LEGAL NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Atomic Energy Commission, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

ORNL-4560

Contract No. W-7405-eng-26

METALS AND CERAMICS DIVISION

FUELS AND MATERIALS DEVELOPMENT PROGRAM QUARTERLY PROGRESS
REPORT FOR PERIOD ENDING MARCH 31, 1970

Compiled by

P. Patriarca

Edited by

D. J. Rucker

AUGUST 1970

OAK RIDGE NATIONAL LABORATORY
Oak Ridge, Tennessee
operated by
UNION CARBIDE CORPORATION
for the
U.S. ATOMIC ENERGY COMMISSION

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

149

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

4. LMFBR FUEL ELEMENT DESIGN AND MODEL DEVELOPMENT

P. Patriarca A. L. Lotts C. M. Cox

The objective of this program is to develop analytical methods to predict and evaluate the performance of fuel pins and fuel elements for the liquid-metal-cooled fast breeder reactor (LMFBR). This work is closely coordinated with the related programs for development of fuel and cladding. It provides methods for designing and systematically evaluating irradiation tests, evaluating the influences of materials properties on the performance of fuel elements, and identifying specific areas in which experimental research needs to be intensified.

The program is divided into three areas: model integration, fuel performance, and cladding performance. The fuel and cladding performance tasks are oriented toward mathematical description of the various phenomena of the fuel and cladding under irradiation such as swelling, gas formation, and mechanical, thermal, and chemical behavior. These individual models are incorporated into a generalized computer program that simulates the performance of an operating fuel pin.

Fuel Performance

We worked toward normalizing our current analytical models for swelling and release of fission gas for $(U,Pu)O_2$ and characterizing the behavior of fission gas and microstructural changes in irradiated $(U,Pu)O_2$.

Normalization of Current Models for Swelling and Release of Fission Gas for $(U,Pu)O_2$ (C. M. Cox)

Many of the models currently used in the FMODEL code are of a semi-empirical nature, and it is important to normalize and verify their predictions with experimental data. We have established a basis for the adjustable parameters used to describe fuel swelling and release of fission gas for $(U,Pu)O_2$ pellet fuels.

The basic cause of fuel swelling is the buildup of solid fission products and bubbles of fission gas. Anselin and Bailly¹ calculated that the contribution of solid fission products to the swelling of

($U_{0.8}, Pu_{0.2}$) O_2 amounts to maximum swelling rates of 0.32 to 1.3% fractional volume increase ($\Delta V/V$, or change in volume divided by original volume) per percent fissions per initial actinide metal atom (FIMA). Experiments² with high density ($U_{0.75}, Pu_{0.25}$) O_2 with the fuel subjected to very strong radial restraint indicated minimum swelling rates of 0.3 to 0.4% $\Delta V/V$ per percent FIMA. The range depends on one's interpretation of an apparent incubation period during which the swelling is presumably being accommodated in some of the fabricated porosity. This lowest swelling rate corresponded to an estimated temperature of 1400°C at the fuel center; and, if one assumes that most of the fission gases are in solution at this temperature and under the large hydrostatic pressure arising from the radial restraint, then the swelling should be representative of solid fission products. Although specific cases would depend on temperature and the thermodynamic properties of the fission products, we assume that the swelling from solid fission products can be estimated at 0.32% $\Delta V/V$ per percent FIMA.

For swelling from gaseous fission products we use the Greenwood-Speight model³ with modifications similar to those of Goldberg *et al.*⁴ Since this model does not account for the effects of large temperature gradients and such defects as grain boundaries, its use for oxide fuels is primarily as a pseudoempirical fit to data.

The model for swelling caused by gas is strongly influenced by release of fission gas. Irradiation tests of (U,Pu) O_2 fuel in a fast reactor typically show gas releases of 50 to 80%, as described previously.⁵ Although release of fission gas seems to depend on burnup, fuel fabrication form, and power cycling, measured gas releases vary fairly uniformly with the calculated average temperature of the fuel.⁵ This indicates that, to a first approximation, gas releases may be empirically represented as a function of temperature distribution. We have assumed a three-zone model, similar to our empirical model for restructuring, in which 98% of the gas is released from fuel above 1800°C, 50% from fuel between 1400 and 1800°C, and 30% at lower temperatures. These values arise from our analysis of data on the microstructure and gas release of irradiated fuel. A comparison with data⁵ for pellets tested to moderate burnup is shown in Table 4.1.

