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## THE TRANSITION PHASE IN LMFBR HYPOTHETICAL ACCIDENTS

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### ABSTRACT

Mechanistic analyses of transient-under-cooling accidents have led in some cases to a mild initiating phase instead of a direct hydrodynamic disassembly of the core. The fuel is then trapped in the core by the strong mechanical surroundings and blockages formed by refrozen cladding steel and/or fuel. The formation of fuel blockages has been verified experimentally. The bottled-up core will boil on fission and decay heat, with steel as the working fluid. Boil-up in a churn turbulent flow regime may prevent recriticality due to fuel recompaction. Ultimate fuel removal from the core is probably by a two-phase blow-down after permanent leakage paths are opened. However, a vigorous recriticality can not be precluded. Reactors with void coefficients larger than that in CRBR are more likely to disassemble in the initiating phase, so the transition phase may be unique to small cores.

### INTRODUCTION

Early studies of hypothetical core disruptive accidents (HCDA) in LMFBRs postulated conservative but arbitrary recriticality events to determine upper limits on possible core and containment damage. More recent work has attempted to calculate detailed mechanistic accident event sequences to postulated accident initiators. This was done to remove the arbitrariness and reduce the large factors of conservatism in early damage calculations. Mechanistic analyses of unprotected transient-under-cooling (TUC) accidents in the Fast Flux Test Facility (FFTF) and the Clinch River Breeder Reactor (CRBR) using the SAS code [1] led in some instances to a gradual core meltdown instead of a hydrodynamic disassembly. [2,3]. This gradual disruption has been termed the "transition phase" because it represents a transition from an intact fuel geometry to one where the entire core is describable in hydrodynamic terms [4]. This paper summarizes some of our development of this area.

The initial work on the transition phase was done for the FFTF reactor [5]. In the early stages of the TUC accident for that reactor, the core remains at about nominal full power [6]. The initiating phase ends with the entire core voided of sodium and about a quarter of the core fuel disrupted. About three full-power-seconds of energy will complete the disruption of the remainder of the core fuel. A full core meltdown can not be avoided. The problem is to

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describe the nature of the fuel dispersal until a permanently subcritical configuration has been achieved. For the case of FFTF, this requires the removal of about 60% of the fuel from the core region.

For an unprotected TUC accident in CRBR, a positive sodium void coefficient leads to a power level which is several times nominal after sodium voiding begins [7]. This causes a greater level of temporal coherence in the accident sequence than was found in FFTF. The initiating phase ends with the entire core disrupted. Indeed, in sharp contrast to FFTF, CRBR may end its initiating phase by a direct hydrodynamic disassembly. For a TUC accident in reactors with sodium void coefficients larger than in CRBR a direct disassembly is more likely. The transition phase is more important in smaller reactors such as FFTF or CRBR. However, large void coefficients may be designed out of commercial reactors. If this occurs, the transition phase will become a more general part of the analysis of HCDAs.

#### THE BOTTLED-UP CORE

A characteristic of recent designs of LMFBRs in the U.S. is that the core is closely surrounded by strong mechanical structures. No large openings exist which could provide paths for easy dispersal of disrupted core fuel. Instead, if the surrounding structure remains intact, fuel must pass through long narrow passages to escape to the surrounding sodium pool. For FFTF, the structures below the core and radially surrounding it are far stronger than the above-core structure [8]. Average core pressures of about 5 MPa would be required to disrupt this structure and allow core expulsion. This would require a reactivity excursion of about 60\$/sec in a voided core. This strong, close mechanical constraint of the core may not occur in other reactor designs. For example, in one design for the GCFR there is no below-core constraint and failure of the subassembly structure at the core midplane would probably allow the bottom of the subassembly to drop out of the core [9]. The existence of a transition phase is less likely for such a design.

