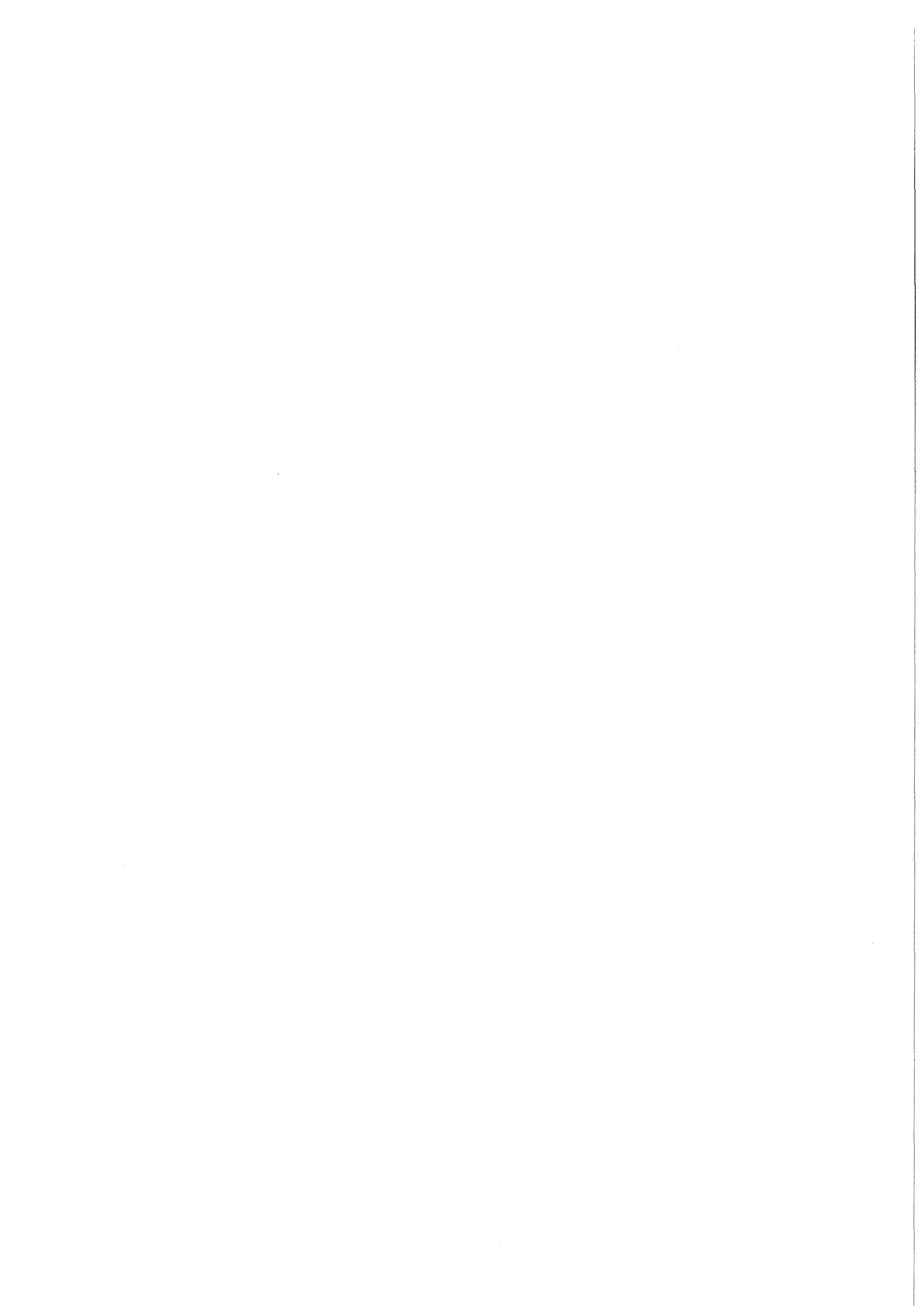


KfK 4854
Juni 1991

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Kernforschungszentrum Karlsruhe GmbH
Postfach 3640, 7500 Karlsruhe 1

ISSN 0303-4003

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Abstract

The vapor explosion process can involve the mixing of fuel with coolant prior to the explosion. A number of analysts have suggested limits to the amount of fuel/coolant mixing that could occur within the reactor vessel following a core melt accident. Past models are reviewed and a simplified approach is suggested to estimate a limit on the amount of fuel/coolant mixing possible. The approach uses concepts first advanced by Fauske in a different way. The results indicate that fuel temperature, ambient pressure and in particular the mixing length scale D_{mix} are important parameters. For large values of D_{mix} the fuel mass mixed in-vessel is limited to the range of 1 - 12 metric tons (1 - 10 % of the core mass).

Abschätzung von Obergrenzen für Vermischung von Brennstoff und Kühlmittel

Kurzfassung

Bei Dampfexplosionen kann es vor Beginn der eigentlichen Explosion zur Vermischung von Brennstoff und Kühlmittel kommen. Mehrere Autoren haben Obergrenzen vorgeschlagen für eine solche Vermischung von Brennstoff und Wasser innerhalb des Reaktordruckgefäßes nach einem Kernschmelzunfall. Über diese früheren Modelle wird ein Überblick gegeben und es wird ein vereinfachtes Vorgehen vorgeschlagen. Dabei werden ursprünglich von Fauske vorgetragene Konzepte in anderer Weise eingesetzt. Die Ergebnisse zeigen, daß die Brennstofftemperatur, der Umgebungsdruck und insbesondere die Mischungs-Längenskala D_{mix} wichtige Parameter sind. Für große Werte von D_{mix} liegt die Brennstoffmasse, die innerhalb des Druckgefäßes vermischt werden kann, im Bereich von 1 - 12 t (1 - 10 % der Kernmasse).

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INTRODUCTION

Prolonged failure of normal and emergency coolant flow in a light water reactor (LWR) would cause melting of the reactor fuel, fuel slumping and eventual contact with residual water in- or ex-vessel. This fuel/coolant contact would allow the fuel to mix with the coolant producing steam and hydrogen as the fuel slowly fragments while in film boiling. This "premixing" phase of the fuel/coolant interaction (FCI) also sets up the initial conditions for a possible vapor explosion that may follow. It is our intent in this discussion to briefly review past analysis on this premixing phase of an FCI, and to estimate possible limits to this fuel/coolant mixing. Identification of mixing limits is of interest because it would suggest a restriction on the size of large-scale vapor explosions; the implication of this result would be to limit the steam explosion damage potential in a reactor-containment system.

PAST WORK IN FUEL-COOLANT MIXING

Past research (1-15) into fuel/coolant mixing (sometimes called premixing) has been mainly directed at predicting the physical limits for which mixing could or could not occur. Fauske (1) and Henry and Fauske (2) originally proposed that the fuel/coolant interface temperature upon liquid-liquid contact must exceed the spontaneous nucleation temperature to allow premixing of the fuel and coolant for an energetic FCI (vapor explosion); i.e., the homogeneous nucleation temperature for a perfectly wetted system (2). The physical picture was that stable film boiling is established above this limit for a liquid-liquid system, and this allows the fuel time to penetrate and mix within the coolant. For the LWR system, the fuel and coolant easily satisfy this criterion. Note that this criterion may be a necessary but not a sufficient criterion for premixing.

Cho et al., (3) considered the energy requirements for fuel fragmentation for both the premixing phase and the rapid fuel fragmentation phase during a vapor explosion. They pointed out that the mixing energy requirements are primarily due to frictional dissipation, and other contributions are quite small (3). From this analysis, Cho et al., made two observations. First, the mixing energy, E_m , required for fuel fragmentation must be considered in relation to the ultimate source of energy in this system, i.e., the internal energy of the fuel, E_{f0} . For the fuel/coolant system, one requires $E_m < E_{f0}$. Second, for a given mixing energy, the maximum volume of fuel that could mix with coolant can be estimated as a function of the mixing time and diameter. For the LWR system, the energy for premixing, E_m is very small compared to the internal energy of the fuel, E_{f0} , for a wide range of these parameters.

Henry and Fauske (4,5) subsequently proposed the concept that for the fuel to exist in a premixed configuration within the coolant, a conceptual picture (as in Figure 1) must be achieved and sustained. Note that this picture has one liquid (the 'fuel') as discrete particles intermixed in a continuous phase of the other liquid (coolant) with vapor present. If this configuration breaks down by driving the water coolant away from the molten fuel, discrete fuel particles may coalesce into larger particles as the coolant liquid is depleted from the mixing region. For in-vessel reactor

considerations [pressurized water reactor (PWR) specifically], Henry and Fauske pointed out that no more than 100 kg of fuel could mix with the water coolant for saturated water at a pressure of one bar with a mixing diameter of 10 mm. The key assumption of the model was that the flow was one-dimensional and steady-state with a pool boiling CHF model used to estimate the onset of fluidization.

Theofanous et al., (7) also addressed the question of a limit of fuel/coolant mixing and took a different approach. They concluded that the mixing process would be driven by the hydrodynamics of transient jet breakup as the fuel pours into a water coolant pool (Figure 2). Their order-of-magnitude calculations indicated that only a few percent of the available fuel mass could mix with the water coolant for in-vessel core melt situations; i.e., 2500 to 4000 kg of fuel with mixing diameters of 10 to 100 mm. The major reason that more mixing could not occur was because the available time for hydrodynamic mixing was limited because the water depth in the lower plenum of the reactor vessel (PWR for these example cases) was relatively limited.

Corradini et al., (8,9) attempted to analyze the Sandia National Laboratories FCI experiments (10-12) designated the fully instrumented test series (FITS). The conceptual picture gathered from high-speed cine-films of the mixing zone was one where the fuel enters the water pool as a single discrete mass (an elongated ellipsoidal shape) in film boiling and begins to breakup into fragments (Figure 3). The steam produced in film boiling flows out through the top of the fuel/coolant mixture and escapes the water pool, and water flows in from the bottom and sides. This concept of dynamic mixing and possible coolant fluidization was applied as a mixing limit (9). A dynamic fuel breakup model was derived as a function of the fuel temperature, the water depth, the fuel entry size, D_{f0} , as well as the empirically correlated data of integral fuel and coolant volume fractions from the FITS tests (10-12). Combining these factors, the fuel diameter was found as a function of the water depth, H_c and D_{f0} (Fig. 4). The coolant fluidization limit for different fuel temperatures was also determined as a function of H_c and fuel temperature. All the diameters to the left of the fluidization mixing limit line for a given fuel temperature could mix without fluidization, while those diameters to the right of the limit for a given H_c and D_{f0} would cause fluidization. Applying this to LWR issues, it was estimated that a maximum of 3000-5000 kg of fuel could mix with saturated water to a diameter of 50-100 mm within the vessel at atmospheric conditions.

Two-dimensional transient effects were first considered as an extension of these concepts by Bankoff et al., (13). Bankoff and his coworkers used the PHOENICS, two-fluid, two-dimensional computer code, to model the transient mixing of an array of fuel droplets entering a coolant pool. Because of the computer model limitations, the coolant (steam and water) was considered as a single homogeneous fluid (i.e., equal velocities, temperatures and pressures) and the fuel was considered to be the other fluid, prefragmented to a user-specified diameter before entry into the coolant pool. Under saturated coolant conditions Bankoff considered these to be reasonable approximations. We would consider this analysis technique to be parametric with the

fuel "prefragmented" size as the key parameter. Fletcher (14) has also begun development of a similar model, CHYMES, for more general cases of fuel jet mixing. The work involves an Eulerian formulation similar to PHOENICS with the fuel and coolant liquid and vapor considered to be three separate fluids.

Recently, Theofanous et al., (15) also developed a two-dimensional model for mixing employing the K-FIX computer model with substantial modifications of the constitutive relations. The basic simplifying assumptions of the computer analysis were exactly the same as those of Bankoff:

- (1) The coolant liquid and vapor are a homogeneous fluid.
- (2) The incoming fuel is modelled as a collection of droplets at a user-specified prefragmented size.

Thus the overall results are similar to those of Bankoff for the case of in-vessel fuel-coolant mixing. Theofanous et al., used a technique to define the mass of fuel mixed similar to that employed by Chu et al., (16), in which the fuel is considered "mixed" when it is in a regime of continuous coolant liquid (e.g., $\alpha_c > .5$). Based on this definition little of the fuel was found to mix with coolant under atmospheric conditions in the LWR. This concept will be discussed further in the next section. The analyses of Theofanous et al., indicated that mixing is enhanced at higher ambient pressures since the vapor density increases as the void fraction decreases. The estimates from the KFIX analysis indicated that 5000-7000 kg of melt would mix with the coolant liquid for vapor volume fractions of less than 90%; D_{mix} was assumed to be 20 mm and the ambient pressure was calculated to rise from 1.0 to 1.5 bars for a PWR lower plenum geometry (Fig. 5). Theofanous et al., stated that a void fraction of 90% conservatively bounded the region where the mixture had a continuous liquid regime ($\alpha_c > .5$ and $\alpha_v < .5$). These estimates indicated that mixing was also dependent on the fuel pour area ($R_o < 1.1m$).