Table 4.1. Comparison of Predicted and Measured Release of Fission Gas from Fast-Flux Tests with Pelletized (U,Pu)O₂

Fuel Smear Density (% of theoretical)	Peak Heat Rate (kw/ft)	Fuel Average Burnup (% FIMA) ^a	Fission Gas Released, %	
			Measured	Calculated
96.7	16.7	5.3	47	52
94.5	9.9	3.2	32	30
96.7	17.5	5.5	55	58
87.2	16.1	5.1	58	62
94.0	16.1	5.1	49	53
94.4	16.7	5.3	50	55
92.6	16.6	5.3	50	55
94.6	16.4	5.3	48	53
72.4	9.8	6.8	44	30

^aFIMA is fissions per initial actinide metal atom.

The total volumetric swelling predicted by these combined models at 10% FIMA is shown as a function of temperature in Fig. 4.1. In this application of the Greenwood-Speight model, we used the UO₂ physical constants suggested by Goldberg *et al.*⁴ Several experimental points are included for comparison. Data from Bettis Atomic Power Laboratory of the Westinghouse Electric Corporation⁶ for UO₂ plate fuels operating at temperatures of 500 to 1000°C indicated bulk swelling rates of the order of 1.7% $\Delta V/V$ per percent FIMA. These data have wide scatter and could be half or twice this value.

The higher temperature points shown in Fig. 4.1 were taken from tests with high burnup and high radial restraint.² The temperatures shown are nominal temperatures at the fuel center. Average fuel temperatures were several hundred degrees lower. These data were adjusted as shown by the arrows in the figure to include an estimate of the swelling that was accommodated in fabricated porosity. More precise data are needed to establish the swelling characteristics; however, the data plotted in Fig. 4.1, with the exception of the datum point at 1400°C

Fig. 4.1. Calculated Swelling of 100% Dense (U,Pu)O₂ at 10% Fissions per Initial Actinide Metal Atom. [Data from R. C. Daniel *et al.*, Effects of High Burnup on Zircaloy-Clad, Bulk UO₂, Plate Fuel Element Samples, WAPD-263 (September 1962); E. L. Zebroski *et al.*, "Review of Status of Technology of Fast Reactor Fuels," pp. 2-63-2-80 in Fast Reactors National Topical Meeting, San Francisco, April 10-12, 1967, American Nuclear Society, San Francisco, 1967.]

indicate that the results predicted by these empirical models will at least be reasonable.

The incubation period during which swelling is accommodated in some of the fabricated porosity depends on the fuel porosity after restructuring, temperature, stress, and time. Karsten *et al.*⁷ summarized data that indicate that about 80% of the porosity of fuel above 1700°C, 50% at 1300 to 1700°C, and up to 30% below 1300°C should be available to accommodate swelling. We modified the temperature ranges to correspond to our empirical model for gas release and used this model to correct the data shown in Fig. 4.1. Fuel swelling is assumed to be accommodated in the porosity left after initial restructuring until these minimum values are reached.

While these models can be expected to give reasonable results, the data from which they have been normalized are neither precise nor tied

to well-known operating conditions. We have thus begun microscopic examinations of $(U,Pu)O_2$ from well-characterized irradiation experiments to improve our understanding of the behavior of fission gas and microstructural changes.

Microscopic Examinations of $(U,Pu)O_2$ Fuel (W. J. Lackey)

Since improved models describing fuel behavior are needed for incorporation into the FMODEL code, irradiated $(U,Pu)O_2$ is being studied by means of light and electron microscopy to improve our knowledge of fuel swelling, release of fission gas, and restructuring. We examined an irradiated mixed oxide fuel pin, fabricated from coarse and fine microspheres produced by the sol-gel process, by means of light microscopy, conventional replica electron microscopy, and scanning electron microscopy.

For the conventional electron microscopy we used the two-stage C replica and selected area techniques developed by Padden;⁸ for the scanning electron microscopy we used a Au-coated polyvinyl chloride replica. Particular attention was given to the size distribution and location of bubbles of fission gas, since these factors are expected to influence fuel swelling and release of fission gas considerably.

