by T. Sofu, J. M. Kramer,
and J. E. Cahalan

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VALIDATION OF THE INTEGRATED SASSYS/SAS4A-FPIN2 MODEL
FOR THE TRANSIENT ANALYSIS OF METALLIC FUEL ELEMENTS

by

T. Sofu, J. M. Kramer,* and J. E. Cahalan

Reactor Analysis Division
Argonne National Laboratory
9700 South Cass Avenue
Argonne, Illinois 60439

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ABSTRACT

The metal fuel version of the FPIN2 fuel element mechanics model has been incorporated into the SASSYS/SAS4A code system. In this implementation, SASSYS/SAS4A provides the fuel and cladding temperatures, and FPIN2 performs the analysis of fuel and cladding deformation. The FPIN2 results aid in the understanding of accident progression by providing estimates of the axial expansion of fuel, the time and location of cladding failure, and the condition of the fuel at the time of failure. The validation of the integrated SASSYS/SAS4A-FPIN2 model has been performed using the data from in-reactor TREAT tests M5 through M7 for the prototypic binary and ternary fuels of the IFR concept. The integrated model calculations are compared to available experimental data for the six fuel elements in these tests and good agreement is obtained for the key parameters related to transient behavior of the metallic fuel elements.

I. INTRODUCTION

SASSYS and SAS4A computer codes are being developed as part of the Integral Fast Reactor (IFR) Program.[1,2] In general, SAS4A addresses the issues related to severe, core disruptive undercooling and overpower accident sequences. SASSYS, on the other hand, simulates the primary and secondary coolant loops as well as the core, and provides a tool for the analysis of passive safety response mechanisms. Both codes share a common object library that consists of basic thermal, hydraulic, neutronic, and mechanical analysis modules.

Since the fuel element behavior during fast reactor transients can significantly affect the accident energetics, an accurate and detailed model for the mechanical response of the fuel and cladding is needed in safety assessments. During transients that lead to overheating of the fuel elements, plastic straining of the cladding due to internal fission gas pressure and the fuel-cladding mechanical/chemical interactions may result in cladding rupture and release of fission products to the primary coolant. In addition to the fundamental concern for cladding failure, the fuel axial expansion also plays an important role in limiting the severity of transients due to associated negative reactivity potential.

FPIN2 provides mathematical models to simulate the fuel and cladding mechanical response and to predict the fuel element performance over a wide range of transients.[3-6] It has been incorporated into the SASSYS/SAS4A code system for the mechanical analysis of individual fuel elements.[1,7] There are many coupling options provided. Typically, the basic SASSYS/SAS4A boiling/nonboiling heat-transfer modules are used to calculate the fuel element temperatures, and the FPIN2 models are used to perform the analysis of fuel element deformation. The FPIN2 results are mainly used to predict the time and location of cladding failure and to estimate the fuel axial expansion and related reactivity effects.

The integrated SASSYS/SAS4A-FPIN2 model has been validated through comparison of the integrated model results with the data from TREAT tests M5, M6, and M7.[8-10] These in-pile transient overpower tests were performed to study the behavior of the metallic reference fuels of the IFR concept. Five of the six fuel elements studied were fabricated with U-19Pu-10Zr fuel and austenitic D9

cladding. The remaining fuel element contained U-10Zr fuel with ferritic alloy HT9 cladding. The tests were designed to be sufficiently severe to cause fuel melting and cladding failure. The comparisons include the coolant temperatures, margin to cladding failure, fuel melt fractions, cladding plastic strain, and fuel axial expansion.

In this report, the mechanical model of FPIN2 and its implementation in SASSYS/SAS4A code system are summarized, and results of the integrated model validation are presented. A brief description of FPIN2 and the finite-element formulation of the fuel element deformation are provided in Sect. II.A. The SASSYS/SAS4A-FPIN2 coupling methodology is outlined in Sect. II.B. The description of TREAT tests and the integrated model representation of the transients are given Sect. III.A. The pretransient fuel element characterization based on sibling pin information[10] and STARS code calculations[5,6,11] are presented in Sect. III.B. Finally, the comparison of the integrated model results with the data from TREAT tests is provided in Sect. III.C.

II. DESCRIPTION OF THE INTEGRATED MODEL

A brief description of FPIN2, the basis of the finite-element formulation for the analysis of fuel element deformation, and the iterative solution scheme for the nonlinear equation set are outlined in Sect. II.A. Implementation of the FPIN2 models in SASSYS/SAS4A code system is described in Sect. II.B.

A. Brief Description of FPIN2

The FPIN2 fuel element mechanics model has been developed to analyze the thermal-mechanical phenomena that control the fuel element behavior during fast reactor transients. The early versions of FPIN2 were based on the characteristics of oxide and carbide ceramic fuels; however, it has recently been adapted for the analysis of metallic fuels.[3,4] The basic fuel and cladding deformation models considered are the thermal expansions, elastic/plastic/creep deformations, cracking, and melting. For the metallic fuel elements, additional models for the fission gas generation and release, molten fuel cavity formation, gas plenum, and fuel-cladding eutectic alloy formation are provided to complement the mechanics calculation.

Since the primary emphasis in FPIN2 is on the mechanical analysis of the fuel and cladding, temperatures are calculated using a simple model based on a pin-in-a-pipe geometry and single-phase flow. The mechanical model, on the other hand, is based on a rigorous force-displacement formulation. It uses an implicit finite-element method with linear shape functions. The finite-element scheme allows convenient modular coding in which different models for material behavior and improvements in specific algorithms are easily implemented.

1. Basic Stress/Strain/Displacement Relationships

Formulas are derived for a large-strain finite-element analysis of deformation in a cylindrical fuel element under accident conditions. The mechanical analysis basically provides the fuel and cladding stresses, σ , and nodal displacements, δ , for each finite-element. The elements are defined in an (r,z) mesh; however, axial symmetry and generalized plane strain are assumed so that the analysis is essentially one-dimensional. At the beginning of the analysis, the fuel element is assumed to be stress-free with the steady-state thermal conditions.

For the annular finite elements, the strain-displacement relations are given by

$$\underline{\epsilon}^t = \{\epsilon_r^t, \epsilon_\theta^t, \epsilon_z^t\}^T = \left\{ \frac{\partial u}{\partial r}, \frac{u}{r} + \frac{1}{r} \frac{\partial v}{\partial \theta}, \frac{\partial w}{\partial z} \right\}^T \quad (1)$$

where u , v , and w are the displacements in the r , θ , and z coordinate directions. In the axial direction, the generalized plane strain condition requires that

$$w = \psi z \quad (2)$$

where ψ is a constant. "Apparent total strain" for a continuous material is separated from the "crack strain" as

$$\underline{\epsilon}^t = \underline{\epsilon}^a + \underline{\epsilon}^c \quad (3)$$

If the element is continuous, the deformation depends only on r due to the symmetry of temperatures and loads. Existence of cracks, in general, destroys

the symmetry. However, sufficiently large number of cracks are assumed to exist in an element to make the deformation essentially axisymmetric. A continuous cracking model is used in the code in which an element is assumed to crack in the radial direction when the tangential tensile stress exceeds a prescribed fracture stress. The crack penetration distance is assumed to be proportional to the average crack opening strain, producing a smooth stress gradient across the fracture surface. Pre-existing cracks are also handled by specifying initial crack strain. Although a one-dimensional analysis cannot describe all the details of two-dimensional stress field at the tips of the cracks, the model used determines the stable growth of cracks to reasonable accuracy.

Thermo-elastic-plastic behavior is assumed for both the fuel and cladding. Swelling and cracking are allowed only in the fuel. Therefore, in general, the total material strain, $\underline{\epsilon}^t$, is expressed as the superposition of the elastic strain, $\underline{\epsilon}^e$, plastic strain, $\underline{\epsilon}^p$, thermal strain, $\underline{\epsilon}^\theta$, and swelling strain, $\underline{\epsilon}^s$

$$\underline{\epsilon}^t = \underline{\epsilon}^e + \underline{\epsilon}^p + \underline{\epsilon}^\theta + \underline{\epsilon}^s . \quad (4)$$

For the metallic fuels, the thermal strain, $\underline{\epsilon}^\theta$, is described in terms of linear thermal expansion data, $\Delta L/L_0$, which account for the total expansion from room temperature to melting including the expansion at solid-to-solid phase transitions.[12] These data have been approximated in the code as three straight line segments with breaks at the two solid-to-solid phase change temperatures associated with the beginning and ending of the transformation to pure γ phase solid solution.

