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HFBR Restart Activity: A 3.3

**DETERMINATION OF MAXIMUM REACTOR POWER LEVEL
CONSISTENT WITH THE REQUIREMENT THAT
FLOW REVERSAL OCCURS WITHOUT FUEL DAMAGE**

April 19, 1990

Prepared for:

Mr. P. R. Shire
Los Alamos National Laboratory
N-Division, MS K557
P.O. Box 1663
Los Alamos, NM 87545

Prepared by:

Dasari V. Rao, John L. Darby, Steven B. Ross and Robert A. Clark
Science and Engineering Associates, Inc.
SEA Plaza
6100 Uptown Blvd. NE, Suite 700
Albuquerque, NM 87110

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EXECUTIVE SUMMARY

The High Flux Beam Reactor (HFBR) operated by Brookhaven National Laboratory (BNL) employs forced downflow for heat removal during normal operation. In the event of total loss of forced flow, the reactor will shutdown and the flow reversal valves open. When the downward core flow becomes sufficiently small then the opposing thermal buoyancy induces flow reversal leading to decay heat removal by natural convection. There is some uncertainty as to the adequacy of flow reversal and natural convection to remove decay heat. BNL- staff carried out a series of calculations to establish the adequacy of flow reversal to remove decay heat. Their calculations are based on a natural convective CHF model developed by Fauske [1]. A review and evaluation of these calculations has been performed and independent confirmatory calculations have been made when necessary. The review included the following tasks:

- Independently evaluated Fauske's CHF model, its accuracy and applicability to HFBR operating conditions (The study revealed that Fauske's CHF model is non-conservative, at best. Hence, the following tasks were performed),
- Developed a flooding limited CHF model that compares well with the experimental data from various investigations that included HFBR flow reversal test,
- Checked Fauske's analysis of flow reversal and helium release phenomena,
- Evaluated BNL-staff calculations of maximum power limit on the HFBR which were based on the Fauske's CHF model and an assumption that maximum break size is limited to 1.39 in²,
- Developed a hydraulic model for HFBR core that can predict core flow rate during LOCA due to static water head pressure, and
- Developed a model capable of predicting core flow rate required to suppress boiling in the core.

The order of performance of these tasks and results from each task are presented in graphical form as Figure 1. Important results of these tasks can be summarized as follows:

- Corresponding to each operating power, there is a certain time period, referred to in this report as the critical time period, over which adequate forced convection is essential to prevent CHF occurrence. Table 3.1 lists estimates of the critical periods for different operating power levels.
- Following a total break of the HFBR effluent pipe, adequate forced cooling is available only for a maximum of 7 secs after the scram. This time period is shorter than the critical time period corresponding to reactor powers larger than 20 MW.

Based on these results it is concluded that HFBR maximum power level is limited by the break size considerations. Total break of effluent pipe may lead to partial core damage due to CHF occurrence for power levels higher than 20 Mw. However, if the break size can be limited to areas smaller than 19 in² (equivalent diameter of 5 in.) then the time period over which adequate forced convection is available is larger than the critical time period corresponding to 40 Mw. For 60 Mw operation the tolerable break sizes are even smaller.

In setting these limits, it should be noted that all calculations performed so far enveloped the steady state CHF phenomena and thus form a very conservative bound. The actual phenomena in HFBR, being transient in nature, are expected to be associated with larger CHF values. Transient CHF modeling is necessary if such predictions are deemed necessary. Additionally, HFBR fuel plates possess a large heat capacity that can store decay heat for several minutes without overheating. None of the models developed in our review or those developed by BNL-staff incorporated these thermal inertia effects. Preliminary calculations by the reviewer indicate that accurate thermal analysis of the fuel plate subsequent to the CHF occurrence is important to establish safety at higher power levels. Future efforts in this direction are strongly recommended.

Figure 1. Graphic Representation of HFBR Restart Activity A3.3

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1. INTRODUCTION

The High Flux Beam Reactor (HFBR) operated by Brookhaven National Laboratory (BNL) employs forced downflow for heat removal during normal operation. In the event of total loss of forced flow, the reactor will shutdown and the flow reversal valves open. When the downward core flow becomes sufficiently small then the opposing thermal buoyancy induces flow reversal leading to decay heat removal by natural convection. There is some uncertainty as to whether the natural circulation is adequate for decay heat removal after 60 MW operation. BNL-staff carried out a series of calculations to establish the adequacy of flow reversal to remove decay heat. Their calculations are based on a natural convective CHF model developed by Fauske [1]. The primary purpose of the present calculations is to review:

1. The accuracy and applicability of Fauske's CHF model for the HFBR, and
2. The assumptions and methodology employed by BNL-staff to determine the heat removal limit in the HFBR during a flow reversal and natural convection situation.

To accomplish these objectives several calculations were performed to quantify various uncertainties involved as well as to verify BNL-staff calculations. The following sections summarize the results of the present analysis. Section 2 provides discussions on the CHF phenomena during natural convection and on various models available to describe such phenomena. Fauske's model for the HFBR formed the basis for these discussions. The next section (section 3) focuses on verifying various assumptions adopted by the HFBR staff and their limitations. The final section presents the conclusions of the present study as well as recommendations for future work.

Before presenting these discussions, however, a description of the accident progression that could culminate in core cooling by natural convection is presented. This description is provided to point out various factors that could affect heat removal by natural convection.

1.1. An Accident Scenario Involving Loss of Forced Flow

Consider an accident scenario that results in total loss of pumping power and consequently loss of forced cooling of the HFBR core. The accident analysis section of 1982

HFBR-FSAR reveals that loss of forced cooling could result as a consequence of the following two design basis accidents:

1. Primary system pipe(s) break (Primary-LOCA), and
2. Beam tube(s) rupture.

In both cases the accident leads to trip of (a) both primary pumps, (b) the shutdown pumps, and (c) the pony motor (by manual action) that is on-line. The period over which the forced convection is available after reactor scram depends on several factors. Discussions of this nature are presented in section 3.

The trip of the primary pumps causes automatic opening of the depressurization valve HCe-101 leading to rapid depressurization of the reactor core. Additionally, the trip of both primary pumps and shutdown pumps also causes automatic opening of the flow reversal valves which provide a by-pass to the core in the initial stage and a flow path to the downcomer during flow reversal mode. Initially, the static head in the vessel drives the flow through the parallel network of flow reversal valves and reactor core to the break. This provides an additional period of forced convection until the buoyant forces overcome the static head. When such a condition is met, flow reversal aided by the natural convection is the only core heat removal mechanism. It is in this context that natural convective CHF becomes relevant. An accurate estimate of the natural convective CHF values is important not only to set a limit on the reactor operational power, but also to determine the minimum period of forced cooling necessary to remove decay heat after scram.

Occurrence of CHF in HFBR is considerably affected by various factors that include release of dissolved non-condensable helium during depressurization, the process of flow stagnation and flow reversal, hydrodynamic instabilities, and variations in channel geometries. Incorporation of all these factors in a single analytical model for natural convective CHF phenomena is complex and requires prolonged effort. In view of this it is imperative that a consistent methodology should be adopted. The methodology adopted by the BNL-staff involved formulation of a simple CHF model and conducting a sensitivity study that quantifies individual effects of each of the aforementioned factors. While we concur with the methodology, our review revealed that several assumptions made during implementation of this methodology are non-conservative in nature. The following sections summarize important findings of our review.

2. NATURAL CONVECTIVE CHF IN VERTICAL RECTANGULAR CHANNELS

To estimate CHF in the HFBR during natural convection, an experimental investigation was conducted by the BNL-staff in which ex-reactor tests were conducted to establish the decay heating rate at which flow reversal aided by natural convection is sufficient to remove heat without overheating the fuel element. These test results were used to determine consequences of various accident scenarios. For example, these test results were the basis for the assumption that fuel overheating does not occur if forced cooling of the reactor core is available for at least three (3) minutes after reactor scram following operation at 60 MW [2-4]. The applicability of these tests to the HFBR thermal-hydraulic conditions was questioned by National Research Council (NRC) and the Advisory Committee on Nuclear Facility Safety (ACNFS). In the course of developing a response to these questions, BNL-Reactor division hired H. Fauske to develop a natural convection CHF model for calculating the heat removal limit during flow reversal. This model was later used by the BNL-staff to calculate an upper limit for the reactor operating power.

