TRANSIENT FUEL-CLADDING DEFORMATION ANALYSIS OF THE IN-PILE H3 'TREAT' TEST WITH THE 'SAS2A/DEFORM-II' CODE

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SUMMARY

A new model for the transient fuel-pin deformation analysis has been developed for oxide-fueled fast breeder reactors. The model, designated as DEFORM-II, is a module of the SAS2A fast-reactor accident analysis code and is currently used for calculations of fuel behavior in unprotected overpower transients in FFTF and in support of the TREAT program at Argonne National Laboratory. The mathematical analysis is based on the following assumptions:

- (1) The mechanical equations are quasi-static.
- (2) The generalized plane strain approximation is used both for fuel and cladding cylinders.
- (3) The fuel is regarded as one homogeneous, isotropic material for the deformation analysis. However, differences in the fuel properties due to steady-state restructuring are included in the thermal analysis by distinguishing between three different fuel regions, i.e., the columnar, equiaxed, and unrestructured regions.

Major improvements of the present model over the DEFORM-I module in SAS1A are:

- (1) A general inelastic behavior with an arbitrary yield surface can be prescribed for both fuel and cladding.
- (2) The thin-membrane approximation for the cladding is replaced with a multinode representation, thus enabling a more detailed analysis of clad stresses and strains.
- (3) The pressure in the central cavity containing any original central void, any molten fuel, and fission gas is determined for the whole cavity volume, which can extend over an arbitrary number of axial fuel nodes.

Also, the new model recognizes some of the most important changes in both fuel and cladding behavior due to steady-state irradiation of the fuel pin, such as the structural regions in the fuel and fission gases in the fuel matrix as well as in the cavity.

The new SAS2A/DEFORM-II code system has been used to analyze the transient fuel-cladding deformation for the H3 in-pile TREAT test, during which a pre-irradiated fuel pin was subjected to an overpower transient corresponding to a 0.5-1 \$/sec power ramp in FFTF. The EBR-II-irradiated pin was surrounded by a hexagonal array of six fresh FTR type pins in flowing sodium and placed in a Mark-II loop. Of particular importance for the transient deformation results were the gap size and fuel porosity. The measured transient power variations and coolant-inlet conditions from the experiment were used for the deformation analysis by the SAS2A/DEFORM-II code system. The calculations are based on the following assumptions:

- (1) Fuel and cladding are treated as elasto-plastic materials with temperature-dependent yield strengths.
- (2) Effects from fuel cracking are neglected.
- (3) A complete slip model is applied after the fuel-clad contact.
- (4) The pin is assumed to be in a stress- and strain-free condition prior to the TREAT transient.
- (5) The gap conductance was taken to be a constant of 1 W/cm²-°C.

The results from our calculations show a maximum calculated permanent cladding deformation of 0.3% at a relative fuel height of 0.25. A sensitivity study showed that the deformations are strongly influenced by variations in the initial gap size and gap conductance. The code predictions agreed reasonably well with the observations for this test.

1. Introduction

The development of analytical tools, usually in the form of a digital computer code, and their verification with both in-pile and out-of-pile tests is an integral part of the Liquid Metal Cooled Fast Breeder Reactor (LMFBR) safety program of the U. S. Atomic Energy Commission. The Safety Analysis System (SAS) code series[1], being developed by the Argonne National Laboratory, is a major computational tool for analyzing both flow and power transients for LMFBR cores. This code series is intended to be a complete accident package; i.e., the transient analysis is carried from a normal operating state of the reactor up to and through thresholds for fuel and/or cladding damage, finally terminating in core disassembly (if prior calculated events so dictate). The code consists of various modules, each one of which is dedicated to a specific realistic problem, for example, the coolant dynamics program treats both single-phase and two-phase coolant flow and heat removal. Data for verification and calibration of different modules of the code are provided from in-pile transient tests being conducted in the Transient Reactor Testing Facility [2] (TREAT) as well as from out-of-pile laboratory tests.

The SAS1A code was the first of the SAS series. One of the first attempts to use this code to analyze TREAT tests has been described in the First Structural Mechanics Conference [3]. It was shown in that work that the code-predicted fuel pin responses were in overall agreement with the results obtained for a nonirradiated pin. Since then a more refined version of the SAS series, SAS2A [4]/DEFORM-II[5], has become operational. The fuel-clad deformation program of this code system has been completely rewritten.