Isotropic swelling strain, $\underline{\epsilon}^s$, and plastic deformation, $\underline{\epsilon}^p$, are expressed by separate implicit functions of the form

$$f(\sigma, \epsilon, d\epsilon/dt, T) = 0 \quad (5)$$

where T is temperature. These constitutive equations indicate that, in general, the deformation depends on the changes in stress and temperature. Also, a major part of the deformation in the fuel at high temperatures is rate dependent. A nonequilibrium model based on diffusive growth of grain boundary bubbles is used

to describe the fission-gas-induced swelling.* In the model, all retained gas is assumed to be in solution within the fuel or in the form of small bubbles. A certain fraction of this gas (an input parameter) is assumed to be contained in grain boundary bubbles, fixed in number and all of the same initial radius. The bubble growth is assumed to be driven by the excess gas pressure over the sum of mean stress and surface tension restraint.[11] The open porosity is assumed to contain fission gas in equilibrium with the plenum pressure, and only the grain boundary bubbles are assumed to contribute to the swelling of solid fuel.

The equation used for plastic behavior of the metallic fuels is obtained from a review of the data and theory for modeling secondary creep of U-Pu-Zr alloys.[13] It considers diffusion and dislocation effects. Values of the various parameters in the equation are estimated from the knowledge of the structure and the values that these parameters take for similar materials. At low temperatures, the creep deformation of uranium alloys is assumed to be dominated by the deformation of the α uranium matrix. At temperatures between 900 K and 975 K, the transformations depend strongly on the composition. It is assumed that the creep rate in this intermediate temperature range falls between the creep rate of α uranium and solid solution γ phase. Beyond 975 K, the solid phase consists of a solid solution of uranium, plutonium, and zirconium.

The tensile properties of the cladding materials under transient conditions at elevated temperatures are also modeled by using a unified plastic deformation model that includes both rate-independent (classical plasticity) and rate-dependent deformation (creep). The equations used describe the deformation of Type 316 stainless steel, D9, and martensitic-ferritic stainless-steel HT9 cladding over a broad range of temperature and strain-rate under a variety of loading conditions.

2. Finite-element Formulation

The finite-element equilibrium equation for each element is derived from equation of virtual work

*Hot-pressing is also considered in the model as a negative contribution to the total swelling.

$$\int_A [\sigma_r \phi_{r,r} + \frac{\sigma_\theta \phi_r}{r} + \sigma_z \psi] dA = \int_C \sigma_r \phi_r \hat{i}_r \cdot \hat{n} dC + \psi F \quad (6)$$

where A is the area of the intersection between the elements, C is the curve bounding A , $\hat{n}$ is the outward normal to A , and $\hat{i}_r$ is unit vector in the radial direction. F describes the axial force between the segments. A linear approximation is used for the finite-element displacement function

$$\phi_r = u_{in} \frac{r_{out} - r}{\Delta r} + u_{out} \frac{r - r_{in}}{\Delta r} \quad (7)$$

where u_{in} and u_{out} are the radial displacements of the nodes at inner and outer radius of the element, r_{in} and r_{out} . With this, equilibrium equation becomes

$$\int_A [B]^T \underline{\sigma} dA = \left\{ \int_C \frac{r_{out} - r}{\Delta r} \hat{i}_r \cdot \hat{n} dC, \int_C \frac{r - r_{in}}{\Delta r} \hat{i}_r \cdot \hat{n} dC, F \right\}^T \quad (8)$$

where

$$[B] = \frac{1}{\Delta r} \begin{bmatrix} -1 & 1 & 0 \\ (\frac{r_{out}}{r} - 1) & (1 - \frac{r_{in}}{r}) & 0 \\ 0 & 0 & \Delta r \end{bmatrix} \quad (9)$$

When the equations for all elements are assembled together, Eq. (8) can be viewed as the force balance between the internal forces on the left-hand side and the external forces on the right-hand side. The external forces (loads at the cavity boundary and the cladding outer surface) are known functions of time. The internal forces, on the other hand, depend on the unknown stress field that must be found to satisfy the Eq. (8) and the constitutive laws for the material.

Since the constitutive equations are, in general, highly nonlinear, an incremental technique is used to calculate the stresses based on iterations

within each increment. Assuming that all the stresses at time t_i are known, the incremental form of the equilibrium equation at time t_{i+1} becomes

$$\underline{\Delta f}^{\text{int}} = \underline{\Delta f}^{\text{ext}} \quad (10)$$

where

$$\underline{\Delta f}^{\text{int}} = \underline{f}_{i+1}^{\text{int}} - \underline{f}_i^{\text{int}} = \int_{\Lambda} [B]^T \underline{\Delta \sigma} dA \quad (11)$$

$$\underline{\Delta f}^{\text{ext}} = \underline{f}_{i+1}^{\text{ext}} - \underline{f}_i^{\text{ext}} \quad (12)$$

The model approximates stress increments in terms of apparent total strain as

$$\underline{\Delta \sigma} = [C]_i \underline{\Delta \epsilon}^a - \underline{\Delta \sigma}_i^{\text{ps}} \quad (13)$$

where $[C]$ is the elastic modulus and $\underline{\Delta \sigma}^{\text{ps}}$ is the incremental pseudo-stress. $\underline{\Delta \sigma}^{\text{ps}}$ is approximated by considering pseudo-forces involving estimates for thermal, plastic/creep, swelling, and crack strains at the beginning of the time step

$$\underline{\Delta \sigma}_i^{\text{ps}} = [C]_i \left[\frac{d[C]_i^{-1}}{dT} \underline{\sigma} \Delta T + \underline{\Delta \epsilon}^p + \underline{\Delta \epsilon}^{\theta} + \underline{\Delta \epsilon}^s - \underline{\Delta \epsilon}^c \right] \quad (14)$$

Given the estimates of initial strain increments, the pseudo-stress term is treated as constant within a time step and the change in stress is approximated using the known $\underline{\Delta \sigma}^{\text{ps}}$ and unknown $\underline{\Delta \epsilon}^a$. The calculated stress increments are then corrected to satisfy the equilibrium and constitutive equations to a desired accuracy using the iterative algorithm in Sect. 5.

3. Equation for Nodal Displacements

The apparent total strain, $\underline{\Delta \epsilon}^a$, is expressed in terms of displacement increments through the strain-displacement relations

$$\underline{\Delta \epsilon}^a = [B] \underline{\Delta \delta} \quad (15)$$

where $\underline{\Delta\delta}$ is the nodal displacement vector

$$\underline{\Delta\delta} = \{ \Delta u_{in} , \Delta u_{out} , \Delta \psi \}^T . \quad (16)$$

Substituting Eqs. (13) and (15) in Eq. (11), the equilibrium equation becomes

$$[K]_i \underline{\Delta\delta} = \underline{\Delta f}^{ext} + \underline{\Delta f}^{ps}_i \quad (17)$$

where

$$[K]_i = \int_A [B]_i^T [C]_i [B]_i dA \quad (18)$$

$$\underline{\Delta f}^{ps}_i = \int_A [B]_i^T \underline{\Delta\sigma}^{ps}_i dA . \quad (19)$$

Equation (17) is the incremental form of the virtual work (equilibrium) equation. It constitutes a set of linear equations that can be solved for the displacement increments, $\underline{\Delta\delta}$. In the solution scheme, first the stiffness matrix, $[K]$, and initial guesses for the force vectors, $\underline{\Delta f}^{ext}$ and $\underline{\Delta f}^{ps}$, are assembled using available information from the previous time step, and the linear equations are solved for the new nodal displacements, $\underline{\Delta\delta}$. Having $\underline{\Delta\delta}$, Eq. (15) then gives the increment in apparent total strain, $\underline{\Delta\epsilon}^a$.

4. Algorithm for Separating the Components of Total Strain

From calculated apparent total strain, the elastic, plastic, thermal, and swelling strain components can be separated using the following four-step algorithm:

Step 1. Approximations to the elastic strains at the end of time step, t_{i+1} , are calculated by subtracting the thermal strain (using new temperatures), estimated plastic strain, and the estimated swelling strain from the apparent total strain as

$$\underline{\epsilon}_N^e = \underline{\epsilon}_{i+1}^a + \underline{\epsilon}_{i+1}^c - \underline{\epsilon}_{i+1}^{\theta} - \underline{\epsilon}_N^p - \underline{\epsilon}_N^s \quad (20)$$

and Hooke's law is used to calculate the stress field

$$\underline{\sigma}_N = [C] \underline{\epsilon}_N^e. \quad (21)$$

Since the algorithm is iterative, an iteration counter, N , is introduced as subscript.*

Step 2. The time rate-of-change of the plastic and swelling strains are approximated from

$$\frac{d\epsilon_N}{dt} = \frac{\epsilon_N - \epsilon_i}{\Delta t} \quad (22)$$

and successive evaluations of the swelling function and plastic flow condition are performed: $f_N(\sigma_N, \epsilon_N, d\epsilon_N/dt, T_{i+1})$.