A metallographically polished transverse section (43-112-3C) of the fuel pin was examined after irradiation in a thermal reactor at a linear heat rate of 13.6 kw/ft to a burnup of 0.7% FIMA. A portion of the transverse section is shown in Fig. 4.2. Bubbles of fission gas up to 1.5 μm in diameter were observed on surfaces at boundaries between columnar grains, as shown in Fig. 4.3. The bubbles were elongated in the direction of the thermal gradient, and, in some cases, they were quite aspherical. The fuel temperature at a radius corresponding to the location of the bubbles was about 1850°C. The standard deviation of the calculated temperatures is estimated to be 100°C.

We occasionally observed in the coarse spheres rows of voids, which we believe to be bubbles of fission gas. The lowest temperature at which these occurred was 1450°C, which corresponded to 0.19 cm from the thermal center. The voids (Fig. 4.4) were 0.1 to 0.2 μm in diameter and occurred in rows, which we believe represent grain boundaries. Even in those areas

Fig. 4.2. Transverse Section of $(U_{0.85}, Pu_{0.15})O_2$ Fuel Pin 43-112-3 After 0.7% Burnup at a Linear Heat Rate of 13.6 kw/ft. The dark regions enclosed by the square are cavities caused by intergranular fracture and subsequent pullout. These are shown at higher magnifications in Fig. 4.3. 50x.

Fig. 4.3. Fission Gas Bubbles Located on Grain Boundaries. The grain-boundaries are at the bottom of the cavities shown in Fig. 4.2. (a) Scanning electron microscopy. 1000x. (b) Conventional electron microscopy. 3200x.

Fig. 4.4. Row of Voids, Presumed to be Bubbles of Fission Gas Located at a Grain Boundary. The large white areas are shadows cast by relatively large voids. Conventional electron microscopy. 4800 $\times$.

where these voids did occur, they were rare. The rows of voids did not appear to be aligned preferentially in any direction.

We observed numerous larger voids at temperatures above about 1200°C (which corresponds to 0.22 cm from the thermal center) but less than that capable of producing columnar grains. These pores are just visible in Fig. 4.2 but are clearly visible in Fig. 4.4. Archive samples of unirradiated microspheres will be examined by electron microscopy to determine if these pores are agglomerated fabrication pores or an irradiation effect.

Near the cladding there was essentially no indication of fuel transport, and the spheres maintained their original shape. Figure 4.5 shows the microstructure of an irradiated coarse microsphere typical of those irradiated below about 1200°C in that the structure is essentially featureless except for an occasional relatively large pore with faceted surfaces such as that shown here. The facets are grain-boundary surfaces of surrounding grains.

Fig. 4.5. Essentially Featureless Surface of a Coarse Microsphere Irradiated at Less than 1200°C. The small black specks are artifacts. A faceted pore is shown in the lower left corner of the photomicrograph. Conventional electron microscopy. 4800 $\times$.

The inner surface of the cladding was observed by electron microscopy at several locations. There was no indication of cladding reaction, which is not surprising because of the low burnup and low cladding temperature (about 320°C). We do not yet know whether a gray material observed by both light and electron microscopy near the boundary between the regions of columnar and equiaxed grains is truly a second phase or whether it is mounting material. It appears that electron microprobe analysis will be necessary to identify this material.

Cladding Performance

Our development of a model for cladding performance was concentrated on the effects of intrinsic cladding swelling, the development of a failure analysis, and evaluation of the mechanical behavior of type 316 stainless steel tubing, the current reference cladding material.

Effect of Cladding Swelling on Kinetics of Gap Closure (F. J. Homan)

The size of the gap between fuel and cladding is important in the treatment of fuel temperature and mechanical interaction of fuel and cladding. In the analysis of the performance of a typical mixed oxide LMFBR fuel pin presented previously,⁹ we used a function for swelling of the cladding suggested by Appleby.¹⁰ Appleby suggested that the swelling effect reached saturation at about 12% $\Delta V/V$. The fuel pin selected for study (as shown in Fig. 4.1 of the previous report⁹) received a high fast-neutron flux approaching 10^{16} neutrons $\text{cm}^{-2} \text{sec}^{-1}$. For this high flux, Appleby's equation predicts that saturation will be reached about halfway through the irradiation lifetime of the fuel pin (as shown in Fig. 4.3 of the previous report⁹). When saturation is reached, there is an abrupt change in the kinetics of closure of the gap between fuel and cladding.