The new deformation analysis module, DEFORM-II, for the oxide fuel pin of the fast breeder reactor replaces the earlier DEFORM-I [6] module in the SAS1A code. DEFORM-II is coupled with a new version of the SAS series, SAS2A, and it is currently used for test calculations of fuel pin behavior in unprotected overpower accidents and in support of the TREAT program at Argonne National Laboratory. The new module features a number of improvements over its predecessor including:

- (a) allowance for elasto-plastic behavior of both the fuel and clad with work-hardening yield surfaces;
- (b) a more detailed analysis of the clad using variable radial meshes within the tube, and
- (c) consideration of the geometrical and thermal changes of irradiated fuel pin characteristics due to steady-state irradiation, such as restructured fuel zones, their density and conductivity.

The improvements under (a) and (c) are the most significant ones over DEFORM-I.

Recently, in-pile test data from the TREAT H-series experiments has become available on the transient response of a mixed oxide fuel pin irradiated to a nominal burnup of 3.5 a/o in EBR-II. This test (H3) [7] was a simulation of a low power (50¢/sec to 1\$/sec) unprotected ramp insertion in an FFTF type fuel pin. We have analyzed this test with the SAS2A/DEFORM-II code for thermal behavior and mechanical deformations. The results are described in this paper.

The organization of this paper is as follows: Section 2 begins with a brief description of the SAS2A/DEFORM-II code, followed by the experimental description of the H3 in-pile test (Section 3). The pretransient characteristics of the pin are described in Section 4,

as determined with the aid of the steady-state irradiation code, LIFE2[8]. The results of the SAS2A/DEFORM-II calculations are presented in Section 5 with an examination of the underlying assumptions in the code. The predictions based upon the calculations are then compared with the experimental results in Section 6. The material properties used in the calculations are listed in the Appendix.

2. DEFORM-II Model

The objective of the DEFORM-II model is to calculate the transient deformation of irradiated oxide fuel pins up to the point of clad failure. The mechanical analysis is based upon the quasi-static, axisymmetric, generalized plane strain approximation both for the fuel and the clad cylinders. Up to 20 mesh points can be employed radially for the numerical solution. The axial length of the fuel pin is divided into an arbitrary number of segments or nodes (up to 20) and no interactions are assumed between one segment and its axial neighbors. The rheological model adopted herein is the elasto-plastic behavior with temperature and work-hardening yield surfaces. To account for effects from the steady-state irradiation, the model allows:

- (1) 3 distinct metallurgical regions in the fuel,
- (2) a central cavity containing fission gas,
- (3) fission gas in the fuel matrix, and
- (4) a release of fission gas to the cavity at the fuel solidus.

The generalized plane-strain solutions are obtained both for the fuel and clad cylinders in semi-closed forms. In the absence of mechanical contact of the cylinders, the two solutions are coupled only weakly through the gap pressure. In the presence of contact, however, the two solutions are coupled with the requirement that the normal stress across the interface be continuous. The mode of contact is currently assumed to be a complete slip. The boundary condition at the inner surface (if any) of the fuel is the cavity pressure and that at the outer surface of the clad, the coolant pressure. Explicit axial motion of fuel upon melting is neglected in the present model, but the cavity pressure is assumed to be uniform within the molten boundary comprising liquified fuel, solid fuel at the solidus, fission gases in the original cavity, and fission gases in the fuel matrix.

At each time step, the equivalent stress is compared to the temperature-dependent yield stress and, if the former exceeds the latter, then the plastic flow is calculated from the Prandl-Reuss flow rule based upon the von Mises yield criterion. Fuel cracking is not modeled by DEFORM-II. Radial iterations are repeated at each axial node, until the stress strain components attain acceptable convergence. In the presence of large cavity pressure changes, however, these radial iterations themselves are repeated along the axial nodes that encompass the cavity.

The interplay between SAS2A and DEFORM-II is as follows: at the end of every transient time step SAS2A provides DEFORM-II with thermal variables such as the temperatures of fuel, coolant and structure, the fuel melt fractions, the coolant and plenum pressures; the major variables transferred back to SAS2A at the end of each DEFORM step are the gap widths, contact pressures and deformed radii. The actual fuel and clad temperature profiles are used to calculate the radial dependence of the thermal expansion and yield strengths in both fuel and clad.

