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THE FPIN2 Code - An Application of the Finite Element Method to the
Analysis of the Transient Response of Oxide & Metal Fuel Elements

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E. H. B.

Introduction

Heat Transfer

Finite Element Mechanics

Material Behavior

Fuel Cracking

Central Cavity and Plenum

Plenum Model

Verification Problems

Demonstration Problem

Acknowledgement

Reference

The FPIN2 code simulates the thermal-mechanical response of fast reactor fuel pins to transient events. Temperatures of the fuel pin and coolant are calculated using a simple pin-in-a-pipe geometry. The mechanical analysis uses an implicit finite element formulation with linear shape functions which allow for general material behavior in the fuel and cladding including cracking and melting. This formulation provides a very convenient structure for implementing different models and improvements in algorithms. The paper summarizes the FPIN2 methodology and presents results for the transient response of both oxide and metallic fuel pins under similar overpower transients.

INTRODUCTION

1. The FPIN2 computer code has been developed to analyze the complex thermal-mechanical phenomena which govern fuel and cladding behavior during fast reactor accident transients. The analysis provides the fuel pin temperatures, stresses, and displacements under hypothetical TOP, LOF, and LDF-driven-TOP transients. These results aid in the analysis of accident energetics by providing estimates of (1) axial expansion of fuel, (2) time and location of cladding failure, and (3) the condition of fuel at the time of cladding failure. The basic program includes a group of subroutines to calculate the temperature of the fuel, cladding, coolant, and outer wall, a separate group of subroutines to calculate the mechanical behavior of the fuel and cladding, and facilities to couple to other related programs. The heat transfer section in FPIN2 uses an implicit finite difference algorithm and the mechanical analysis makes use of a nonlinear finite-element procedure. A major reason the finite-element method was chosen is because it allows for convenient modular coding of the fuel pin mechanics such that different models for material behavior and improvements in specific algorithms can be implemented easily into the code. The FPIN2 computer program has evolved over the past decade based initially upon the characteristics of oxide fuel. At one time a separate metallic fuel version of FPIN2 was planned, but it was found that the FPIN2 structure is sufficiently robust to easily allow for the additions required to handle metallic fuel.

2. The FPIN2 code simulates a single clad fuel pin which may be a representative of a particular subassembly or may simulate a fuel pin in an actual transient test. Axial symmetry of temperatures and external loads is assumed. The fuel pin is assumed to be cooled by flowing sodium contained in an annular coolant channel. At the start of the

analysis, the fuel pin is assumed to be stress-free with steady-state thermal conditions.

3. In paragraphs 4 through 18 of this paper we outline the models and algorithms in FPIN2 and describe in somewhat more detail the various features of the code that we feel are unique. In general we use iterative methods to solve the nonlinear problems in order to assure ourselves that the inaccuracies in our results are due to the models used and not to errors due to nonconvergent algorithms. A variety of calculations have been performed using the FPIN2 code as an aid to the understanding of small-bundle fuel-pin transient tests, as a check on individual sections of the code, and as a guide in understanding the behavior of proposed fuel pin materials and designs. Starting at paragraph 20 we use FPIN2 to demonstrate some of the differences between the transient response of oxide and metallic fuel.

HEAT TRANSFER

4. Although the primary emphasis in the FPIN2 code is on the mechanical analysis of fuel and cladding, a complete heat transfer analysis is also included. The basic assumption is that heat is conducted only radially in the fuel and cladding and convected axially by the sodium coolant. Two overall options for the temperature calculation are available. In the first, temperatures are calculated for a symmetric system consisting of a single fuel pin surrounded by an annular coolant flow channel with an outer boundary formed by a cylindrical outer wall. In this case, the user must supply the coolant inlet temperature and flow rate as a function of time. For the second option, the user supplies the clad outer surface temperature history and only the fuel and cladding temperatures are calculated. The following comments summarize the major features and approximations in the FPIN2 heat transfer