Step 3. The increments in plastic and swelling strains are then obtained from

$$\Delta\epsilon_N = -\frac{f_N}{df_N/d\epsilon} \quad (23)$$

with

$$\frac{df}{d\epsilon} = -3M \frac{\partial f}{\partial \sigma} + \frac{\partial f}{\partial \epsilon} + \frac{1}{\Delta t} \frac{\partial f}{\partial \dot{\epsilon}} \quad (24)$$

where M represents the *shear modulus* of the element for the plastic flow function and the *bulk modulus* for the swelling strain function.

*Note that for a continuous material $\epsilon^c=0$. Existence of radial cracks in the element alters the form of $[C]$ due to nonzero tangential crack strain term. It requires modifications to algorithm to determine crack strain first using the known tensile stress at the crack boundary.

Step 4. Finally, the new plastic and swelling strains are calculated from

$$\epsilon_{N+1} = \epsilon_N + \Delta\epsilon_N . \quad (25)$$

Here, the increments are added to the plastic and swelling strains corresponding to the last iterate, rather than to the strains corresponding to the beginning of time step. Iteration continues through Steps 1 through 4 until these increments, which are Newton-Raphson corrections to the strains, are acceptably small. When the algorithm is converged, the elastic, plastic, and swelling components of the strains are known for both continuous and cracked elements at the end of the time step.

5. Iterative Solution of the Equilibrium Equation

Once the elastic component of the increments of total strain is separated, it is used to calculate the stress field throughout the fuel element at time t_{i+1} as discussed above (Step 1). These stresses, in turn, are used in Eq. (11) to calculate the internal force vector. However, since the expression for the stress increment in Eq. (13) is only approximate, the equilibrium equation [Eq. (10)] is not satisfied in general.

In the model, another iterative procedure is used to ensure that the stress estimates satisfy the equilibrium equation to an acceptable degree of accuracy. By defining the residual, or the error, in Eq. (10) as

$$\underline{r} = \underline{\Delta f}_{i+1}^{\text{ext}} - \underline{\Delta f}_{i+1}^{\text{int}} , \quad (26)$$

the successive evaluations of the nodal displacement increments, $\underline{\Delta\delta}$, are calculated from

$$[K]_i \underline{\Delta\delta}_j = \underline{\Delta f}^{\text{ext}} + \underline{\Delta f}_i^{\text{ps}} + \underline{r}_{j-1} \quad (27)$$

where j is the iteration index. At each iteration step, new $\underline{\Delta\delta}$ is used in Eq. (15) to calculate new $\underline{\Delta\epsilon}^a$. From these apparent total strain increments, modified elastic strain increments are separated using the algorithm in Sect. II.A.4 and they, in turn, are used to determine the updated stress field. The new residual

vector, $\underline{r}_j$, is calculated using these stresses and added to the right-hand side of Eq. (27). (Note that the previous residual vector, $\underline{r}_{j-1}$, remains there as well.) The convergence of this iterative scheme is monitored through the norm of the residual vector. If this error is sufficiently small, then the equilibrium equations are assumed to be satisfied and the computation is advanced to the next time step.

When the fuel-cladding gap is open, the fuel and cladding axial displacements are calculated independently. When the fuel and cladding are locked together, the axial strain increments in the fuel are assumed to be equal to the axial strain increments in the cladding. This is handled in mechanics calculation by modifying the terms in the stiffness matrix. Specifically, the finite-element algorithm is solved for the resultant fuel and cladding axial forces by combining the fuel and cladding stiffness matrices in a noniterative procedure. In this procedure, the two equations for the axial displacements are replaced by a single equation for their joint displacement. The redundant equation is then replaced with an identity so that the size of the stiffness matrix and all the associated coding remains the same.

6. Other Metal Fuel Element Models

The first level of iteration, or outer loop, in the mechanical analysis is the search for the internal pin pressure. It is determined using straightforward volume accounting models of the molten fuel cavity and gas plenum using calculated changes in temperature, cavity boundary, mass of the gas and volume available from porosity released after fuel melting, and thermal expansion of the sodium in the plenum. The radial boundary of the cavity for each axial segment is assumed to be at the point where the fuel has reached the solidus temperature. Elements inside the cavity boundary are assumed to be in a hydrostatic state of stress equal to the cavity pressure and are dropped out of stress-strain calculation.

The extrusion of the molten cavity material into the plenum is calculated at the completion of the pressure iteration. It consists of calculating the excess volume of the cavity materials over the volume available in the cavity. Both the radial and axial dimensions of the cavity for each axial

segment are based on the deformed geometry. The materials in the cavity include liquid and solid fuel, and the fission gas that is assumed to obey the ideal gas law. Open porosity is available to the fission gas volume when the fuel reaches the solidus temperature. But closed grain boundary porosity is released in proportion to the melt fraction.

Cladding rupture is predicted in the code by using various life fraction criteria. The TDC-2 life fraction criterion[14] is used for the fuel elements with D9 and Type 316 stainless steel cladding, and the HEDL transient Dorn parameter correlation[15] is used for the fuel elements with HT9 cladding. The fuel adjacency effect in the TDC-2 correlation has been neglected. The life fraction over a time step, dt , is calculated from the rupture time, t_r , for the instantaneous average cladding temperature, T_c , and hoop stress, σ_c , and the failure is predicted to occur when

$$\int_0^{t_r} \frac{1}{t_r(\sigma_c, T_c)} dt = 1 \quad (28)$$

where t_f is the failure time. The location of the failure is then the first cladding segment to reach a life fraction of 1.0.

An additional complication for the metallic fuels is the formation of a low melting point eutectic alloy between the fuel and the cladding that can contribute to fuel element failure during transient overheating events. The primary effect of the liquid eutectic alloy formation on fuel element failure is thinning of the cladding wall. Experimental data documenting this effect comes from Fuel Behavior Test Apparatus (FBTA) tests on segments of irradiated EBR-II fuel elements and from out-of-pile dipping tests of penetration of iron by molten uranium-iron eutectic alloy.[19] The major feature of the data is that the melt rate is very rapid for temperatures above 1353 K. On the other hand, for the transient rate of concern for a typical application, negligible cladding penetration occurs until this "rapid eutectic attack" temperature is reached. The effect of eutectic penetration is included in the model by considering only the unaffected cladding thickness available to carry the load. The hoop stress

in the cladding tendon is calculated by the thin-shell equations consistent with the methodology used in developing the life fraction correlations.

B. SASSYS/SAS4A-FPIN2 Coupling Methodology

A general purpose interface has been designed between SASSYS/SAS4A and FPIN2 to minimize the impacts of coupling on both codes.[1,7] This coupling strategy allows for maintaining the stand-alone capability of the codes and assures that future improvements to FPIN2 are automatically reflected in SASSYS/SAS4A.

Two modes of coupled operations are provided. In the stand-alone mode, FPIN2 reads its own input deck and executes without linking to SASSYS/SAS4A. This mode is provided for direct code verification. It allows FPIN2 to continue its role as a tool for analysis of individual fuel elements. In the interfaced mode, the SASSYS/SAS4A mechanics module DEFORM-5 is excluded and the FPIN2 models are used in the analysis of fuel element deformation. In this mode, FPIN2 input is mainly interpreted from corresponding SASSYS/SAS4A variables. The remainder of the input needed to run FPIN2 is included as part of the SASSYS/SAS4A input.

The SASSYS/SAS4A-FPIN2 interface consists mainly of steady-state and transient FPIN2 driver routines. The steady-state driver performs the setup of FPIN2 for the interfaced calculation and initializes its input from SASSYS/SAS4A data for proper pretransient fuel element characterization. The transient driver routine incorporates the time-advancement scheme and interfaces dynamic variables between the codes. Normally, the FPIN2 heat-transfer routines are bypassed in the interfaced mode and the fuel element temperatures are linked with SASSYS/SAS4A-calculated fuel, cladding, plenum, and cavity temperatures.* To accomplish this bypass, the FPIN2 mechanics/thermal-hydraulics boundary is identified, and routines that are used in heat-transfer calculations are isolated. All the common block variables that are used in the mechanics

*FPIN2 heat-transfer may be included by setting an input flag. This option is provided mainly for debugging and code verification purposes. It utilizes information regarding the heat generation rate and cladding outer surface temperature for each axial segment at each time step as the dynamic boundary condition.

calculations, but altered in one of these heat-transfer routines, are linked with their SASSYS/SAS4A counterparts. The FPIN2 results for stresses and displacements, in turn, are used for the estimates of axial expansion of fuel and associated reactivity effects, time and location of cladding failure, and the condition of the fuel at the time of failure.

The consistency between the initial mesh structures of the two codes in the interfaced mode is accomplished by pulling SASSYS/SAS4A-calculated mesh information into FPIN2 common blocks and forcing FPIN2 to use the same mesh structure. When the FPIN2 heat-transfer is bypassed, however, an interpolation is necessary for the mesh-centered temperatures of the boundary nodes in the fuel and cladding.