Some consequences of the above mentioned assumptions made in DEFORM-II shall be mentioned briefly: The plane strain approximation used in DEFORM-II is an economical approach, but it neglects the end effects of each dished pellet within the fuel pins. Besides, the thermal expansion in the axial direction can only be calculated as one average value for the whole pin area. If the cracking effect were included, the fuel-clad gap is expected to close earlier in the transient, but the effect of fuel cracking can properly be treated only in a three-dimensional anisotropic model. The assumption of complete slip between the fuel and the cladding after their contact tends to underestimate the loading on the clad by permitting the fuel pellets to slide over the clad, and hence underestimating the axial strains in the cladding. The other extreme can be thought of as "no-slip" treatment, in which the clad would be axially stretched along with the fuel. This would lead to an increase in axial strain in the clad, somewhat reducing the radial displacements. The complete slip model therefore tends to underestimate, while the no-slip model would overestimate the axial strains. Its effect on the circumferential strain, however, seems to be small. The circumferential strain is affected not only from the lateral contraction, but also from the redistribution of the axially restrained fuel under thermal expansion. A comparison of a free-slip model calculation with no-slip model (PECT) [10] had shown little difference in the circumferential strains.

3. Experimental Descriptions

The H3 test [7] was performed with an EBR-II irradiated FTR type central element (PNL-17-24) surrounded by a hexagonal array of six prototypical fresh FTR pins (PNL-17) in flowing sodium. Figure 1 shows the experimental setup in the Mark-II loop, and it also shows the cross sectional view of the seven pin assembly. A flow rate of approximately 400 cm/sec through the test section (typical of EBR-II) is maintained at 371°C by the annular linear induction pump. The loop is wrapped with a coating of B_6Si to minimize the radial power depression in the highly enriched pins and also to simulate an axial power shape typical of an FTR pin. Figures 2 and 3, respectively, show the measured axial and calculated radial power distributions for the central and peripheral pins. The axial peak-to-average power factor for the central pin is 1.08 and the radial factor is 1.14.

The TREAT power transient was specified to approximate the pin thermal response of the FTR following a ramp reactivity insertion of about 1\$/sec. The normal operating conditions of the FTR pins were simulated in the H3 test by holding the axial peak power level at 16.8 kw/ft for approximately three seconds. The overpower part of the TREAT power history was specified such that the fuel center line temperature in the central element would approach, but not exceed the solidus, with a temperature rise rate comparable to that for a 50¢/sec to 1\$/sec FTR excursion. The actual TREAT power and the energy during the transient are given in Fig. 4. Also shown in this figure is the power-energy history of the central pin at the peak axial location based on a constant calibration factor of 1.73 w/g-TREAT-Mw. The calibration factor was experimentally determined from a test run by radiographical measurements. Fresh pins were substituted in the Mark-II loop and the TREAT control rods were moved into the same position which they had during the actual H3 Test. Both the steady-state and the transient state deformations of the central test pin were analyzed using the LIFE-2 and the SAS2A/DEFORM-II codes, respectively. Out of the fresh peripheral elements, only the hottest pin was considered for the transient deformation analysis.

4. Post-EBR-II Conditions

The central test pin (PNL 17-24) was irradiated to an average burnup level of 25,000 Mwd/to in EBR-II at reactor power levels of 50 and 62.5 Mwt for 110 and 80 days, respectively. The corresponding peak pin power ratings were 8.7 and 10.8 kw/ft, and the axial peak to average ratio was 1.125. The fuel-pin specifications are summarized in Table I. The state of this pin at the end of the EBR-II irradiation can be obtained, in principle, from the LIFE-2 code[8]. The various parameters of the code were adjusted in order to obtain an agreement with the results of a destructive examination of a sibling pin (PNL-17-10). The calculation was carried out at the axial midplane. However, the adjusted set of parameters could not consistently predict the axial variations of the restructured fuel boundaries, the gas retention and the gap size. Hence, the axial variation of the outer radii of the equiaxed and columnar grain region [5] were assumed to follow the 1350 and 1700°C isothermals in the pin, respectively. These temperatures were calculated for the measured outer radii of the columnar and equiaxed fuel regions at the axial midplane. The fuel density was not allowed to vary from one region to another; hence, there was no central void due to the fuel restructuring. The fission-gas retention in the different fuel regions was varied, however.