For the FFTF reactor an analysis of the initiating phase of the TUC accident [6] found that molten cladding may be levitated by sodium vapor to the top of the core where freezing could produce a thin blockage. The remainder of the steel would drain downwards and freeze at the bottom of the core, forming a much thicker blockage. Until these are melted out, they prevent axial fuel removal through the normal sodium flow sub-channels between fuel pins. For CRBR, the high power level during the initiating phase shortens the time span between melting of cladding and disruption of the fuel. For about 75% of the subassemblies, this time span is less than 0.2 seconds. Thus, insufficient time exists for much of the molten steel to relocate to the axial ends of the core and blockages would not form in a majority of the subassemblies. The bottling-up of the core by relocated cladding steel may thus be unique to reactors with a small sodium void coefficient.

A second and more general mechanism for blockage formation is the freezing of molten fuel as it is axially ejected between the pins. The fuel will freeze because of very high convective heat transfer from the fuel [10,11]. The hydraulic diameter between the pins is about 3.4 mm, so for fuel driven from the core at a reasonable velocity, say 10 m/s, Reynolds numbers are on the order of 70,000. Thus a turbulent heat transfer correlation must be used. The following relationship for the Nusselt number, Nu, which has been tuned to the Deissler correlation [12], is appropriate.

$$Nu = 0.023 (Pr Re)^{0.8}, \quad (1)$$

where Pr is the Prandtl number and Re is the Reynolds number. When the fuel to steel temperature difference is on the order of the difference between their melting points (1400K) heat transfer rates on the order of 100 W/mm<sup>2</sup> are obtained. Such large heat transfer rates can rapidly freeze the fuel.

One mechanism that could limit the heat transfer is the formation of a thin crust of solid fuel frozen onto the steel surface. This would insulate the molten fuel from further high heat losses. This mechanism was considered and found to be improbable [10] for the following reason. When molten  $UO_2$  wets a surface of stainless steel with the steel temperature greater than about  $650^\circ C$ , the steel surface instantly melts [13]. Thus, a layer of solid fuel would not have an underlying solid surface to support it and would break up under stresses imposed by the flowing liquid.

A computer program was written to analyze the freezing problem. It included calculations of momentum transfer and flow of the molten fuel, convective heat transfer from the fuel to the steel and conduction in the steel. As seen in Table I, predictions of the model are in good agreement with experiments performed by R. E. Henry [11]. In these experiments a high void fraction mixture of molten fuel and molten molybdenum at  $3200^\circ C$  was injected at 15 m/s into a seven pin bundle of steel tubes which simulated hollow fuel pins. These results demonstrated that molten fuel can not penetrate the fission gas plenum in FFTF or the axial blankets in CRBR without freezing and forming a blockage.

Starting from Eq. 1, a simple analytic approximation can be obtained for the penetration distance at which the leading edge of the flow is frozen solid. The rate of temperature drop of the fuel per unit distance of travel is given by

$$\frac{dT}{dz} = -0.092 \frac{\alpha^{0.2}}{D^{1.2} V^{0.2}} (T - T_{wall}), \quad (2)$$

where  $\alpha$  is the thermal diffusivity of the fuel,  $D$  is the hydraulic diameter and  $V$  is the flow velocity. Since the flow velocity appears to the fifth root, it can be approximated as a constant and Eq. 2 can be integrated. If we assume that the wall temperature is at the melting point of steel,  $1400^\circ C$ , and add  $550^\circ C$  to the initial temperature to account for the heat of fusion of the oxide, the penetration distance,  $Z$ , is given by

$$Z = 10.9 \frac{D^{1.2} V^{0.2}}{\alpha^{0.2}} \ln \left( \frac{T_o - 850}{1400} \right) \quad (3)$$

where  $T_o$  is the initial fuel temperature in degrees Celsius. For the experiments summarized in Table I, the formula predicts penetration distance of 130 and 310 mm for penetration between and inside the pins, respectively. The agreement is reasonable although the formula may be low by as much as a factor of two in some cases.