calculation: (1) Only radial heat conduction is considered in the fuel, cladding, and outer wall. Adiabatic conditions are assumed at the fuel center (or central void surface) and the outer surface of the outer wall is assumed to be in contact with a constant temperature heat sink. (2) The fission energy generation rate is user supplied and can vary axially, radially, and with time. (3) Thermal properties are temperature- and porosity-dependent where appropriate. (4) The effect of the fuel-clad radial gap on the thermal behavior is accounted for by a gap conductance term. (5) The energetics of melting fuel is handled by keeping track of the fraction of each radial element that has melted and incorporating a latent-heat source-sink term into the heat conduction equation. (6) The coolant channel model is very simple, using bulk properties and a uniform channel area.

5. The one-dimensional heat conduction problem for the fuel, cladding, and outer wall is solved using the Crank-Nicholson implicit finite difference method. Since the thermal conductivity and heat capacity are temperature-dependent, the resulting finite difference equations are nonlinear. These equations are solved by a simple iteration in which the material properties are evaluated using the most recently calculated temperatures so that the finite difference equations become linear. Convergence of this direct iteration is very fast for the time steps generally used in FPIN2. The spatial finite difference equations are derived using an energy balance and the concepts of thermal resistance. For each axial segment the equations for the fuel, cladding, coolant, and outer wall temperatures are combined into a system of linearized equations of tridiagonal form. The temperatures for the entire fuel pin and coolant system are calculated by solving the nonlinear equations for each axial segment in turn starting at the coolant inlet and working up to the outlet. No iteration is required between the coolant and cladding temperatures. A simple direct-decomposition procedure is used to solve the tridiagonal set of equation at each axial elevation.

FINITE ELEMENT MECHANICS

6. The mechanical analysis in the FPIN2 code uses the finite-element method based upon a force-displacement formulation. Axial symmetry and generalized plane strain are assumed so that the analysis is essentially one-dimensional. The finite elements are defined by first dividing the fuel and blanket portion of the fuel pin into a number of axial segments (as many as 20) and then further dividing each axial segment radially into a number of rings (as many as 20 in the fuel and up to 10 in the cladding). The elements are assumed to interact only at the radial boundaries or nodes and the displacements $\{\delta\}$ at the nodes are the basic unknowns in the analysis. The displacements within elements are approximated by simple linear

functions of the nodal displacements $\{\delta\}$.

7. The finite-element equilibrium equations are derived from the equations of virtual work. For each axial segment, the equations for both the fuel and cladding elements are assembled into a single set of equations for the unknown radial and axial displacements. In this way, fuel-cladding contact forces come out of the analysis without separate iteration. Simple modifications during equation assembly are used to account for the fuel and cladding being either locked together or free to slip in the axial direction. When fuel melts, the number of radial elements included in the mechanical analysis decreases. In FPIN2 the number of equilibrium equations is kept fixed and the extraneous equations describing molten fuel are replaced by identities with simple modifications to other equations.

8. By using the concept of virtual work the equilibrium equations lead to a force balance at the nodes which can be written symbolically as

$$\{F\}^{\text{in}}(\sigma) = \{F\}^{\text{ext}}, \quad (1)$$

The only nonzero terms in the external force vector $\{F\}^{\text{ext}}$ describe the external loads at the cavity surface, exterior of the cladding and at the top of the fuel and cladding. The internal force vector $\{F\}^{\text{in}}(\sigma)$ depends upon the unknown stress field which must be found to satisfy this equation and the constitutive laws for the materials. Equations (1) are generally nonlinear and various approximations are employed to reduce them to a set of linear equations necessary to calculate the nodal displacements $\{\delta\}$. These linear equations are written in the form

$$[K] \{\delta\} = \{F\} \quad (2)$$

where $[K]$ is the stiffness matrix and the force vector $\{F\}$ includes the external loads, thermal and other initial strains and pseudo-forces involving approximations to the plastic and/or creep strains. Initially we used Hooke's law and forward-time-difference approximations to the plastic strains to derive an "elastic" stiffness matrix. However, when using the iteration scheme described in the next paragraph, convergence to a solution of equation (1) was slow even for very small time steps. We now use an elastic-plastic approximation to derive $[K]$ involving the total derivatives of the constitutive equations. This has speeded up convergence somewhat, but work is still needed in this area.