As pointed out earlier one of the primary objectives of this study is to independently evaluate the Fauske model [1] and its applicability to HFBR. As a part of this task, a thorough review of literature reported on natural convective CHF in rectangular and similar geometries was conducted. The review was instrumental in identifying various dominant phenomena that trigger CHF as well as available approaches to model these phenomena. The acquired knowledge was also helpful in the review of Fauske's analysis. Consequently, a summary of the important findings of the literature review are presented before pursuing detailed discussions on the Fauske analysis.

2.1. CHF Phenomenon during Natural Convection

The experimental investigations of Mishima and Ishii [5], Mishima and Nishihara [6], Hewitt et al. [7], and numerous other investigators [8,9] indicate that natural convective CHF in narrow channels is very similar to forced circulation CHF. If the flow rate through the channel during natural convection can be accurately estimated, then the CHF value can be estimated using various correlations available or based on phenomenological models. However, estimation of core flow rate is very difficult since it is a complex function of channel power, inlet subcooling, channel geometry and inlet and outlet designs. In general it is an increasing function of channel power that gives rise to buoyant forces and a decreasing function of frictional pressure drops in the channel. For long and narrow channels, for example, the frictional pressure drops are usually large in comparison to the buoyant forces

resulting in low mass flow rate through the channel. At such low mass fluxes the CHF is caused by the flooding condition [6, 7, 10, 11]. This means that even though all the liquid entering from the bottom evaporates before reaching the exit, the upper dry part of the heated surface is continuously rewetted by the falling liquid film until the flooding condition is reached. Flooding condition, also known as the flooding limit, in this context refers to a situation where the upward vapor velocities are sufficiently large to cause stagnation of the downward liquid film flow. Approach to flooding condition would thus lead to formation of large dry patches that can no longer be cooled. With time (which depends on the thermal inertia of the heater) these dry patches grow rapidly resulting in burnout of the heated surface. A theoretical model of flooding limited CHF can be developed based on the phenomenological description of this scenario provided by Mishima and Nishihara for rectangular channels [6].

The CHF estimates based on the flooding condition provide a conservative bound for CHF during natural convection because they neglect the contribution of the flow through the channel. Experimental investigations indicate that if the established flow rate through the core during natural convection is larger than $25 \text{ kg/m}^2\text{-s}$, then the CHF values could be higher than the CHF estimates based on the flooding condition. In this region the CHF values were found to increase linearly with the mass flux G as described by the following functional form [5, 6, 8, 9]:

$$q''_{\text{flow}} = q''_{\text{flooding}} + (\text{const}) \cdot G \quad \text{--- 1}$$

The constant in equation 1 is geometry and inlet subcooling dependent. Examination of equation 1 would reveal that flooding limited CHF forms the most conservative bound for natural convective CHF corresponding to the case of no flow ($G = 0$).

The increase in CHF at larger mass fluxes can be explained in the following way. At larger mass fluxes, the resultant vapor velocities are usually large which restricts the falling liquid film flow rate. Consequently, the complete evaporation of liquid film on the heated surface results in the occurrence of CHF near the exit of the channel. This type of CHF, referred to as dry-out, occurs during the annular flow regime where a thin film of liquid covers the heated surface. The investigations of Mishima and Ishii, and Mishima and Nishihara reveal that for mass fluxes in the range of $25\text{-}75 \text{ kg/m}^2\text{-s}$ the dryout heat fluxes can be conservatively estimated using the churn-annular flow transition criteria. At mass fluxes higher than $100 \text{ kg/m}^2\text{-s}$, the annular flow models developed by Hewitt et al. [7] can be used to estimate the dryout heat fluxes. This situation is in contrast with the flooding

model in which the vapor drag on the falling liquid film is small until the flooding condition is reached. Once the flooding condition is reached then the dry patches form on the heater due to the inability to replace the falling film.

2.2 Fauske's Model for Natural Convective CHF [1]:

Fauske assumed that CHF in HFBR occurs during annular flow where the liquid film on the heated surface evaporates. Such an assumption is justifiable only if the flow rates are shown to be relatively large; Fauske did not make any attempt to resolve this factor. In fact, Fauske assumed the pool water to be saturated. In which case it is not clear to the reviewer how large upward liquid flow rates are possible except by interfacial drag which comes into flow during annular flow but not prior to churn-annular flow transition. Nevertheless, Fauske proposed to estimate the CHF value based on the churn-annular flow transition. He obtained one such transition criterion based on a 0-dimensional force balance. The criterion developed in the analysis can be given as

$$J_g = \sqrt{\frac{\Delta P \cdot g \cdot 2w}{\rho_g}} \quad \text{--- 2}$$

The heat balance leads to the following estimate for CHF:

$$q''_{\text{Fauske}} = \frac{A_c}{A_h} \cdot h_{fg} \cdot \sqrt{(\Delta P) \cdot g \cdot \rho_g \cdot 2w} \quad \text{--- 3}$$

In obtaining equations 2 and 3, it was implicitly assumed that CHF during annular flow occurs when the liquid film on the unheated narrower walls stagnates. Such an assumption is not only non-conservative but also inconsistent with the previous experimental observations. As evident from the experimental results of Mishima and Nishihara [6], it is the flooding limited CHF that is caused by stagnation of falling liquid film on the narrower wall. To the contrary, CHF during annular flow is due to evaporation of liquid film on the heated surface [7] which also reverses its direction during churn-annular flow transition. Consequently, any consistent theory should obtain equation 2 from the force balance on liquid film that envelops the heated surface rather than the narrower unheated wall. It can be easily shown that such an analysis would result in substantially lower CHF values as compared to equation 3. It should be noted that equation 2 is valid in case of a flooding condition only if the empirical constant C is taken to be its maximum possible value of 1 as subsequently discussed. But, the heat balance equation for flooding condition is quite different from that used to derive equation 3. Thus Fauske's model is inaccurate to predict CHF during annular

flow and at the same time non-conservative when applied to predict flooding limited CHF.

Line by line verification of Fauske's model is presented in Appendix I.

For the purpose of verification Fauske compared equation 2 with Wallis flooding correlation [10] that is expressed as

$$J_g^{*1/2} + J_l^{*1/2} = C \quad \text{--- 4}$$

where

$$J_g^* = J_g \cdot \left[\frac{\rho_g}{D \cdot (\Delta\rho) \cdot g} \right]^{1/2} \quad \text{--- 5}$$

$$J_l^* = J_l \cdot \left[\frac{\rho_g}{D \cdot (\Delta\rho) \cdot g} \right]^{1/2} \quad \text{--- 6}$$

Assuming C to be equal to 1 and the characteristic length D to be equal to $2W$, Fauske arrives at equation 2 from equation 4 for the case of no water flow at the exit of the channel ($J_l = 0$). This verification is non justifiable considering that the value of C has been recommended to vary between 0.6 and 1.0 [10, 11] instead of 1 as assumed in Fauske's analysis. Most of the experimental investigations report the value of C for rectangular geometries to be close to 0.6. For example, Mishima and Nishihara [6] reported C to be equal to 0.63 for rectangular channel heated on both wide surfaces. Using this value for C in equation 4 we obtain that

$$J_g^* = (0.63)^2$$

or

$$J_g = 0.397 \sqrt{\frac{g \cdot (\Delta\rho) \cdot 2W}{\rho_g}} \quad \text{--- 7}$$

This value is very close to the flooding condition reported by Mishima and Nishihara [6] for rectangular geometries. This flooding equation is presented in the following section.