Since that study Theofanous et al. (17) have developed a three-fluid model (PM-ALPHA) to remove the restriction of the coolant being treated as a homogeneous fluid. The major limitation of the analysis remaining is that the fuel is modelled as a collection of droplets entering at a prefragmented user-specified size. The predicted fuel mass mixed for similar conditions was reduced compared to the past study where now the mixed mass was 4000 kg for $R_o = .88 m$ compared to 5000 kg previously. The reason for the difference seemed to hinge on the allowed slip between the vapor and liquid coolant where a larger liquid coolant depleted zone forms in the central region of the mixture.

In an attempt to remove the restriction of a constant fuel diameter during mixing Chu and Corradini (16) developed a technique to predict the dynamic fragmentation of the fuel due to relative velocities. This was incorporated into a transient, multifluid, one-dimensional model developed to allow one to consider mixing between fuel and coolant with dynamic fuel fragmentation. Chu and Corradini (18) based this model on the TEXAS code (Young (19)), a fuel-coolant interaction model for LMFBR safety. Two Eulerian fields (coolant vapor and liquid)

and one Lagrangian particle field (fuel) are employed in the model. The current model results are limited to nonexplosive fuel-coolant interactions, although this restriction could be relaxed. The key constitutive relation is a fuel fragmentation model based on Rayleigh-Taylor instabilities. This constitutive model considers the fuel to be dynamically fragmented from its initial entry diameter to smaller sizes. This is an improvement over the assumption of the fuel being prefragmented at a fixed diameter. In the model parallel velocity shear forces (e.g., Kelvin-Helmoltz instabilities) are neglected because of their limited effect with a vapor film present (Fauske (20)); although this restriction can be relaxed. The model for fuel breakup as used in TEXAS was simplified from the detailed model to a linear time-independent form where

$$D_f^{n+1} = D_f^n (1 - C_0 \Delta T^+ We^{0.25}) \quad (1)$$

where: $n, n+1$ designate the old and new timestep values

We is the fuel Weber number

ΔT^+ is a dimensionless timestep

$$We = \rho V_{rel}^2 D_f^n / \sigma_f \quad (2)$$

$$\Delta T^+ = \frac{V_{rel}(t^{n+1} - t^n)}{D_f^n} \left(\frac{\rho_c}{\rho_f} \right)^{1/2} \quad (3)$$

$$C_0 = 0.108 - 0.0785 \left(\frac{\rho_c}{\rho_f} \right)^{1/2} \quad (4)$$

When this TEXAS model is applied to the breakup of a fuel jet in a coolant for conditions similar to those considered by Fauske (20), it also predicts that under steady-state conditions fuel jet breakup and mixing is small. However, for the initial fuel jet entry the leading edge can mix with the coolant. If the coolant depth is shallow relative to the jet diameter ($H_c/D_j < 10$), then the jet has time available to only break up a couple of jet diameters. In LWR safety applications, Chu and Corradini (18) considered an in-vessel situation of a corium fuel jet similar to those of Bankoff (13). The results (Table 1) indicate that for the base case, less than 20% of the available submerged mass mixes with coolant where the vapor void is less than 50% (one-third of the pool has a void fraction $> .5$). If one correlates the location of the fuel stripped off the main jet ('discrete' mass) and this void fraction one finds that more than 50% of this total mass stripped is in a region where $\alpha_v > 0.5$ ('dispersed' mass). Thus according to the original concept of Fig. 1 less than half of the fuel mass is "mixed" with a continuum of liquid coolant; most of the fuel is mixed with liquid coolant in a vapor rich region. For the discrete fuel mass apart from the jet the

particle diameters are predicted to be about 10 mm near the pool base and about 1 mm near the top part of the fuel-coolant mixture where the fuel is fluidized. Although this modelling incorporates the insights of past analyses, it is still limited in its mechanistic treatment because it is one-dimensional. Such mechanistic treatments must eventually consider multi-dimensional effects.

Quite recently, Young (21) has also considered the concept of dynamic mixing and, with separate dynamic mixing models by Pilch, developed a two-dimensional model for fuel-coolant mixing, IFCI. This represents an advance in modelling of mixing prior to a vapor explosion because 1) it is multi-dimensional, 2) it allows for four separate fluids to be considered with different temperatures and velocities (fuel, liquid and solid, coolant liquid and vapor) and 3) it employs a model for dynamic fuel fragmentation instead of a user-specified pre-fragmented size. The results published to date are quite preliminary, but the model capabilities seem promising. To investigate the mechanistic details of fuel-coolant mixing an approach similar to that employed by PM-ALPHA (17) or IFCI (21) may be needed. One point that should be considered are the advantages and disadvantages of using an Eulerian approach rather than a Lagrangian approach to track the fuel transport (TEXAS employs a Lagrangian approach while IFCI uses an Eulerian technique).

MIXING AND CRITERIA FOR A WELL-MIXED SYSTEM

In our discussion of the past work on fuel-coolant mixing it becomes obvious that "fuel mixed with coolant" is a statement that does not have a precise meaning. In fact although many investigators have similar conceptual pictures their interpretation of test data and subsequent computational analysis may differ based on their meaning of "fuel mixed with coolant." Since our purpose in this work is to present an analysis which estimates the possible bounds on fuel-coolant mixing it is important that we first define what we mean by "fuel mixed with coolant."

The first difficulty with any such definition is that one lacks precise quantitative values to establish mixing criteria. One way to appreciate this fact is to examine the data available on fuel-coolant mixing. Although many investigators have proposed mixing models and analyses the available data (10-12; 22-24) is quite sparse. Let us limit ourselves to the contact mode of molten fuel pouring into a coolant pool (coolant injection is not considered relevant here and stratified fuel-coolant liquid layers by definition preclude this premixing). In these FCI experiments reviewed the molten fuel free falls into coolant and as it submerges below the coolant pool surface it undergoes some amount of mixing. In most of the Sandia FITS tests (10-12, 22-24) the coolant chamber was transparent allowing the leading edge of the fuel mass (2-20 kg) and its overall shape to be observed. The fuel mass seemed to breakup into smaller pieces as it fell through the pool, with the discrete fuel masses moving radially as they fell. As the discrete fuel masses fell downward coolant intermixed with the fuel, and vapor, produced in film boiling, flow upward to the pool surface. If a vapor explosion did not occur or if the interaction was so weak that the transparent lucite chamber did not break, the fuel was found on the chamber base with a range of particle sizes

(1-10 mm Sauter mean diameter, Ref. 10). If the mass of fuel injected was well known and the pool level height measured, the integral void fraction could be estimated. However, the actual fuel diameter distribution or void distribution within the mixture could not be quantitatively measured, only the mixture region exterior boundaries could be qualitatively viewed (conceptual picture in Figure 3). No other data has generated more insight. It is from these sparse data we try to outline criteria for "fuel mixed with coolant."

Two possible criteria have already been mentioned: 1) stable film boiling should exist between the two liquids and 2) mixing energy requirements are small compared to the fuel thermal energy. In the context of a vapor explosion we would propose two other necessary criteria for "fuel mixed with coolant": 3) the local length scale for the discrete liquid phase, l , should be much smaller than the system length scale, L , ($l \ll L$) and 4) the continuous phase in this local region should be one of the two liquids. This latter condition is based on a continuous liquid volume fraction ($\alpha_c > 50\%$ or $\alpha_f > 50\%$) as Figure 1 illustrates and may be necessary for a vapor explosion to occur.

These criteria are based on the premise that for the vapor explosion to be initiated, a trigger is available and the vapor explosion escalation/propagation process can proceed. The spontaneous triggering of the explosion is empirically (10) known to be retarded if the ambient pressure increases or the void fraction increases (due to saturated conditions or noncondensable gas). The detailed mechanisms for the explosion escalation and propagation are not well understood. However, it seems to be accepted that for the explosion to be triggered the fuel and coolant liquids must be in close proximity (small length scales) and with a minimum of vapor present (i.e., vapor film collapse and liquid-liquid contact, Ref. 1-5).

Consider the criterion that the discrete liquid phase should have a length scale, l , much less than the overall system length scale, L . If our estimate for a fuel mixing limit is to make sense, the fuel 'mixed' must participate in the vapor explosion. This implies two points. First all of the fuel should be in close proximity to the coolant liquid, and should breakup to a size where the mixture of the fuel, coolant and vapor appear homogeneous at the system length scale (Fig. 1). Without this occurring not all of the fuel would be dispersed in the coolant and we would have a heterogeneous mixture situation approaching stratified liquids. Second, this local length scale for the discrete mass should be small enough that the fuel can be completely fragmented and quenched during the explosion escalation and propagation/expansion time. This characteristic time would be that interval in which the explosion shock wave grows in strength and traverses the system length scale as the fuel coolant mixture expands (e.g., a factor of two expansion). If this local fuel length scale is too large some of the fuel will remain unquenched during the explosion expansion and would not contribute to the explosion energy release. One might argue that this criterion does not really matter since one should be conservative in search of this mixing limit. However, we feel that it is too easy to be overly conservative and we try to precisely draw a distinction between a large fuel mass in the water with only 5% of the mass quenched during the explosion and a smaller

mass mixed with water and most of the mass fragmented and quenched. By analogy to fuel-oxidant mixing in diesel engine combustion (25), it is not correct to assume that all the diesel fuel injected into the engine chamber is mixed with the oxidant; rather it must be only that fuel which has atomized to small enough length scales to undergo vaporization and eventual combustion during the burning and expansion phase (large fuel droplets are not mixed and actually hamper the combustion process efficiency).

The final criterion for fuel-coolant mixing is proposed mainly because of its impact on the vapor explosion. Consider first the effect of the vapor void fraction on the explosion from strictly a thermodynamic perspective. As the void fraction increases the ideal work potential can decrease significantly (e.g., a 50% decrease in efficiency is possible for a void fraction increase to 90% at equal fuel and coolant liquid volumes). This is because the mixture has already expanded to allow for the vapor void. In addition, a void fraction increase may suppress the kinetics of the explosion, and we feel this effect will be most significant when the threshold from a continuous liquid to continuous vapor field is crossed; $\alpha_v > 0.5$. One reason is that the explosion should be much more difficult to trigger. This was noted in the parametric calculations by Fletcher et al (26), where initial trigger pressures in excess of about 50-100 MPa were needed for voids of 70-90%. Our past work on explosion triggering (27-29) also indicates that to initiate liquid-liquid contact and to begin rapid fuel fragmentation in the explosion, the vapor volume fraction should be small ($\alpha_v < 50\%$). These observations do not imply that one could not trigger at higher vapor volume fractions (smaller liquid volume fractions), but that the necessary trigger energy (or impulse) would increase substantially (27-29). Another reason is that the physical concept of 'liquid-liquid' contact which drives the vapor explosion loses meaning when the vapor becomes the continuous phase. One now has to propose that the thermal interaction of dispersed droplets of fuel and coolant would escalate and propagate an explosive heat transfer process. It would be our judgement that explosion propagation with liquid-liquid contact and rapid heat transfer would be significantly reduced or suppressed at higher vapor void fractions. Another effect of having a continuous liquid phase during mixing would be to allow for an inertial constraint from the fuel-coolant mixture itself. If the void fraction is too large it is not clear that the explosion will collapse these large vapor voids and propagate without simply venting through this dilute mixture (30, 31). In the analysis by Fletcher et al (26) the explosion propagated through the high vapor fraction mixture (70-90% void) by the assumption of a large trigger (100MPa) and a large parametric liquid-liquid heat transfer coefficient ($10^7 \text{W/m}^2\text{K}$).

In our past work (e.g., Chu et al (18) and Corradini et al (32)) we have been consistent with this definition of a fuel coolant mixture. Theofanous (15, 17) and Bankoff (13) have also considered similar definitions when analyzing their multidimensional calculations. In his recent work (15, 17) Theofanous and coworkers have considered fuel-coolant mixtures with vapor void fractions as high as 75-90% to estimate the possible fuel mass mixed, and have considered this to be a conservative limit. However, one should realize these are bounding values chosen to assure

the real fuel mixture mass is well below such limits. Also, the values for fuel mixed mass, when $\alpha_v < 50\%$, were not significantly smaller.

One might argue that the explosion could begin in a low void region of the mixture and propagate into a highly voided region. This has never been conclusively shown to occur experimentally. For example, in the FITS experiments (10,11) it was usually observed that the explosion was triggered near the base and propagated upward. Probably, the void increased in the central region of the mixture with height in the pool, but it is not clear what this void was and if the explosion propagated through this vapor rich region. Also, based on the parametric results of Fletcher et al (26) it appears that the explosion pressure needed for such a sustained propagation through a highly voided region is much larger than any measured explosion pressure data.

The final point to make is that these proposed criteria are based on our analysis of a sparse set of data. Clearly more data is needed to assess these criteria and the derived model on which it is based.

FUEL-COOLANT MIXING MODEL

Our objective is to provide an estimate on the limits to fuel-coolant mixing given the previous definition. In this analysis we neglect the effect of solid structure within the coolant pool on mixing even though it may be realistically present. This is the case for the in-vessel lower plenum core/diffuser plates and support structure (Figure 6). We discuss the possible effects of the structure after the analysis is presented along with other realistic considerations. Comparing the work of the previous investigators, the mixing energy criterion does not limit the amount of fuel that can mix with the coolant; rather, it is hydrodynamic considerations either due to boiling processes (4, 5) or transient jet breakup (6, 7), or some combination of these two effects (8, 9) that limit mixing. A second observation is that for a given set of conditions (e.g., 1 atm saturated water, PWR geometry neglecting structure), these past studies on mixing limits yield a range of results (Table II).