The amount of fission gas retained in the fuel matrix was assumed to be the same (i.e., 51%) as observed in the PNL-17-10 sibling pin. The axial distribution of the retained fission gas was calculated based on the axial EBR-II power profile, assuming that the fission-gas retention in the columnar and equiaxed regions were 0 and 30%, respectively. This assumption leads to the fission-gas retention of 59.1% in the unrestructured fuel. The results are shown in units of moles per cm of fuel height in Fig. 5. The effect of the retained fission-gas distribution and its subsequent release can be important in predicting the clad rupture and any subsequent fuel-coolant interactions.

The LIFE-2 code predicted no plastic deformation of the PNL-17-10 elements for the EBR-II irradiation, which was consistent with the profilometric measurements. But it calculated gap sizes, which were too big compared with data obtained from the destructive examination of this pin, which was irradiated in the same subassembly of EBR-II as the actual test pin (PNL-17-24). Therefore, the fuel-clad dimensions used in the DEFORM-II calculations were taken from the destructive examination of this sibling pin. Figure 6 shows its cross-sectional view at the axial midplane. The cold radial gap determined in a preliminary measurement [9] was in the range of 0.5 to 1.0 mil at this axial location. The gap size at the top of the pin was measured to be about 2 mils. The calculations, to be described below, are based on an axially uniform radial gap size of 0.75 mils. Later, a more detailed measurement of the radial gap has indicated the gap size to vary from 0.6 to 1.0 mils with an average value of 0.85 mils at the axial midplane. The post-TREAT transient examination of the PNL-17-24 test pin (see Fig. 6) indicates the gap size to vary from 0.6 to 1.4 mils with an average value of 1.0 mils. These values are considered to be the lower limits for the actual gap size prior to the TREAT transient, since there exists a possibility of plastic or permanent deformation of the mixed oxide during the transient testing. It will be shown later that the calculated values of the permanent deformation are very sensitive to the gap parameters (e.g., the width and the conductance). In the following transient calculations, an axially uniform initial fuel radius of 0.2521 cm has been used.

The porosity measurements, taken from a microscopic inspection at the axial midplane of the PNL-17-10 pin, can be seen in Fig. 7. The porosity was found to be decreasing (density increasing) from the center of the mixed-oxide pellet to its outer surface. It is noted that no measurements were performed in the first 6 mils frame from the center, since fuel was not symmetrically distributed in this region. It is interesting to point out that the fuel in the inner-most region (which is considered to be the columnar region) showed even a lower density ($\sim 90\%$ T.D.) than the initial pellet density (93% T.D.).

The fuel porosity affects the pin performance in a transient through a number of mechanisms including changes in the thermal conductivity, transient swelling and eventually release of fission gases. The first factor, namely, the decrease in thermal conductivity, was included in the present DEFORM-II calculations by using a smeared fuel density (see Table I). The fuel density used for the DEFORM-II calculations is obtained by spreading uniformly the entire fuel mass over its outer radius. Hence, the smeared porosity (11.2%) used in our transient deformation calculation is greater than the average fuel porosity ($\sim 7\%$) obtained from the measurement. This approximation results in somewhat smaller conductivities. The influence of the change in conductivity will be discussed later. The transient swelling due to the retained gases (fission and pores) was not included because of the lack of experimental data and of an adequate calculational model. The effect of fission-gas release from the solid fuel was accounted for in the current DEFORM-II model by assuming that 50% of the retained fission gas was released when the fuel temperature exceeded 2600°C . However, the current version of the DEFORM-II model assumes that an instantaneous equilibrium is achieved between the released gas and the plenum gas. Since the amount of the fission gas released is small compared to the plenum volume, this proved to have no significant influence on the loading of the clad.

5. DEFORM-II Results

The DEFORM-II analysis of the H3 test is based on the following assumptions regarding the input data:

- (a) The fuel and cladding were modeled by 10 axial segments of equal length and 15 and 5 equidistant radial nodes were used for the fuel and cladding, respectively.
- (b) A uniform cold radial gap size of 0.75 mils was used all along the fuel axial length.
- (c) A smeared density of 9.85 g/cm^3 (or 88.8% T.D.) has been assumed throughout the pin.
- (d) The pin was assumed to be in a stress, strain free condition prior to the TREAT transient.
- (e) The fuel thermal conductivity is calculated with 11.2% porosity.
- (f) The gap conductance was taken to be constant ($1 \text{ w/cm}^2\text{-}^\circ\text{C}$).

It will be later shown that the clad permanent deformation is very sensitive to the assumed gap size and conductance values. The assumption of a stress, strain free condition in the clad prior to the transient does not appear to be a serious limitation, since there was no indication of any clad deformation in the PNL-17-24 pin after its EBR-II irradiation. The effect of changing the thermal conductivity has been investigated in the parametric study, which will also be discussed later.