More recent experimental results [14] support the concept that breakup of the fuel film will prevent formation of an insulating layer. However, the experiments demonstrated a catastrophic ablation of the steel. This effect is not included in our model and an attempt to do so leads to sharp disagreement with experiment. To demonstrate this, suppose that steel ablation is a continuous process with the ablation rate dependent on the ratio of wall heat flux to the steel heat of fusion,  $L$ . Assume that the ablated steel first mixes with the fuel and then comes into thermal equilibrium with it. Then the power,  $Q$ , needed to raise the steel to the fuel temperature is given by

$$Q = q C_p \Delta T / L, \quad (4)$$

where  $q$  is the convective heat transfer rate to the wall. The temperature rise  $\Delta T$  is about 1400 K and the heat of fusion of steel is equivalent to a temperature rise of 350 K. Thus the energy needed to heat up the steel is four times that needed to ablate it, and the total heat drain on the fuel is a factor of five greater than that given by Eq. 1. Thus the model would predict a penetration distance 5 to 10 times less than that seen experimentally. Epstein et al have recently attempted to address this problem by assuming that the fuel crust can not break up until the thickness of the underlying liquid steel layer is equal to the laminar sub-layer of the turbulent flow [15]. On the other hand, if the

TABLE I

Comparison of Experimental and Predicted  
 $UO_2$  Penetration Distances

Initial Steel Temperature, C	Fuel Penetration Distance, mm			
	Between Pins		Inside Pins	
	Experiment	Model <sup>a</sup>	Experiment	Model <sup>a</sup>
20	90	100	180	240
400	200	110	380	270
800	230	130	400	320
900	230	140	380	330

<sup>a</sup> Computer results are based on a heat transfer correlation slightly different from Eq. 1 due to a change in the accepted value of fuel viscosity from 8 to 4 mPa's.

high void fractions of the ejected material in the experiments leads to a different freezing process than for a dense liquid, the agreement shown in Table I is fortuitous. We would still expect rapid fuel freezing, but the penetration depth becomes uncertain.

The discussion so far has neglected the effect of sodium. Fuel attempting to penetrate the axial blanket region would probably contact dry steel. However, fuel flowing between the subassemblies or down into the shield block-orifice region below the core is likely to encounter sodium. In our FFTF analysis it was assumed that such an encounter would cause a rapid freezing of the fuel and subsequent blockage formation. However, recent experiments [16] have indicated that no strong thermal interaction takes place. The sodium is pushed ahead of the fuel and the penetration distance is not drastically affected.

When the fuel stops its axial flow between the pins and forms a blockage, it comes into thermal equilibrium with the surrounding material. In FFTF, the low heat capacity of the empty fission-gas plenum structure leads to an equilibrium temperature which is not much below the melting point of fuel. The blockage is a slush of solid fuel and molten steel which may be easy to wash out by any boiling turbulence in the core. The blockage formed in the axial blanket region in CRBR will be much colder due to the added heat capacity of the blanket fuel and should be more difficult to penetrate. When blockage melt-through does occur, molten fuel and steel will be injected further into the pin structure where it will freeze again. The formation of permanent openings will require several cycles of injection, blockage formation and blockage meltout.

In summary we have found that for a mild initiating phase in a TUC accident, the core fuel tends to be bottled up by a combination of strong and close mechanical surroundings and blockages formed by the freezing of relocated steel and fuel.

#### THE BOILED-UP CORE

An important characteristic of an oxide-fueled LMFBR is that the boiling point of steel is very close to the melting point of fuel. For an operating pressure in the FFTF core of about 0.15 MPa, the boiling point of Type 316 stainless steel is 3180 K. This is only 120K above the liquidus of mixed oxide fuel ( $U_0.8Pu_0.2O_2$ ) and 160 K above the solidus. Thus as the fuel in the core is disrupted, steel vapor pressure will begin to provide a dispersive force [17]. If heat losses to the surrounding material are counterbalanced by fission and decay heating, violent boiling of the steel has the potential to prevent fuel compaction and a subsequent recriticality. It is not likely, however, that steel vapor pressure can significantly retard initial fuel slumping, if it

occurs, at the time of fuel disruption. This is partly because the steel boiling point is slightly but significantly above the fuel melting point. In the R series tests on unirradiated fuel in the TREAT reactor, disrupted fuel at normal power levels always slumps despite the presence of molten steel [18]. In this discussion, the effect of fuel vapor pressure is neglected because it is a factor of five to ten times lower than that of steel at these temperatures.