Table II indicates that the prediction using the model of Fauske is substantially lower in both the in and ex-vessel cases compared to the other analyses. A major reason for this difference seems to be caused by the use of the pool boiling CHF model as a measure of the maximum energy flux that can be removed from the top of the mixture; i.e., a limit to mixing due to coolant fluidization. This model assumes the fuel/coolant mixture is in a quasi-steady-state and occupies the whole cross-sectional area (Fig. 1) down to the chamber base, and that the only way water coolant can enter the mixture region is from above counterflow to the steam which is exiting the mixture. This integral approach to the problem is quite appealing because it bypasses the complexities of multidimensional analysis by including the whole coolant pool surface area as available for mixing and focuses on a local mixing limit where vapor carries away the coolant liquid. We find the overall concept useful to derive a fluidization limit for mixing, although the CHF concept need not be used.

The limit to mixing would correspond to the point when the coolant liquid is fluidized by the vapor upflow. One could recast the model in these terms to be

$$\frac{6m_f}{\rho_f D_{mix}} q''_{drop} = \rho_v i_{fg} A_{ch} V_{SC_{CHF}}, \quad (5)$$

where

$$q''_{drop} = h_{film} (T_f - T_{sat}) + \sigma_r (T_f^4 - T_{sat}^4) \quad (6)$$

and h_{film} is the film boiling heat transfer coefficient from the drop. Note the energy removed per unit volume is $\rho_v i_{fg}$ and $V_{SC_{CHF}}$ is the superficial critical (SC) steam velocity that will fluidize the water coolant. Now, using Kutataladze's or Zube's model (33) for CHF in pool boiling as Fauske used, one finds the $V_{SC_{CHF}}$ is derived to be

$$V_{SC_{CHF}} = 0.14 \left[\frac{\sigma g (\rho_c - \rho_v)}{\rho_v^2} \right]^{1/4} \quad (7)$$

where the constant 0.14 was determined empirically from pool boiling data. Borishanskii (34) indicated that this empirical constant can be explained in terms of the hydrodynamic size of the liquid and vapor as CHF is reached and their relative velocities. The functional form of the characteristic velocity given in Eq. (7) can be explained in terms of coolant fluidization assuming that the coolant is considered to be droplets with a diameter derived from hydrodynamic stability. Let us also use this picture here in our analysis where the fuel mixes within the coolant pool with an ever increasing mass, until fluidization of the coolant liquid is reached. This fluidization would first occur at the top of the pool where all the vapor flow exits and where the vapor void fraction would first become large enough ($\alpha_v > .5$) that coolant droplets are formed and could be swept away by the vapor flow. At this point if more fuel is added to the coolant pool the coolant vapor void fraction would rise further and coolant liquid could be carried out of the mixture. Based on our proposed criteria the actual mass of fuel mixed would decrease because more and more mass resides in a vapor rich region where the explosion can be difficult to trigger and propagate and where its own inertial constraint is significantly reduced. It is the fuel mass mixed at this limit that we seek.

Consider a coolant droplet existing at the top of the pool with a diameter, D_c ; if this droplet were to become fluidized by the surrounding vapor flow, a simple force balance gives us this fluidization velocity V_{fl} :

$$V_{fl} = \left[\frac{4}{3} g \frac{D_c (\rho_c - \rho_v)}{C_D \rho_v} \right]^{1/2} \quad (8)$$

where all the terms have been previously defined. Now, for a collection of coolant droplets, the local vapor velocity would increase because the volume occupied by the coolant liquid decreases the available flow area and the local drag coefficient would also increase due to this same effect. Using the empirical results from Wallis (35) on the drag coefficient in an array of particles, the SC fluidization velocity becomes

$$V_{SC} = \alpha_v \left[\frac{4}{3} g \frac{D_c}{C_D(\alpha_v)} \frac{(\rho_c - \rho_v)}{\rho_v} \right]^{1/2} \quad (9)$$

where

$$C_D(\alpha_v) = \frac{C_{Ds}}{(\alpha_v)} 2.65 \quad (10)$$

and where C_{Ds} is the drag coefficient for a single sphere.

The next question to consider is what is the proper value for the coolant characteristic length scale, D_c . The coolant is the continuous phase just before fluidization occurs at the pool surface. Based on geometry considerations as the liquid coolant volume fraction decreases below about 50% due to an increase in the amount of coolant vapor, the coolant liquid will become discontinuous between the intermixed fuel droplets and the vapor film surrounding them. The characteristic length scale would be to consider the coolant surrounding any individual fuel droplet as a single coolant droplet at the point of fluidization.

$$D_{cI} = D_f \left(\frac{\alpha_c}{\alpha_f} \right)^{1/3} \quad (11)$$

Substituting this value into Eq. (9), one obtains

$$V_{SC_I} = \alpha_v \left[\frac{4}{3} g \frac{D_f}{C_D(\alpha_v)} \left(\frac{\alpha_c}{\alpha_f} \right)^{1/3} \left(\frac{\Delta\rho}{\rho_v} \right) \right]^{1/2} \quad (12)$$

Now, this coolant length scale is an upper bound on the size of a coolant droplet. This droplet is the largest diameter that could be formed based on the geometry, and it neglects the fact that as the coolant liquid begins to fluidize, it would fragment in the vapor flow. If one considers droplet fragmentation then the coolant liquid will break up in the vapor flow down to sizes given by the critical Weber number

$$D_{cII} = \frac{We_{crit}\sigma_c}{\rho_v V \frac{2}{SC}} \quad (13)$$

where σ_c is the coolant surface tension. Substituting this length scale into Eq. (9), yields

$$V_{SCII} = \alpha_v^{1/2} \left[\left(\frac{4}{3} \right) \frac{We_{crit}}{C_{D(\alpha_v)}} \left[\frac{g \sigma \Delta \rho}{\rho_v^2} \right] \right]^{1/4} \quad (14)$$

Comparing this to the CHF velocity (Eq. 7) one notes the similar functional form where 0.14 is replaced by the vapor volume fraction, α_v , the critical Weber number, We_{crit} , and the local drag coefficient (given by Eq. 10).

Note that the actual coolant length scale would be between these two bounds because the actual fuel/coolant mixing process is probably a transient phenomenon. For the case of light water reactor fuel in saturated water at one atmosphere the ratio of D_{cI}/D_{cII} is about 5-10. This transient aspect can be demonstrated by considering the time it takes the coolant with a characteristic length given by D_{cI} to break apart due to coolant vapor flow down to the characteristic length given by D_{cII} . This is a classic case of hydrodynamic breakup where the time for breakup is approximately given by

$$T^+ = 3 \text{ to } 6, \quad (15)$$

where, as before, T^+ is defined as

$$T^+ = \frac{V_{SCI} t_B}{D_{cI}} \left[\frac{\rho_v}{\rho_c} \right]^{1/2} \quad (16)$$

For our example case assuming $T^+ = 4$, with a pour velocity of 5 m/s and D_{cI} of 30 mm, one finds the time for breakup t_B is less than a second. This is about the time it takes the fuel to pour by gravity into the coolant pool and reach the bottom of the chamber, both for in-and ex-vessel situations.

Now the final point to make is that to use this model one must know the vapor void fraction, α_v , at the point of fluidization. Since all of the vapor produced must eventually exit the pool, the location of greatest vapor flow occurs at the pool surface. Fluidization would also first occur at this location. To estimate this void fraction and the associated liquid fractions (α_c , α_f), this fluidization point is calculated based on the drift-flux model and the flooding criterion by Wallis (35). In the drift flux model, an average velocity of vapor, V_v , is given as,

$$V_v = C_0 j + V_{gj} \quad (17)$$

where j is the volumetric flux and V_{gj} is the average drift velocity.

For churn-turbulent flow, the average drift velocity is approximately equal to the terminal rise velocity, U_t , given by

$$V_{gj} = U_t = 1.53 \left[\frac{\sigma_c g (\rho_c - \rho_v)}{\rho_c^2} \right]^{1/4} \quad (18)$$

where σ_c is the coolant surface tension. Since $j_c = 0$ at the flooding point and $V_v = j_v / \alpha_v$, Eqs. (17) and (18) can be combined to yield.

$$\left(\frac{1}{\alpha} - C_0 \right) j_v = 1.53 \left[\frac{\sigma_c g (\rho_c - \rho_v)}{\rho_c^2} \right]^{1/4} \quad (19)$$

The general flooding criterion given by Wallis is

$$j_v^* = j_v \sqrt{\frac{\rho_v}{g D_H (\rho_c - \rho_v)}} = 0.9 \quad (20)$$

Now from Eqs. (19) and (20), we can obtain the fluidization void fraction at the top of the mixture to be equal to

$$\alpha_v = \frac{1}{C_0 + a} \quad (21)$$

where

$$a = 1.7 \left[\frac{\sigma_c \rho_v^2}{\rho_c^2 g D_H^2 (\rho_c - \rho_v)} \right]^{1/4} \quad (22)$$

Within this model is the hydraulic diameter for the coolant flow, D_H . This length scale can also be related to the fuel diameter. We consider the fuel particles to be stationary relative to the coolant vapor flow out of the pool, since this velocity is much larger in magnitude. The coolant hydraulic diameter, D_H is given by the standard relation of the ratio of the flow area to the wetted perimeter as

$$D_H = \frac{4[A_{ch} - m_f / \rho_f H_{mix}]}{P_w + 4m_f / (\rho_f H_{mix} D_f)} \quad (23)$$

where P_w is the chamber perimeter, H_{mix} is the depth of the pool and m_f is the fuel mass mixed in the pool.

RESULTS AND DISCUSSION

To illustrate the use of the proposed model we have estimated the fuel-coolant mixing limit that would occur in the lower plenum of a pressurized water reactor. The German PWR, BIBLIS-B, was chosen as the specific design for the geometric details in the analysis (Figure 6). As the figure indicates there are three solid structures in the lower plenum; the lower core plate, the core mixing plate and lower plenum support structure. The first two structures are immediately below the reactor core and represent relatively thin-walled structures, which have relatively narrow passages into the open plenum region below. The lower plenum support structure is quite massive, although it does not extend throughout the plenum as shown in Figure 6b. Considering this geometry and in the event of a severe accident, we would expect that the core melt would pour from the core region through these upper two plates without being impeded. If an explosion is not triggered by melt contact with the solid structure the greatest effect of these mixing plates would be to subdivide a large pour into smaller melt streams (if an explosion is triggered in this region the fuel melt mixed would be quite limited and we have conservatively neglected this possibility). Once the fuel pours through these structures it will see a relatively open lower plenum pool of water coolant. Because of the shape and position of the lower support structure it does not seem it will have a major impact in mixing, particularly in the overall limit as considered here.

The remaining initial conditions needed for the analysis depend to a large extent on the severe accident sequences considered, especially on the dominant accident sequences. For illustrative purposes of the model the following ranges for initial conditions were chosen;

Pool Depth:	$1.0 < H_{mix} < 2.5$ m
Ambient Pressure:	$0.1 < P_{\infty} < 1$ MPa
Fuel Temperature:	$2000 < T_f < 3000$ K
Water Temperature:	Saturated at P_{∞}

The pool depth values represent a range of depths from the bottom of the vessel to the middle of the massive lower plenum support structure, or to above the core plate, where for most severe accident conditions the coolant is saturated at the vessel ambient pressure. The nominal value chosen for the analyses is 1.5m and this represents a water coolant depth from the bottom of the vessel to just below the core mixing and diffuser plates. The cross-sectional chamber area chosen ($9.5m^2$) represents the complete core region. The ambient pressure is also quite sequence specific so a range of 0.1 to 1.0 MPa was chosen to represent likely values for low pressure sequences. Because of the increased difficulty to trigger explosions at higher ambient pressures (27-29) and the current views that suggest depressurizing the RPV under severe accident conditions is a reasonable strategy, $P_{\infty} = 1.0$ MPa seemed a reasonable upper limit with $P_{\infty} = 0.3$ MPa as a

nominal case, for illustrative purposes. The nominal value of 3 bars represents two effects. First, during severe accident sequences the containment will begin to pressurize due to primary system blowdown and further steam generation. It is not unusual for the containment pressure to rise to over 2 bars due to this effect. Secondly, there will be some pressurization in the lower plenum when the fuel enters the coolant pool due to steam generation during mixing. This was noted by past investigators (13, 17, 18) and can be on the order of a bar. Finally, the range of fuel temperatures was chosen to bound actual values during the accident, with 2700 K taken as the nominal value from past severe accident analyses (6).