The material properties given in the Appendix of this paper were used in the analysis. The thermal expansion coefficient in the fuel was assumed to be linear in temperature. The cladding yield strength for 20% cold worked, 316 type stainless steel irradiated to a fluence level of 3×10^{22} n/cm² does not appear to be significantly different from the data for fresh steel of the same type in the temperature range of interest, as shown in the Appendix. The strength curve of the fresh cladding is therefore used. A work-hardening effect, i.e., a strain-dependent increase of the yield strength has not been included either for the fuel or for the cladding.

The actual pin deformations due to the TREAT transient were calculated for the PNL-17-24 pin surrounded by the equivalent coolant volume. The variations in the axial and radial power profile of this pin were taken from Figs. 2 and 3, respectively. The pin power and energy histories used are shown in Fig. 4. The temperature profiles calculated by SAS2A/DEFORM-II are in overall agreement with the results [11] obtained from the detailed thermal-hydraulic calculations with the COBRA-III-M code, which is the Argonne modified version of COBRA-III [12] to calculate transient fuel temperatures in a multipin assembly. The fuel temperature distributions, calculated by SAS2A in the central pre-irradiated element (PNL-17-24 pin) during TREAT testing, are shown in Figs. 8 and 9.

The fuel pin deformation results are shown in the following figures (10 through 13). It was predicted that the gap closes first in the lower section of the pin at a transient time of 8.9 sec. As more heat is produced in the pin, the fuel-clad gap closes at all other locations. If the gap size were allowed to increase linearly from 0.75 mils at the axial midplane to the measured 2 mils at the top, there would not be any contact of the fuel and clad near the top of the pin. The maximum contact pressure of 730 atm is predicted near the axial midplane. Soon after the transient is terminated, both the fuel and clad start to shrink and the gap reopens. The posttransient gap width is expected to be at most equal to the pretest size since any possible plastic deformation of mixed oxide is irreversible.

Figure 10 shows the behavior of the equivalent clad stresses and the equivalent clad strains as functions of the transient time for three different axial positions. These stresses refer to the outer part of the cladding, where they are highest. Before gap closure takes place, the clad stresses are caused mainly by thermal stresses and slightly by the plenum pressure. The temperature drop within the clad at the midplane is about 70°C at 10.2 sec shortly before the power is shut down. After gap closure, both contact pressure and equivalent clad stress have the similar behavior. The equivalent clad stress cannot exceed the yield strength prevailing at the local temperature. The drop of the equivalent clad stress during the overpower time is due to the lowering of the yield strength of the cladding material with temperature. Once the reactor has been shut down and the gap has opened again, stresses decrease to the low value of 800 atm, which is due to the plenum pressure and the residual thermal stresses.

The time behavior of the total and plastic clad strains are shown in Fig. 11 for the most highly strained point. The clad strain is predicted to be highest at a relative height of 0.25. The power at this location has almost reached the axial peak value but the clad is slightly cooler than at the midplane. Hence, the clad could not expand as much as in the upper portions. For this axial location, the cladding strain remains elastic until slightly later than 9.75 seconds, when it reaches a value of 0.26%. Plastic deformation follows

beyond this time. The maximum equivalent clad strain of 0.57% is attained at the end of the transient (10.2 sec), out of which the elastic strain of 0.26% should be subtracted to obtain the equivalent plastic strain. After the end of the power transient, the elastic strain is fully recovered, leaving a residual (plastic or permanent) equivalent strain of 0.3%.

The axial dependence of the plastic deformation, which is experimentally measurable, is shown in Fig. 12. This is a plot of the circumferential plastic strain or relative permanent radial displacement as function of the axial height at different transient times. This quantity is to be compared with the measured displacements. Within the limitations of these calculations and the uncertainties of experimental data, measurements can be compared with the maximum instantaneous displacements at the transient time of 10.2 sec. In terms of relative values, DEFORM-II gives permanent radial displacements ranging from 0.1% at the bottom to 0.27% at a relative height of 0.25. The top section of the pin does not experience any plastic deformation. The corresponding equivalent plastic clad strain was shown to reach a value of 0.3%. In other words, the circumferential strain component is the most dominant in the equivalent strain (at least for the assumed complete slip model). The axial variation of the permanent displacements is believed to be due to the shaped power profile in the test and to the assumption of an axially constant initial fuel-clad gap.