Bloom¹¹ suggested that the combination of tensile stresses in the cladding and buildup of the He concentration due to (n, α) reactions may actually lead to accelerated swelling of the cladding. Not enough is known about this effect to allow speculation, except in qualitative terms; but it suggests that the assumption of swelling saturation is not appropriate.

Without swelling saturation, the gap between fuel and cladding in a typical LMFBR fuel pin appears to grow without bound (see Fig. 4.3 of the previous report⁹). A more recent correlation, suggested by Biancheria,¹² predicts this sort of behavior at high cladding temperatures and neutron fluxes. Biancheria's swelling equation for type 316 stainless steel with 20% cold work relates the volume change to neutron fluence raised to a power of 1.69; however, use of his equation for LMFBR fuel pins requires extrapolation of the swelling equation far beyond its range of validity. This is demonstrated in Fig. 4.6. The fluence necessary to achieve 10% average burnup for a typical LMFBR pin is about 2.2×10^{23} neutrons $\text{cm}^{-2} \text{sec}^{-1}$; but swelling data are not available past about 7×10^{22} neutrons $\text{cm}^{-2} \text{sec}^{-1}$. From the solid curves in Fig. 4.6 it can be seen that cladding with temperatures above 500°C would have extremely large diametral expansions at fluences commensurate with 10%

Fig. 4.6. Swelling of Cladding as a Function of Fluence.

average burnup. The extrapolated predictions of cladding swelling are intolerable from a design standpoint and probably are not realistic. Bloom¹³ thinks that part of the problem may lie in the data selected for the curve. He independently analyzed the available data for swelling and concluded that a fluence exponent of unity might be more appropriate.¹³ We have modified Biancheria's¹² equation for swelling to use his correlation within the range where there are data and have used a fluence exponent of one for extrapolation. This change is represented by the dashed curves in Fig. 4.6. The modified curves for swelling represent a more acceptable situation from the design standpoint; however, cladding exposed to high temperatures and neutron fluxes still swells faster than the fuel. It appears that an accurate analytical investigation of the kinetics of gap closure in this case must wait for additional data on swelling at higher fluences.

Effect of Cladding Swelling on Stress-Strain Distributions (F. J. Homan)

We previously reported¹⁴ our technique for calculating cladding stresses and strains. The variables involved in the calculation are the physical and mechanical properties of the cladding, its creep and yield characteristics, temperature gradient, boundary conditions

(internal and external pressures), and time. An LMFBR fuel pin operating at about 16 kw/ft would have temperature gradients of about 50°C across the 0.015-in.-thick cladding wall. Temperature gradients of this magnitude (over 3000°C/in.) would be expected to cause initial compressive stresses at the inner cladding surface and tensile stresses at the outer surface that would relax with time. The rate of relaxation would increase with average cladding temperature. We studied the relaxation of initial stresses for our typical LMFBR fuel pin cladding.⁹ This pin was divided into nine axial regions for purposes of calculation. Pertinent data for each of these nine regions are given in Table 4.2. Figure 4.7 indicates the relaxation of the inner compressive stresses and outer tensile stresses with time for the top, bottom, and center axial regions of the typical LMFBR fuel pin. As noted in Table 4.2, the average cladding temperature increases from 475°C for region 1 to 644°C in region 9. As expected, stress relaxation for the cladding in axial region 9 is much more rapid than that for regions 1 or 5. For this calculation, we used literature data¹⁵⁻¹⁸ for the physical and mechanical properties. Pressures on the outside of the cladding are given in Table 4.2; the pressures on the inside of the cladding were assumed to vary linearly from 100 to 1500 psi in 10,000 hr.