9. The equilibrium equations (1) are solved iteratively by a sequence of steps that modify the linear equations (2) by making use of residuals. Each axial segment of the fuel pin is calculated separately using the following procedure:

Step 1. Assemble Stiffness Matrix $[K]$ & Initial Value of $\{F\}_0$

Step 2. Solve $[K] \{\delta\}_n = \{F\}_n$ for

new nodal displacements $\{d\}_n$

Step 3. Find $\{\sigma\}_n$ that satisfies the constitutive equations

Step 4. Calculate Residuals to Eqs. (1)

$$\{R\}_n = \{F^{ext}\} - \{F^{in}(\sigma_n)\}$$

Step 5. Update: $\{F\}_{n+1} = \{F\}_n + \{R\}_n$

Step 6. Convergence Test, if not Converged, Return to Step 2

The above procedure has been written in terms of total forces and displacements whereas the FPIN2 codes solve the problem in terms of increments over one time step. The incremental form of the problem allows us to handle large strains by updating the geometry at each time step. The choice of convergence test must balance solution accuracy with computational expense. We have tried various tests and are currently using the root-mean-square norm of the residual force vector.

MATERIAL BEHAVIOR

10. The structure of FPIN2 and the equilibrium equations form and solution procedure is general enough to allow for a wide range of material behavior. Superposition of strains is assumed to hold. The total material strain, $\{\epsilon^t\}$, can be written as a sum of the elastic strain, $\{\epsilon^e\}$, the plastic strain, $\{\epsilon^p\}$, the thermal strain, $\{\epsilon^\theta\}$, and the swelling strain, $\{\epsilon^s\}$:

$$\{\epsilon^t\} = \{\epsilon^e\} + \{\epsilon^p\} + \{\epsilon^\theta\} + \{\epsilon^s\}.$$

Fuel cracking strains are also included but in a special way. This equation and the required constitutive equations enter the analysis in the following way: We first solve the finite-element equilibrium equations for improved estimates of the nodal displacements and from them obtain the increments of total strain. Next, the algorithm described below is used to decompose the increments of total strain into elastic and nonelastic parts such that all constitutive equations are satisfied. Knowing the elastic strains, we can then calculate the stresses and the correction terms for the finite-element equilibrium equations. These steps are repeated until all equations are satisfied to acceptable accuracy.

11. The increments of total strain $\{d\epsilon^t\}$ are known from the solution of the equilibrium equations and the thermal strain increments $\{d\epsilon^\theta\}$ are known from the heat transfer calculations. We must therefore calculate the plastic strain increments $\{d\epsilon^p\}$ and the swelling strain increment $d\epsilon^s$ in order to find the elastic strain increments $\{d\epsilon^e\}$ and stresses $\{\sigma\}$. In order to simplify the following discussion we assume that the only nonelastic strains are thermal and swelling and ignore for the moment cracking and plastic strains. All quantities of interest, such as stresses and

strains, are known at the beginning, t_1 , of a time step and the total strain increments

$\{d\epsilon^t\}_{1+1}$ are known at the end, $t_{1+1} = t_1 + \Delta t$, of the step. The algorithm used to find swelling strains that satisfy the swelling constitutive equation can be broken into the following six steps.

Step 1. Calculate approximations to the elastic strain increments by subtracting the estimated increment in swelling strain from the total strain increments at t_{1+1} :

$$\{d\epsilon^e\}_N = \{d\epsilon^t\}_{1+1} - d\epsilon_{1+1}^\theta \{1\} - d\epsilon_N^s \{1\},$$

where $d\epsilon_{1+1}^\theta$ is the increment in thermal

strain and the brackets $\{ \}$ denote a column vector of three components. Since this procedure is iterative, an iteration counter, N , has been introduced as a subscript. For the starting value ($N = 1$) for the swelling strain increment, we use zero.