Since FPIN2 has been primarily developed for the transient analysis, it lacks models to describe pretransient irradiation features such as fuel restructuring, fission gas retention and fuel-cladding gap narrowing. These pretransient conditions are to be provided as input for the metallic fuels. The irradiated pin geometry, fuel elongation, fission product and porosity distributions, and effect of fast neutron fluence on cladding are typically obtained from relevant in-reactor fuel performance database at a desired burnup, or from fuel performance computer codes such as LIFE-METAL[16] and STARS.[5,6] FPIN2 has the capability to receive input directly from the steady-state fuel behavior code LIFE-METAL. This capability is also extended to the integrated model.

The integrated model also takes advantage of new modeling capabilities in SASSYS/SAS4A on multiple radial zone formation and binary/ternary alloy fuel material property correlations as a function of composition based on IFR Metallic Fuels Handbook data.[12] Additional capabilities include empirical or theoretical burnup correction on fuel thermal conductivity and fission product modification on fuel density. Information on these new capabilities as well as a detailed description of the integrated model along with its usage are provided in SASSYS/SAS4A Version 3.0 code manual.[1]

Although the mechanics model of FPIN2 uses an implicit solution scheme, the coupling between the SASSYS/SAS4A thermal hydraulics and FPIN2 mechanics

calculation is explicit. In the SASSYS/SAS4A code system, a multilevel time-step hierarchy is used in which a main time step is divided into one or more primary loop, heat-transfer, and coolant dynamics time steps. The control over the length of a computational time step is performed using variety of internal and user-specified restrictions. The FPIN2 mechanics calculation is performed at each heat-transfer time step using updated temperatures. Limitations imposed by stability and accuracy requirements assure that heat-transfer time steps are small enough to avoid any problem due to explicit unstable coupling between the thermal and mechanical calculations. The implicit treatment in FPIN2 is somewhat inconsistent with the explicit nature of the SASSYS/SAS4A code system and it results in a notable increase in computation time. However, the capabilities gained by this coupling are often worth the cost.

Since the current data management scheme is limited to single pin calculations, no restart capability is provided yet. Also, the SAS4A in-pin and ex-pin fuel relocation modules, PINACLE and LEVITATE, are not included in this study since the communication between the FPIN2 and these modules is not fully established. Therefore, the analyses performed cover the transient sequences only up to the anticipated failure time.

III. VALIDATION OF THE INTEGRATED MODEL

The validation of the integrated SASSYS/SAS4A-FPIN2 code system for prototypic IFR fuel has been performed using data from TREAT tests. The comparisons are performed to demonstrate that the phenomena describing the fuel element behavior are modeled correctly and that the integrated model can be used for the analysis of fast reactor transients. The description of the tests and the transient characterizations for the integrated model analysis are provided in Sect. III.A. As-irradiated features of the six fuel elements are outlined in Sect. III.B. Finally, validation results are discussed in Sect. III.C.

A. Description of the Tests

The experiments used for the integrated model validation are the TREAT tests M5, M6, and M7. TREAT tests M5 and M6 were the first transient overpower tests of margin to failure and prefailure elongation of the reference ternary

(U-Pu-Zr) alloy fuel of the IFR concept.[8] Test M7 extended the results to a higher burnup (9.8 at.%) ternary fuel and also initiated transient testing of binary (U-Zr) fuel with HT9 cladding.[9] In comparison with other metallic alloy fuels used in earlier TREAT tests,[17] the IFR reference ternary and binary fuels are irradiated at higher temperatures and they have different physical characteristics. Also, the fuel elements in tests M5 through M7 used advanced D9 and HT9 steels for the cladding.

The transients were designed to be sufficiently severe to cause extensive fuel damage (an exponential power rise with 8 s period at full flow), and were terminated at, or near, cladding failure with an overpower of 3.5 to 4.5 times the nominal. Baseline thermal conditions in the test fuel included a peak linear power density of 40 kW/m, an inlet temperature of 630 K, and 150 K coolant temperature rise along the active length of the fuel column. In test M5, the fuel elements were subjected to two transients because the first test was terminated prematurely. In the integrated SASSYS/SAS4A-FPIN2 model analysis, the two M5 transients are treated as separate tests assuming that the effect of first transient on the second is negligible. The two M5 transients are referred to as M5F1 and M5F2 (Final 1 and Final 2) throughout this report.

Each test contained two EBR-II irradiated fuel elements in separate flow tubes within a Mk-IIIC sodium loop with thermocouples mounted on the flow tubes. A description of the six fuel elements studied in TREAT tests M5 through M7 is presented in Table I. Nominal design parameters common to all six fuel elements are presented in Table II and Fig. 1. Flow tube characteristics are also presented in Table III. Similarity of the fuel elements and conditions in these tests allows direct comparison of the results with respect to burnup, fuel composition, and cladding alloy.

For each of the TREAT tests M5 (F1 and F2), M6, and M7, the power coupling factor, axial and radial power density profile, transient power, coolant inlet temperature, and coolant flow rate histories are obtained from the reported experimental data.[10] The selected values for these variables in the integrated model analysis are given in Tables IV through IX.

TABLE I. Description of the Six Fuel Elements in the TREAT Tests M5 Through M7

TREAT Test		Fuel Type	Cladding Type	Peak Burnup (at.%)
M5	Pin 1	U-19Pu-10Zr	D9	0.8
	Pin 2	U-19Pu-10Zr	D9	1.9
M6	Pin 1	U-19Pu-10Zr	D9	1.9
	Pin 2	U-19Pu-10Zr	D9	5.3
M7	Pin 1	U-19Pu-10Zr	D9	9.8
	Pin 2	U-10Zr	HT9	2.9

TABLE II. Common Design Parameters and Dimensions of Fuel Elements in TREAT Tests M5 Through M7[10]

Fuel	Length (m)	0.343
	Diameter of fuel slug (m)	$4.32 \cdot 10^{-3}$
	Mass (kg)	0.078
	Radial smear density -- to cladding inner diameter (%)	72.5
Bond Sodium	Initial height above fuel column (m)	$6.35 \cdot 10^{-3}$
	Total volume (m ³)	$2 \cdot 10^{-6}$
Gas Plenum	Length above sodium (m)	0.246
	Volume (m ³)	$5 \cdot 10^{-6}$
Cladding	Outer diameter (m)	$5.84 \cdot 10^{-3}$
	Inner diameter (m)	$5.08 \cdot 10^{-3}$
	Thickness (m)	$0.38 \cdot 10^{-3}$

Fig. 1. Design Features of the Fuel Elements in TREAT Tests M5, M6, and M7[10]

TABLE III. Characteristics of the Flow Tubes in TREAT Tests M5 Through M7

Fuel element outer diameter (m)		$5.84 \cdot 10^{-3}$
Flow tube inner diameter (m)		$7.90 \cdot 10^{-3}$
Coolant flow area (m ²)		$2.223 \cdot 10^{-5}$
Hydraulic diameter (m)		$2.06 \cdot 10^{-3}$
Coolant exit pressure (Pa)	M5F1	$0.413 \cdot 10^6$
	M5F2	$0.417 \cdot 10^6$
	M6	$0.381 \cdot 10^6$
	M7	$0.450 \cdot 10^6$

TABLE IV. Initial Total Pin Power Corresponding to 0.1 MW Initial Reactor Power for the Fuel Elements in TREAT Tests M5, M6, and M7

Test	Pin	Initial Total Pin Power (W) *
M5	Pin 1	34.5333
	Pin 2	34.2025
M6	Pin 1	34.3914
	Pin 2	33.7983
M7	Pin 1	26.4091
	Pin 2	36.2429

*Based on average power coupling factor.

TABLE V. Axial and Radial Power Density Profile for Test Pins in TREAT Tests M5 Through M7

Normalized Axial Power Profile				Normalized Radial Power Profile*			
				Binary Fuel		Ternary Fuel	
1	0.745	11	1.0	1	0.6232	1	0.4538
2	0.795	12	0.995	2	0.6281	2	0.4834
3	0.845	13	0.985	3	0.6373	3	0.5199
4	0.890	14	0.9725	4	0.6652	4	0.5578
5	0.930	15	0.9575	5	0.7080	5	0.6046
6	0.960	16	0.9325	6	0.7749	6	0.6809
7	0.980	17	0.8975	7	0.8705	7	0.7979
8	0.9925	18	0.855	8	1.0207	8	1.0026
9	0.9975	19	0.805	9	1.2912	9	1.3875
10	1.0	20	0.755	10	1.5868	10	1.9584

*For 10 equal-thickness radial nodes with first and last nodes being half-size.

