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An assessment of accident energetics in LMFBF core-disruptive accidents is given with emphasis on the generic issues of energetic recriticality and energetic fuel-coolant interaction events. Application of a few general behavior principles to the oxide-fueled system suggest that such events are highly unlikely following a postulated core meltdown event.

1. Introduction and Overall Approach

The basic protection to the public from fast reactors as from all other reactor types against the escape of radioactive material is through a multiplicity of barriers, e.g., pin cladding, subassembly structures, the reactor primary system, cleanup systems, and the secondary containment system. In addition to these engineered barriers, there may be other inherent or natural barriers or attenuators such as meteorological dispersion. Furthermore it should be clear that the issue of public safety or environmental risk from fast reactors like for thermal reactors can only enter through the hypothetical core-disruptive accident (HCDA), e.g., all accidents of significance to the environment lead to a core melt resulting in breach of the engineered barriers associated with the core disruption (leading to rapid release of radioactive materials) or from thermal meltthrough due to continued decay heat generation (leading to radioactive release at a relatively later time). It is only with respect to accident energetics that LMFBFs differ significantly from LWRs. This is not because the probability of an accident is any greater for the LMFBF, but because the accidents that require analysis have been quite different in character. Unlike LWRs, LMFBFs can be very sensitive to dimensional changes or relocation of core materials since the intact LMFBF core is not in its most reactive configuration. Therefore, it is theoretically possible that rearrangement of geometry can lead to prompt-

critical reactivity excursions and to hydrodynamic disassembly of the reactor resulting in large quantities of vaporized fuel, as first discussed by Bethe and Tait in 1956, [1] which raises the general issue of energetic recriticality accidents. A typical idealized configuration used to produce an upper-bound limit on the recriticality excursion is shown in Fig. 1, where the upper portion of the core following an arbitrary separation is assumed to fall by gravity onto the lower portion of the molten core. The relatively greater concern voiced over LMFBF core meltdown accidents results therefore principally from its significantly increased potential for producing vaporized plutonium (airborne fuel particles) and breach of the engineered barriers due to a violent disassembly accident. Only the recriticality issue is unique to the LMFBF and other fast reactors. Also, even if a core meltdown occurs without an energetic excursion, the possibility of an energetic fuel-coolant interaction (vapor explosion) must be considered, as first discussed by Hicks and Menzies in 1965. [2] An upper-bound estimate as used by Hicks and Menzies requires the idealized configuration illustrated in Fig. 1 where the molten fuel and liquid sodium must be finely mixed to result in optimum and instantaneous transfer of energy. Ever since Bethe-Tait and Hicks-Menzies studies, the assessment of CDAs, including recriticality and fuel-coolant interaction events, has been a major consideration in LMFBF safety analysis and development. This is because it is generally not considered practical to

Fig. 1. Accident Energetics - Generic Issues.

accommodate upper "theoretical" bounds resulting from idealized configurations as illustrated in Fig. 1. Furthermore, since the levels of energetics resulting from hydrodynamic disassembly are rather sensitive to small variations in the core average temperature (the work is essentially proportional to fuel vapor pressure which is an exponential function of temperature) and hence to initial conditions like the driving ramp rate, it follows that it is desirable to be able to rule out energetic hydrodynamic disassembly conditions altogether. [3] For example, Fig. 2 illustrates slightly different idealized recriticality configurations including both gravity and pressure driven collapse modes and associated work potential for the Fast Flux Test Facility (FFTF). If these conditions were indeed realistic, it would be difficult if not impossible to sort out the difference between ramp rates of 100 \$/s and 125 \$/s which lead to an increase in work potential by a factor of 2. It would therefore be correspondingly unrealistic to claim that FFTF is providing any significant safety margin for the conditions illustrated in Fig. 2.

Adequate safety margins are rather assured by illustrating that highly energetic recriticality events are very unlikely. Similar arguments can be made relative to energetic fuel-coolant interaction events.