The calculated axial profiles of the equivalent clad stress and strain are shown in Fig. 13 as a function of the relative fuel height for three transient times. Also included is the temperature dependence of the yield strength of the 20% cold-worked, 316 type stainless steel corresponding to a time shortly before the termination of the transient. It was calculated from the axial temperature distribution. The drop in the clad stress with the axial height is due to the decrease in the clad yield strength with temperature. Since the work-hardening effect was not included, the equivalent clad stresses are solely determined by the temperature. The other two profiles are characteristic of the elastic state of the clad in which equivalent stress and strain are proportional.

One of the hottest fresh peripheral elements (P5, see Fig. 3) was also analyzed for the transient deformation. The DEFORM-II calculations were made by approximating the skewed power distribution with an equivalent symmetric power shape. For this problem permanent strain was calculated to be nonexistent since the fuel-clad radial gap size for the fresh pin was 3.0 mils, and in fact, gap closure was not predicted during the transient. Neither of the profilometry measurements indicated any permanent strain.

6. Discussions and Conclusions

It has been pointed out earlier that the present DEFORM-II analysis of the H3 experiment predicted a maximum of 0.27% permanent increase (i.e., about 0.6 mils) in the outer clad diameter. The posttransient examination of the pin showed, within the experimental accuracy of ± 0.2 mils, no indication of the diametrical change. Hence, the DEFORM-II calculation slightly overpredicts the plastic deformation. This can be due to the uncertainty of the major input parameters, including (a) the gap size, (b) the gap conductance, (c) the fuel thermal conductivity, and (d) the power calibration factor. The influence of these parameters has been investigated by calculating four additional cases, changing one parameter at a time:

- 1) The gap size was increased from 0.75 to 1.0 mils (the measured gap size was 0.75 ± 0.25 mils).

- 2) The gap conductance was changed from 1 to 2 w/cm²-°C.
- 3) The fuel thermal conductivity was calculated for the measured average porosity of 7%.
- 4) The calibration factor was decreased by 5% as an upper limit for burnup effects.

All of these parameter variations are well within the range of the uncertainty of these numbers. The results are summarized in Table 2. An increase in the gap size from 0.75 to 1.0 mils reduces the plastic diametrical change in the clad from 0.27% to 0.1%, which is within the uncertainty of the measurement. For elastic fuel, the effect of the gap size on the permanent clad displacement can be approximated by the following equation:

$$\epsilon = \epsilon_0 - \Delta r$$

where ϵ and ϵ_0 are the permanent radial displacements for initial fuel-clad radial gap sizes of Δr and zero, respectively. The above relation overestimates the consequences of a gap size change, when the mixed oxide fuel is treated as an elasto-plastic material. For the 0.25 mils increase in the gap size it predicts the plastic strain to decrease from 0.27 to 0.02%, while the DEFORM-II calculations for elasto-plastic fuel result in a smaller change to a value of 0.1%.

Increasing the gap conductance from 1 to 2 w/cm²-°C causes about the same decrease in the plastic clad deformation as increasing the gap size from 0.75 to 1 mil. The higher gap conductance decreases the fuel temperatures and, hence, the overall fuel thermal expansion is decreased, thereby reducing the load on the clad. Again one sees that the permanent change in the clad diameter lies within the uncertainty of the measurement. When the fuel thermal conductivity corresponding to the measured average fuel porosity of 7% was used, a flatter fuel temperature profile was obtained with a maximum centerline temperature of 2615°C. The total fuel thermal expansion was therefore decreased and, correspondingly, the plastic clad deformations.

As mentioned earlier, the calibration factor relating the TREAT reactor power to the pin power was measured with fresh pins. It is estimated that the decrease in this factor due to burnup effects may be in the vicinity of 5%. Recent radiographical measurements done with the actual test pin indicated an even smaller calibration factor around 1.6 w/g-Mo-TREAT-Mw. Our calculations indicate that this 5% reduction in the calibration factor lowers the maximum calculated plastic strain to about 0.2%.