After blockages are formed at the axial ends of the subassemblies the fuel will eventually all melt and form a boiling pool. This pool will consist of a mixture of molten steel and molten fuel with steel vapor rising through the liquid. The fuel and steel are immiscible, so in a quiescent system they would separate with the lighter molten steel (which has a specific gravity of 7 compared to 8 for molten fuel) floating on top. However, a dense quiescent system can not form at this point without causing a supercritical excursion. The pool must remain boiled-up with a high void fraction. The turbulence this will cause, combined with the mixing effect of strong convection currents, should maintain a generally uniform mixture.

In the disrupted fuel region the void fraction will generally be between 1/3 and 2/3 depending on the degree of fuel or steel removal to blockages at the axial ends of the core. For FFTF, where much of the cladding has been relocated but the fuel is still in place the initial average void fraction inside a disrupted subassembly will be about 1/2. Such high void fractions in a continuous liquid system lead to a churn-turbulent flow regime [19] in which the bubbles strongly interact and agglomerate. This flow regime is a transition between ideal bubbly flow, where bubble agglomeration is unimportant, and slug flow, where the bubble diameter is on a par with the pipe that contains it. The formation of slug flow requires a considerable developing length. A fuel subassembly has a ratio of active core height to diameter of about nine. This is probably too low for significant slug flow to develop, especially in view of the peculiar end effects caused by the axial blockages. After subassembly walls are melted through to form a larger coherent boiling region, the height to diameter ratio decreases even further and slug flow becomes impossible. In a churn-turbulent flow regime with no net liquid flow, the axial vapor current,  $j$ , is given by [19]

$$j = \frac{\alpha}{1-\alpha} \left[ 1.53 \left( \frac{g\sigma}{\rho_l} \right)^{1/4} \right] \quad (5)$$

where  $\alpha$  is the local void fraction, the term in brackets is the velocity of isolated bubble rise,  $g$  is the acceleration of gravity,  $\sigma$  is the surface tension of the liquid fuel (0.45N/m), and  $\rho_l$  is the liquid density. If convection currents generated by the cold walls of the pool do not disturb the flow pattern, an approximate solution for the void distribution can be found as follows. From conservation of energy,

$$\frac{dj}{dz} = q(1 - \alpha)/\rho_v h \quad (6)$$

where  $\rho_v$  is the vapor density,  $q$  is the power density in the liquid which is forming vapor and  $h$  is the heat of vaporization. If we use the approximations that the power density,  $q$ , and the vapor density,  $\rho_v$ , are axially independent, and use the boundary condition that the void fraction is zero at the base of the pool, then Eq. 6 can be integrated to yield

$$\alpha = 1 - \left( 1 + \frac{4\bar{\alpha}}{(1 - \bar{\alpha})^2} \cdot \frac{z}{H} \right)^{-1/2} \quad (7)$$

where  $H$  is the pool height and  $\bar{\alpha}$  is the average void fraction in the pool. This demonstrates that the axial distribution of fuel and steel can be substantially nonuniform.