Step 2. Use the generalized Hooke's law to calculate the stresses:

$$\{\sigma\}_N = [C] \{\epsilon^e\}_N$$

where $[C]$ is a 3×3 matrix of elastic coefficients and the total elastic strains $\{\epsilon^e\}_N$ are calculated using the equation

$$\{\epsilon^e\}_N = \{\epsilon^e\}_1 + \{d\epsilon^e\}_N.$$

Step 3. Calculate the time rate-of-change of the swelling strain from

$$\dot{\epsilon}_N^s = d\epsilon_N^s / \Delta t.$$

Step 4. Determine the mean stress

$$\sigma_{mN} = (\sigma_{rN} + \sigma_{\theta N} + \sigma_{zN}) / 3$$

and calculate the swelling function

$$g_N(\sigma_{mN}, \epsilon_N^s, \dot{\epsilon}_N^s, \dots)$$

for the chosen swelling model. The algorithm is terminated when g_N is sufficiently close to zero so that the constitutive equation $g=0$ is satisfied to the desired accuracy.

Step 5. Calculate the change in swelling strain increment from

$$\Delta \epsilon_N^s = \frac{g_N}{dg_N / d\epsilon^s}$$

where

$$\frac{dg}{d\epsilon^s} = -3K \frac{\partial g}{\partial \sigma} + \frac{\partial g}{\partial \epsilon^s} + \frac{1}{\Delta t} \frac{\partial g}{\partial \dot{\epsilon}^s}$$

and K is the bulk modulus.

Step 6. Use this estimate of the change in the swelling strain increment to update the swelling strain increment for the time step:

$$d\epsilon_{N+1}^s = d\epsilon_N^s + \Delta \epsilon_N^s.$$

These six steps are repeated until the constitutive equation is satisfied to an acceptable accuracy. When cracking and plasticity are included, the same procedure

is used with additional terms and constitutive equations to be satisfied. For the time steps and constitutive equation used in FPIN2, we find that three or four iterations are sufficient.

FUEL CRACKING

12. The fuel cracking model used in FPIN2 allows cracks in the radial ($\theta = \text{constant}$) and/or transverse ($z = \text{constant}$) planes. To account for the four possible crack patterns, we have defined four types of elements as follows:

Type I: Uncracked fuel,

Type II: Only radial cracks ($\sigma_\theta = -P_g$),

Type III: Only transverse cracks ($\sigma_z = -P_g$),

Type IV: Both radial and transverse cracks.

The type of a particular element is determined by a fracture and crack closure rule. Since all radial elements are treated in the same manner, fuel cracking and crack closure can occur at any point and cracks are not forced to proceed from the outer surface inward.

13. The particular set of cracking rules coded into FPIN2 is the continuous cracking model. If an element is uncracked in, for instance, the θ plane, it is reasonable to assume cracking will occur if the tensile stress σ_θ across that plane exceeds some fracture stress $\sigma_{\theta F}$. At that point, the element type will change (say from Type I to Type II) and a non-zero crack strain ϵ_θ^C will begin to accumulate. Should at some time during the subsequent deformation the crack close ($\epsilon_\theta^C = 0$), the element type would switch back to one which does not have radial cracks (Type I or III). As long as σ_θ is compressive and greater in magnitude than the gas pressure P_g , the element will remain a Type I or Type III element. However, if σ_θ exceeds P_g at some time, the element type will again change and ϵ_θ^C will be non-zero. Obvious modifications can be made to this cracking model to accommodate other crack-related phenomena such as an initial crack opening and/or crack healing. One problem with the model, however, is that by insisting that an entire element crack at once, large unphysical perturbations may be introduced into the mechanical analysis giving unrealistic results and causing difficulties with the convergence of the basic solution algorithm. In reality, of course, stable crack growth will not necessarily penetrate across an entire element of fuel during a single time step. Consider an element with a radial crack only partially across the element at a given time. If the tangential stress in the uncracked part of the element is assumed to have reached the fracture stress $\sigma_{\theta F}$, it is easy to show that the average tangential stress for the element is just

$$\sigma_\theta = \sigma_{\theta F} - (\sigma_{\theta F} + P_g) x / \lambda \quad (3)$$

where λ is the width of the element and x is the penetration distance.