The deformation behavior of the EBR-II irradiated PNL-17-24 fuel pin was calculated by the SAS2A/DEFORM-II code for the mild-overpower transient (H3) in the TREAT reactor. The permanent cladding deformation was calculated to be 0.3%. Thus, pin failure was not predicted, consistent with the posttest observations. The posttest examination indicated no measurable strain (within $\pm 0.1\%$). It should be pointed out, that the final plastic strains depend strongly on the values of the assumed initial fuel-clad gap width, and of the gap conductance. If the radial gap width were increased from 0.75 to 1.0 mil (the experimental uncertainty is even larger than this), only 0.1% permanent displacement would be predicted, which is roughly equal to the experimental uncertainty.

DEFORM-II, the new deformation module of the SAS2A Safety Analysis System code is found to be a useful tool for analyzing the transient fuel-clad deformation for this in-pile TREAT test. The DEFORM-II predictions agreed reasonably well with the experimental data obtained from the H3 test, particularly when the experimental uncertainties are included.

APPENDIX

MATERIAL PROPERTIES

The set of physical, thermal, and mechanical properties for the mixed-oxide fuel and cladding used in the present calculations are compiled below. These properties were taken from the best available sources, including open literature publications and some private communications.

Fuel Properties

1. Density: The variation of mixed-oxide (80 w/o UO_2 , 20 w/o PuO_2) density with temperature is expressed as

$$\rho(T) = \frac{\rho(T_0)}{[1 + \bar{\alpha}(T)(T - T_0)]^3}; \quad T_0 \leq T \leq T_m, \quad (A.1)$$

where

$$\rho(T_0) = \rho_0 \times \text{fractional theoretical density}, \quad (A.2)$$

T_0 is the room temperature (27°C), $\bar{\alpha}(T)$ is the average thermal coefficient of linear expansion between T_0 and T , and ρ_0 is the theoretical density of the fuel at T_0 . For mixed-oxide fuel, $\rho_0 = 11.04 \text{ g/cm}^3$ [13], and $\bar{\alpha}(T)$ is given by

$$\bar{\alpha}(T) = 6.5693 \times 10^{-6} + 2.997 \times 10^{-9} T, \quad (A.3)$$

with T in °C. Equation A.1 can alternately be represented by the fractional linear elongation, $\Delta L/L_0$, as

$$\frac{\Delta L}{L_0} = 2.997 \times 10^{-9} T^2 + 6.482 \times 10^{-6} T - 1.77 \times 10^{-4}, \quad (A.4)$$

where T is in °C. This representation of density variation up to the melting point is consistent with the data compiled by Lyons *et al* [14]. In addition, $\Delta L/L_0$ reduces to zero when T is set equal to room temperature (27°C). The melting temperature of the irradiated 20% mixed-oxide is 2760°C and that for the fresh fuel is 2800°C.

2. Specific Heat: The specific heats of both pre-irradiated and fresh mixed oxide were taken to be the same as that for fresh UO_2 . The variation of the specific heat [15] with fuel temperature is shown in Fig. 14. The heat of fusion is 65.6 cal/gm and the specific heat for molten fuel is 0.1202 cal/gm-°C.

3. Thermal Conductivity: The thermal conductivity of UO_2 is represented by the following relations: [16]

$$k(T) = \frac{k_0}{12.044 + 0.0196T} \quad \text{for } T \leq 800^\circ\text{C}; \quad (A.5)$$

$$k(T) = 0.0130 + \frac{1}{T(0.4848 - 0.4465D)} \quad \text{for } 800^\circ\text{C} \leq T \leq 2000^\circ\text{C}, \quad (A.6)$$

where k is in $\text{w/cm-}^\circ\text{C}$, and D is the fractional theoretical density ($0.82 \leq D \leq 0.95$). The value of k_0 is obtained by matching Eqs. (A.10) and (A.11) at $T = 800^\circ\text{C}$. For temperatures above 2000°C , $k(T)$ is taken to be a constant value equal to $k(T = 2000^\circ\text{C})$. This form of representation takes into account the fact that the effect of pores on conductivity changes with increasing temperature.

4. Mechanical Properties: The following set of values for the mechanical properties of mixed oxide fuel was used:

$$\text{Poisson's ratio } \nu_f = 0.5 - 1.25 \times 10^{-5} T ; \quad (\text{A.7})$$

$$\text{Shear modulus } G_f = 7.41 \times 10^{-5} - 52.0T - 0.02T^2 , \quad (\text{A.8})$$

where T is in $^\circ\text{C}$ and G_f is in atm. The fuel yield strength and the ultimate strength are shown in Fig. 15.

