Mitchell, "Head Compartment Heat Loads".
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- B.8 CENC Report No. 1200, P. L. Anderson, et.al., "Analytical Report for FTR Reactor Guard Vessel", July 1973.

FIGURE B.1 FTR Decay Heat at Goal Exposure

FIGURE B.2 HEAT FLOW THROUGH HOLE AT BOTTOM OF GUARD VESSEL

FIGURE B.3 TEMPERATURE OF SODIUM BENEATH GUARD VESSEL

$Q,$
 10^5 Btu/hr

FIGURE B.4 HEAT LOSS FROM SODIUM BENEATH
GUARD VESSEL TO REACTOR VESSEL

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5. DETAILED ANALYSIS A. SYSTEM GEOMETRY

FIGURE B.6 LOWER GUARD VESSEL
GEOMETRY

FIGURE B.7 CREEP-RUPTURE MARGIN FOR GUARD
VESSEL SKIRT IF INSULATION DETERIORATES