One can perhaps think of three possible ways to resolve the generic issues associated with an LMFBR meltdown accident:

- Detailed mechanistic approach* using large complex computer codes tracking the accident sequentially from the initiating phase to a permanent subcritical and coolable fuel configuration with radiological consequence assessment.
- Large-scale test(s) involving an integrated sequence of events (simulating an LOF accident, etc.).
- Assessment of the core-meltdown process based on a limited number of "general behavior principles," which are verified experimentally.

*The role of mechanistic accident analysis in fast reactor safety is discussed in another paper at this meeting. [4].

Fig. 2. Illustration of Energetic Recriticality Events (Gravity and Pressure Driven) and Associated Work Potentials. Illustrations are made for FFTF where 1 unit of fuel represents the fuel inventory in 1 subassembly. For CRBR, 1 unit would correspond to ~ 3 subassemblies.

However, since we are dealing with low probability events and therefore the problem is what consequences would result if a large fraction of the core fuel should become disrupted in some unspecified way (known initiators which could mechanistically lead to core meltdown should be prevented by design features) and thereby provide the possibilities for either rapid coherent fuel compaction and/or energetic thermal interactions between disrupted fuel and coolant, the only available option would appear to be the last one. The recommended approach to the resolution of the generic issues is based on a few general physical principles as follows:

1. A self-heated liquid becomes dispersive. At nominal LMFBR power level, the fuel is monotonically dispersive and can remain in a highly boiled-up state down to and including decay-heat power levels. (The separated fuel picture in Fig. 1 should be replaced by the boiled-up configuration).

2. Fine mixing of a cold and hot fluid, a necessary condition for developing sustained pressures and large damage potential from thermal interactions; requires spontaneous nucleation on contact. The contact temperature for the mixed oxide-fuel-sodium system is well below the spontaneous nucleation temperature for liquid sodium. (The picture of finely mixed molten fuel and liquid sodium in Fig. 1 should be replaced by the separated configuration).

The application of these two general behavior principles to an oxide-fueled LMFBR leads to the following conclusions regarding generic accident energetics issues:*

- Principles 1 and 2 rule out gravity and pressure driven recriticality.
- Principle 2 rules out energetic fuel-coolant thermal interactions (vapor explosions).

Further details pertaining to the two principles are given below.

2. First General Behavior Principle

During the initial stages of a postulated core-disruptive accident (e.g., loss of flow with failure to scram in FFTF or CRBRF), fuel disruption due to release of entrapped fission gas from the fuel melt is likely to lead to early swelling and frothing[5] and subsequent mixing with the molten cladding material.[6] The boiling point of steel is very close to the melting point of fuel so that the molten fuel-steel mixture can be treated as a saturated liquid.

*In addition to the generic issues of fuel compaction and fuel-coolant interactions, considerations must also be given to the design-dependent issue of the large core positive sodium void coefficient (theoretically large ramp rates and autocatalytic effects are possible from fuel failures into nonvoided channels).[3]

subjected to volumetric heat generation.* Thus heat transfer from the fuel to the entrained steel (entrained steel particles in the fuel has been observed in the TREAT meltdown experiments) [8] will result in rapid steel vaporization and further dispersal and boil-up of the mixture. The above process, while complex and difficult to describe in detail, suggests that preirradiated fuel at near nominal power levels and above, will be monotonically dispersive from the beginning of fuel disruption following the development of an uncooled fuel pin geometry. Furthermore, if we ignore the important early contribution due to fission gas release, the subsequent dispersal and boil-up of fuel by steel vaporization down to and including decay-heat power levels can be illustrated by straightforward application of two-phase flow theory.

The two-phase flow stability criterion suggested by Kutateladze [9] can be used to predict the flow regime boundaries in a boiling system with internal heat generation, [10]

$$K^4 = \frac{\rho_c j^2}{g (\rho_H - \rho_L) \left[\frac{\sigma}{\rho_c j^2} \right]} \quad (1)$$