Clad Properties

1. Density: The density of Type 316 stainless steel is represented by

$$\rho = 7.925 - 4.55 \times 10^{-4} T, \text{ g/cm}^3 , \quad (\text{A.9})$$

where T is in $^\circ\text{C}$.

2. Specific Heat: Because of the short span of the temperature range of interest, the specific heat is assumed to be independent of temperature ($= 0.607 \text{ J/gm-}^\circ\text{C}$).

3. Thermal Conductivity: The thermal conductivity is also taken to be temperature independent ($= 0.216 \text{ w/cm-}^\circ\text{C}$).

4. Mechanical Properties: Yield strength is shown in Fig. 16.

$$\text{Poisson's ratio } \nu_c = 0.28 ;$$

$$\text{Shear Modulus } G = 9.05 \times 10^{-5} - 616.3 T ,$$

where T is in $^\circ\text{C}$ and G_c in atm.

Sodium Properties

The properties of liquid sodium were taken from G. H. Golden and J. V. Tokar. [17]

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TABLE I. Fuel-pin Specifications

	Central Pin	Peripheral Pins
Fuel Composition	75 w/o UO ₂ 25 w/o PuO ₂	75 w/o UO ₂ 25 w/o PuO ₂
Burnup	3.5 a/o	0
Enrichment	~61.6% ²³⁵ U in U ~83.3% ²³⁹ Pu in Pu	65% ²³⁵ U in U 89% (²³⁹ Pu + ²⁴¹ Pu) in Pu
Pellet density	2 to 3 restructured regions of different densities	10.34 g/cm ³ (93% TD)
Smeared density	9.73 g/cm ³ (9.85 g/cm ³ *)	9.73 g/cm ³ (87.5% TD)
Pellets, ID	0.0-0.09 cm	0.0 cm
OD	0.498-0.505 cm	0.493 cm
Length	34.3 cm	34.3 cm
Fuel-clad diametral gap	0.003-0.010 cm	0.015 cm
Fission-gas retention	~51%	0
Cladding Type	20% cold worked, Type 316 SS irradiated to 25,000 Mwd/t	20% cold-worked, Type 316 SS
Wall	~0.038 cm	0.038 cm

* Smeared Density for .75 mil radial gap used in DEFORM-II

TABLE II. DEFORM-2 Sensitivity Studies

Case	T _{max} , fuel	ε _{eg} _{max, clad}	ε _{plastic} _{max, clad}	(ΔD/D) _{p1, max}
1 Base Case*	2700°C	0.57%	0.31%	0.27%
2 1 mil gap	2700°C	0.39%	0.14%	0.10%
3 h _R = 2 w/cm ² °C	2615°C	0.40%	0.14%	0.10%
4 Fuel conductivity for 7% porosity	2615°C	0.47%	0.22%	0.18%
5 Cal. Factor = 1.6397 w/g-Mw-TREAT-Mw	2600°C	0.47%	0.22%	0.18%
6 Elastic Fuel	2700°C	0.61%	0.35%	0.31%

* Elasto-plastic fuel, 0.75 mil radial gap, h_R = 1 w/cm² °C, Conductivity for 11.2% porosity, Calibration factor = 1.726 w/g-oxide Mw TREAT.

Fig. 1 The Seven-pin Assembly and the Mark-II Loop

Fig. 2 Axial Power Profiles for the Central and Peripheral Elements

Fig. 3 Radial Power Distribution at the Axial Midplane

Fig. 4 Transient Power and Energy vs Time

Fig. 5 Axial Dependence of the Region Boundaries and the Retained Fission Gas

Fig. 6 Cross-view of PNL-17-10 and PNL-17-24 (after TREAT exposure)

Fig. 7 Radial Porosity Distribution at the Axial Midplane for PNL-17-10 Pin

Fig. 8 Radial Temperature Profiles in PNL-17-24 Pin

Fig. 9 Maximum Fuel Temperature vs Transient Time in PNL-17-24 Pin

Fig. 10 Equivalent Clad Stress and Strain vs Transient Time

Fig. 11 Total and Plastic Clad Strain vs Transient Time at $Z = 0.25$

Fig. 12 $(\Delta D/D)_{\text{Plastic}}$ vs Axial Height

Fig. 13 Equivalent Clad Stress and Strain vs Fuel Height

Fig. 14 Specific Heat and Enthalpy of UO₂

Fig. 15 Fuel (Mixed Oxide) Yield and Ultimate Strengths

Fig. 16 Yield Strength for 316 Type Stainless Steel