The final initial condition that one must specify for the model is the fuel mixing diameter, $D_f = D_{mix}$. In the previous section it was proposed that if this model is to be used to estimate the upper bound on the fuel mass mixed this diameter should be of an appropriate size that all of the droplet mass would be rapidly fragmented during the explosion propagation/expansion time. If this fuel local length scale is too large, some of the fuel would still remain unquenched during the explosion expansion, and not participate in the energy transfer or contribute to the explosion mechanical energy release. Thus, although it is present in the coolant pool it is not involved in the explosion, and thus is not really an active part of the mixture. Therefore, the final parameter needed to estimate a mixing limit for the vapor explosion is a range of values of D_{mix} (sometimes called the coupling length scale) that will completely fragment during the explosion. In the past analyses (e.g., Ref. 15, 17 in Table II) the value for D_{mix} has been chosen to be between 10-100 mm, where some investigators feel the real value is near 10 mm and 100 mm is considered an extreme upper bound. The dynamic fragmentation models employed in the TEXAS model (Chu and Corradini, 16, 18) also indicate that in the region where the fuel is "mixed" with a continuum of liquid water (Table I) the fuel fragments to sizes near 10 mm. As Table I indicates for a variety of initial conditions (water depth, pour rates and jet number) the fuel particles "mixed" within the coolant pool have diameters between 5-15 mm. As the coolant begins to fluidize in vapor rich regions the fuel liquid (and coolant) fragments further to smaller sizes (fuel mass "dispersed," 1-4 mm). This model developed by Chu was also used to analyze the FITS experiments conducted by Sandia (e.g., Ref. 10-12). For these experiments the TEXAS model also predicted mixing diameters on the order of 10 mm. This was verified to some extent for the few non-explosive FITS tests where the recovered fuel debris average diameters were on the same order (<10 mm). This estimate should not be taken as a completely validated prediction since no mixing diameters have been precisely measured during any experiments. Recent experimental work has been completed by Bang et al (36-38) in which this length scale was measured for horizontally stratified layer explosions with water as the fuel material and liquid nitrogen (LN2) and freon (R-12) as the coolant materials. In these tests an electrical resistance probe was used to measure the depth to which the fuel was fragmented as the explosion shock wave passed the interface. This depth of involvement was also verified by varying the stratified liquid depths. Based on these experiments and associated scaling analysis a model was developed (38) to predict this "coupling length scale."

For the materials in these experiments, the measured and predicted length scale was much less than 10 mm for small scale and large scale tests (2-4 mm for 20-50 cm apparatus sizes). For the LWR initial conditions specified here, this model suggests 10 mm to be the appropriate estimate; this assumes this length scale is similar for a mixed system as for a stratified system. However, to be conservative we also chose a value of 30 mm for the D_{mix} to bound this best estimate. We should clearly point out that in the absence of more data this value for the coupling length scale is not a certainty. However, the values used previously for D_{mix} (10-100 mm) were arbitrarily chosen without any mechanistic justification of the explosion phenomena. Thus we feel this suggested range represents a good illustrative example based on a first set of data and mechanistic models that directly attempts to determine this parameter.

The results of the analysis are presented in Figures 7-14. One should note that curve marked "alpha-dept diameter" refers to the use of DC_I for the coolant length scale (Eq. 11) while "We crit" refers to the use of DC_{II} for the coolant length scale (Eq. 13). Since the coolant liquid fluidization involves droplets formed from the continuous coolant liquid phase one can only bound this length scale. Based on the models discussed we believe that coolant droplet fragmentation would rapidly occur as fluidization proceeds. This would favor the lower limit line in the model, DC_{II} . However, because this effect is uncertain one can only bound its effect here. Thus the values for fuel mass mixed would lie within this region. As the figures indicate the estimated fuel mass mixed is about 1-12 metric tons of fuel over the range of initial conditions considered. For our nominal values of $T_f = 2700$ K and $P_\infty = 0.3$ MPA the range is a few metric tons. One should note that the vapor void fraction at the top of the pool at the point of coolant liquid fluidization is predicted to be greater than 50% (55-65%) and thus, these mixing values may be slight overestimates. Also note that the amount of fuel which can mix with the coolant under these conditions increases as the water coolant depth and ambient pressure increases or as the fuel temperature decreases. The increase in the water depth allows a larger coolant volume within which the fuel can mix. However, because the vapor produced throughout the volume must exit the top of the pool, only a small depth of water (~ 0.5 m) is needed before fluidization occurs and the vapor void fraction becomes large ($\alpha_v > 50\%$). The net result is that water depth has a relatively small effect above this minimum depth. Although not shown, an increase in the pool cross-sectional area results in a linear increase in mass mixed since more coolant volume is linearly added. The increase in ambient pressure increases the vapor density and thus decreases the local void fraction proportionately; therefore, more fuel can mix before coolant liquid is fluidized by its vapor. As the fuel temperature decreases more fuel can mix because less coolant vapor is produced per unit surface area of fuel, since the incident heat flux from the fuel decreases. Thus, more fuel mass (fuel surface area) can exist within the pool before the vapor produced fluidizes the liquid in the pool. Counterbalancing this effect is the fact that as the temperature decreases the fuel thermal energy decreases proportionately as would the explosion work output per unit of fuel mass. The final effect to consider is the initial mixing diameter. Note that as D_{mix} increases the amount of fuel mixed also increases. The reason for this directly relates to the reduction in vapor production due to a reduction in the fuel surface area. Note though that if D_{mix} is too large not all the fuel mass will participate in the explosion, thus the fuel mass would not be efficiently mixed.

When this model was first presented in the previous section it was pointed out that this integral approach to the problem of a limit to fuel mixing with coolant was appealing because it bypassed the complexities of a multidimensional analysis. Both the original model by Fauske (4,5) and this proposed model include the whole coolant pool volume as available for mixing with the fuel and the whole pool surface area available for coolant vapor outflow. It is our belief that the multidimensional aspects of fuel-coolant mixing may be bounded by this approach. To make such a claim, the available mixture volume must be larger and the surface area for vapor outflow must be also larger. This always implies the steam flow velocity is minimized and the volume of fuel mixed is maximized. Now the only way to conclusively prove this hypothesis is to perform well-instrumented mixing experiments and supporting analyses that demonstrate this to be the case. In the absence of such empirical evidence let us consider some of the possible multidimensional configurations that the model neglects and examine if they are bounded by the model.

One possible effect is that in the actual accident case for the reactor the fuel may pour into the central region of the pool and as the fuel-coolant mixture dynamically grows (axially and radially) the vapor flows up axially through the fuel eventually exiting at the pool surface. Qualitative observations from tests (10-12) and some simulations of this test data during mixing (13, 15, 17) suggest mixing occurs at the edge of the mixture and a vapor rich region exists internal to the mixture. In reactor scale simulations (e.g., Ref 17) similar behavior is demonstrated for finite fuel masses. In both cases the dynamic lateral movement of the mixture volume progressively allows more mixing. However, this effect would not allow more mixing compared to the whole pool surface area and pool depth because this volume is always greater than the growing multi-dimensional mixture volume. In addition, the surface area for vapor out flow is always larger, minimizing the vapor velocity. Thus, by taking the complete pool surface area we have bounded this geometric effect.

A second configuration would be the fuel mass enters as a series of jets spread across the pool surface instead of one large jet. In the limit this situation actually approaches our one dimensional picture, and the model approximations become unimportant.

Another effect to consider is fuel-coolant remixing during the explosion expansion. Past CSQ calculations (30, 31) indicated that a steam channel (void fraction, 35%) could be collapsed by the explosion propagation and expansion (note, that this case falls within our definition of mixed fuel). This concept may allow for more mixing between the liquids as the large voids collapse. In this case one must compare two fuel masses; the one that initially mixes in our control volume approach and the other being that mass mixed in the more realistic case where part is in a vapor rich region. If the initial available pool volume for fuel mixing is conserved between these two cases, then our conceptual picture always bounds this situation since the integral analysis considers coolant liquid is the continuous phase in the pool volume and all the fuel in the pool would be initially mixed. No additional coolant and fuel liquids would be available to remix during the explosion expansion.

A final consideration is the possibility of multiple explosions, where the first explosion is too weak to disperse the fuel but of sufficient strength to aid in fragmentation. These multiple explosions were observed in some Sandia experiments (10-12). The proposed model in a certain sense could include this possibility since no specific path is considered by which fuel and coolant liquids become initially intermixed. In the last few paragraphs we considered three different paths by which the mixture configuration could develop. In this situation the mixture may not significantly expand and disperse from an energetic FCI and yet the fuel fragments to smaller sizes. Now there are two possibilities. First, the expansion is appreciable enough that the mixture void rises significantly. In our analysis the average void is slightly greater than 50%, in the range of 55-65%. A factor of two expansion would increase the average void to about 65-75%. (A factor of ten expansion corresponds to a void of about 90%). We would contend this mixture of fuel and coolant could not efficiently undergo a vapor explosion for the reasons we mentioned previously. Even if this mixture could undergo a vapor explosion in which all the fuel participates the additional fuel mass that could be mixed at a given D_{mix} is not much greater. Figure 14 shows a sensitivity study where we plot the mixed fuel mass for an arbitrary pool void fraction of 65% and 90% to show the effect of this void limit. As one can see this only increases the fuel mass mixed by 25%. A second possibility is that the FCI expansion is minimal. In this case the pool volume remains about constant. If the fuel mixing diameter decreases then the vapor velocity would increase and fluidization would drive away more coolant liquid and the mixed fuel mass decreases as the mixture expands. The model considers the amount of fuel that can mix only to the point where fluidization begins within the pool. One can investigate the effect of different mixing diameters or fuel masses by parametrically varying these values in the analysis, and determining the maximum amount of fuel that can enter before liquid is expelled by vapor flow.

Now one should note that the difference between these two scenarios is the time it takes for the fuel-coolant mixture to expand after the fuel has been pre-fragmented by a mild FCI. However, this expansion time would be short in all cases ($\ll 1$ sec) because the heat flux between fuel and coolant is quite large for LWR conditions (3-4 MW/m² based on eqn. 6 and representative properties and temperatures). Under the conditions of approximately constant pressure in the mixture the expansion time, Δt , is given by

$$\Delta t = H_{mix} A_{ch} \alpha_v \left[\frac{6q''_{drop} m_f}{\rho_f i_{fg} D_{mix} \rho_v} \right]^{-1} \quad (24)$$

where $H_{mix} A_{ch} \alpha_v$ is the vapor volume and all other quantities are defined. For nominal conditions ($m_f \sim 1000$ kg, $D_{mix} \sim 10$ mm, $P_\infty = 3$ bar) and a volume expansion of a factor of two, the expansion time, Δt , is less than 100 msec. Thus, the transient time between these bounding cases of a multiple explosion is short and the simple model is applicable at these bounds.

One effect that has not been directly examined in this study is the effect of coolant liquid subcooling. In some accident sequences the coolant may be subcooled and this can affect the fuel mass mixed by reducing the amount of coolant vapor produced by the mixing process. Instead a portion of the fuel thermal energy may go into sensible heat, by raising the temperature of the liquid coolant in the mixing region. One can bound this effect by finding the amount of fuel energy required to raise the coolant liquid within the pool to saturation, thus reducing the vapor produced from fuel energy transfer. For the nominal case of $D_{\text{mix}} = 10$ mm as shown in Figure 7 one finds the fuel mass mixed increasing by about 40 % for a subcooling of 25K (Figure 14). Thus, this is a second order effect if the degree subcooling is small. (See Appendix A).

CONCLUSION

The vapor explosion process involves the mixing of fuel with coolant prior to the explosion. In this work we have reviewed past models to describe the evolution of the mixing concept and the various analytical approaches. We have also tried to compare the results of these models. With this as a basis a simplified approach is proposed here to estimate the limits to fuel-coolant mixing. The approach uses the concept first advanced by Fauske but directly derives the fluidization point. The results indicate that decreasing fuel temperature and increasing ambient pressure are important parameters in increasing the fuel mass mixed with the coolant; the water coolant depth has a minor effect above some minimum depth (~0.5 m). As expected fuel mixing diameter is a key factor in determining this limit to mixing and our analysis here considers a range of fuel diameters up to 30 mm. Based on the nominal values for these initial conditions we find that the range of fuel masses mixed can range from 1-12 metric tons with increasing pressure and subcooling causing this to increase by about a factor of two. It is important to realize that experiments and supporting analyses are eventually needed to verify this integral approach, and until that time one must consider a range of initial conditions to get a proper assessment of mixing limits.

ACKNOWLEDGEMENTS

We would like to thank Dr. H. Jacobs and Dr. G. Kessler for their suggestions and detailed discussions about this work. Also the criticisms and suggestions of Dr. M. Berman are gratefully acknowledged. This work was supported by a research grant from KfK Nuclear Research Center.

NOMENCLATURE

a - drift flux parameter
A - area
 c_p - specific heat
 C_0 - drift flux void distribution parameter
 C_D - drag coefficient
D - diameter
 D_H - hydraulic diameter
g - gravitational acceleration constant
h - heat transfer coefficient
H - depth
 i_{fg} - latent heat of vaporization
j - volumetric flux
k - thermal conductivity
m - mass
 P_w - wall perimeter
 P_∞ - ambient pressure
 q'' - heat flux
t - time
 t_B - breakup time
T - temperature
 T^+ - dimensionless time
 U_t - terminal velocity
V - velocity
 V_{gj} - average drift velocity
 V_{ol} - volume
We - Weber number
 α - volume fraction
 μ - viscosity
 ρ - density
 σ - surface tension
 σ_r - Stefan-Boltzman constant

SUBSCRIPTS

c - coolant
ch - chamber
crit - critical
d - droplet
drop - droplet
f - fuel
film - film value
fl - fluidization
j - jet

mix - mixture
o - initial value
sat - saturated
s - single sphere
v - vapor

SUPERSCRIPTS

*dimensionless quantity

Appendix A

The effect of subcooling in the water pool on a limit to mixing can be estimated in a couple of ways. As an upper bound on the effect one can find the amount of energy required to saturate the liquid coolant pool and then find the steam vapor fluidization limit with the fuel at this lower temperature. The temperature decrease is given by

$$\Delta T_f = m_c C_{p_c} (T_{sat} - T_c) / m_f C_{p_f} \quad (A.1)$$

where the terms have been defined previously. The main disadvantage with this bounding estimate is that it neglects the time it takes for the pool to heat up to saturation.