The results presented in Fig. 4.7 are based on the assumption that the geometry of the cladding is changed only by the initial deformation and by creep. However, if the effect of cladding swelling due to formation of voids is superimposed on the changes in geometry due to creep, a different set of conditions results. To model this situation we assumed that cladding swelling can be included in the calculation by adjusting the linear coefficient of thermal expansion to include the volume changes due to formation of voids in the cladding:

$$\alpha = \alpha_T + \frac{\% \Delta V/V}{300 T}, \quad (1)$$

where

- α = the effective coefficient of linear thermal expansion,
- α_T = the coefficient of linear thermal expansion due to thermal considerations alone,

Table 4.2. Reference Data for a Typical LMFBR Fuel Pin

Axial Region Number	Coolant Temperature (°C)	Average Cladding Temperature (°C)	External Pressure on Cladding (psi)	Neutron Flux (neutrons cm ⁻² sec ⁻¹)	Temperature Rise Across Cladding (°C)	Temperature of Fuel Center (°C)	Rate of Heat Generation (kw/ft)
				× 10 ¹⁵			
1	440	475	95	7.26	44	2238	13.36
2	462	498	88	7.79	46	2399	14.32
3	484	523	81	8.43	48	2587	15.45
4	506	547	73	8.75	50	2708	16.09
5	528	568	66	8.75	49	2735	16.09
6	549	588	59	8.51	48	2689	15.61
7	570	607	51	8.05	44	2597	14.80
8	592	625	44	7.26	40	2417	13.36
9	614	644	37	6.74	37	2309	12.40

Fig. 4.7. Changes in Cladding Stresses with Time. Swelling of the cladding was not considered.

$\% \Delta V/V$ = the amount of cladding swelling due to void formation, as calculated from the equation given in Fig. 4.6 (for type 316 stainless steel cold worked 20%), and

T = temperature ($^{\circ}\text{C}$).

This approach has also been suggested by other investigators.¹⁹

When swelling of the cladding is considered in the calculation for stress relaxation, we find that relaxation is retarded at the cool end of the pin because the inner surface of the cladding expands faster than the outer surface due to the formation of voids. At the hot end of the fuel pin, the outer surface of the cladding expands faster than the inner surface (a recent equation¹² for swelling of 20% cold-worked type 316 stainless steel cladding predicts peak swelling at about 627°C ; therefore, for cladding hotter than this temperature, the cooler surface will expand faster than the hotter surface) and relaxation is enhanced. This effect is summarized by the results plotted in Fig. 4.8 for our typical fuel pin. The same physical and mechanical properties and the same boundary conditions used for the calculations presented in Fig. 4.7 were

Fig. 4.8. Changes in Cladding Stresses with Time Using Modified Equation for Swelling of Cladding.

used for the plot in Fig. 4.8. The modified equation for cladding swelling shown in Fig. 4.6 was used for the results in Fig. 4.8.

The effect of cladding swelling on stress relaxation is demonstrated by comparison of Figs. 4.7 and 4.8. Figure 4.9 indicates the effect of swelling of the cladding due to formation of voids on the calculated diametral expansion of the cladding for axial regions 1, 5, and 9 of the typical LMFBR fuel pin. Three cases are presented in Fig. 4.8 based on the assumptions of (1) no cladding swelling, (2) swelling according to Biancheria's equation¹² (solid curves in Fig. 4.6), and (3) swelling according to our modification of Biancheria's equation (dashed curves in Fig. 4.6). The dangers associated with gross extrapolation beyond the range of experimental data are demonstrated graphically by the results of the assumption of swelling according to Biancheria's equation. This suggests strongly the need for data for cladding swelling at higher fluences. Even our modifications of the equation for swelling result in calculated diametral expansions that would probably be unacceptable to designers.

Fig. 4.9. Effect of Cladding Swelling on Diametral Expansion.

Thermophysical and Mechanical Properties of Type 316 Stainless Steel (W. H. Bridges)

A review of the physical and mechanical properties of type 316 stainless steel is in preparation. The section on thermophysical properties²⁰ covers density, melting range, specific heat, thermal expansion, thermal conductivity, thermal diffusivity, emissivity, and electrical resistivity. The influence of environment, particularly neutron irradiation, is given when such data are available. The section on tensile properties is undergoing review before issuance as an internal memorandum.

Tensile Yield Data for Type 316 Stainless Steel Tubing (R. W. Swindeman)

The data we obtained on type 316 stainless steel tubing fabricated at ORNL are summarized here for use in modeling studies for LMFBR fuel pins. Four-inch segments were tested longitudinally on an Instron machine. Both cold-worked (20%) and annealed (1065°C for 15 min) conditions were studied at room temperature and 650°C. Of primary concern were the shape

of the yield curve and the effect of strain rate at 650°C. Some data regarding the ultimate strength and elongation were also obtained.