where K is the Kutateladze number, j is the critical superficial velocity of the lighter fluid phase, ρ_c is the density of the continuous phase, σ is the surface tension of the heavy fluid, and ρ_H and ρ_L are the densities of the heavier and lighter fluid, respectively. Note that K^4 is the ratio of the dynamic pressure ($\sim \rho_c j^2$) exerted on particles of the discontinuous phase to the weight of the heavier fluid. The bracketed term in the denominator of Eq. (1) is proportional to the maximum particle dimension based on mechanical stability criteria such as the Weber number or the Kelvin-Helmholtz wavelength. In other words, the Kutateladze criterion is simply that balance between dynamic pressure (or drag) and buoyancy forces that is consistent with fluid particle stability. K takes on various values depending upon the flow regime transition under consideration. Based primarily on two-component data in the absence of heating, the transition between bubbly flow and churn-turbulent flow is characterized by $K \sim 3$ and $\rho_c = \rho_H$. [11] Based again on primarily two-component data and interpretation of critical heat flux occurrence

in liquid pools with external heat generation, [9] the transition from churn turbulent to a fluidized droplet regime is characterized by $K \sim 0.14$ and $\rho_c = \rho_L$. For fuel with internal heat generation, the corresponding superficial vapor velocity is given by

$$j = \frac{q}{\rho_L h_{fg}} \quad (2)$$

where q is the equivalent surface heat flux corresponding to volumetric heat generation in a given fraction of the total fuel inventory that is converted to latent heat of evaporation, h_{fg} . By eliminating j between Eqs. (1) and (2), the flow regime boundaries for a boiling fuel-steel system can be constructed as shown in Fig. 3.

Fig. 3. Flow Regimes in a Boiling Fuel-Steel Pool. [10]

It follows that for a given equilibrium condition, the magnitude of boil-up and fuel transport outside the active core region depends upon the flow regime. If the vapor continuous regime prevails (liquid fuel and steel droplets surrounded by vapor), this leads to maximum vapor drift (or maximum slip) and hence represents the minimum boil-up condition. Assuming stagnant liquid condition, the maximum vapor drift velocity is given by

$$j = \alpha^n V_t \quad (3)$$

where α is the local void fraction and V_t is the terminal velocity of a single

*Therefore steel vaporization is likely to be responsible for the boil-up process as first discussed by Ostensen and Jackson. [7]

particle in an infinite fluid medium. Using Wallis' [11] recommended value for n and V_m (these are determined from steady-state requirements with two-component systems with no heat generation) results in

$$j = 1.4 \alpha^2 \rho_L^{-\frac{1}{2}} [(\rho_H - \rho_L) g]^{1/4} \quad (4)$$

It should be noted that Eq. (4) represents the necessary vapor velocity to sustain incipient fluidization over the whole range of void fraction values while Eq. (1) represents only the initiation of incipient fluidization which has been observed to occur at a void fraction of approximately 0.38 in two-component systems, and may be as low as 0.3 in heated systems (boiling off a hot surface). [9,11]

Similarly, if the liquid continuous regime prevails (vapor bubbles imbedded in liquid fuel and steel), this leads to minimum vapor drift (minimum slip) and hence represents the maximum boil-up condition. The vapor drift velocity for this regime is given by

$$j = \alpha (1 - \alpha)^{n-1} V_m \quad (5)$$

Again using Wallis' [11] recommended values for n and V_m for the bubbly and foamy regimes results in

$$j = 1.53 \alpha (1 - \alpha) \rho_H^{-\frac{1}{2}} [(\rho_H - \rho_L) g]^{1/4} \quad (6)$$

while Zuber's [11] recommended value of n equal to zero for the limited churn-turbulent regimes results in

$$j = 1.53 \frac{\alpha}{1 - \alpha} \rho_H^{-\frac{1}{2}} [(\rho_H - \rho_L) g]^{1/4} \quad (7)$$