B. Pretransient Fuel Element Characterization

Prior irradiation of the fast reactor fuel elements significantly influences their thermal and mechanical performance during accident transients. Important features for the metallic fuels are the as-irradiated fuel geometry, fission product-alloy-porosity distributions, fission gas retention, and the effects of fast neutron fluence on cladding properties. In this analysis, the pretransient fuel element characterization is based on sibling pin information[10] and STARS code calculations.[11]

1. Geometry and Nodal Structure

The fuel elements are modeled by considering 20 equal-length axial segments with each segment containing 10 equal-thickness radial elements in the

TABLE VI. Power Histories for TREAT Tests M5 (Final 1 and Final 2), M6, and M7

M5F1		M5F2		M6		M7	
Time (s)	Power (MW)	Time (s)	Power (MW)	Time (s)	Power (MW)	Time (s)	Power (MW)
0.0	0.1	0.0	0.1	0.0	0.1	0.0	0.1
3.5	0.6	4.0	1.2	3.6	0.92	7.0	1.03
4.0	2.7	4.5	6.2	4.2	6.17	7.8	9.40
4.5	14.1	5.0	27.9	4.75	32.02	8.2	28.6
5.0	38.3	5.4	38.7	5.0	61.45	8.4	48.2
5.5	42.6	6.0	40.7	5.5	74.04	8.6	57.0
7.5	54.3	7.0	46.3	6.0	79.07	9.0	65.5
9.5	69.9	8.0	52.5	7.0	89.43	10.0	76.2
10.5	78.9	9.0	59.5	8.0	100.97	11.0	85.9
11.5	87.8	10.0	68.2	9.0	114.55	12.0	98.5
13.5	114.4	11.0	77.1	10.0	128.21	13.0	112.3
14.5	130.9	12.0	85.4	10.5	136.53	14.0	126.6
15.5	158.2	13.0	97.8	11.5	158.59	15.0	149.9
15.525	158.24	14.0	112.1	12.5	182.47	16.0	172.3
15.575	143.83	15.0	128.4	13.0	196.38	17.0	199.3
15.625	89.56	15.6	143.0	13.25	203.05	17.5	212.8
15.675	45.99	16.125	157.8	13.35	119.53	17.725	218.9
16.025	13.84	16.226	92.0	13.45	37.55	17.75	213.3
		16.95	10.0	14.0	23.52	17.8	177.2
		20.0	4.6	20.0	7.15	18.0	27.3

TABLE VII. Coolant Inlet Temperature History for TREAT Tests M5 (Final 1 and Final 2), M6, and M7

Test	Flow Tube 1		Flow Tube 2	
	Time (s)	Temperature (K)	Time (s)	Temperature (K)
M5F1	0	573.5	0	579.0
	4	573.5	4	578.0
	15	581.0	15	594.0
	18	577.5	18	581.0
	22	581.0	22	585.0
M5F2	0	579.0	0	581.0
	5	579.0	4	581.0
	16	587.0	16	603.0
	20	581.0	17	586.0
			20	584.0
M6	0	582.3	0	583.5
	4	582.3	4	583.5
	13	590.5	14	593.0
	16	585.0	17	587.0
	20	587.0	20	588.0
M7	0	586.0	0	587.0
	8	586.0	8	587.0
	18	596.0	18	596.5
	20	596.0	20	591.0

TABLE VIII. Steady-state Inlet Flow Rate for TREAT Tests M5 (Final 1 and Final 2), M6, and M7

Test	Pin	Volumetric Flow (m ³ /s)	Inlet Temperature (K)	Mass Flow Rate (kg/s)
M5F1	Pin 1	100.0·10 ⁻⁶	573.5	87.8·10 ⁻³
	Pin 2	100.0·10 ⁻⁶	579.0	87.7·10 ⁻³
M5F2	Pin 1	77.5·10 ⁻⁶	579.0	67.9·10 ⁻³
	Pin 2	77.5·10 ⁻⁶	581.0	67.9·10 ⁻³
M6	Pin 1	96.25·10 ⁻⁶	582.3	84.3·10 ⁻³
	Pin 2	96.25·10 ⁻⁶	583.5	84.3·10 ⁻³
M7	Pin 1	88.50·10 ⁻⁶	586.0	77.5·10 ⁻³
	Pin 2	102.25·10 ⁻⁶	587.0	89.5·10 ⁻³

fuel and three in the cladding.* The mechanics calculation is initiated from a stress-free state for the hot and irradiated fuel elements in which the swollen fuel is assumed to be in contact with the cladding. As-irradiated dimensions are shown in Table X.

2. Porosity Distribution

The magnitude and distribution of the total porosity do not seem to play a large role in the analysis of TREAT tests; therefore, a uniform fractional porosity distribution is assumed in the fuel. Since the fuel mass is accurately known,[10] the porosity value is determined from the as-irradiated fuel geometry and fuel/fission-product densities to obtain the correct fuel mass (Table XI). The difference between the total porosity and the grain boundary bubble porosity is equal to the porosity of the large pores that are interconnected and free of surface tension restraint (open porosity). Fission gas residing in the open porosity after steady-state irradiation is calculated from the local open pore volume and temperature assuming that the pore is in pressure equilibrium with the plenum.

*In the fuel and cladding, first and last radial nodes are half-size.

TABLE IX. Normalized Coolant Driving Pressure Histories for TREAT Tests M5 (Final 1 and Final 2), M6, and M7

Test	Time (s)	Normalized Pump Head*	
		Pin 1	Pin 2
M5F1	0	1.0	1.0
	5	1.0	1.0
	12	1.1027	1.1027
	16	1.1955	1.1955
M5F2	0	1.0	1.0
	6	1.0	1.0
	8	1.0148	1.0148
	13	1.0568	1.0568
	17	1.1166	1.1166
M6	0	1.0	1.0
	5	1.0	1.0
	11	1.08	1.075
	14	1.15	1.145
M7	0	1.0	1.0
	8	1.0	1.0
	12	1.0483	1.035
	15	1.043	1.052
	18	1.004	1.08

*Formulated to mimic the reported coolant inlet flow histories.[10]

TABLE X. As-irradiated Initial Dimensions of the Six Fuel Elements in TREAT Tests M5 Through M7

	M5		M6		M7	
	Pin 1	Pin 2	Pin 1	Pin 2	Pin 1	Pin 2
Fuel column length (m)	0.351	0.348	0.347	0.347	0.358	0.373
Fuel outer radius (m)	$2.54 \cdot 10^{-3}$	$2.54 \cdot 10^{-3}$	$2.54 \cdot 10^{-3}$	$2.54 \cdot 10^{-3}$	$2.54 \cdot 10^{-3}$	2.5410^{-3}
Cladding inner radius (m)	$2.54 \cdot 10^{-3}$	$2.54 \cdot 10^{-3}$	$2.54 \cdot 10^{-3}$	$2.54 \cdot 10^{-3}$	$2.54 \cdot 10^{-3}$	2.5410^{-3}
Cladding outer radius (m)	$2.92 \cdot 10^{-3}$	$2.92 \cdot 10^{-3}$	$2.92 \cdot 10^{-3}$	$2.92 \cdot 10^{-3}$	$2.92 \cdot 10^{-3}$	2.9210^{-3}
Length of plenum (m)	0.267	0.260	0.261	0.264	0.268	0.282
Length of sodium in plenum (m)	0.0988	0.0730	0.0734	0.0749	0.0705	0.0532

TABLE XI. As-irradiated Fuel Porosity and Related Variables for the Six Fuel Elements in TREAT Tests M5 through M7

	M5		M6		M7	
	Pin 1	Pin 2	Pin 1	Pin 2	Pin 1	Pin 2
Total fuel mass (kg)	0.07726	0.07801	0.07765	0.07662	0.07680	0.0788
Density at room temp* ($\text{kg}/\text{m}^3 \times 10^3$)	15.22	15.17	15.07	14.44	13.31	15.04
As-irradiated fuel porosity (%)	28.6	27.1	26.7	24.6	20.5	30.7
Sodium logging (% of porosity)	0	30	30	35	42	42

*After burnup correction to as-fabricated densities.

3. Fission Gas Distribution

The distribution of fission gas retained in the fuel matrix and the fraction of it contained in grain boundary bubbles are calculated separately with the STARS code as a function of burnup[11] and specified as input in the model (Table XII). The STARS code results give a detailed self-consistent picture of the distribution of the gas between the fuel matrix, grain boundaries, edge tunnels, large pores, and the plenum. Analyses performed shows that the fraction of the retained gas on the grain boundaries increases with burnup as more gas is released and as the plenum pressure becomes significant compared to surface tension constraint on the grain boundary bubbles.