It has been suggested that the continuous liquid flow regime postulated here will break down into a continuous vapor flow regime with droplet fluidization of the liquid, and that this would prevent recriticality [20]. The onset of fluidization will occur at a critical vapor flux,  $j^*$ , given by [20]

$$j^* = 0.14 (g\sigma\rho_l)^{1/4} / \sqrt{\rho_v} \quad (8)$$

This can be combined with equations 5 and 7 to obtain the critical average void fraction,  $\bar{\alpha}^*$ , at which the churn-turbulent flow regime will break down and fluidization will begin at the top of the pool, viz.,

$$\bar{\alpha}^* = (1 + 21.8 \sqrt{\rho_v/\rho_l})^{-1} \quad (9)$$

At low temperatures, and therefore low vapor densities, very high void fractions are required for fluidization. The critical average void fraction does not drop to 2/3 until a temperature of almost 4000 K is reached. From this approximate analysis, fluidization appears to be unlikely at this stage of the accident. A system that starts out with vapor as the continuous phase should quickly convert to a bubbly system. One reason is that a continuous liquid region, which does not satisfy the fluidization criterion, will rise from the base of the pool due to natural circulation and quickly fill the entire system.

The internal heating of the fuel can cause bubble nucleation within the liquid, away from the walls and other bubbles. Bubble formation in the fuel itself is unlikely because it would be suppressed by the surrounding steel vapor pressures. The difference in the boiling points of fuel and steel is about 400K. A fuel droplet at full power would have to be larger than 5 mm in diameter for conduction to support such a temperature difference and turbulent convection would substantially increase this minimum diameter. Nucleation could take place in the steel in an isolated mixture of steel and fuel droplets. A reasonable superheat of 10K could be supported by conduction from a steel droplet of 3 mm diameter with a volumetric heat generation equal to that of fuel at full power. If the thermal conductivity, of the region is controlled by fuel instead of steel, the region diameter is only 1 mm. Turbulent convection should increase these sizes. If the boiling turbulence induces local Nusselt numbers in the range of 10 to 100, local regions of 3 to 30 mm diameter would be large enough to support bubble nucleation. Thus at high power, the pool might tend to be "foamy." At low power levels, say 1/10 of nominal, which would be more likely than full power in a boiled-up subcritical system, the necessary region size becomes 10 to 100 mm and the effect is less important. With respect to fluidization, this bubble formation is not relevant since it does not affect the criterion given in equation 8.

The effect of rising vapor will be superimposed on strong convection currents generated by the cold walls. Heat transfer to the walls will prevent the neighboring presence of steel vapor bubbles. Thus a dense liquid exists near the walls and a low density region exists in the rest of the pool. Strong convection currents should be generated with liquid rising in the center of the pool and draining along the walls. Since the walls consist of steel which melts at least 1400 K below the pool temperature, a rapid melting attack will occur. At the same time, the low temperature of the walls will freeze a layer of fuel which can form an insulating crust. The thickness of this crust depends on the heat flux from the pool to the crust, the volumetric heat source in the crust, and the temperature drop across the crust. The crust temperature drop should be on the order of the difference in melting points of fuel and steel, or 1400 K. For a single subassembly pool in FFTF with the power level at decay heat (5% of nominal), if all of the heat is removed through the walls, the crust thickness is about 5 mm. In calculating the heat output from the pool, we have neglected the fact that part of the fuel is in the crust instead of in the pool. For a larger pool of say 100 subassemblies, because of the factor of ten decrease in the surface to volume ratio, the stable crust thickness drops to only 0.6 mm. This may not be thick enough to maintain its integrity. Two factors will lead

to thicker crusts. First, some of the energy will be removed from the pool in the axial direction. Second, the power drain involved in heating up steel which is entering the pool can reduce the net heat load to the wall by a factor of five. This is the same effect that was discussed in the previous section with regard to the effect of steel entrainment on the rate of fuel freezing. This effect would increase the stable crust thicknesses for the two cases discussed to 9 mm and 3 mm.