A more realistic estimate of the energy absorbed by the subcooled coolant which would not be available for vapor production is found by looking at the heat flux at the coolant vapor-liquid interface and determining the net vapor generation rate. Our model for this considers transient conduction into the coolant at this interface to estimate the conduction length scale and partial volumetric heating of the bulk coolant by the radiative heat flux. The amount of vapor produced per unit area, \dot{m}_v , is given by this difference of heat fluxes at the coolant vapor-liquid interface.

$$\dot{m}_v = \frac{q''_{drop} - q''_c}{i_{fg}} \quad (A.2)$$

where

$$q''_c = \frac{k_c}{\sqrt{a_t \tau}} (T_{sat} - T_c) + f \sigma_r (T_f^4 - T_c^4) \quad (A.3)$$

where

$$a_t = k_c / \rho_c C_{p_c} \quad (A.4)$$

and the terms have been defined previously, with f as the fraction of radiative energy that is deposited volumetrically (i.e., not in the thermal boundary layer) and τ as the characteristic time for the fuel to fall through the coolant pool depth. The parameter, f , must be estimated separately from a radiation transport calculation, but is usually small (0.01 - 0.3 depending on the fuel temperature). The characteristic time, τ , is usually assumed to be given by the pool depth divided by the fuel pour velocity; this takes on values of typically 0.1 to 1 sec.

When these models are used we find that for the nominal conditions of 3 bar, a water pool depth of 1.5 meters, and a fuel temperature of 2700k the fuel mass mixed increases by about 40% for the upper bound estimate and 15% for the more realistic estimate. Thus, this is a second order effect for small subcoolings.

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Table I

Initial Conditions for Full-Scale Applications

	Base Calculation	2	3	4	5	6	7
Jet diameter (m)	0.4077	0.4077	0.4077	0.1823	0.1289	0.8154	0.258
Ambient pressure (MPa)	0.1	1.0	0.1	0.1	0.1	0.1	0.1
Water depth (m)	3.0	3.0	5.0	3.0	3.0	3.0	3.0
Jet number	1	1	1	5	10	1	10
Mass flow rate (kg/s)	5000	5000	5000	5000	5000	20 000	20 000

Summary of Predicted Results for Full Scale

	Base Calculation	2	3	4	5	6	7
Mass diameter (mixed) (mm)	10	12	5.5	14.8	12.7	5.0	8
Mass diameter (dispersed) (mm)	3	4	2.0	3.0	3.14	0.25	0.5
Discrete fuel mass (kg)	540	540	810	607	687	2176	2066
Mixed fuel mass (kg)	339	540	460	250	263	306	860
Fuel mixing area (m ²)	68	65	140	72	65	500	210
Vapor void (mixed pool)	0.1	0.03	0.2	0.2	0.25	0.3	0.35
Vapor void (dispersed pool)	0.4	0.01	0.65	0.6	0.65	0.9	0.8
Peak pressure (differential) MPa)	0.1	0.02	0.12	0.06	0.065	0.65	0.27

TABLE II
LWR In-Vessel Fuel-Coolant Mixing Estimates*

Investigator (Ref)	Mixed Mass M _{max} (kg)	Fuel Diameter D _{mix} (mm)
Henry and Fauske (4, 5)	100	10
Theofanous, Saito (6, 7)	1500-4000	<100
Corradini, Moses (8, 9)	3000-5000	60-100
Theofanous et al (17, 19)+	4000-7000	20
Corradini, Moses (27)	1000-10000	10-100

*The PWR (Zion) geometry in-vessel water depth in 3 m, the vessel cross-section area is 15 m², and structure in lower plenum is neglected. The pressure at 1 atm; saturated water; fuel temperature is 2700 K, with blackbody radiation.

+These estimates conservatively defined the mixture as all fuel within a vapor void of 90%. If one uses results consistent with the other investigators these estimates would be reduced.

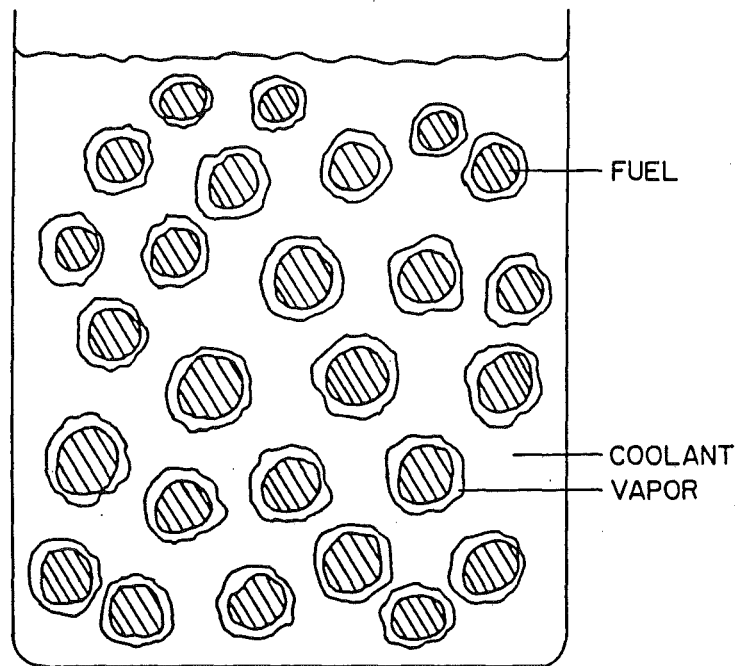


Figure 1. Conceptual picture of fuel/coolant mixing in Fauske's model

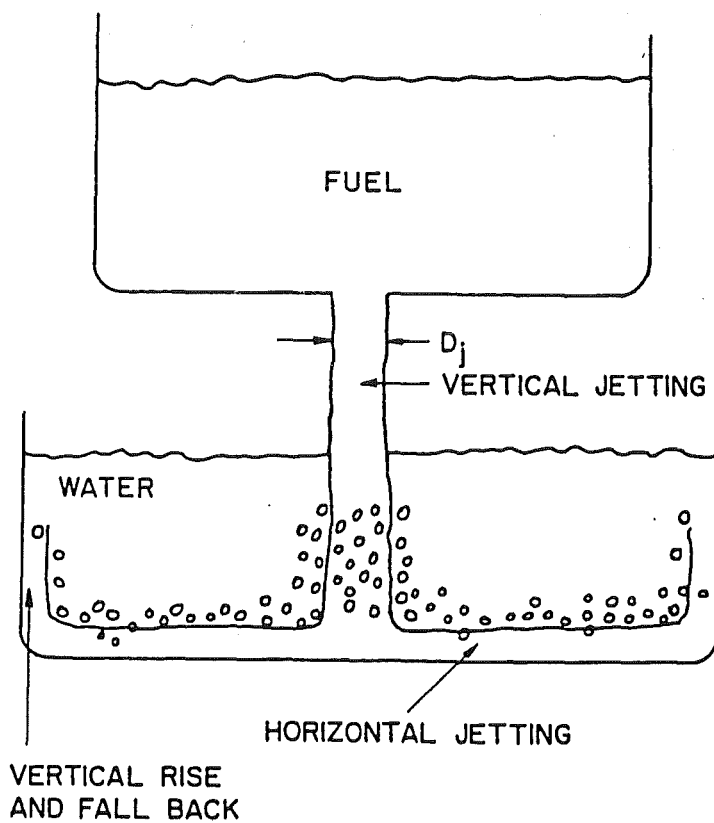


Figure 2. Geometry of fuel pouring into the lower plenum of the vessel in Theofanous model

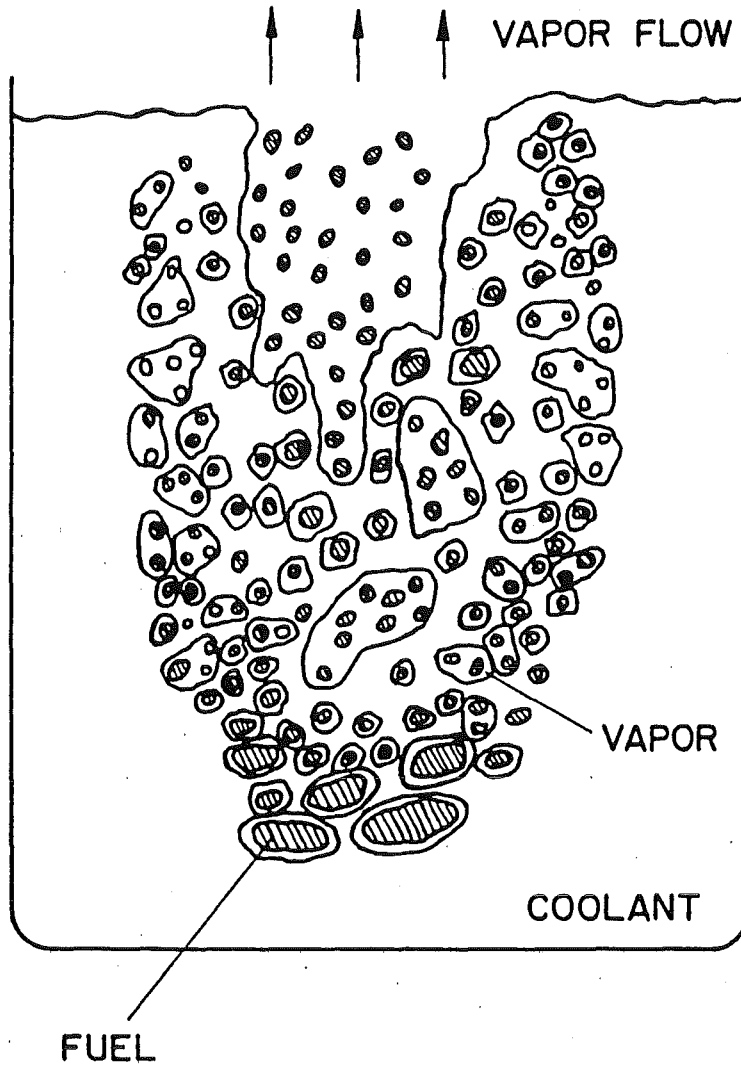


Figure 3. Conceptual picture of fuel/coolant mixing as observed in FITS experiments

FUEL-COOLANT MIXING

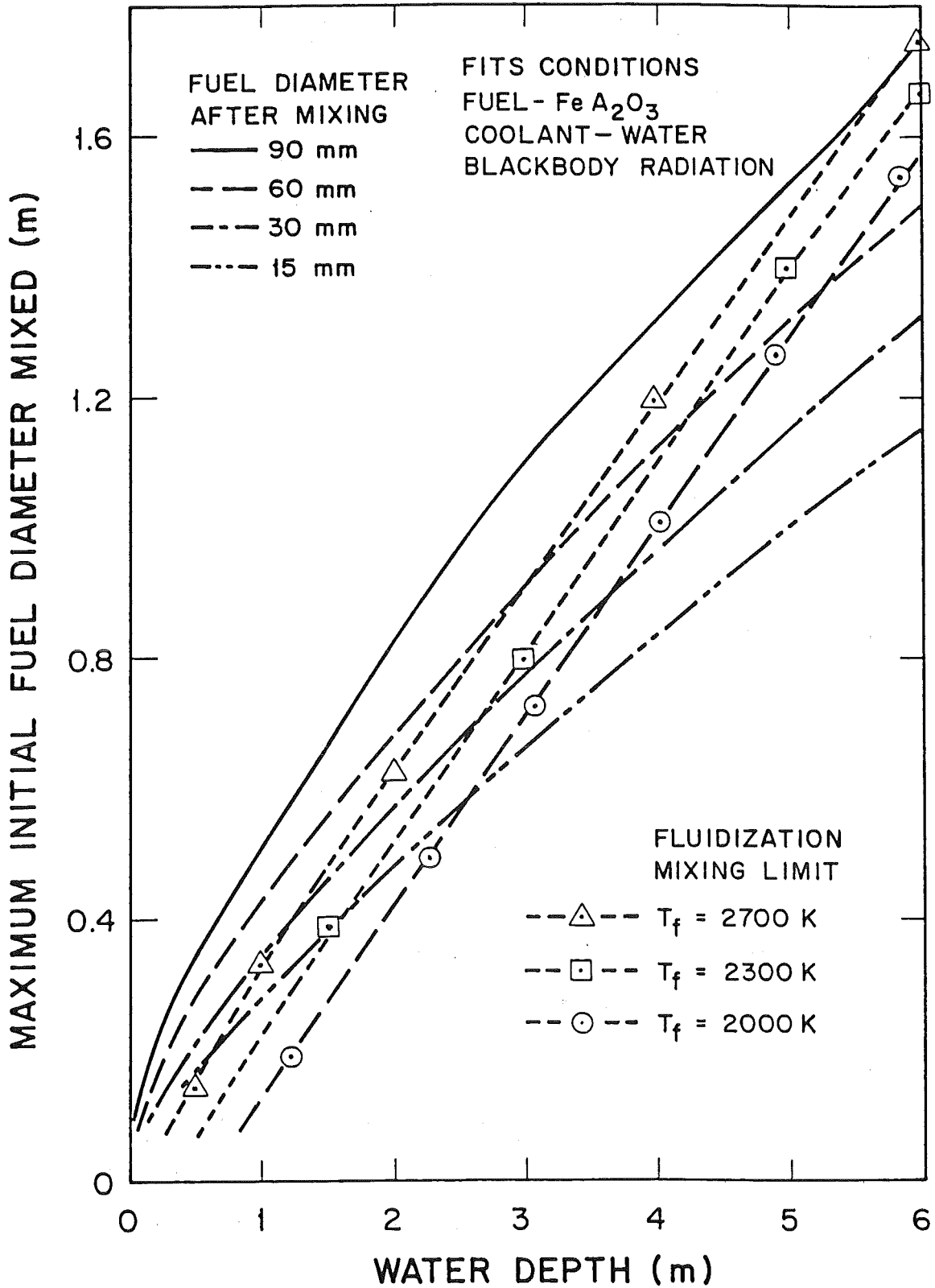


Figure 4. Limits to fuel/cooling mixing based on fluidization model with transient jet breakup