14. Equation (3) suggests a means of defining a continuous cracking model.

Suppose that the crack penetration distance x is assumed to be directly proportional to the average crack opening strain ϵ_θ^C . We can then write

$$\sigma_\theta = \sigma_{\theta F} + (\sigma_{\theta F} + P_g) \epsilon_\theta^C / \epsilon_\theta^C \quad (4)$$

for radial cracks and

$$\sigma_z = \sigma_{zF} + (\sigma_{zF} + P_g) \epsilon_z^C / \epsilon_z^C \quad (5)$$

for transverse cracks. In Eqs. (4) and (5), ϵ_θ^C is a positive constant. The sign change in going from Eq. (3) to (4) is due to the fact that increased penetration leads to crack opening, and crack opening strains are negative by definition in the FPIN2 Code. The continuous cracking laws in Eqs. (4) and (5) produce a stress across the fracture surface of a finite element that varies smoothly from the fracture stress down to the gas pressure as the crack strain increases from zero to ϵ_θ^C . Of course, once ϵ_θ^C or ϵ_z^C exceeds ϵ_θ^C , it is assumed that the stress across the fracture surface remains equal to the gas pressure.

15. It should be realized that no one-dimensional stress analysis can represent the details of the two-dimensional stress field at the tips of actual cracks. What this means for one-dimensional fuel pin analysis is that phenomena such as unstable propagation of startup cracks in the fuel cannot be represented. Fortunately, restructuring of the fuel during steady-state operation will heal startup cracks out to the point where any subsequent cracking would be expected to be stable. If the cracks are sufficiently numerous, it can be shown (ref. 1) that the one-dimensional cracking model given above reasonably approximates two-dimensional cracking calculations. The one-dimensional model therefore can be used to determine stable crack growth.

CENTRAL CAVITY AND PLENUM

16. The calculation of the internal fuel pin pressure makes use of straight-forward volume accounting models of the central cavity and gas plenum. In the case of oxide fuel it is assumed that once the fuel starts to melt, the resultant molten fuel and fission gas mixture is contained within the remaining solid fuel with no connection between the molten cavity and the fission gas plenum. This "gas bottle" model of the molten cavity leads to fairly large internal pin pressures and is based on the following observations: (1) solid fuel and cladding deformation must occur to augment the fuel porosity in response to the 10-20% expansion upon melting, (2) the low fuel thermal conductivity means that the axial profile of the fuel centerline temperature is dominated by the axial power profile with most melting occurring somewhat above the fuel pin center height and (3) oxide fuel pin designs contain insulation pellets, knit mesh plugs, etc., which, along with condensed fission products, effectively block interaction with the

plenum. The situation with metallic fuel pins leads to rather low transient induced internal pin pressures because (1) fuel porosity of up to 25% easily accommodates the fuel volume expansion upon melting of about 3.5%, (2) the high fuel conductivity means that fuel melting begins at or near the top of the fuel column with the axial profile of the fuel centerline temperature closely following the coolant temperature profile and (3) metal fuel pins have a less restrictive flow path for fission gas passage to the plenum. We assume that pressure equilibrium is maintained throughout the length of the molten cavity (and also plenum for metallic fuels) and do not attempt to calculate pressure variations of short time scale.