The final step in Fauske's analysis involved comparison with the experimental data. For this purpose he has selected the experimental CHF data of Monde et al. [12] obtained for rectangular channels (width = 10 mm, length = 20, 35, and 50 mm, and spacing = 0.45, 0.8, 1.05, 2.05, 3.0, 5.0, and 7.0 mm). One of the wide surfaces made of copper (large thermal conductivity) was heated uniformly while the other surface made of glass acted as an insulator. In the experiments the l/s values varied between 3 and 95 which is much

smaller than the l/s value of a typical HFBR rectangular channel (l/s for HFBR = 150). In the process of comparison, Fauske argued that equation 3 represents an asymptote at very large l/s values where as pool boiling correlation of Kutateladze [13] is accurate at very low values of l/s ($l/s \sim 0$). Therefore, he states that interpolation between equation 3 and the pool boiling correlation should be used to predict CHF for intermediate l/s values. Thus, he proposed a new equation given as:

$$q_{CHF}'' = \frac{q_{PB}''}{\left[1 + \left(\frac{q_{PB}''}{q_{Fauske}''} \right)^2 \right]^{1/2}} \quad \text{--- 8a}$$

where q_{PB} is the pool boiling CHF value given by Kutateladze's equation [13]:

$$q_{PB}'' = 0.16 \cdot h_{fg} \cdot P_g \cdot \left[\frac{\sigma \cdot \Delta P \cdot g}{\rho_g^2} \right]^{1/4} \quad \text{--- 8b}$$

Fauske claims that equation 8a is in good agreement with the experimental data of Monde et al. and therefore accurate enough to be used for CHF prediction in HFBR. The reviewer disagrees with this claim for the following reasons. First of all independent verification by the reviewer demonstrated that equation 8a is in most cases on an average 20% higher than the experimental data of Monde et al. In fact the reviewer found out that use of a different scheme of interpolation suggested by Tichler [14] seemed to provide a better agreement with the experimental data. This interpolating equation is given as [14]:

$$q_{CHF}'' = \frac{q_{PB}''}{1 + \frac{q_{PB}''}{q_{Fauske}''}} \quad \text{--- 9}$$

It should be pointed out that there is no experimental basis that supports either of the interpolating schemes. Nor is there experimental evidence that such an interpolation (irrespective of the specifics of the interpolating scheme) is necessary. Simple analytical reasoning such as that provided by Fauske is not adequate. In view of this, it is difficult to accept either equation without further verification. Until then, a more conservative flooding equation should be used to predict CHF in HFBR.

A more important shortcoming noted by the reviewer pertains to the fact that Fauske's analysis does not adequately represent the experimental observations. In formulating equation 2 and equation 3 Fauske's analysis implicitly assumed that CHF occurs

during annular flow¹. To the contrary, the experiments indicate that in long narrow channels cooled by natural convection of nearly saturated liquids, CHF is very close to the flooding limit. Additionally, if one extrapolates the correlation proposed by Monde et al. it approaches the flooding limited CHF value for L/s values larger than 135 rather than being close to either equation 8a or equation 9.

In conclusion, the uncertainties pointed out so far indicate that Fauske's analysis is not only non-conservative but is also inconsistent. The agreement with the BNL-experiments can only be interpreted at this stage as coincidental. Consequently, it is recommended that a conservative flooding equation should be applied to predict CHF in HFBR. One such equation is presented in the following sub-section.

2.3. A Model for Flooding Limited CHF

Any accurate flooding limited CHF model would have to account for thermal properties of the heater (thermal conductivity, specific heat and thermal diffusivity), inlet and pool temperatures, and two-phase hydrodynamics. Although development of such a model is preferable, it is beyond the scope of the present work. A conservative limit however can be obtained by simply assuming that stagnation of the falling liquid film leads to occurrence of CHF. Several models reported in the literature are based on this assumption. Of these the models based on the Wallis flooding equation [10,11] have been widely used. In a recent investigation Mishima and Nishihara confirmed validity of Wallis equation to the rectangular geometry whose dimensions (width = 30 mm, length = 35 cm and gap = 2.4 mm) are very close to those of a typical HFBR channel. As a part of the investigation Mishima and Nishihara derived a flooding equation for rectangular geometries in a manner similar to that of Wallis. Their model was based on the experimental observations that can be summarized as follows. Mishima and Nishihara observed that flooding phenomena in thin rectangular channels with only two walls heated to be unique. In these channels the majority of falling liquid flows down along the narrow walls but not uniformly over the entire perimeter as in the case of tubes. The interfacial drag at the liquid-vapor interface creates disturbance waves that transfer part of the liquid from narrower wall to the wider heated wall. This phenomenon of wetting continues until the falling liquid film on the narrower walls stagnates due to interfacial drag induced by counter-current vapor flow. At

¹It is not to be understood that formulation of Fauske's model is consistent with this assumption. As pointed out earlier, this assumption should have led to force balance on the liquid film covering the heated wall rather than the narrower unheated wall.

this point, the dry patches on the heated surface grow rapidly resulting in burnout of the surface. Consequently Mishima and Nishihara [6] obtained an equation for the flooding limit from the force balance that leads to stagnation of liquid film on the narrow wall. Details of the derivation can be found in their paper. The final equation derived by them to estimate CHF due to flooding can be given as:

$$q''_{\text{Flooding}} = c^2 \cdot \frac{A_c}{A_h} \cdot h_{fg} \cdot \frac{\sqrt{2W \cdot (\Delta P) \cdot g \cdot \rho_g}}{\left[1 + (\rho_g / \rho_l)^{1/4}\right]^2} \quad \text{--- 10}$$

where

$c = 0.63$ for rectangular geometry with both wide sides heated, and

$c = 0.73$ for rectangular geometry with only one wide side heated.

There are several means of verifying this equation. One of the methods is actually considering the Wallis flooding equation (Equation 4). As shown in the previous equation, taking a conservative limit for C in equation 4 we arrive at equation 7 which is only 15-20% higher than the flooding equation of Mishima and Nishihara (Equation 10). It should be noted that Fauske's equation (Equation 3) can only be obtained from equation 4 assuming a value of 1 for C ; an obviously non-conservative assumption considering that C was experimentally shown to vary between 0.6 and 1.0.

There are however some drawbacks associated with equation 10. First of all, equation 10 in its present form does not accurately explain either Monde's experimental data or the HFBR flow reversal data [14]. Secondly, equation 10 implies that for a given length and gap size the flooding limited CHF value increases with the heater width. Such predictions are contrary to what is expected. To overcome these drawbacks the reviewer developed an independent model for flooding limited CHF. This model is very similar to that of Mishima and Nishihara except in the choice of characteristics length. Details of the model are presented as Appendix II. Based on the model the following equation was recommended:

$$q''_{\text{Flooding}} = (2.57)^2 \cdot \frac{A_c}{A_h} \cdot h_{fg} \cdot \sqrt{2 \cdot \lambda \cdot (\Delta P) \cdot \rho_g \cdot g} \quad \text{--- 11}$$

Equation 11 was found (see Appendix II) to be in good agreement with the data of:

- (a) Monde et al. for one-size heated rectangle channels,

- (b) Mishima and Nishihara's data for both one-side heated and two-side heated rectangle geometries,
- (c) Equation 10 for $w = 30$ mm, and
- (d) BNL flow reversal tests.

Equation 11 is therefore recommended for CHF evaluation in HFBR. It should be noted that for HFBR conditions equation 11 predictions are slightly lower than those of equation 10; which is more conservative.

2.3.1. Flow Effects:

As pointed out earlier the flooding limited CHF is the predominant mechanism for the case of zero inlet flow (bottom completely blocked) or for small inlet mass fluxes (0-20 kg/m²-s). At larger mass fluxes, however, the CHF is a linearly increasing function of the mass flux. Mishima's experiments suggest a lower bound for these flows as

$$q''_{\text{flow}} = q''_{\text{flooding}} + \frac{A_c}{A_H} \cdot (\Delta H)_{\text{sub}}^{\text{inlet}} \cdot G \quad \text{--- 12}$$

The equation reveals that for nearly saturated liquid, the natural convective CHF is very close to the flooding equation. This equation can be easily be shown to be very close to the churn-annular flow transition criterion originally derived by Ishii [15, 16]. This criterion is given as [6]:

$$J_g = (\alpha - 0.11) \left[\frac{\Delta \rho \cdot g \cdot w}{\rho_g} \right]^{1/2} \quad \text{--- 13}$$

where the area averaged void fraction α can be obtained from the drift flux formulation as

$$\alpha = \frac{J_g}{C_0 J + \sqrt{2} \left[\frac{\sigma \cdot g \cdot \Delta \rho}{\rho_g^2} \right]^{1/4}} \quad \text{--- 13a}$$

As shown in Figure 2.1 equation 13 conservatively bounds the experimental data obtained in Mishima and Nishihara's experiments [6].