Void profiles relating to the steady-state boil-up condition for a given internal heat generation, q , can be obtained by integration of Eq. (8)

$$\frac{dj}{dz} = \frac{q'(1 - \alpha)}{h_f \rho_L} \quad (8)$$

together with the individual flux functions [Eqs. (4), (6), and (7)], where q' is the volumetric heat generation in the fuel-steel mixture. In this way the inherent dispersive nature of the heat generating fuel in an LMFBF can be readily demonstrated. Figures 4 and 5 illustrate

calculations for two different equilibrium conditions. If all the fuel is to remain within the active fuel zone, (Fig. 4a) the corresponding pressure to satisfy heat removal by upward vapor transport must be large (Fig. 4b). Therefore in the absence of plugging extended fuel dispersal will take place leaving behind a very small amount of liquid fuel in the active zone (Fig. 5). Early fuel dispersal for near nominal power levels is clearly illustrated in Fig. 5a even for the condition of maximum vapor slippage. Likewise, Fig. 5b illustrates that boil-up will even prevent a criticality configuration at 1% of nominal power. The latter calculation is based upon the churn-turbulent regime and would appear to be on the conservative side based upon recent experiments using water in a microwave oven to simulate decay-heat power levels. [12] For superficial vapor velocities exceeding the bubbly flow regime ($K \sim 0.3$ and $\rho_C = \rho_H$), rapid boil-up and a foamy regime were observed in good agreement with Eq. (6) over the whole range of void fraction values, rather than a transition to churn-turbulent flow as indicated by Fig. 3. Analytical studies of the above transient boil-up process have also been provided by Epstein and Condiff. [14-17]

However, in the case that the core becomes "bottled" up, the quasi-equilibrium conditions of the boiling pool may be better represented by the flow regime boundaries illustrated in Fig. 3 and the void profiles according to Figs. 4 and 5. Axial and radial heat losses in this situation would promote boil-up rather than encourage liquid-vapor separation due to pressurization as observed with volumetric boiling of water in a closed container again using the microwave oven technique. [12] Since the melting point of the steel confinement boundary is well below the melting point of fuel, this necessitates the presence of a solid fuel crust. Furthermore, since the boiling point of steel is approximately equal to the melting point of fuel, the maximum nonboiling layer is given by

$$\delta = \sqrt{2k(T_{f,M} - T_{s,M})/q'} \quad (9)$$

*Startup from a liquid pool at its saturation temperature with internal heat generation and the presence of ample nucleation sites, a foam regime might be inevitable if the rate of void growth always exceeds the rate of bubble rise and agglomeration thereby "locking" the bubbles into a foam regime. [13]

**This may turn out to be a nonproblem, since evidence for the absence of fuel plugging is beginning to emerge. [18]

(a)

(b)

Fig. 4. Illustration of Equilibrium Void Profiles and Corresponding Pressure Levels with All the Fuel Located within the Active Fuel Zone. (a) Void profiles are based upon two different flux functions for describing vapor drift through an essentially stationary liquid. $n = 0$ corresponds to churn-turbulent flow according to Zuber, while $n = 2$ corresponds to the continuous vapor-like regime with fluidization generally occurring at a void fraction of ~ 0.38 . The latter flux function gives maximum vapor drift and hence results in the minimum boil-up condition. (b) Required pressure levels in order to remove all the generated heat in the axial direction. Calculations based upon maximum vapor drift velocities.

(a)

(b)

Fig. 5. Equilibrium Void Profiles within the Active Fuel Zone at 1 atm. (a) Based upon the flux function resulting in maximum vapor drift through the stationary liquid. (b) Based upon the flux function corresponding to churn-turbulent flow regime.

where $T_{f,M}$ and $T_{s,M}$ are the melting temperatures for fuel and steel, respectively. The values of the crust thickness listed in Table 1 would approximate the situation in an open system (corresponding to essentially no radial heat losses from the dispersed fuel in the active core region), while reduced values would be expected in a "bottled-up" system experiencing pressurization (implying radial heat losses). In the latter case the crust will provide a regulating effect and significant pressurization from decay heating appears therefore very unlikely.