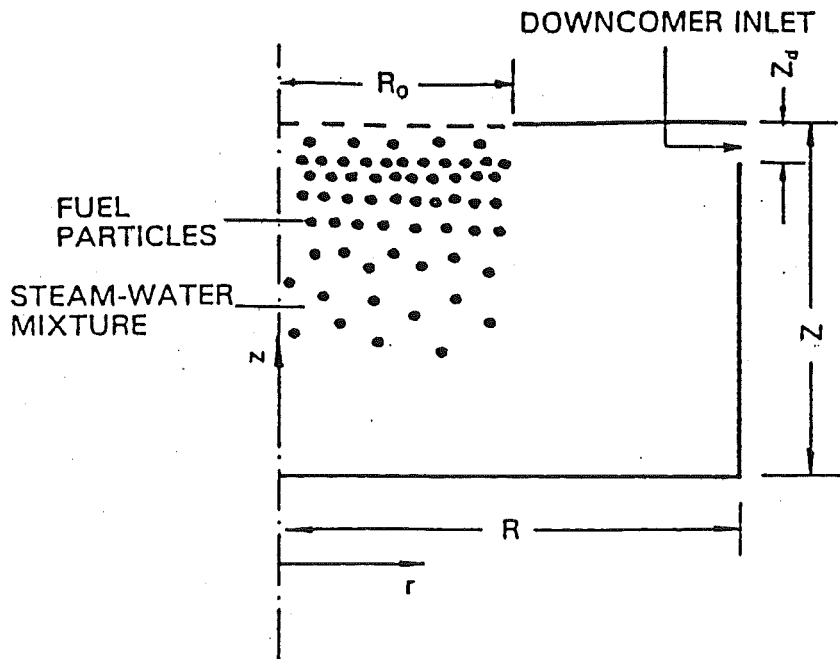


Figure 5A. Geometry of premixing model (Ref. 15, 17)

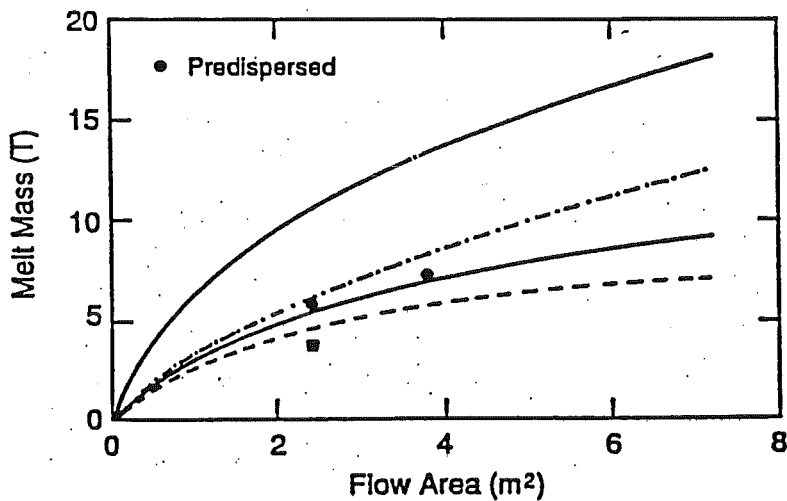
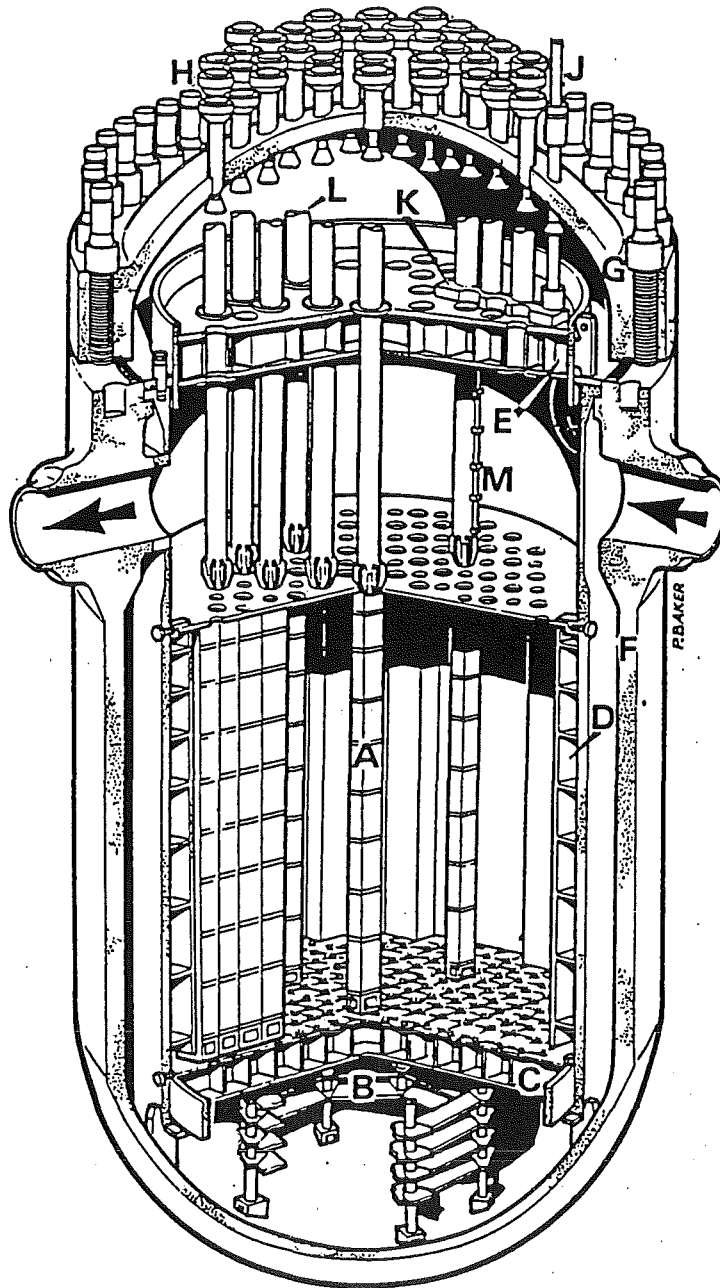


Figure 5B. Estimates of melt masses in mixture

- Estimate from simple model (eq. 2, Ref. 15)
- .-. Estimate including leading edge effect (Ref. 15)
- Results from two-fluid K-FIX calculations (Ref. 15)
- 5 % and 95 % limit lines developed from K-FIX analysis to subjectively envelop the mixture range (Ref. 15)
- Result from the PM-ALPHA calculation (Ref. 17)

Figure 5. Results of premixing models



- A REACTOR CORE
- B SUPPORT STRUCTURE
- C LOWER CORE AND DIFFUSER PLATES
- D CORE TANK, THERMAL SHIELD
- E UPPER GRID
- F PRESSURE VESSEL
- G REACTOR PRESSURE VESSEL HEAD
- H NOZZLE FOR CONTROL ELEMENT DRIVES
- J NOZZLE FOR CORE INSTRUMENTATION
- K INSTRUMENTATION LANCE
- L CONTROL ELEMENT GUIDE TUBE
- M CORE INSTRUMENTATION GUIDE TUBE