Theoretically, if the total inlet superficial mass velocity, J , is known then the void fraction corresponding to churn-annular flow transition can be obtained. However, estimation of J is complicated. It was therefore decided to obtain a range of experimentally determined

values for α corresponding to churn-annular flow transition. Through out the literature it has been shown that churn-annular flow transition occurs for α in the range of 0.8-0.9 [17], except for the investigation of Jones and Zuber [18] who noted it to be between 0.75 and 0.9. To be conservative, a value of 0.75 was substituted for α in equation 13. This substitution leads to the following equation for churn-annular flow transition:

$$J_g = 0.42 \cdot \sqrt{\frac{(\Delta P) \cdot g \cdot 2W}{\rho_g}} \quad \text{--- 14}$$

Corresponding CHF value can be obtained to be equal to:

$$q''_{\text{Flow}} = 0.42 \cdot \sqrt{\frac{(\Delta P) \cdot g \cdot 2W}{\rho_g}} \quad \text{--- 15}$$

Predictions of equation 15 are within 10% of the flooding condition and approximately 2.0 times lower than Fauske's analysis.

2.3.2. Recommendations:

It is recommended that equation 11 be used for CHF predictions if the established flow through the channel is less than 20 kg/m²-s and/or if the flow rate estimates are not available. These estimates provide the most conservative bound and hence are subjected to very few uncertainties. If the flow rate estimates exceed 25 kg/m²-s, then equation 15 can be used with accurate void fraction determined from equation 13b. The estimates obtained using equation 15 are however subjected to uncertainties introduced during the process of coolant flow rate estimation. CHF values presented in the following section were based on the flooding limited CHF equation (equation 11) and hence should be treated as conservative.

Figure 2.1.1. Comparison of Mishima and Nishihara's flow boiling CHF data with equation 13. As evident, equation 13 conservatively bounds CHF data over a wide range of mass fluxes.

3. APPLICATION TO HFBR

In order to determine HFBR core coolability during natural convection one needs to establish that

1. Critical Heat Flux estimates are not effected by the flow reversal mechanism,
2. Critical Heat Flux phenomena is not accelerated by the presence of helium that is released during depressurization, and
3. The estimated channel power corresponding to CHF situation is larger than the decay heat at that time.

The BNL-staffs calculations [1, 14, 19-26] regarding all three concerns have been reviewed. Also, we have performed additional calculations where necessary. Our conclusions and the results of our calculations are presented in the following sub-sections.

3.1. Flow Reversal Considerations:

We concur with Fauske's conclusion that flow reversal and flow stagnation have little effect on the CHF estimates. However, we disagree with his analytical model. His model is based on lumped mass, momentum and energy balances; similar to a homogeneous model with no drift-velocity considered. The major drawbacks of the model include its inability to accurately incorporate two-phase dynamics. For example, in Fauske's model the stagnation condition is calculated assuming the slip velocity to be negligible. At low pressure and low flow rates, slip ratios have been shown to be as high as 5 which implies that liquid stagnation occurs possibly through the interfacial momentum exchange as modeled in the flooding condition rather than due to thermal buoyancy of homogeneous fluid as assumed by Fauske. Until the stagnation by flooding condition occurs it is possible that counter-current two-phase flow could exist in the core. In this context, it should be noted that the flooding model assumes occurrence of CHF due to stagnation of the liquid film and thus inherently adopts the most conservative flow conditions. Therefore, the flow reversal or the flow stagnation phenomena have little impact on the CHF estimates obtained in this analysis using the flooding model.

3.2. Helium Release Considerations:

Independent calculations by the reviewers indicate that the helium release contributes little to the occurrence of Critical Heat Flux. Thus we agree with the conclusion of Fauske's analysis.

3.3. Evaluation of Critical Power Ratios:

Estimation of critical heat power ratios in the HFBR is some what complex, in that it involves continuous evaluation of thermal and hydraulic conditions. To simplify the discussions, it is proposed to present the results of our calculations in two steps. First step involved calculation of a critical time period over which the channel heat power is larger than that corresponding to CHF. During this period, therefore, forced circulation through the core is essential for adequate heat removal. The second step is determination of time period over which forced cooling is available as a function of the break size.

3.3.1. Estimation of Critical Time Period:

As pointed out earlier, the critical time period represents a period of time over which forced cooling of HFBR core is essential to adequately remove decay heat. Thus accurate and conservative estimates of the critical time period are very important in the PRA as well as in the safety analysis. The following two-step methodology was adopted to evaluate the critical time period:

1. Determine the channel power that would result in onset of CHF, and
2. Estimate the maximum channel decay power as a function of time.

The time period during which channel power corresponding to step 2 is larger than step 1 is the critical time period in which forced circulation is necessary. This methodology is very conservative in that it does not account for thermal inertia which would contribute to substantially reduce the fuel element burnout probability.

Critical Heat Power estimates:

The channel power corresponding to the CHF situation was conservatively estimated based on the flooding condition (see Appendix II) from the following equation:

$$q''_{\text{flooding}} = c^2 \cdot \frac{A_c}{A_H} \cdot h_{fg} \cdot \sqrt{2 \cdot \lambda \cdot (\Delta P) \cdot \rho_g \cdot g} \quad -3.1$$

where,

$$c^2 = \left[\frac{\alpha^2 D_{ch}}{K \cdot \lambda} \right] \approx 2.57$$

λ = Taylor wave length (2.5 mm)

A_c = Channel cross-sectional area

A_H = Channel heated area

Decay Power estimates:

The channel decay power is calculated from the decay heat data given by BNL [25, 26]. Reportedly, this data is based on best estimate computer codes that were consistent with NRC Regulatory guide 1.157. The reviewer however could not verify these claims as such a procedure is complex and falls beyond the scope of the present analysis. This decay heat data was correlated to obtain an equation that provides an accurate estimate of channel decay heat power. This equation, given below, predicts decay heat power in the hottest channel of the core.

$$P_{\text{channel}} = 410 \cdot P_0 (\text{MW}) \cdot t^{-0.363} \quad \text{--- 3.2}$$

P_{channel} = channel Power in Watts,

P_0 = Reactor power prior to scram (MW),

Appendix - II presents detailed discussions on the decay heat calculations. Figure 3.1 presents the decay heat curve for the hottest channel after operation at 30 MW.

The channel power given by equation 3.1 is compared with that given by equation 3.2 in Figure 3.2 for three different operating powers (30, 40 and 60 MW). As expected, the critical time period increases with reactor power prior to shutdown. Additionally, the following equation can also be used to estimate the critical time period:

$$t_{\text{critical}} = [0.073 \cdot P_0 (\text{MW})]^{1/0.363} \quad \text{--- 3.3}$$

Table 3.2 lists these critical periods for various power levels. As shown in this table, at 60 MW operation forced cooling is essential for at least 1 minute to prevent CHF occurrence. This critical period estimate is approximately 30 seconds (100%) longer than that predicted based on the Fauske's analysis.

Table 3.2. List of critical time period for various power levels.

Reactor Power	Critical period
5 Mw	1 sec
10 Mw	1 sec
20 Mw	3 sec
30 Mw	9 sec
40 Mw	20 sec
60 Mw	59sec

After Operation at 30 Mw

Figure 3.1. The Channel Decay heat curve. Based on the BNL model results.

Figure 3.2. Comparison of Channel Power Following 20, 40, and 60 MW Operation with the Critical Power

3.4 Forced Cooling during accident situation:

As discussed in the previous section, for each power level there is a critical time period over which adequate forced circulation is essential for core cooling. During this critical period channel power is larger than that corresponding to the flooding limited CHF. The purpose of this section is to select the worst case scenario and determine if adequate forced cooling is available during the critical period for various postulated accidents. Although, the BNL - staff [22, 24] also reported similar calculations, their calculations are based on a major assumption regarding the break size. BNL - staff categorized the HFBR primary piping as medium energy lines and assumed that the break size can be calculated according to the NRC - guide line (Branch Technical Position ASB 3-1; Definitions). Such a methodology resulted in a break size estimate of 1.39 sq. inch. We feel these assumptions are non-conservative because:

- a) NRC guideline under discussion was specified for the purpose of specification of pipe whip restraint and calculation of environmental effects of the break or crack rather than to estimate limiting break/crack sizes for LOCA analysis, thus
- b) The maximum break for LOCA calculations should not be limited by this criteria. Instead it should be based on the total breakage of inlet or outlet pipes.

3.4.1. Conservative Accident Scenario

Based on the plant description three accidents have been identified by us to have severe consequences, in that these accidents could result in rapid coolant leakage and primary vessel depressurization. These are:

- 1) Total breakage of outlet pipe (20" I. D.)
- 2) Total breakage of inlet pipe (20" I. D.)
- 3) Total breakage of central beam tube.