In addition to further analysis and creative laboratory experiments, demonstration with real reactor materials (in-pile tests) appear desirable in order to achieve general acceptance of the inherent dispersive nature of oxide fuel and the absence of the potential for energetic recriticality events. The objective of the in-pile tests would be to verify the dispersive behavior of a fuel-steel mixture starting from 1) the initial pin structure configuration (at nominal power or above) and 2) an initially separated state (at decay heat power levels). The question of early monotonic fuel dispersal can be adequately assessed by TREAT with modest improvements (TREAT Upgrade) [19,20] and the SLSF program (37- and 61-pin bundle tests should be adequate). For demonstration of boil-up at decay heat power levels it is of utmost importance to recognize the inherent effect of boundary heat losses which are controlled by the presence of the fuel crust. As seen from Table 1, for any significant portion of the core undergoing a hypothetical disruption, the material associated with the fuel crust at the confinement boundary only represents a small fraction of the disrupted fuel. However, this ratio is highly dependent on the surface-to-volume ratio of the sample in question and indeed becomes quite large or even exceeds 1 for fuel sample sizes of the order of a single LMFBF subassembly (i.e., in the range of the capability provided by the planned SAREF program). [18,19] Melting down fuel pin samples of this size (much larger samples are not considered practicable), with the objective of verifying fuel dispersal at decay heat power levels most likely would lead to highly nonprototypic and highly undesirable results. This objective can, however, be achieved with reasonable sample sizes, by including as part of the makeup of the test fuel sample (see Fig. 6), the nonboiling fuel layer at the confinement boundary as it would be present in hypothetical core meltdown situations as discussed above. By providing for the fuel crust prior to boil-up in the small sample (geometric dimensions illustrated in Fig. 6

Fig. 6. Proposed In-pile Test Sample for Resolution of Key Recriticality Issues.

corresponds approximately to an equivalent 61-pin bundle test), an essentially adiabatic system is established much like in the hypothetical large system. Because of the large time constant of the fuel, the required energy deposition in both the crust and the fuel sample (should approach the melting point of the crust prior to boil-up of the fuel sample) can be achieved by a power burst prior to the flat-topped simulating decay heat power level (see Fig. 6). It is suggested that the proposed test sample in Fig. 6 can be used as a reference for a test matrix that could include separate effects proof-tests of the following nature: 1) boil-up in an initially open system including measurements of the rate of fuel dispersal and fuel penetration into the upper simulated blanket and fission gas plenum regions to verify current analysis [7,10] and out-of-pile experiments as they relate to boil-up tests with simulant materials [12] and freeing and plugging tests using the thermite method to produce molten fuel, [18] 2) the

Table 1. Fuel Crust Characteristics

P/P ₀	δ_{max}, cm	Fraction of Fuel Inventory as Crust		
		FFTF Core	1 Subassembly	7-pin Bundle
1	0.16	0.03	0.18	1.1
0.1	0.5	0.09	0.57	3.5
0.05	0.07	0.13	0.82	5.0
0.1	1.6	0.30	1.82	11.2

potential for pressure driven compaction by including the presence of liquid sodium in the simulated blanket and fission gas plenum regions to verify current analysis[3] and out-of-pile experiments using the thermite method, [21] and 3) boil-up in a "bottled-up" system including measurements of potential pressurization, [12] stability of the crust layer [22] and rate of unplugging to verify current analysis and out-of-pile experiments. [18] The above proposed test matrix should provide adequate resolution (public acceptance) to the recriticality question for LMFBRs and would not appear to require fuel sample sizes exceeding the capability being provided by TREAT Upgrade within the proposed SAREF program. [19,20]

3. Second General Behavior Principle

General agreement now appears to emerge in the technical literature that the contact temperature T_i , determined from

$$\frac{T_i - T_c}{T_H - T_i} = \left(\frac{K_H \rho_H C_H}{K_c \rho_c C_c} \right)^{\frac{1}{2}} \quad (10)$$

exceeds the spontaneous nucleation temperature T_s ,* determined from

$$J = \omega N_L \exp \frac{W}{kT_s} f(\alpha) \quad (11)$$

in all observed energetic large-mass vapor explosions between a hot and a cold fluid (see Fig. 7), where K = thermal conductivity, ρ = density, C = specific

Fig. 7. Requirements for Premixing and Development of Pressure Generation and Large-scale Vapor Explosion. T_i is the interface temperature upon contact and T_s is the spontaneous nucleation temperature of the cold fluid.