Figure 6. Geometric details of BIBLIS-B reactor

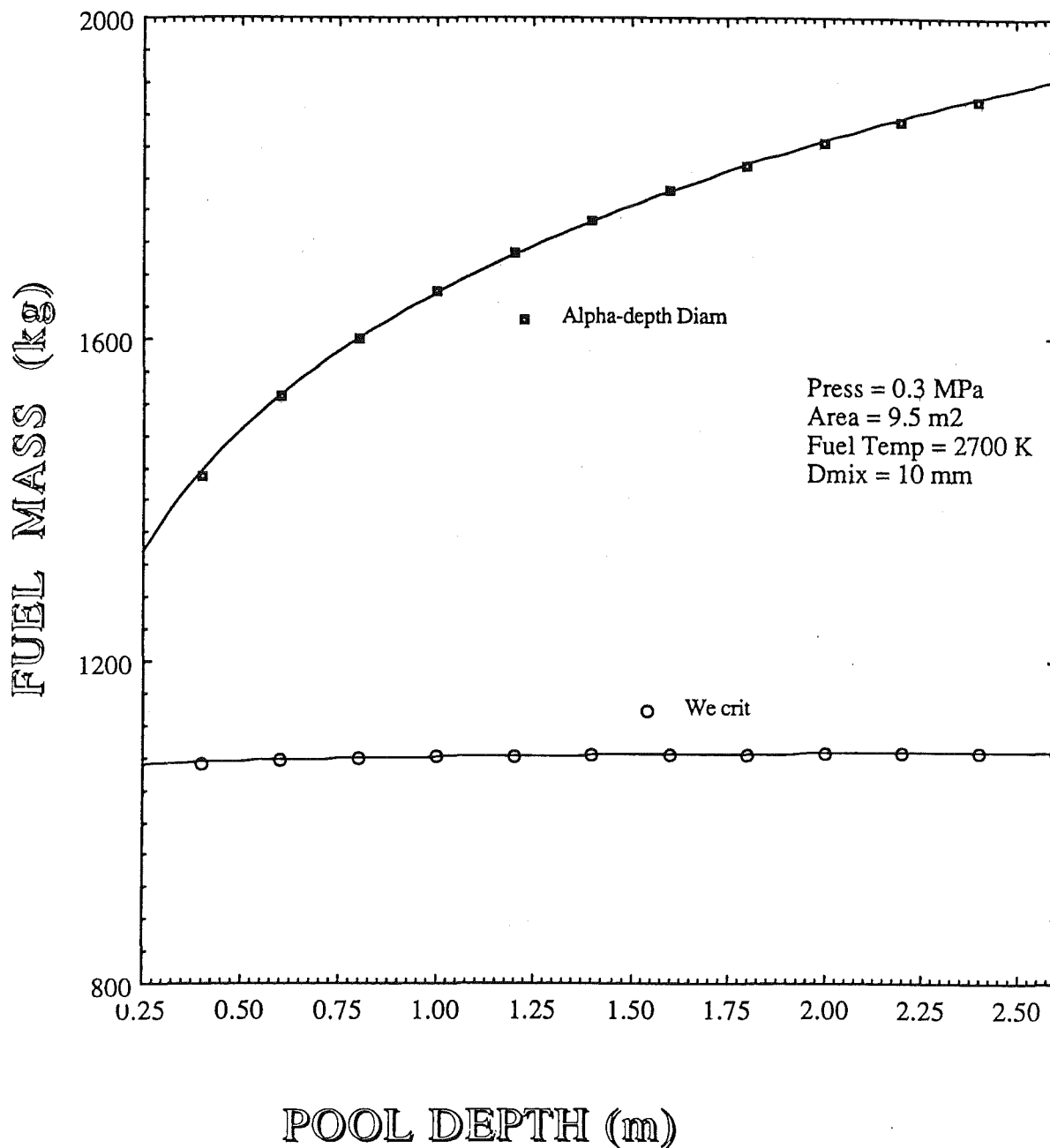


Figure 7. Amount of fuel mixed vs pool depth for $D_{mix} = 10$ mm

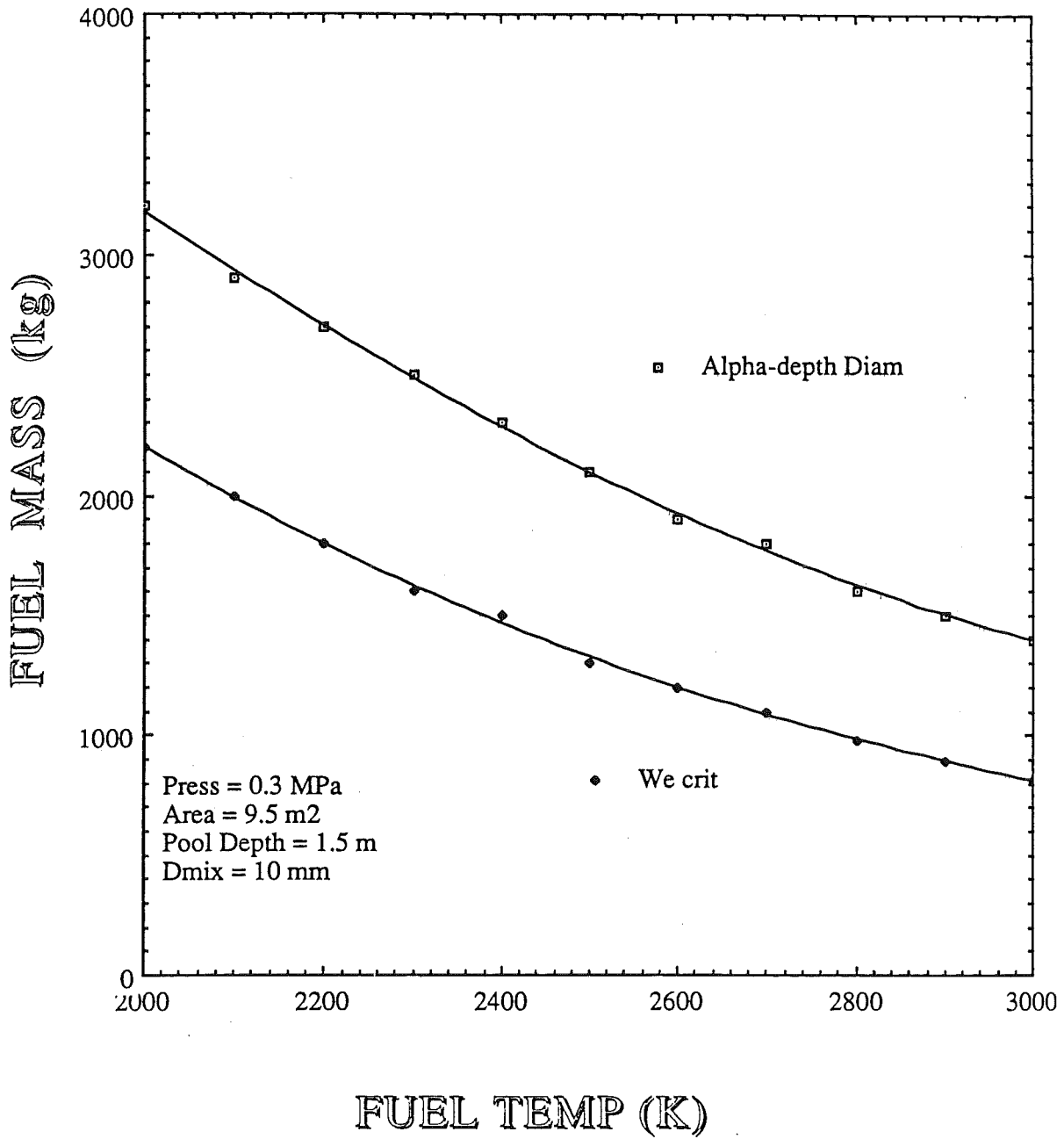


Figure 8. Amount of fuel mixed vs fuel temperature for $D_{mix} = 10$ mm

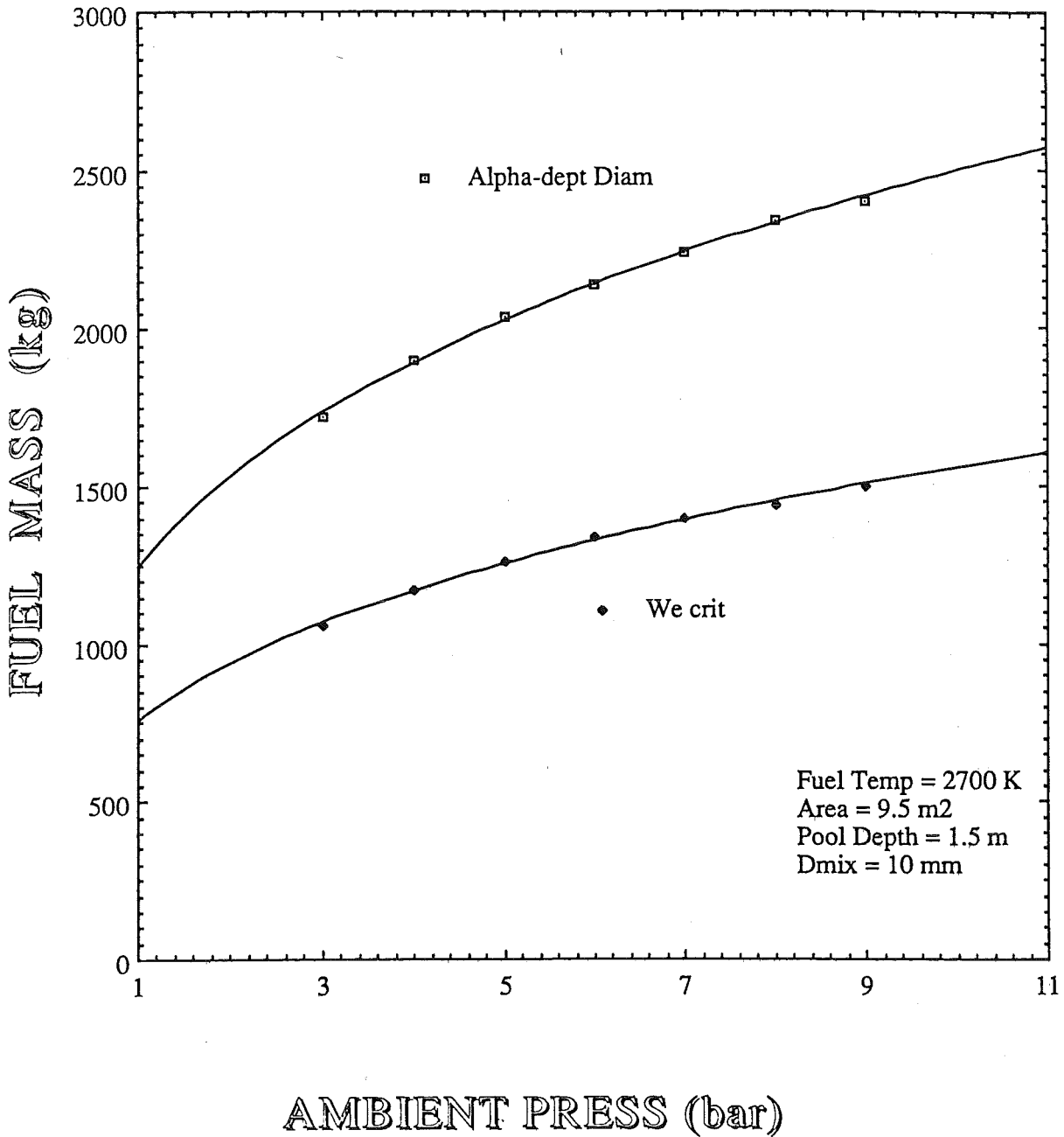


Figure 9. Amount of fuel mixed vs coolant pressure for $D_{mix} = 10$ mm

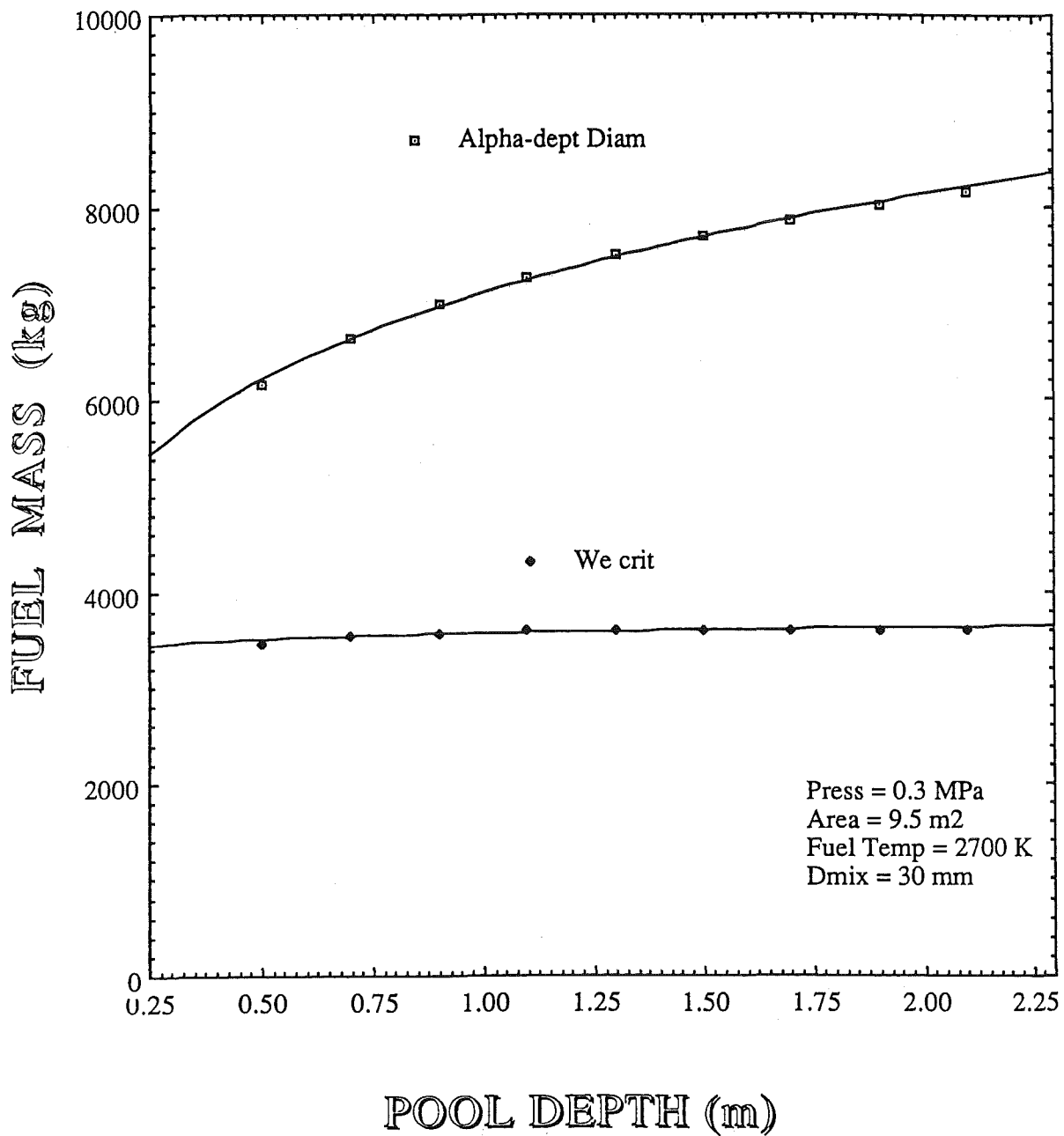


Figure 10. Amount of mixed fuel vs pool depth for $D_{mix} = 30$ mm