The consequences of any other primary pipe breaks are have less impact on safety when compared to the consequences associated with these accidents. It is preferable that consequences of each of the three accidents be analyzed individually. However, such a task is time consuming and can be carried out at a later time, if need be. At present, it is advantageous to identify the most severe accident among these three and study the consequences.

Independent calculations by the reviewer indicated that total breakage of the outlet pipe close to the bottom of biological shield formulates the worst case scenario. Such a break would instantaneously disable all pumping power, the coast down included. The longest period over which coast down can be considered is in the order of a second. Thereafter forced flow through the core is maintained for several seconds due to reactor vessel draining. During this period the static head pressure provided by the liquid above the elevation of the outlet pipe drives the flow through parallel network of the core and flow reversal valves, to the break where it leaks to the atmosphere. The purpose of present calculations is to estimate this period as a function of break size. The calculations included the following sub-tasks:

1. Estimate total driving pressure as a function of time. The driving pressure is a sum of cover gas pressure and the static head.
2. Estimate total flow rate through the break as function of time.
3. Estimate the fraction of total leakage that flows through the core and the fraction that flows through the flow reversal valves using parallel network theory, and
4. Determine if the core flow is adequate to avoid onset of boiling during this period. We assume that onset of boiling coincides with the occurrence of CHF, an obviously conservative assumption.

Tasks 1 through 3 were completed through the development of a single hydraulic model for the core. Task 4 was accomplished by adopting a model similar to that developed by Fauske. These models are presented as Appendices IV and V. The results from these models are presented in this section as Figures 3.3 and 3.4.

As evident from Figure 3.3, following a total break of the effluent pipe (20 in. I.D.) the static head driven flow is available for a period of seven (7) seconds at which time the outlet pipe is totally uncovered. During this period the flow through the core is sufficient to avoid subcooled boiling and thus prevent the occurrence of flow stagnation. Beyond this time period natural convection is the only available mechanism of heat removal. Comparison of this time period estimates with the critical time period listed in Table 3.2 indicate that for operating powers are less than or equal to 20 MW then adequate core cooling is possible. However, such a conclusion is only valid if the assumptions made in Appendix IV are appropriate. Furthermore, as also evident from Figure 3.3 if the break sizes are less than 19.63 sq.in., then adequate core cooling is possible over the critical period for powers as high as 48 MW (Tech. Spec. limiting safety system setting corresponding to 40 MW operation).

Corresponding to these smaller break sizes the leakage rate is small compared to the total break conditions. Hence, it takes longer for the reactor vessel outlet pipe to be uncovered. As the break sizes become even smaller (1-2 in. in diameter), the pumps do not trip instantaneously. That may possibly result in longer availability of forced circulation. The models developed in this study have to be modified to analyze these cases since at the present stage they do not model dynamic phenomena such as coast down and pump characteristics. Such modeling may be considered in the future analyses. Additional recommendations are presented in the following section.

Core flow for two-different break sizes

Figure 3.3. Core flow rate for two different break sizes.

Core Flow Rate corresponding to
Incipient Boling in the Narrowest Channel

Figure 3.4. Core flow rate corresponding to incipient boiling.

4. CONCLUSIONS

The analyses reveal that HFBR maximum power level is limited by the break size considerations. The break size estimate of 1.39 in^2 provided by the BNL-staff based on the medium energy line criterion is non-conservative. To the contrary in the present analysis limiting break size was estimated based on total break of effluent pipe break. The analysis demonstrated that Total break of effluent pipe may lead to partial core damage due to CHF occurrence for power levels higher than 20 Mw. However, if the break size can be limited to areas smaller than 19 in^2 (equivalent diameter of 5 in.) then the time period over which adequate forced convection available is larger than the critical time period corresponding to 40 Mw. For 60 Mw operation the tolerable break sizes are even smaller. Core flow rate estimation at such small breaks is complicated by dynamic phenomena such as pump characteristics, coastdown and location of break. Such an analysis was not performed in this study, but it can be conducted in the future if need be.

In setting these limits, it should be noted that all calculations performed so far enveloped the steady state CHF phenomena and thus form a very conservative bound. The actual phenomena in HFBR, being transient in nature, are expected to be associated with larger CHF values. Transient CHF modeling is necessary if such predictions are deemed necessary. Additionally, HFBR fuel plates possess a large heat capacity that can store decay heat for several minutes without overheating. None of the models developed in our review or those developed by BNL-staff incorporated these thermal inertia effects. Preliminary calculations by the reviewer indicate that accurate thermal analysis of the fuel plate subsequent to the CHF occurrence is important to establish safety at higher power levels. Future efforts in this direction are strongly recommended.

NOMENCLATURE

A_c	channel cross-sectional area (cm^2),
A_h	channel heated area (cm^2),
A_l	liquid cross-sectional area (cm^2),
C	dimensionless parameter used in several flooding equations,
C_{fi}	dimensionless parameter used in several flooding equations,
D	diameter (m),
D_{ch}	Characteristic length (m),
g	Acceleration due to gravity (m/s^2),
G	mass flux ($\text{kg/m}^2 \cdot \text{s}$),
h_{fg}	enthalpy of vaporization (J/kg),
J_g	superficial vapor velocity (m/s),
J_g^*	dimensionless superficial velocity (Eqn. 5)
J_l	superficial liquid velocity (m/s),
J_l^*	dimensionless superficial velocity (Eqn. 5),
K	dimensionless friction coefficient,
l	channel length (m),
$P_{channel}$	channel power (w),
P_i	Interfacial perimeter during transition (m),
P_o	steady state reactor power (Mw),
P_w	Channel wetted Perimeter (m),
ΔP	Pressure drop or pressure difference (Pa),
q''_{flow}	critical heat flux during flow boiling (w/m^2)
$q''_{flooding}$	Flooding limited CHF (w/m^2)
q''_{CHF}	critical heat flux (w/m^2)
q''_{pb}	pool boiling critical heat flux (w/m^2)

q''_{fauske}	Critical heat flux based on Fauske's models (W/m^2),
s	channel gap (m),
t_{critical}	critical time (s) (time of necessary forced flow after scram),
U_g	volume averaged vapor velocity (m/s),
W	channel width (m),

Greek Letters:

α	void fraction,
δ	film thickness (m),
$\Delta\rho$	the density differential ($\rho_l - \rho_g$)
ρ	density (kg/m^3)
ρ_g	vapor density (kg/m^3)
ρ_l	liquid density (kg/m^3)
λ	Taylor wave length ($\sqrt{\sigma/\Delta\rho \cdot g}$) (m),
σ	surface tension,
τ_i	Interfacial Shear,
τ_w	wall shear.

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Appendix - I
Subject

Prepared By SKS
Checked By _____

APPENDIX - I

REVIEW OF FAUSKE' MODEL FOR CHF

Purpose: To perform line-by-line review of Fauske's model for CHF during flow reversal and natural convection.

References: A list of references used in the review is presented on page 26.

Details: The Fauske model assumes that CHF occurs during churn-annular transition. I presume from the description he implies such transition on the narrow unheated wall. For counter-current flow that condition can also be referred to as flooding limit. If Fauske means annular flow by definition then he needs to justify existence of large inlet mass fluxes necessary.

He then considered force balance on the "falling liquid film on the narrow wall" to obtain

$$\tau_i = \Delta P \cdot g \cdot S \quad \text{--- (F-1)}$$

The interfacial shear was then calculated based on Bharatham and Wallis equation

$$\tau_i = \frac{1}{2} C_g \left[1 + 300 \cdot \frac{S}{D} \right] \rho_g \cdot v_g^2 \quad \text{--- (F-2)}$$

He then assumed that $D \approx 2W$ to obtain

$$\tau_i = \frac{1}{2} \times C_g \times 300 \times \frac{S}{2W} \rho_g \cdot v_g^2$$

Client

Appendix - I
 Subject

 Prepared By AKA

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He claims that this is in agreement with Mishima and Nishihara's analysis. This claim was found by the reviewer to be inaccurate because Mishima and Nishihara assumed that

$$\tau_i \propto \frac{1}{2} \cdot c_g \cdot 300 \cdot \frac{S}{2W} \cdot \rho_g \cdot u_g^2$$

(or)

$$\tau_i = K \cdot \frac{1}{2} \cdot c_g \cdot 300 \cdot \frac{S}{2W} \cdot \rho_g \cdot u_g^2 \quad \text{--- F-3}$$

They treated K as unknown and evaluated it from the experiments. Assuming a value of 0.75 for K their data indicates that K is approximately '4'. In other words their analysis can also be interpreted to reveal that

$$De_g = W/2$$

Such a substitution in Fauske's model would reduce the CHF estimates considerably.