17. The first level iteration, or outer loop, in the mechanical analysis section of the FPIN2 code is the search for the internal gas pressure. The FPIN2 calculation sequence is such that the temperatures of the cavity materials are known at the new time before the cavity pressure is found. Using these temperatures, the following items can be calculated: (1) cavity boundary changes due to melting, (2) total mass of cavity gas, including gas released upon melting, (3) average temperature of the gas, (4) volume available from porosity released at melting, and (5) fuel vapor pressure at the hottest fuel element. Since we assume that the pressure is not large enough to compress the solid and liquid fuel, the cavity pressure is the sum of the pressure of the gas mixture and the pressure of the fuel vapor. The gas mixture is assumed to obey the ideal gas law. Since the fuel vapor pressure, the mass of gas, and the average temperature of the gas are known, the ideal gas law reduces to an inverse relationship between the volume and pressure. Once the volume available to the gas is determined, the cavity pressure can be found. This volume is determined by the solid fuel displacements, as well as other factors that can be calculated once the temperatures are available. Therefore, in order to find the cavity pressure, two relationships between volume and pressure must be satisfied: the ideal gas law and the relationship between pressure and fuel displacement. During the development of FPIN2, various root finder methods have been used to find the pressure. Difficulties often occur because the method chosen must treat both the case of cold, strong fuel and cladding when large changes in pressure only result in small deformations, and the opposite case when the deformation of hot, weak cladding is very sensitive to cavity pressure. A unique feature of FPIN2 is the very fast and effective method that has been developed. If for a given pressure P , the gas volume using the ideal gas law is V_g and the volume available due to boundary displacements u is called V_v , then the function $f(P) = V_g(P - P_v) - V_v(P)$ will be zero at the desired cavity pressure. The term P_v is the fuel vapor pressure. The Newton

iteration equation, $P_{i+1} = P_i - f(P_i)/f'(P_i)$, where i is the iteration counter, requires the derivative of V_g and V_v with respect to P . The derivative of V_g is easily calculated from the ideal gas law, and the derivative of V_v is found using a finite difference quotient from two calculations of V_v at two values of P . The iteration at a new time is started using a pressure estimate found by linear extrapolation from the two previous time steps.

PLENUM MODEL

18. In the FPIN2 code detailed calculations involve only the fuel column and axial blanket portions of the pin. Early versions of FPIN were constructed for oxide fuel, did not contain a fission gas plenum model and assumed a constant plenum pressure during a transient. For metallic fuel pins we have added a simple volume accounting plenum model consisting of a cladding tube containing sodium and a gas mixture. The model has the following features: (1) Pressure is uniform throughout the plenum. (2) The plenum portion of the fuel pin is assumed to be at a uniform temperature of the coolant at the top of the fuel column. (3) Sodium compressibility is ignored, but thermal expansion of the fill sodium and its expulsion from the fuel-clad gap as the gap closes is accounted for. (4) The deformation of the cladding tube in the plenum region is treated as elastic thin-shell behavior. (5) The molten cavity materials and the plenum materials are accounted for separately so that the extrusion of cavity material into the plenum can be calculated.

VERIFICATION PROBLEMS

19. As part of the development of the FPIN2 code over the past decade, a variety of calculations have been performed. Many small-bundle fuel-pin transient tests in the TREAT reactor have been analyzed with particular emphasis on the time and location of cladding failure. The latest TREAT experiments analyzed are J1, L02, L03, C05, IS-1, IS-2, M2 and the canisters in the P4 experiment in the ETR reactor. As a more fundamental check on the FPIN2 code we have made calculations of simple idealized problems having analytical solutions. While these problems bear little relationship to the complex behavior of a fuel pin, their correct solution by FPIN2 is a necessary prerequisite to more realistic problems.

DEMONSTRATION PROBLEM

20. A subject of current interest is the comparison between oxide and metallic fuels during transients. As a demonstration of the complete capabilities of FPIN2 we calculated the behavior of both a mixed oxide fuel pin and a U-Pu-Zr alloy metallic fuel pin undergoing an overpower transient of 10 s period from normal operating conditions to cladding failure. Both pins are assumed to have the same overall dimensions,

to have experienced medium burnup, and to be operating at the same power and flow conditions.