From the above discussion, the picture that emerges for a full core boiling pool in CRBR is shown in Figure 1. The pool is trapped above and below by frozen fuel and is insulated on the side by a frozen fuel crust. For FFTF, the blockage at the base of the pool would be refrozen cladding steel covered by an insulating fuel crust. The pool is boiled up to fill all of the available space. Vapor bubbles rise up through the pool and condense at the upper blockage. The denser liquid at the wall generates a strong convection current that causes much of the heat to drain from the system at the walls. A detailed analysis of such a system is a complex task and has not been done. For studies on FFTF we have used heat transfer correlations developed from data on boiling salt water pools with internal heat generation [21]. These correlations are based on a model of bouyancy-driven, forced-convection heat transfer and their results should give a reasonable indication of the heat losses. They are included in a Bottle-Model pool program which calculates the pressure and temperature history of a pool, and melting rates for the surrounding steel. The program assumes that all of the pool material is at the same temperature.

Figure 2 shows an application of the model to the FFTF reactor. Initially, the pool is at 3305 K and is contained in a single subassembly. Twelve such pools are assumed to exist adjacent to each other in the center of the core. The initial heat transfer rate includes the effect of steel melt-in. The point of sudden rise in the curves indicates that the subassembly walls have melted through and a larger, coherent pool has formed from the original twelve separate ones. It was assumed that steel melt-in does not occur for the larger pool because of external sodium cooling. These results demonstrate that a complete boil-up is possible on decay heating alone. However, too many uncertainties exist to guarantee pool boilup, especially when contained in single subassemblies where the surface to volume ratio is high. Indeed, from Figure 2 we would infer pool subsidence at 5% of nominal power before the walls melt through. For larger pools, and especially a full core pool, decay heat alone can probably maintain boil-up and prevent recriticality due to pool subsidence.

There are significant uncertainties in this analysis. Some examples are: the accuracy of the heat transfer correlations; the stability of the fuel crust; the mechanism by which melting steel penetrates the crust and enters the pool; the heat transfer rate at the upper blockage. One unsolved problem is to describe the transition from the original intact fuel geometry to the boiled-up state. The problem here is that the nature and mechanism of initial fuel break-up and dispersal is uncertain [22], no satisfactorily detailed picture of a boiling pool in a single subassembly has yet been developed, and tools to analyze the dynamic shift from fuel break-up to boil-up are only now being developed [23,24].

## CORE DISPERSAL

Our analysis of the transition phase has identified four distinct mechanisms for removal of fuel from the core to a permanently subcritical geometry. The first of these and the most likely one is a two-phase blowdown of the molten core due to steel vapor pressures. This could take place after the meltout of axial blockages opens a permanent leakage path through the pin structure. For a full core pool in FFTF starting at 0.5 MPa and at 10% of nominal power, ejection of 80% of the core through a single, opened subassembly can occur in less than four seconds [5]. Alternatively, if the control rods are partially out of the core, melt-through of control subassembly walls can lead to a blowdown through the below-core shield-orifice region of these subassemblies.

The second dispersal mechanism is a core blowdown through openings formed due to high pool temperatures and correspondingly high steel vapor pressures. This might occur if the heat losses from the pool are too low to counterbalance the fission and decay heating. A slow pressurization of the core would lead to a slow bending open of the above-core structure until a permanent leakage path formed. A two-phase blowdown of the core would follow. The pressurization that might occur would probably be very slow. At decay heat levels, the average core temperature would rise roughly 15 K/s. At that rate, sufficient pressure to open the core would take on the order of a full minute to develop. Other leakage paths would probably be melted open within that time scale. The third dispersal mechanism is a mild mechanical disassembly due to high sodium vapor pressure in the core. This may apply only to FFTF which has pressurized test loops that could crack open and inject sodium into a boiling core.

The final mechanism is a mechanical disassembly due to a high ramp rate recriticality. In FFTF, a series of mild excursions may occur during the initial stages of the core disruption and slumping. These excursions should be mild (less than 10\$/sec) and would be insufficient to cause disassembly. Their main influence would be to accelerate the melting attack and ultimate boil-out of the molten core. However, the dynamic behavior of a boiling core is not well understood. A mild power excursion would temporarily disperse the pool material away from the point of peak power density. The subsequent rearrangement of fuel thus dispersed cannot be analyzed in detail at this time, but the potential for a large ramp rate recriticality may exist.