Upon few algebraic manipulations substitution of F-3 in F-1 leads to

$$J_g = \frac{\alpha}{\sqrt{K}} \cdot \sqrt{\frac{\Delta p \cdot g \cdot 2W}{\rho_g}} \quad \text{--- F-4}$$

He assumed that

$$\frac{\alpha}{\sqrt{K}} \approx 1 \quad \text{to obtain}$$

$$J_g = \sqrt{\frac{\Delta p \cdot g \cdot 2W}{\rho_g}} \quad \text{--- F-5}$$

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Appendix - I
SubjectPrepared By XPS

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It is not clear how $\frac{\alpha}{\sqrt{K}}$ can be assumed to be equal to 1 while the data of Mishima & Nishihara clearly demonstrates it to be considerably less.

$$0.4 \leq \frac{\alpha}{\sqrt{K}} \leq 0.55.$$

This assumption is the basic reason for the difference between Fauske's Model predictions and the flooding limited CHF models.

Fauske claims that his model compares well with Monde's data. As shown in the following appendix Monde's data can be also explained by a flooding model. Additionally Fauske's model is in agreement with the HFBR flow reversal data obtained for "subcooled pool of liquid". We feel that both these agreements are purely coincidental.

Conclusion: We feel that Fauske's model is non-conservative by up to 100-150% for HFBR operating conditions.

APPENDIX-II

Flooding CHF Model for Rectangular Geometries

Basic Principle: The basis for flooding limited CHF is to calculate vapor flow rate required to stagnate falling liquid film on the walls. For rectangular geometries that are not heated all over the perimeter the stagnation of liquid film on the unheated wall leads to formation of dry-patch (Mishima and Nishihara).

Mechanics: The phenomena of CHF during flooding condition is due to formation of dry patch on the heated surface. At this stage the geometry can be shown to be as below:

In cases where the geometry is equipped with non-heated parallel surfaces, then stagnation of liquid film on this surface seems to coincide with the CHF-occurrence (Mishima and Nishihara). If such a condition is assumed, then a force balance on this liquid film gives rise to the flooding limited CHF.

Details: The force balance on the liquid film is:

$$\tau_i \cdot P_i + \tau_w \cdot P_w = (\Delta P) \cdot g \cdot A_l \quad \text{--- 1}$$

τ_i = Interfacial shear

P_i = Interfacial Parameter

T_w = Wall Shear

P_w = Channel Perimeter

A_l = Liquid Cross-Sectional Area

g = Film Thickness

Assumption 1:

$$\tau_w \cdot P_w \ll \tau_i \cdot P_i$$

- This assumption is especially true at very low liquid flow rates. (Wallis, Hewitt & several other investigations)

Assumption 1 modifies equation 1 to give

$$\begin{aligned} \tau_i \cdot P_i &= (\Delta P) \cdot g \cdot A_L \\ \Rightarrow \tau_i &= (\Delta P) \cdot g \cdot S \end{aligned} \quad \text{--- 2}$$

Assumption 2: The interfacial shear can be evaluated using Wallis interfacial momentum equation:

$$\tau_i = (C_f)_i \cdot \frac{1}{2} \rho_g u_g^2$$

In the case of separated flow model formulation for a simple sub-channel geometry such as a rectangle, this assumption can be avoided, but for now, this assumption is necessary. Wallis recommended a form based on experimental measurements for flow in tubes:

$$C_{fi} = C_{f0} \left[1 + 300 \frac{S}{D} \right] \quad \text{--- 3}$$

where D is a characteristic length. For tubes, Wallis showed it to be equal to its diameter. For rectangular geometries Mishima and Nishihara assumed it to be equal to the width 'W'. Such an assumption leads to the result that the CHF value increases with the width; an obviously inconsistent result for flooding limited CHF. In this analysis consequently we refrain from making any similar assumption. Instead we retain D as it is and reduce above equation to the following form:

$$C_{fi} = K \cdot \frac{S}{D_{ch}} \quad \text{--- 4}$$

Where D_{ch} is a characteristic length.

Then we have

$$\tau_i = \left[K \cdot \frac{S}{D_{ch}} \right] \cdot \frac{1}{2} \rho_g u_g^2 \quad \text{--- 5}$$

From equation (2) now we have

$$\frac{1}{2} \rho_g u_g^2 = \frac{D_{ch}}{K} \cdot (\Delta P) \cdot g \quad \text{--- 6}$$

$$\Rightarrow u_g = \left[\frac{2 \cdot D_{ch} \cdot (\Delta P) \cdot g}{K \cdot \rho_g} \right]^{1/2}$$

Knowing that superficial velocity J_g is related to V_g as

$$J_g = \alpha u_g \quad \text{--- 6}$$

we have

$$J_g = \alpha \left[\frac{2 \cdot D_{ch} \cdot (\Delta P) \cdot g}{K \cdot \rho_g} \right]^{1/2} \quad \text{--- 7}$$

In equation 7 there are three unknowns: α , K & D_{ch} . Unlike Fauske we will make no assumption on their relative values. Instead we will attempt to determine these values from experiments. For this we will re-write equation 7 as

$$J_g = \alpha \sqrt{\frac{D_{ch}}{K}} \cdot \left[\frac{2 \cdot (\Delta P) \cdot g}{\rho_g} \right]^{1/2} \quad \text{--- 7a}$$

Based on heat balance it can be shown that

$$q_{CHF} = c^2 \cdot \frac{A}{A_h} \cdot \rho_{gg} \cdot [2 \cdot (\Delta P) \cdot \rho_g \cdot g]^{1/2} \quad \text{--- 8}$$

$$\text{where, } c^2 = \alpha \sqrt{D_{ch}/K} \quad \text{--- 8a}$$

We will now attempt to obtain an estimate for C^2 based on experiments. For this purpose we consider the experimental data of Mishima and Nishihara first. A summary of their experiments is shown below in Table 1.

Length (mm)	Width (mm)	gap (mm)	$\alpha \sqrt{\frac{D_{ch}}{K}}$	q_{CHF} (Mw/sq. m)	Heating Conditions
350	40	2.4	0.129	0.13	one side heated
350	40	2.4	0.139	0.06	two side heated

This table shows that $\alpha [D_{ch}/K]^{1/2}$ is approximately constant; variation is within $\pm 10\%$. We now try to verify this value by comparison with other experiments.

Comparison with Monde's data:

Experiments from various geometries indicate that natural convective CHF lies very close to flooding limited CHF. If such a trend is also true for rectangular geometries then equation 7 with C determined from table 1 should provide good comparison with the experimental data of Monde et al.

For this purpose we will formulate a table similar to Table 1 for Monde's data.

Table 2. Comparison of Equation 7 with the experimental data of Monde et al.

Length (mm)	Width (mm)	gap (mm)	$\propto \sqrt{\frac{D_{ch}}{K}}$	q_{CHF} (Mw/sq.m)	Heating Conditions
50	10	0.45	0.129	0.20	one side heated
50	10	0.8	0.1285	0.35	one side heated
50	10	1.05	0.127	0.45	one side heated
50	10	2.05	0.128	0.90	one side heated
50	10	3.0	0.105	0.975	one side heated

Although not presented here comparison with ethanol also was found to be good. The fact that the value of C in Table 1 and Table 2 are so close leads us to the conclusion that Monde's data is actually due to flooding, not annular flow as claimed by Fauske.

Comparison with 1963 BNL Data

To compare with BNL obtained @ 23 psia we use the following properties:

$$\begin{aligned} \rho_l &= 955 \text{ kg/m}^3 \\ \rho_g &= 0.88 \text{ kg/m}^3 \\ h_{fg} &= 2.221 \text{ MJ/kg} \end{aligned}$$

The predictions are listed below.