21. We have summarized in Table 1 below some of the key events in order to contrast the behavior of metallic and oxide fuel. At time zero, prior to the transient, the peak fuel temperature in the metallic fuel is considerably less than that for the oxide and tends to occur at a higher relative axial location X/L because of higher thermal conductivity of the metal. However, relative to their absolute melting points both pins are operating at about 0.7 times their solidus temperature. As the power increases the oxide pin experiences first melting near the midplane at a relative power $P/P_0 = 2.1$ compared to first melting in the metallic fuel at $P/P_0 = 3.2$ near $X/L = 0.75$. Prior to fuel melting there was no significant cladding plastic strain calculated for either pin. In the metallic pin the similarity in fuel and cladding expansion coefficients and temperature histories produced little differential thermal expansion. In the oxide pin, open startup cracks effectively transmitted any differential expansion loading to hotter regions near the center of the pin where fuel could creep into the central void.

22. As the transient continues, fuel melting eventually progresses to the top of the column at $X/L = 1.0$. However, the time between first fuel melting and melting to top of the column is much less rapid for the oxide pin. During this time the pressure in the central cavity of the oxide pin is calculated to increase to about 10 times the plenum pressure. If there were no axial blanket or only short insulator pellets, it would be reasonable to expect that this pressurization could be arrested by axial motion of molten fuel. However, we have assumed for the calculation reported in Table 1 that fuel will freeze in the narrow gaps of the blanket region so that pressurization continues to a maximum of 80 MPa at 11.5s before cladding plastic deformation in the top quarter of the pin acts to relieve the pressure. In contrast, liquid metallic fuel would not be expected to freeze because the sodium outlet temperature is not far below the fuel solidus temperature of 1373K and because liquid fuel and cladding form a low melting point eutectic.

23. The onset of rapid plastic straining in the oxide pin is nearly coincident with the often-used 50% areal melt fraction failure criterion because this is roughly the point where all of the available volume for fuel and fission gas expansion has been consumed in the medium power pin. The calculations presented here used a life fraction failure rule based on independent out-of-reactor cladding burst tests. The fact that the life fraction reached a value of 1.0 near the same time where plastic straining of the cladding is rapid tends to

Table 1

confirm that FPIN2 is correctly calculating the cladding loading. Satisfaction of the life fraction failure criteria in the oxide pin occurred nearly simultaneously over the top 30% of the pin at a power of about 100 KW/m. It should be noted that similar FPIN2 calculations for FFTF driver pins without long axial blankets result in failures that are delayed by 1-2s. Failure of the metallic fuel pin is delayed even longer because of the equilibration of the pressure in the molten fuel with the plenum pressure. However, at about 140 KW/m the interface temperature between the fuel and the cladding has reached a point where rapid eutectic penetration of the cladding increases the cladding stress so that failure follows shortly. The failure location as given in the table is at the very top of the active fuel column where cladding temperatures are highest. These results are in general agreement with the results of the recent TREAT tests on metallic fuel.

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Table 1. Comparison of Transient Event Sequences

Time: s	Peak Power: Kw/m	Metal	Event	Oxide
0.0	29.0	Peak fuel temp. 942K @ X/L =0.75	Peak fuel temp. 2118K @ X/L =0.6	
7.4	61.0	---	First melting @ X/L =0.6	
10.0	79.0	---	Melting @ X/L =1.0	
11.5	91.5	First melting @ X/L = 0.75	Peak cavity press. = 80 MPa	
12.0	96.0	---	50% Areal melt frac. @ X/L = 0.6	
12.0	96.0	---	2% clad strain, X/L > 0.85	
12.0	96.0	Coolant outlet temp. = 1106K	Coolant outlet temp. = 1022K	
12.1	97.0	Melting @ X/L =1.0	Cladding failure X/L < 0.85	
12.8	104.0	---	Cladding failures 0.70 < X/L < 0.85	
15.9	142.2	Rapid eutectic attack @ X/L =1.0	---	
16.0	143.5	Cladding failure @ X/L = 1.0	---	