heat, subscripts H and c = hot and cold liquids, respectively, N_L = the number of molecules per unit volume of liquid, ω = a constant with a numerical value close to 10^{10} g^{-1} , k = the Boltzmann constant, W = the reversible work of formation of the critical embryo from the liquid, and $f(\alpha)$ = a function of contact angle α . This threshold condition, which has been verified experimentally with a number of liquid-liquid systems [23-27] was discussed by Fauske at the Second CSNI Meeting on Fuel-Coolant Interaction in terms of two requirements: [28]

*As shown at the Beverly Hills Fast Reactor Safety Meeting, [23] this nucleation threshold is dependent on both contact mode and contact time. These effects can be represented by specifying the contact angle α (see Fig. 8). For the molten $\text{UO}_2\text{-Na}$ system, α is believed to have a value between 0 and 90° .

Fig. 8. Illustration that Spontaneous Nucleation is Unlikely to be Reached upon Contact between Molten Oxide Fuel and Liquid Sodium for Temperatures of Interest.

1) spontaneous nucleation allows for film boiling and therefore provides for the necessary initial intermixing of the cold and hot fluids, and 2) also allows for "continuous explosive boiling" to initiate or trigger the large-mass explosion when sufficient liquid-liquid contact and required constraint are established (see Fig. 7).

As seen in Fig. 8, for the $\text{UO}_2\text{-Na}$ system the contact temperature T_c is well below the spontaneous nucleation temperature T_s for temperatures of interest, thereby suggesting that sustained pressure generation with these materials due to lack of the necessary intermixing and trigger is not possible. This observation is consistent with well over 100 interaction tests with LMFBR materials (molten UO_2 , mixed oxide, molten steel and liquid sodium) including various contact modes (fuel injection above and below the liquid sodium surface, fuel dropping, fuel squirting, sodium injection above and below the molten fuel surface, etc.) and simulated accident sequences (loss of flow and overpower transients including prompt burst simulations), and includes tests with molten-fuel inventory in the kilogram range. [29] All of these tests, which include both out-of-pile and at least 50 in-pile varieties, have consistently demonstrated very mild thermal interactions, in agreement with the general behavior principle. Only the *small-scale* laboratory experiments carried out by Armstrong, [30] in which a small jet of liquid sodium was injected into a crucible of molten UO_2 , have produced thermal interactions with

significant above-surface shock waves. The observed interaction was first interpreted by Fauske as follows. [31] When liquid sodium is injected into molten UO_2 , some of the liquid sodium will be entrained and wet the molten UO_2 surface (which in the ideal laboratory experiment can be considered free of gas bubbles). Because of lack of nucleation sites in the liquid-liquid system (subsequent UO_2 freezing is not important if gas is absent), the sodium temperature is raised to the temperature limit corresponding to spontaneous nucleation (see Fig. 9). When

LABORATORY CONTACT MODES $\text{UO}_2\text{-Na}$ SYSTEM

Fig. 9. Illustration of Different Behavior in Idealized Laboratory Experiments with Small Quantities of Molten Fuel and Liquid Sodium: I - Na Injected into UO_2 , II - UO_2 Injected into Na. T_{HOM} = homogeneous nucleation temperature for liquid sodium, $T_{\text{B,Na}}$ = normal boiling temperature for Na at 1 atm.

this temperature is reached, vaporization is rapid enough to produce shock waves. This interpretation implies that in a real reactor system, even a *small-mass* vapor explosion as observed in Armstrong's experiments would be difficult because of the ample supply of pre-existing nucleation sites.

However, while experiments with molten UO_2 and sodium and with other simulant fluids appear to confirm the second general behavior principle, it

should be noted for completeness that it has not been generally accepted in the technical community. For example, Board and Hall[32] claim that spontaneous nucleation cannot be the source of rapid generation of vapor for the explosions (this writer obviously disagrees) and suggest the above criterion may determine "only the initial conditions for the explosion and is not relevant to the process of explosion development" and "it is not possible to rule out large-scale fragmentation explosions if there are any other circumstances which could lead to relevant initial conditions." Hopefully, these fundamental differences as they relate specifically to the role and type of nucleation in the explosion process itself can be clarified by further laboratory experiments and detailed analysis of the type discussed in Ref. 33.

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