Test	Pressure <u>psia</u>	Length <u>in</u>	Width <u>in</u>	Gap <u>in</u>	CHF <u>(MW/m²)</u>	CHF Predict <u>(MW/m²)</u>
29	23	21	2.0	0.098	0.161	0.1
30	23	21	2.0	0.098	0.151	0.1

The fact that the predicted values are below the experiments can be attributed to the inlet subcooling. If originally a more appropriate heat balance equation is formulated then we obtain:

$$q_{CHF} = q_{\text{Flooding}} + (\rho_g \cdot u_g) \cdot \frac{A}{A_h} \cdot (\Delta H)_i$$

Use of such a formula gave our model predictions to be 0.121 MW/M² which are only 25% lower than the experimental data.

Final Refinements:

Although equation 8 is accurate one may prefer to express it in terms of dimensionless numbers. This leads us to the following formulation

$$q_{\text{Flooding}} = c^2 \cdot \frac{A}{A_h} \cdot h_{fg} \cdot [2\lambda \cdot \Delta p \cdot g \cdot \rho_g]^{1/2}$$

Where

$$c^2 = \frac{\alpha}{\sqrt{K}} \cdot \sqrt{\frac{D_{ch}}{\lambda}} = 2.57$$

$$\lambda = \text{Taylor wave length} \left(\frac{\sigma}{g \cdot \Delta \rho} \right)^{1/2}$$

(2.52 mm @ atmospheric pressure)

Discussions: The data seems to indicate that the characteristic length is directly related to the Taylor Wave length. This is confirmed by the fact that equation 8 grossly over estimates CHF for gap sizes $>\lambda$ (see Table 2). It is to be determined whether this result implies anything. However, for the present case where both sides are heated equation 8 appears to be accurate. Advantages of equation 8 pertain to the fact that it is very conservative. As evident from all the data shown above, the equation predictions have always been lower than the experimental data. This leads us to the conclusion that equation 8 forms a conservative lower bound and may not require any uncertainty evaluation. Additionally, equation 8 does not predict the CHF to increase with the heater width which is in agreement with the analytical reasoning. The disadvantage of equation 8 is that it has not been bench marked against a large experimental data base. Such comparison in the future is preferable and recommended.

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Appendix - III

Subject

Decay Heat

Prepared By

Checked By

APPENDIX - IIIESTIMATION OF DECAY HEAT POWER
IN A HOT CHANNELPurpose of the calculations:

There is a need for accurate estimation of decay heat power in the hottest channel of HFBR. The purpose of the present calculations is to estimate this decay power.

References

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Appendix - II

 Prepared By SVK

LANL

Subject

 Checked By SVK

The channel power during decay is calculated using the decay heat data given by BNL. There are two sets of these data attached here as tables 1 & 2. Respectively, both tables are obtained based on NRC - standard computer codes. The data from both these tables are correlated as shown in Figures A.1 and A.2. The correlation in figure A.1 can be divided by the total No. of plates and then multiplied by the radial hot channel factor to determine channel power in the hottest channel after operation at 60 MW. On the other hand the correlation predictions from figure A.2 need to be multiplied by 2 to obtain similar results based on the second set of data. The comparison of these two values showed them to be close to each other up to 200 seconds from the scram. Based on the comparison the following equation was adopted for decay power estimates in the hottest channel

$$P_{\text{channel}} = 410 \cdot P_0(\text{MW}) \cdot t^{-0.363}$$

P_{channel} = channel Power in Watts

P_0 = steady state reactor power in MW

t = seconds from scram.

TABLE 1^{*}
60 MW DECAY HEAT DATA

Time after shutdown, sec	Decay heat, MW
0.5	8.93
1.0	6.28
5.0	5.00
10	4.05
20	3.27
30	2.86
40	2.61
50	2.45
60	2.31
70	2.19
80	2.09
100	1.96
200	1.62
300	1.47
400	1.38
500	1.31
600	1.26
700	1.20
800	1.16
1000	1.09
2000	0.880
3000	0.762
4000	0.686
5000	0.632
6000	0.591
7000	0.557

* This table was based on BNL Best-estimate - calculations.
The numbers have not been verified by us.

60 MW DECAY HEAT DATA FROM BNL

Figure A.1. Decay Heat curve for HFBR. Based on data from BNL.

TABLE 2 ENERGY DEPOSITION FROM ALL SOURCES IN HOT CHANNELS
FOLLOWING SHUTDOWN FROM 30 MW OPERATION, WATTS

Decay Time Sec.	<u>Channel No.</u>			
	1	2	3	4
10	3170	5294	4850	4488
20	2530	4161	3820	3542
30	2213	3605	3313	3076
40	2005	3242	2983	2772
50	1854	2982	2746	2553
60	1723	2758	2541	2365
70	1646	2623	2418	2252
80	1571	2495	2302	2144

After Operation at 30 Mw

Figure A.2. The Channel Decay heat curve. Based on the BNL model results.

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Appendix - IV

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HFBR - CHF Report

Prepared By

AKS

Checked By

AKSAPPENDIX - IVCORE FLOW DUE TO STATIC HEAD DURING LOCA

During leakage period a large inventory of Heavy water from the reactor vessel is drained through the break due to static head. This liquid inventory flows through a parallel network of core and flow reversal valves. The Purpose of these calculations is to estimate core flow rate as a function of time for outlet Pipe LOCA.

Methodology:

- (1) Estimate the covergas pressure as a function of time,
- (2) Estimate core flow rate based on parallel network theory.

References:

1. Crane, Flow of Fluids, Page 410
2. HFBR FSARS
 - Original FSAR
 - 60 MW Addendum
3. Steam Tables
4. HFBR PDM
5. John Darby et al., HFBR Initiating Events and Review of Draft PRA Section 2, Calculations # C 87-258-23-A:1

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Prepared By SVAS

Checked By SVAS

Estimation of cover Gas Pressure :

Step 1. Figure out the total mass of He cover gas prior to the accident as well as liquid inventory in the reactor vessel at the start of the accident.

@ $t=0$

$$P = 195 \text{ Psia}$$

$$T = 120^\circ\text{F} (580^\circ\text{R})$$

$$V = (\text{Free Volume in Cylinder}) + (\text{He Cap \& Piping})$$

$$\approx 27 \text{ ft}^3 + 85 \frac{\text{gal}}{\text{ft}} \times \left(\frac{253-237}{12} \right) \text{ ft} \times 0.134 \frac{\text{ft}^3}{\text{gal}}$$

$$\approx 42.19 \text{ ft}^3$$

$$PV = nRT$$

$$R = 10.73 \frac{\text{Psia} \cdot \text{ft}^3}{\text{lb mole}^\circ\text{R}}$$

$$n = \frac{PV}{RT} = \frac{(195)(42.19)}{(10.73)(580)}$$

$$\approx 1.32 \text{ lb mole}$$

$$\text{He is Monoatomic} = 4 \text{ lb/mole}$$

$$\Rightarrow m_{\text{initial}} = 4 \cdot n \text{ lb} = 5.3 \text{ lb.}$$

- Assumption 1: He make up is very small compared to leakage. It is neglected.

Hereafter, we assume that depressurization is due to combined effect of He venting through 1" diameter orifice (HCE-102) and isentropic expansion due to water leakage.

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For the choked flow we estimate the choked flow condition from the following equation (Flow of fluids by crane):

$$W = 1891 \gamma d^2 c \sqrt{\frac{\Delta P}{v}}$$

$$v = \text{Specific volume} = \frac{44.74}{5.6} \approx 8 \text{ ft}^3/\text{lbm}$$

$$c \approx 0.48$$

$$\gamma \approx 0.8$$

$$\frac{\Delta P}{P} \leq 0.48 \quad \text{Flow is choked}$$

$$W = 1891 (0.8) (0.8) (1)^2 \sqrt{\frac{93.60}{8}}$$

$$= 4141.7 \text{ lbm/hr}$$

$$= 1.15 \text{ lbm/sec}$$

The choking condition continues until the pressure falls below 23 PSia. Thereafter usual flow equation is required.

- Assumption 2: In view of this large venting and additional depressurization due to D2O leakage, we assume that reactor vessel depressurizes instantaneously. John Darby's earlier calculations estimated it to be 2-3 seconds.

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Estimation of Leakage Rate :

The following figure presents a schematic of the accident situation.