The retained gas is the driving force for expansion of the fuel during transient overheating. This expansion is an important mechanism for the metallic fuels since it provides negative reactivity feedback that limits the severity of transients. The amount of gas in the pin plenum is also important since the plenum pressure is a major contributor to cladding loading and, therefore, cladding failure. The materials in the pin plenum include sodium and a mixture of fill (He) and fission gas. The sodium in the plenum includes the fill sodium and sodium expelled from the fuel-cladding gap less the sodium logged in fuel open porosity. The effective molecular weight of the fission gas atoms is assumed to be 126.55.

4. Alloy Redistribution

Irradiation of the metallic IFR fuel often results in the radial alloy redistribution which, along with the radial temperature gradients, produces distinct zones with different metallurgical phases. Typically, a three-ring structure is observed for the ternary fuels with the intermediate ring being depleted in zirconium and the inner and outer rings being enriched in zirconium. The weight fraction of plutonium remains nearly uniform. This constituent migration affects the radial power density profile and fuel material properties such as thermal conductivity, solidus, and liquidus temperatures.

TABLE XII. Parameters Related to Fission Gas Distribution for the Fuel Elements in TREAT Tests M5, M6, and M7

	M5		M6		M7	
	Pin 1	Pin 2	Pin 1	Pin 2	Pin 1	Pin 2
Peak axial burnup (at.%)	0.8	1.9	1.9	5.3	9.8	2.9
ROGSPI* (kg/m ³ -at.%)	4.809	3.143	3.143	2.581	3.239	2.836
Fraction of retained gas on grain boundaries (%)	15	29	29	53	69	33
Initial plenum pressure at reference temp (MPa)	0.269	0.648	0.648	2.29	4.87	1.41

*Mass of fission gas generated in the fuel pin per unit volume and percent burnup.

The representative alloy distribution chosen to characterize the irradiated ternary alloy fuels in tests M5 through M7 is presented in Table XIII. The fuel solidus and liquidus temperatures and material thermal-mechanical properties for each zone are calculated internally in the integrated model using given weight fractions. The binary fuel in test M7 is modeled with a single radial zone since studies do not indicate significant interdiffusion of zirconium in U-10Zr fuel for burnups up to 5.5 at.%.

TABLE XIII. Zone Structures Representative of Irradiated U-19Pu-10Zr Fuel Pins in TREAT Tests M5 Through M7

	Inner Zone	Intermediate Zone	Outer Zone
Innermost element	1	4	7
Outermost element	3	6	10
Zirconium weight fraction (wt.%)	15.0	5.0	11.7
Plutonium weight fraction (wt.%)	19.0	19.0	19.0
Uranium weight fraction (wt.%)	66.0	76.0	69.3

5. Cladding Fluence and Hardness

The axial distribution of the cladding fluence, needed in life fraction correlation evaluations, is determined from the burnup, fast-flux to linear-power ratio, and axial power profile. The peak cladding fluence values are presented in Table XIV. Also shown in Table XIV is the cladding hardness parameter that is used to describe the plastic deformation of the cladding for the known initial cold work and irradiation history. The value of this parameter was determined from supplementary calculations that considered irradiation hardening, annealing, and grain growth.[18]

C. Comparison of Integrated Model and Test Results

The results of the integrated SASSYS/SAS4A-FPIN2 model analysis of TREAT tests M5, M6, and M7 are compared to available experimental information on the cladding failure time and location, flow tube temperatures, fuel melt fraction, extrusion of molten fuel into the plenum, and cladding plastic strain.

1. Failure Location and Times

Available information on temperatures, stresses, and strains from the integrated model analysis are used to estimate the location and times of cladding rupture using a variety of failure criteria. A comparison of measured and calculated cladding failure times for six fuel elements in TREAT tests M5 through M7 are presented in Table XV. The integrated model analysis results indicate no cladding failure for M5F1 and M5F2 transients, in agreement with the experiments. In tests M6 and M7, the cladding failure was observed for the high burnup fuel elements after which the power excursion was abruptly terminated. In the analysis, these fuel elements are predicted to fail at a time very close to the measured failure time. However, the integrated model predicted a failure for the low burnup binary fuel with HT9 cladding in test M7 also which was not observed during the experiment. For all cases, the estimated failure location was the top of the fuel column, in agreement with the experimental observations.

TABLE XIV. Peak Cladding Fluence and Hardness Parameter as a Function of Burnup in TREAT Tests M5 Through M7

TREAT Tests		Burnup (at.%)	Peak Fluence n/m^2	Hardness parameter at 800 K
M5	Pin 1	0.8	$0.7 \cdot 10^{26}$	0.0
	Pin 2	1.9	$1.7 \cdot 10^{26}$	0.223
M6	Pin 1	1.9	$1.7 \cdot 10^{26}$	0.223
	Pin 2	5.3	$4.8 \cdot 10^{26}$	0.223
M7	Pin 1	9.8	$8.8 \cdot 10^{26}$	0.223
	Pin 2	2.9	$2.6 \cdot 10^{26}$	N/A

TABLE XV. Comparison of Measured and Calculated Cladding Failure Times and Locations for the Six Fuel Elements in TREAT Tests M5 Through M7

TREAT Test		Burnup (at.%)	Measured Failure		Calculated Failure	
			Time (s)	Location	Time (s)	Location
M5F1	Pin 1	0.8	No Failure		No Failure	
	Pin 2	1.9	No Failure		No Failure	
M5F2	Pin 1	0.8	No Failure		No Failure	
	Pin 2	1.9	No Failure		No Failure	
M6	Pin 1	1.9	No Failure		No Failure	
	Pin 2	5.3	13.24	Top of fuel column	13.27	Top axial segment
M7	Pin 1	9.8	17.72	Top of fuel column	17.64	Top axial segment
	Pin 2	2.9	No Failure		17.34	Top axial segment

The integrated model analysis indicates that observed failure of the high burnup fuel element in test M7 is primarily due to pressure loading and resulting plastic strain in the cladding. For the high burnup fuel element in test M6, in addition to plastic strain in cladding, eutectic formation also appears to be partially responsible for the observed failure. The predicted failure of the low burnup fuel element in test M7, on the other hand, is mainly due to excessive eutectic attack.

The correlation used in the model suggests a rapid eutectic penetration for temperatures in excess of 1353 K.[19] The measured flow tube temperatures of nearly 1300 K almost certainly require that cladding inner surface temperatures exceeded this limit during the tests. Therefore, the lack of agreement on failure for this fuel element suggests that the eutectic penetration correlation does not accurately depict the behavior of the binary fuel with HT9 cladding. Indeed, a supplemental analysis that assumes no acceleration in eutectic penetration rate does not predict a failure for this test, in agreement with the experiment.

2. Flow Tube Temperatures

Measured flow tube temperatures along the fuel column have been compared to calculated coolant temperatures from the integrated SASSYS/SAS4A-FPIN2 model. In each of the TREAT tests, thermocouples were mounted on the flow tubes with the attachment points relative to the as-fabricated fuel column height as given in Table XVI.

Comparisons of the measured and calculated flow tube temperatures in tests M5 through M7 are shown in Figs. 2 through 9. The efficient heat-transfer in metal fuel systems causes flow tube temperatures to closely follow the applied fission power with time delays less than a second. The sudden jumps in thermocouple readings at the end of transients for the failed pins in tests M6 and M7 mark the cladding failure. The calculated sodium temperatures near the bottom and the top of the fuel column are determined by neglecting the temperature rise beyond the active length of the fuel column. In general, good agreement is obtained especially near the top of the fuel column. The agreement between the measured and calculated temperatures near the outlet is partially

because the temperature measurements were used by the experimenters to fine tune the reported power coupling factors. The larger discrepancies for the other thermocouple results along the fuel column are attributed to unaccounted thermal mixing delays for the sodium in flow tube and the uncertainties in the reported axial power density profile that comes from auxiliary reactor physics calculations.

TABLE XVI. Axial Location of the Thermocouples Within the Flow Tubes Relative to the Length of the Fuel Column

Tests M5 and M6			Test M7		
Z/L	Thermocouple ID		Z/L	Thermocouple ID	
	Flow Tube 1	Flow Tube 2		Flow Tube 1	Flow Tube 2
-0.04	TC8	TC1	-0.11	TC9	TC1
0.50	TC9	TC2	0.26	---	TC2
0.78	TC10	TC3	0.48	TC10	TC3
0.93	TC11	TC4	0.85	TC11	TC4
1.07	TC12	TC5	1.07	TC12	TC5

The oscillations in the thermocouple readings in Figs. 2 through 9 indicate flow vibrations due to resonant movement of the fuel elements within the flow tubes. These deviations in flow geometry introduce uncertainties to calculated results since they were not accounted for in the thermal hydraulic model. Therefore, a separate sensitivity analysis has been performed to assess the effects of this and other uncertainties such as in the reported power coupling factors.[10] The analysis reveals that the results for the high burnup fuel element in test M6 are somewhat sensitive to changes in flow rate. No failure is predicted for that fuel element when the inlet flow is slightly increased. The uncertainties in the flow rate or power coupling factor, however, do not justify the calculated low-burnup fuel element failure prediction in test M7.