#### SUMMARY AND CONCLUSIONS

A transient-under-cooling accident in an oxide-fueled LMFBR with a low sodium void coefficient will lead to whole core involvement, probably without an immediate hydrodynamic disassembly, but with a gradual fuel meltdown. The melting fuel is trapped in the core by the close, strong mechanical surroundings and by the formation of axial blockages due to refrozen cladding steel or fuel. The trapped fuel will tend to boil up on steel vapor pressure. Boiling pools of steel and fuel with steel vapor bubbles rising through them will form in individual subassemblies. They will melt radially to form larger coherent boiling regions at the same time as the blockages undergo a melting attack.

In FFTF, this early stage of the disruption will be accompanied by a series of mild excursions as previously undisrupted fuel slumps. Mild excursions might also be induced by subsiding of pools in individual subassemblies if the heat transfer rate to the walls is high enough and the core power is low enough. Recriticalities which challenge the primary containment do not appear to be likely. However, further effort is needed before a quantitative assessment of probabilities and magnitudes of recriticality excursions will be defensible.

The most likely path to accident termination is a two-phase blowdown of the core into the surrounding sodium after permanent leakage paths are formed. Such a blowdown would be non-energetic and would have no potential to mechanically challenge the primary containment. If the leakage paths are through the above-core pin structure, a series of fuel injection, fuel freezing and blockage melt-out cycles would occur. Before the final core blowdown, a single whole-core boiling region would probably form. Alternative leakage paths may exist through the below-core structure of control subassemblies. This would lead to earlier fuel removal. For post accident heat removal considerations, a knowledge of the direction of fuel removal, whether above the core or below it, is highly desirable. However, with our present understanding, the direction can not be firmly established.

A great deal of experimental and analytical work remains to be done on the transition phase. However, now that some of the basic phenomenology is becoming clear, a reliable, mechanistic computer analysis of this portion of the accident sequence is beginning to appear feasible.

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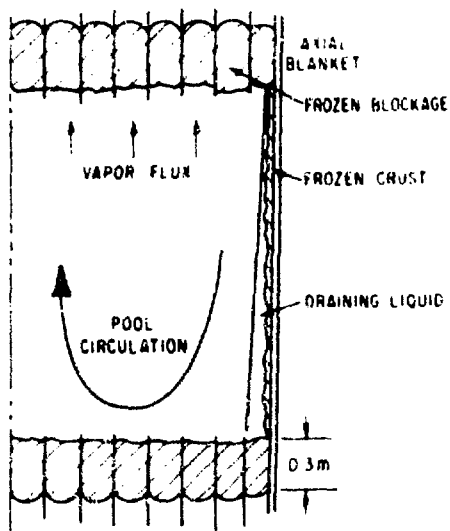


Fig. 1. Schematic of a full core boiling pool in CRBR.

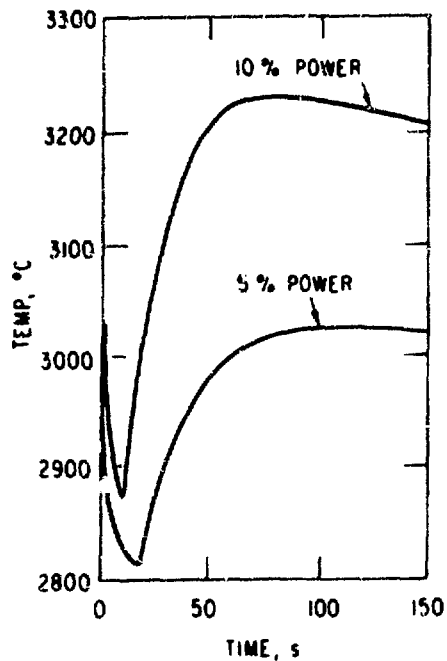


Fig. 2. Temperature history of boiling pools in FFTF at low power. The minimum points occur at the time of subassembly wall melt-through when larger pools are formed.