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Momentum Balances:

$$P_{ex}(t) = P_1(t) + \rho \cdot g (l + 6.3') - (\Delta P)_{frict} \quad \text{--- (1)}$$

ρ = water density

P_{ex} = Pressure inside the outlet @ the break

l = water level with respect to outlet pipe elevation

$(\Delta P)_{frict}$ = Frictional Pressure drop due to dynamics

P_1 = Head Pressure = 14 psia

P_{atm} = Atmospheric Pressure

$$P_{ex}(t) - P_{atm} = K_1 \frac{\rho V^2}{2g_c} \quad \text{--- (2)}$$

K_1 = Friction coefficient @ break $\approx \frac{1}{2}$

$$(\Delta P)_{frict} = K_{bypass} \cdot \frac{\rho u_{bypass}^2}{2g_c} = K_{core} \cdot \frac{\rho u_{core}^2}{2g_c} \quad \text{--- (3)}$$

Mass Balances

$$\frac{\pi}{4} D_{break}^2 \cdot V_{leakage} = A_{bypass} \cdot u_{bypass} + A_{core} \cdot u_{core} \quad \text{--- (4)}$$

$$u_{core} = \sqrt{\frac{K_{bypass}}{K_{core}}} \cdot u_{bypass} \quad \text{--- (5)}$$

$$l(t) = l(t - \Delta t) - \Delta l \quad \text{--- (6)}$$

$$\Delta l = \frac{\pi}{4} D_{break}^2 \cdot V_{leakage} \cdot \frac{\Delta t}{A_{vessel neck}} \quad \text{--- (7)}$$

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Various quantities in equations 1-7 need to be evaluated separately. Numerical values for some of these quantities are listed below. Rest are evaluated in the following pages.

$$P_1 \approx P_{atm} \approx 15 \text{ psia} \quad (1.034 \times 10^5 \text{ Pa})$$

$$A_{core} = 129.85 \text{ in}^2 \quad (0.084 \text{ m}^2)$$

$$A_{bypass} = 28.274 \text{ in}^2 \quad (0.018 \text{ m}^2)$$

$$A_{vessel \text{ neck}} = 11.39 \text{ ft}^2 \quad (1.058 \text{ m}^2)$$

The quantities K_{core} & $K_{pressure}$ are estimated in the following pages. Upon substitution in equation 1-7 we have the following final equations:

$$V_{leakage}(t) = \left[\frac{19.62(l(t) + 1.92)}{K_1 + 254.33(D_{break}^4)} \right]^{1/2}$$

$$u_{core}(t) = 7.8125 \cdot D_{break}^2 \cdot V_{leakage}(t)$$

$$l(t) = l(t - \Delta t) - 0.095 \cdot (\Delta t) \cdot u_{core}$$

A small computer program was written to evaluate $u_{core}(t)$. The program is attached at the end. A plot of the results is attached as Figure IV.1. Additional discussions are presented in section 3.4 of the main report.

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Prepared By AKAChecked By MPDetermination of K_{core}:

The pressure data provided by BNL (60 MW FSAR, Table S.4) shows that at a flow rate of 15,480 GPM (976.5 kg/sec) the channel velocity is approximately equal to 38.3 ft/sec (11.6815 m/sec) and the core pressure drop of 40.6 PSid. (280.14 kPa).

$$\Delta P = K_{core} \cdot \frac{\rho V^2}{2g_c}$$

$$\Delta P = 40.6 + \text{static head} = 41.3 \text{ PSid.}$$

$$K_{core} = \frac{284.97 \times 10^3 \times 2}{1000 \times (11.6815)^2}$$
$$= 4.17$$

In actuality K_{core} is a weak function of channel velocity for turbulent flow. For simplicity however it can be assumed to be independent of channel velocity. This is checked using similar data on HFBR @ low flow rates. The difference was 2% while the flow rate varied by 20%.

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Prepared By SWAChecked By SWADetermination of K_{bypass} :

The flow data provided by FSAR (60 MW addendum, section 9.12) states that during pump motor operation, after both primary and shutdown pumps trip, 16% of the total 1700 GPM is driven through bypass flow reversal valves. During such an operation

$$\text{core flow} = 1430 \text{ GPM} = 90.2063 \text{ kg/sec}$$

$$\begin{aligned} \text{channel velocity} &= \frac{5506.3}{129.85} \text{ in/sec} \\ &= 1.0771 \text{ m/sec} \end{aligned}$$

Also from the data

$$\begin{aligned} A_{bypass} \cdot u_{bypass} &= \frac{0.16}{0.84} A_{core} \cdot u_{core} \\ &= 0.2 A_{core} \cdot u_{core} \end{aligned}$$

$$\begin{aligned} A_{bypass} &= 4 \times \frac{\pi}{4} (3)^2 \text{ in}^2 \\ &= 28.274 \text{ in}^2 \end{aligned}$$

$$A_{core} = 129.85 \text{ in}^2$$

$$\begin{aligned} u_{bypass} &= 0.2 \times \frac{129.85}{28.27} \times 1.0771 \\ &= 0.9893 \text{ m/sec} \end{aligned}$$

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 Checked By JB

From the equivalent diagram

$$\frac{K_{core}}{K_{bypass}} = \left(\frac{u_{bypass}}{u_{core}} \right)^2$$

$$\Rightarrow K_{bypass} = 4.87$$

Verification of K_{bypass} estimates:

The K_{bypass} is verified by comparing pressure drop corresponding to above situation with pressure-flow characteristics of flow reversal valves (Figure 7.3 of Original FSAR). Although, the valves are slightly modified the pressure characteristics are not changed.

ΔP during pony motor operation is

$$\Delta P = K_{bypass} \cdot \frac{\rho (u_{bypass})^2}{2}$$

$$= 2383.17 \text{ Pa}$$

$$= 0.34 \text{ psid}$$

From Figure 7.3 this estimate is approximately 0.3 psid at a velocity of 0.98 m/sec. The variation of 10% can be attributed to arrangement differences.

Core flow for two-different break sizes

Figure IV.1. Core flow rate for two different break sizes.
The breaks were assumed to be clean cuts

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Appendix V

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HFBR Report

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DTA

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APPENDIX - V

Flow Rate corresponding to Incipient Boiling as a function of time during Decay

Background:

Present analysis assumes the flow stagnation and CHF occurrence coincides with incipience of boiling in HFBR.

Purpose of calculations:

conservatively, the condition for incipient boiling can be estimated from the following equation

$$q''_{\text{decay}} = h (T_{\text{sat}} - T_b) \quad \text{--- 1}$$

$$T_b = T_{\text{in}} + \frac{(W \int_0^{x_{\text{NB}}} q''(x) dx)}{\dot{m} c_p} \quad \text{--- 2}$$

W = The fuel plate width (m)

x_{NB} = Axial location of incipient boiling (m)

$\dot{m}$ = channel mass flow rate (kg/sec)

c_p = Specific Heat (J/kg)

$q''(x)$ = Decay heat flux distribution

We assume that

q'' in equation 1 as q''_{max} &

$$W \int_0^{x_{\text{NB}}} q''(x) dx = Q_{\text{plate}}$$

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HFB R Report

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$$T_b = T_{in} + \frac{Q_{plate}}{P \cdot u \cdot A_c \cdot c_p}$$

$$\Rightarrow q''_{max} = h (T_{sat} - T_{in} - \frac{Q_{plate}}{P \cdot u \cdot A_c \cdot c_p})$$

From the heat and momentum analogy we have

$$h = \frac{1}{2} f P_L u_L c_p$$

$$f = 0.01$$

$$\Rightarrow u_L = \frac{\left[\frac{2 q''_{max}}{f \cdot P_L \cdot c_p} + \frac{Q_{plate}}{P_L \cdot c_p \cdot A_c} \right]}{(\Delta T)_{sub}^{in}} \quad \text{--- 3}$$

where

$$(\Delta T)_{sub}^{in} = T_{sat} - T_{in}$$

To simplify the equation and at the same time to be conservative we assume that

$$Q_{plate} = q''_{max} \cdot A_h$$

This leads to

$$u_L = \frac{q''_{max} \cdot A_h}{(\Delta T)_{sub}^{in} \cdot P_L \cdot c_p \cdot A_c} \left[1 + \frac{2 A_c}{f A_h} \right] \quad \text{--- 4}$$

If the decay power is known as function of time then

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the coolant velocity corresponding incipient boiling is calculated from Equation 4. Figure V.1 presents one such figure. The decay heat rates used in obtaining these curves are from Appendix III.

Core Flow Rate corresponding to Incipient Boling in the Narrowest Channel

Figure V.1. Core flow rate corresponding to incipient boiling.

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