Fig. 2. Measured and Integrated Model Calculated Flow Tube Temperatures for Pin 1 in TREAT Test M5, First Transient (F1). Data points and dashed lines are thermocouple readings, solid lines are calculated temperatures.

Fig. 3. Measured and Integrated Model Calculated Flow Tube Temperatures for Pin 2 in TREAT Test M5, First Transient (F1). Data points and dashed lines are thermocouple readings, solid lines are calculated temperatures.

Fig. 4. Measured and Integrated Model Calculated Flow Tube Temperatures for Pin 1 in TREAT Test M5, Second Transient (F2). Data points and dashed lines are thermocouple readings, solid lines are calculated temperatures.

Fig. 5. Measured and Integrated Model Calculated Flow Tube Temperatures for Pin 2 in TREAT Test M5, Second Transient (F2). Data points and dashed lines are thermocouple readings, solid lines are calculated temperatures.

Fig. 6. Measured and Integrated Model Calculated Flow Tube Temperatures for Pin 1 in TREAT Test M6. Data points and dashed lines are thermocouple readings, solid lines are calculated temperatures.

Fig. 7. Measured and Integrated Model Calculated Flow Tube Temperatures for Pin 2 in TREAT Test M6. Data points and dashed lines are thermocouple readings, solid lines are calculated temperatures.

Fig. 8. Measured and Integrated Model Calculated Flow Tube Temperatures for Pin 1 in TREAT Test M7. Data points and dashed lines are thermocouple readings, solid lines are calculated temperatures.

Fig. 9. Measured and Integrated Model Calculated Flow Tube Temperatures for Pin 2 in TREAT Test M7. Data points and dashed lines are thermocouple readings, solid lines are calculated temperatures.

3. Fuel Melt Fractions and Effects of Constituent Migration

All previous analyses performed with SASSYS/SAS4A code system have assumed uniform fuel compositions in the metallic fuel pins.[20] The recent improvements in SASSYS/SAS4A include a provision for the user to supply radial and axial variations in the fuel constituent concentrations. The distributions are used in the fuel material property routines to determine variations in properties (such as the fuel solidus/liquidus temperatures and thermal conductivity) with alloy content. This work has been the first application in which the effects of fuel composition variations are analyzed.

The effects of the alloy redistribution on the fuel melt fraction have been demonstrated by comparing the results from a single and a three-radial-zone models for the failed fuel element in test M7 (Fig. 10). As shown in Fig. 10, due to substantial zirconium migration from the middle to the central and outer regions, the initial melting of the high burnup ternary fuel does not occur at the fuel centerline. Posttest examination of unfailed pins reveals the existence of unmelted regions near the fuel centerline and supports the integrated model results.[10] Since the boundary of the molten cavity is determined in the model by the outermost radial element that has reached the solidus temperature, solid elements inside the cavity boundary are assumed to be in a hydrostatic state of stress equal to the cavity pressure.

The calculated peak melt fractions are in good agreement with the posttest microstructural examinations of the unfailed pins. The calculated melt profiles at peak power and, for unfailed pins, measured maximal once-molten area fraction at several axial locations are presented in Figs. 11 through 18. In these figures, unmelted regions near the fuel centerline are included as part of the cavity. Agreement between measured and calculated extent of melting provides a qualitative validation of the fuel thermal conductivity correlations and the empirical burnup correction for the irradiated fuel elements.

4. Fuel Axial Expansion and Molten Fuel Extrusion to Plenum

The transient fuel motion and relocation in the TREAT tests were measured using a fast neutron hodoscope. Posttest static scans were also

Fig. 10. Effects of Alloy Redistribution on Fuel Melt Fraction for Top Axial Segment of Failed Pin in Test M7: (a) single radial zone model, (b) three radial zone model

Fig. 11. Calculated Molten Fuel Cavity Boundary at Peak Power for Pin 1 in TREAT Test M5F1

Fig. 12. Calculated Molten Fuel Cavity Boundary at Peak Power for Pin 2 in TREAT Test M5F1

Fig. 13. Comparison of Measured and Calculated Molten Fuel Cavity Boundary at Peak Power for Pin 1 in TREAT Test M5F2

Fig. 14. Comparison of Measured and Calculated Molten Fuel Cavity Boundary at Peak Power for Pin 2 in TREAT Test M5F2

Fig. 15. Comparison of Measured and Calculated Molten Fuel Cavity Boundary at Peak Power for Pin 1 in TREAT Test M6

Fig. 16. Calculated Molten Fuel Cavity Boundary at Peak Power for Pin 2 in TREAT Test M6

Fig. 17. Calculated Molten Fuel Cavity Boundary at Peak Power for Pin 1 in TREAT Test M7

Fig. 18. Comparison of Measured and Calculated Molten Fuel Cavity Boundary at Peak Power for Pin 2 in TREAT Test M7

performed. A comparison of the measured and calculated fuel axial expansion for the fuel pins in TREAT tests M5 through M7 is shown in Table XVII. Maximum axial expansions in Table XVII are the total prefailure elongations (axial displacement plus molten fuel extrusion to the pin plenum) relative to as-irradiated test fuel height. Since the fuel density is nonuniform near the fuel top, measured values include large uncertainties.

TABLE XVII. Maximum Axial Expansion of the Fuel

Test		Maximum axial expansion (%)	
		Experimental[10]	Calculated
M5F1	Pin 1	1	4.8
	Pin 2	0.5	4.6
M5F2	Pin 1	1	6.7
	Pin 2	2	5.8
M6	Pin 1	2 to 3	9.1
	Pin 2	3	6.2
M7	Pin 1	3	3.7
	Pin 2	2 to 4	4.1*

*Pin failed before peak power is attained.

Both the integrated model analysis and the hodoscope measurements indicate that a significant contribution to axial expansion comes from the molten fuel extrusion to the pin plenum. Rather large discrepancies between the measured and calculated axial expansion data appear to be mainly due to the differences in the amount of molten fuel extruded. The fuel extrusion is primarily because of retained gas bubble swelling and coalescence upon initiation of melting. Posttest examination of the unfailed elements reveals the existence of large bubbles that are not typical of normal as-irradiated fuel and supports this analysis.[10]

A number of factors could contribute to difference between the measured and calculated molten fuel extrusion. Among these are the retained fission gas data, radial fuel composition variations, material thermal

properties, and porosity distributions used to characterize the as-irradiated fuel pins. A supplementary analysis indicates that the axial expansion is particularly sensitive to the variations in fuel thermal conductivity since 1) the resulting changes in radial temperature profile affect the amount of molten fuel and, therefore, the amount fission gas released upon melting, and 2) the temperature difference also reflects upon the cavity gas temperature which, in turn, influences the swelling rate.

Irradiated fuel thermal properties are not well known. In SASSYS/SAS4A, quadratic functions of temperature are used to interpolate thermal conductivity of a desired metallic binary and ternary alloy composition.[1] The conductivity during and after melting is assumed to remain constant and equal to the value at the solidus temperature due to lack of data in that range. An empirical or theoretical burnup correction is employed to account for the irradiation and sodium logging effects. Considering all the uncertainties in the magnitude of conductivity at elevated temperatures and the related effects of irradiation and sodium logging, the discrepancies seen in Table XVII are not regarded as hindering. Indeed, using the built-in FPIN2 correlation, which predicts a 20% larger conductivity that increases linearly beyond solidus temperature, reduces the calculated axial expansion by about 40%.

In all the TREAT tests analyzed, initial fuel melting is estimated to be below the fuel-plenum interface; therefore, the molten fuel zone does not have direct access to the plenum when melting begins. The resulting temporary imbalance between the cavity and plenum pressure is important in determining the amount and timing of molten fuel extrusion. As a typical example, the calculated time dependent axial expansion for the failed element in test M7 is shown in Fig. 19. Basically, extrusion is postulated to occur when the retained fission gas in molten fuel expands until its pressure equals that of the plenum after the plenum-cavity link is established. Fast neutron hodoscope measurements support this assumption and indicate a rapid expansion of the fuel coinciding with significant fuel melting. Since the initial extrusion rate is quite high, small variations in the timing of extrusion initiation would have significant impacts on axial expansion. Therefore, employing more restrained criteria for fuel mobility (in terms of fraction of heat of fusion absorbed) and extrusion threshold such as those used in SAS4A in-pin prefailure molten fuel relocation

Fig. 19. Calculated Fuel Axial Expansion in TREAT Test M7 for Pin 1

module, PINACLE, has the potential to improve the axial fuel expansion comparisons.