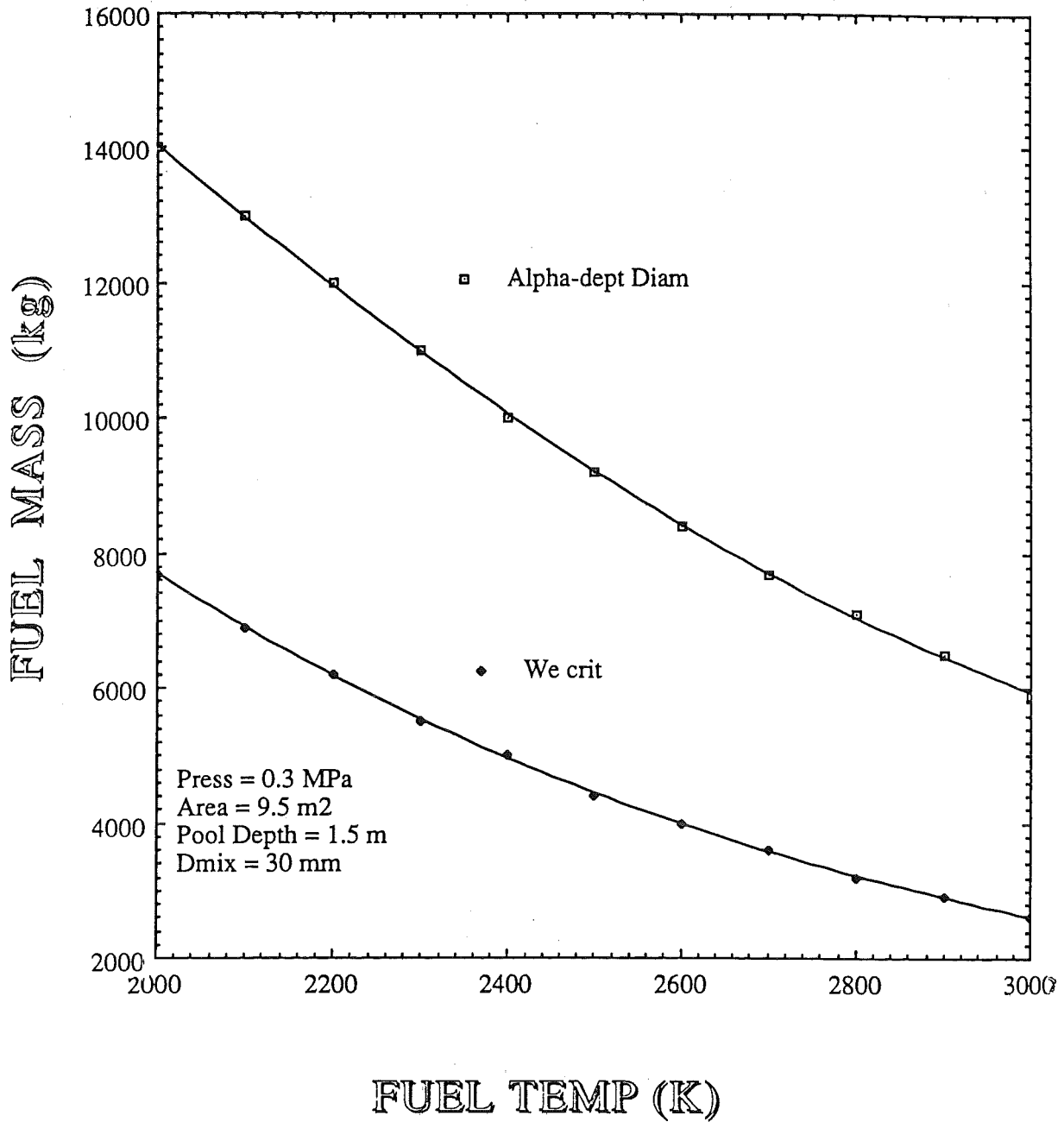


Figure 11. Amount of mixed fuel vs fuel temperature for $D_{mix} = 30$ mm

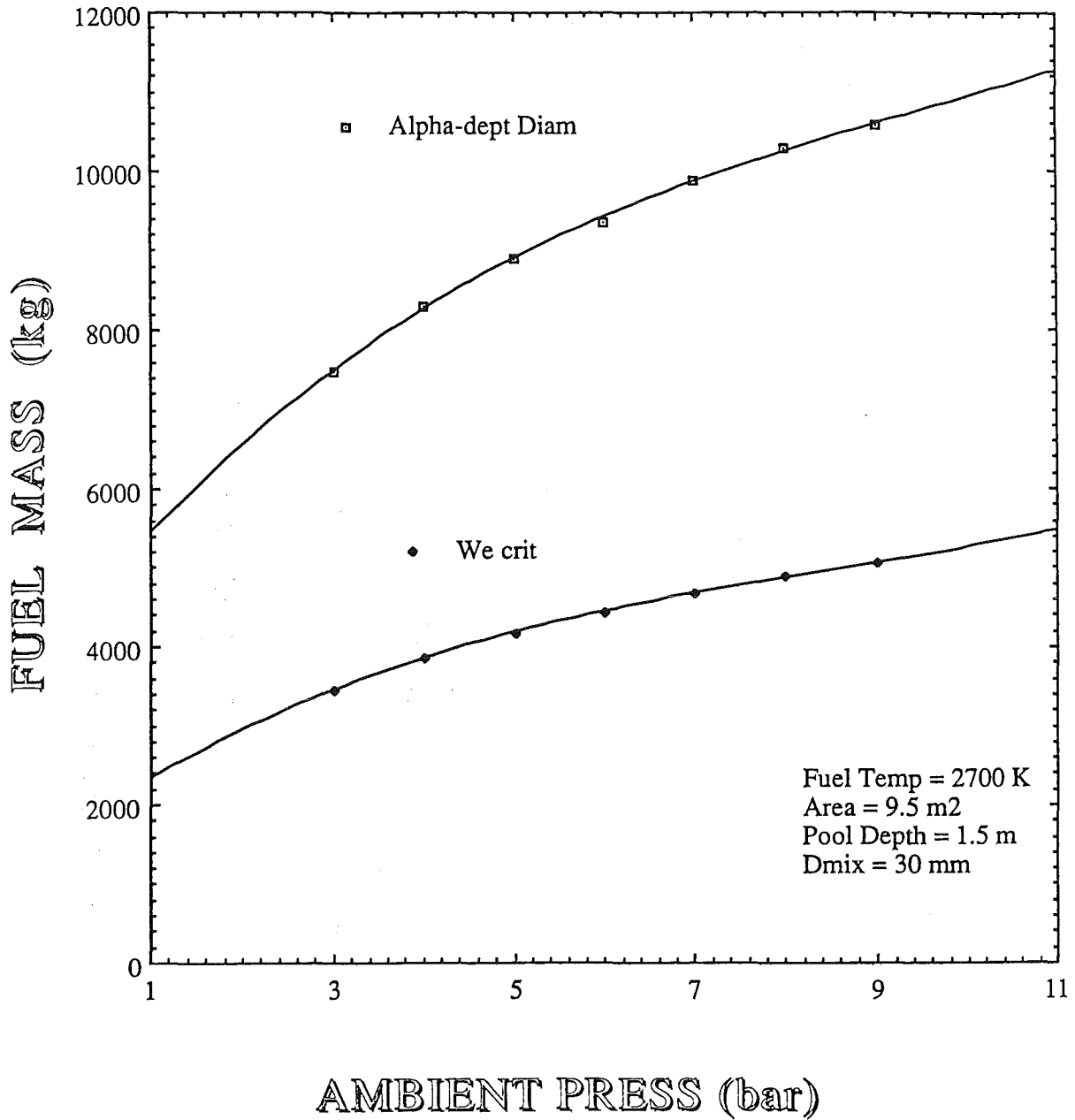


Figure 12. Amount of mixed fuel vs coolant pressure for $D_{mix} = 30 \text{ mm}$

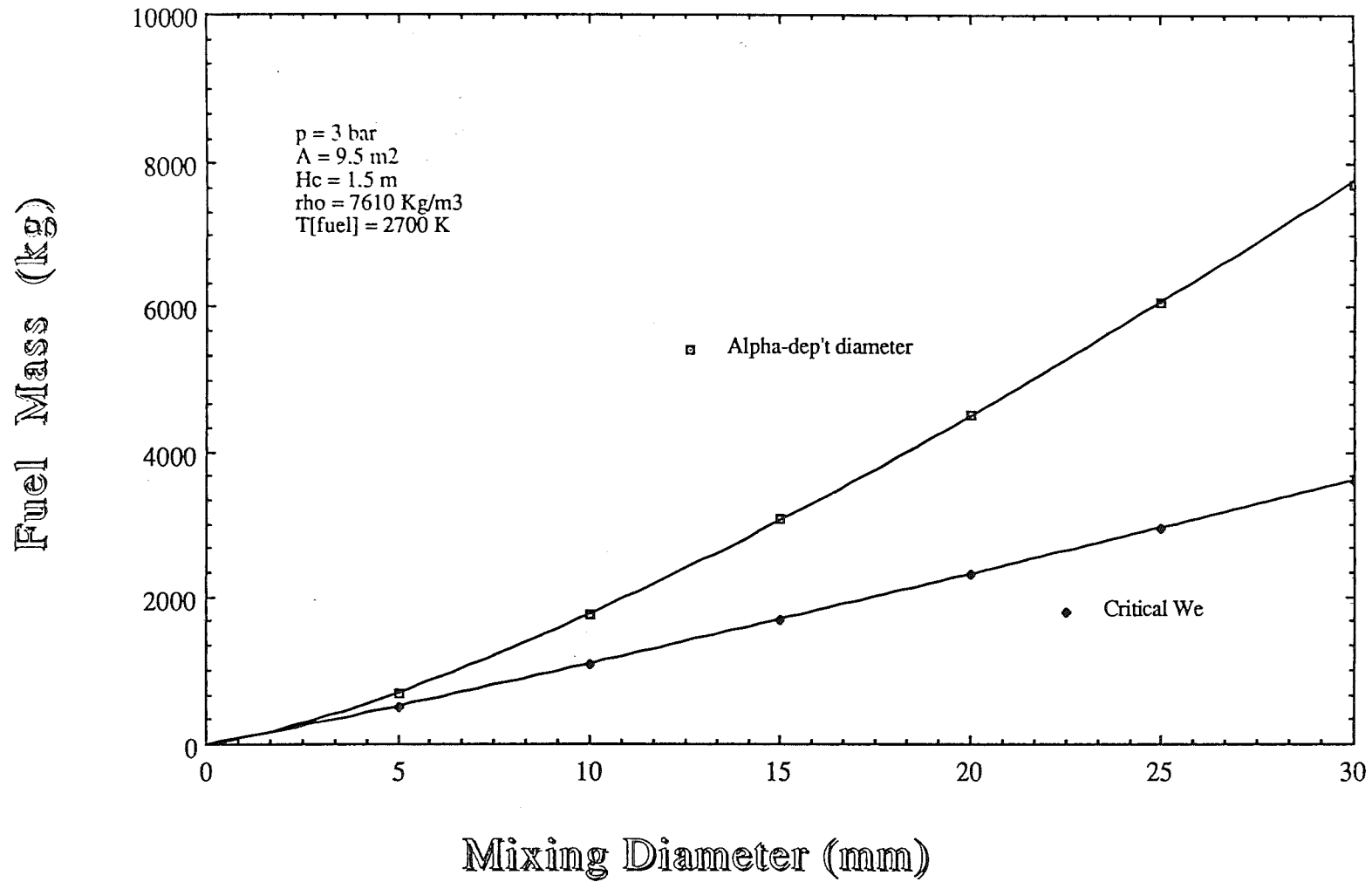


Figure 13. Effect of varying the fuel mixing diameter

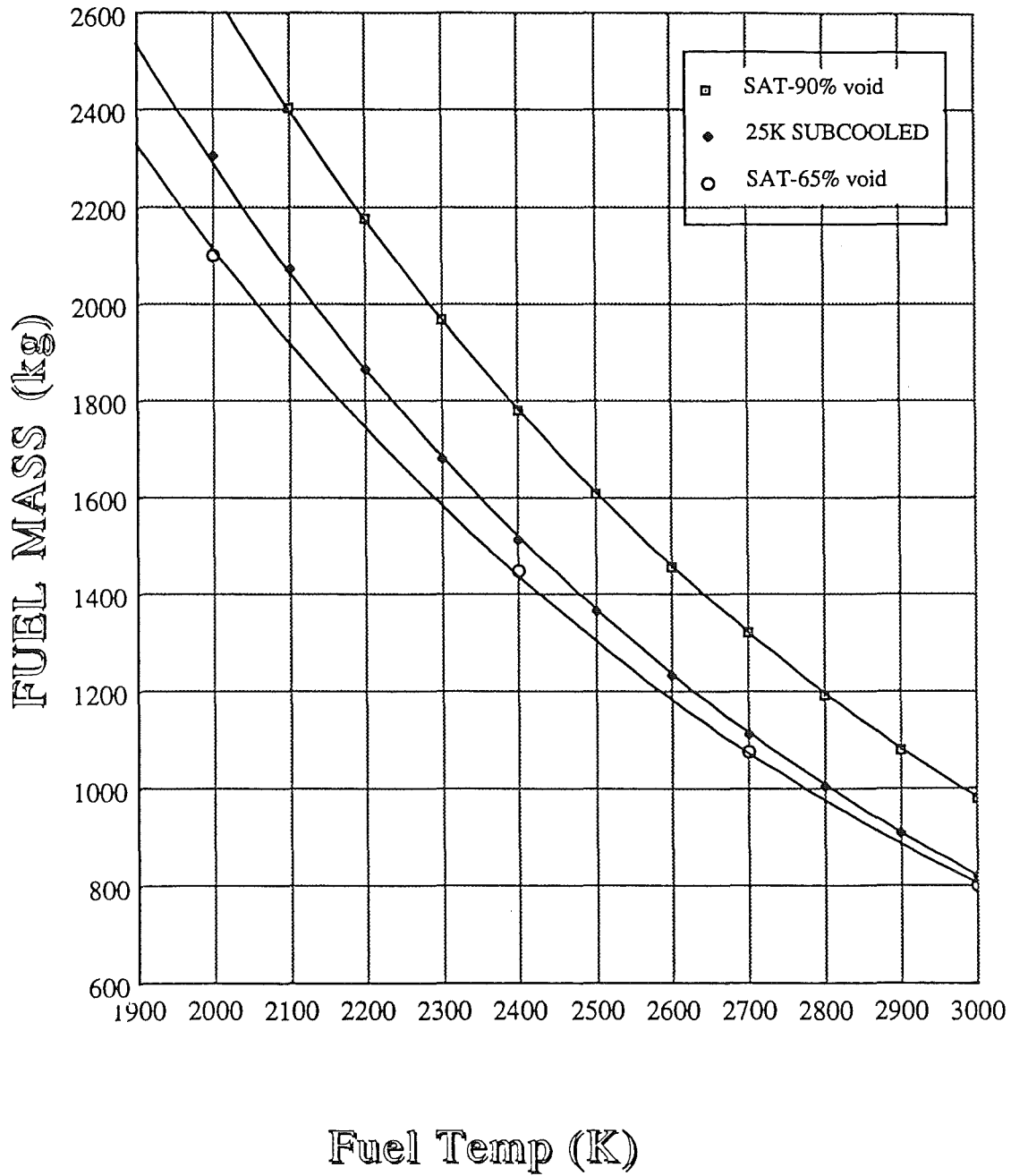


Figure 14. Sensitivity study of void and subcooling effect for $D_{mix} = 10$ mm and D_{cII}