Although important parameters for expansion of molten fuel, the variations in thermal conductivity and timing of extrusion initiation do not seem to play an important role in margin-to-failure assessments. Using the FPIN2 thermal conductivity correlation increases the margin to failure slightly, causing only minor modifications in Table XV for the calculated failure times.

5. Cladding Plastic Strain

Contact and laser profilometry performed on the unfailed fuel elements in TREAT tests M5 through M7 showed essentially no diameter change (less than 1 mill) for all fuel elements studied.[10,11] The calculated peak cladding plastic hoop strain for the fuel elements in TREAT tests M5 through M7 is given in Table XVIII. The table shows that the calculated strains in the unfailed

TABLE XVIII. Calculated Peak Plastic Hoop Strain in Cladding at Radial Midpoint

Test		$\Delta D/D$ (%)
M5F1	Pin 1	$4.5 \cdot 10^{-4}$
	Pin 2	$7.6 \cdot 10^{-4}$
M5F2	Pin 1	$2.2 \cdot 10^{-3}$
	Pin 2	$7.7 \cdot 10^{-3}$
M6	Pin 1	$6.6 \cdot 10^{-3}$
	Pin 2*	$7.3 \cdot 10^{-1}$
M7	Pin 1*	$5.7 \cdot 10^{-1}$
	Pin 2	----

*Just before failure.

D9-clad elements are all negligible ($\Delta D/D < 0.006\%$) in agreement with measurements. In all cases, the peak permanent strain occurred at the top of the fuel column (top axial segment). On the other hand, the analysis indicates that observed failures of the high burnup fuel elements in tests M6 and M7 are due to accelerated yielding of the cladding at the elevated temperatures although the applied stress is not maximum at the time of the failure.

As an illustrative example, cladding average hoop stress and cladding plastic strain at radial midpoint for the top axial segment of the high burnup fuel element in test M7 are shown in Figs. 20 and 21. The discontinuities in the hoop stress curve (Fig. 20) are due to the cladding axial constraint model. At each axial segment, the fuel that is in contact with the cladding is assumed to be locked to the cladding, either by metallurgical bonding or by friction, at temperatures below the eutectic alloy melting threshold. At temperatures above the threshold, the fuel and cladding are assumed to slip freely because of the liquid phase that forms at the fuel cladding interface. Therefore, at the eutectic alloy melting temperature, there is a sudden drop in stress as the fuel expands slightly in the axial direction, and contracts in the radial direction due to elastic recovery.

Fig. 20. Calculated Cladding Average Hoop Stress for Top Axial Segment in TREAT Test M7 for Pin 1

Fig. 21. Calculated Cladding Plastic Strain for Top Axial Segment in TREAT Test M7 for Pin 1

One exception in cladding plastic strain comparisons is the low burnup fuel element with HT9 cladding in test M7 for which the integrated model calculations predict a measurable increase in cladding diameter. However, this is primarily due to the unverified eutectic thinning of the cladding at elevated temperatures. Therefore, the results for this fuel element cannot be used as validation basis for the cladding plastic deformation model.

IV. SUMMARY AND CONCLUSIONS

The FPIN2 fuel element mechanics model has been implemented in SASSYS/SAS4A Version 3.0. In a typical coupled calculation, the fuel element (pin, cladding, pin plenum, bond sodium, and molten fuel cavity) temperatures are computed by SASSYS/SAS4A and transferred to FPIN2; and the updated dimensions, stresses, and strains are returned to SASSYS/SAS4A to be used in the analysis of accident energetics. The intended application for the integrated SASSYS/SAS4A-FPIN2 model is the design basis analysis of the metallic LMR fuel elements for the purpose of margin-to-failure assessments.

Validation of the integrated model has been performed using the data from TREAT tests M5, M6, and M7. The six metallic fuel elements in these TREAT experiments have been analyzed using the integrated SASSYS/SAS4A-FPIN2 code system. The calculated results for thermal-mechanical response of the fuel elements have been compared to available information such as flow tube temperatures, cladding failure time and location, fuel axial expansion, and melt fraction. In general, the calculated results are in good agreement with the experimental data for the key parameters related to timing, characteristics and the magnitude of the thermal and mechanical response of the fuel elements.

One of the major shortcomings of the integrated model is the lack of pretransient pin characterization models. A new steady-state irradiation module, SSCOMP (currently under development[1]), will provide the integrated SASSYS/SAS4A-FPIN2 model with the capability to describe the transition of the fuel elements from cold and clean as-manufactured conditions to hot and swollen as-irradiated conditions in a consistent manner.

REFERENCES

1. J. E. Cahalan et al., "The SAS4A/SASSYS-1 LMR Accident and System Analysis Codes," Version 3.0 Code Manual (to be published).
2. J. E. Cahalan, A. M. Tentner, and E. E. Morris, "Advanced LMR Safety Analysis Capabilities in the SASSYS-1 and SAS4A Computer Codes," Proc. Intl. Topical Mtg. Advanced Reactors Safety, 2:1038, Pittsburgh, PA (April 17-21, 1994).
3. T. H. Hughes, "FPIN2 Analysis of Metal Fueled Pins," Argonne National Laboratory Report, ANL-IFR-23 (September 1985).
4. T. H. Hughes and J. M. Kramer, "The FPIN2 Code - An Application of the Finite-Element Method to the Analysis of the Transient Response of Oxide and Metal Fuel Elements," Proc. Conf. Sci. & Technol. Fast Reactor Safety, 427, BNES, Guersnay, U.K. (May 12-16, 1986).
5. J. M. Kramer, T. H. Hughes, and E. E. Gruber, [REDACTED]
[REDACTED]
Argonne National Laboratory Report, [REDACTED] (April 1987).
6. J. M. Kramer, T. H. Hughes, and E. E. Gruber, "Validation of Models for the Analysis of the Transient Behavior of Metallic Fast Reactor Fuel," Proc. Intl. Conf. Struct. Mech. in Reactor Technol., C:65, Anaheim, CA, (August 1989).
7. T. Sofu and J. M. Kramer, "Implementation, Verification and Validation of the FPIN2 Metal Fuel Pin Mechanics Model in the SASSYS/SAS4A LMR Transient Analysis Codes," Proc. Intl. Topical Mtg. Advanced Reactors Safety, 2:784, Pittsburgh, PA (April 17-21, 1994).
8. W. R. Robinson et al., "First TREAT Transient Overpower Tests on U-Pu-Zr Fuel: M5 and M6," Trans. Am. Nucl. Soc., 55:418 (1987).

9. A. E. Wright et al., "The IFR Overpower Test M7 in TREAT," Trans. Am. Nucl. Soc., 56:383 (1988).
10. T. H. Bauer et al., "First Overpower Tests of Metallic IFR Fuel in TREAT: Data and Analysis from Tests M5, M6, and M7," Argonne National Laboratory Report, ANL-IFR-124 (December 1989).
11. J. M. Kramer and T. H. Hughes, personal communication, Argonne National Laboratory (January 13, 1993).
12. G. L. Hofman et al., "Metallic Fuels Handbook," Argonne National Laboratory Report, ANL-IFR-29 (November 1985).
13. R. G. Pahl et al., "Postirradiation Examinations of U-Pu-Zr Fuel Elements from Subassemblies X419 and X419A," Argonne National Laboratory Report, ANL-IFR-55 (October 1986).
14. W. F. Brizes and M. L. Hamilton, "Development of a New Transient Damage Correlation for CW 316 Fuel Pin Cladding," Westinghouse Hanford Laboratory Report, HEDL-TC-160-35, 201 (1982).
15. F. R. Shober, "Physical and Mechanical Properties of Alloy HT9 Used in the Design Analysis of the Core Demonstration Experiment," Westinghouse Hanford Laboratory Report, HEDL-TC-2845, Sect. 5.7.2 (July 1986).
16. M. C. Billone, personal communication, Argonne National Laboratory (May 11, 1993).
17. T. H. Bauer et al., "Update of Safety Testing in TREAT on U-5Fs Fuel: Data from Test M4 and Combined Analysis of Tests M2, M3, and M4," Argonne National Laboratory Report, ANL-IFR-69 (May 1987).
18. "Reactor Development Program Progress Report," Argonne National Laboratory Report, ANL-RDP-87, 2-20 (September 1979).

19. T. H. Bauer, G. R. Fenske and J. M. Kramer, "Cladding Failure Margins for Metallic Fuel in the Integral Fast Reactor," Trans. 9th Intl. Conf. Struct Mech in Reactor Technol., 31, Lausanne, Switzerland (August 17-21, 1987).
20. A. M. Tentner and Kalimullah, "SAS4A Analysis of the M7 Metal Fuel TREAT Experiment," Argonne National Laboratory Report, ANL-IFR-186 (January 1993).