Yield curves at room temperature are shown in Fig. 4.10. Annealed tubes exhibit a slightly higher modulus of elasticity than expected (about 30.5×10^6 psi) and a well defined yield stress at 43,000 psi. The cold-worked tubes exhibited little, if any, truly elastic behavior. Between 0 and 10,000 psi the value for Young's modulus averages about 28.4×10^6 psi. Based on this value, the yield stress appears to be $114,000 \pm 7,000$ psi. One yield curve shown in Fig. 4.10 corresponds to a test after prestraining at 650°C. It is apparent that this treatment produced some recovery, inasmuch as the elastic behavior has been restored and the yield stress significantly reduced.

Yield curves for annealed material tested at 650°C are shown in Fig. 4.11. The value for the modulus of elasticity, 21.3×10^6 psi, is in fair agreement with the range of recognized values and shows little if any effect of strain rate. Discontinuous yielding occurs at the

Fig. 4.10. Tensile Strength of Type 316 Stainless Steel Tubing at Room Temperature.

Fig. 4.11. Yield Curves for 0.250-in.-OD $\times$ 0.016-in.-wall-thickness Annealed Type 316 Stainless Steel Tubing at 650°C.

higher strain rates, and it appears that the 0.2% offset yield strength is slightly higher at the lower strain rate, 0.00067 min^{-1} . The yield stress is about 21,000 psi.

The yield curves for cold-worked material tested at 650°C are shown in Fig. 4.12. There appears to be a significant effect of the strain rate on Young's modulus. At 0.067 and 0.0067 min^{-1} the value of the modulus of elasticity, $21.6 \times 10^6 \text{ psi}$, is close to the range of accepted values. At 0.00067 min^{-1} , and even more so at $0.000067 \text{ min}^{-1}$, nonelastic behavior is apparent. The yield stress is also reduced by a significant amount. Although not shown in the figure, the elongation at the maximum load decreases with decreasing strain rate from about 5% at 0.067 min^{-1} to less than 1% at $0.000067 \text{ min}^{-1}$.

Other data are summarized in Table 4.3.

Creep of Cold-Worked Modified Type 316 Stainless Steel (R. W. Swindeman)

We have reviewed data on the creep behavior of the British cold-worked modified type 316 stainless steel. There is evidence that this

Fig. 4.12. Tensile Strength of 20% Cold-Worked Type 316 Stainless Steel Tubing at 650°C.

Table 4.3. Tensile Data for Type 316 Stainless Steel Tubing^a

Test Temperature (°C)	Extension Rate ^b (in./min)	Young's Modulus of Elasticity (psi)	Strength, psi		Elongation (%)	Reduction in Diameter (%)
			Yield	Ultimate		
		× 10 ⁶	× 10 ³	× 10 ³		
<u>Annealed at 1065°C</u>						
Room	0.2	30.5	44	98	c	20
Room	0.02	30.5	43	98	c	21
650	0.2	21.6	21	60	40	20
650	0.02	21.3	21.5	d	d	d
650	0.002	20.4	22	d	d	d
<u>20% Cold Worked</u>						
Room	0.005	28.4	114	124	c	21
Room	0.2	21.6	70	77	15	12
650	0.02	21.6	65	71	25	18
650	0.002		52	60	42	21
650	0.0002		37	47		

^aAll tubing was produced at Oak Ridge National Laboratory and was 0.250 in. in outside diameter and 0.016 in. in wall thickness.

^bActual strain rate (inches per inch per minute) is about one-third of extension rate.

^cFailed outside gage mark.

^dFailed at extensometer clamp.

material undergoes "creep softening" at low stresses, which makes it difficult to describe both primary and secondary creep analytically.

To what extent the creep softening is a real and reproducible effect is questionable. For the purposes of developing models for the behavior of cladding for fuel pins, it is probably best to assume some extremes and determine how performance is affected.

One possible equation for creep rate ($\dot{\epsilon}$) for the stress range 0 to 20,000 psi, which assumes no strain softening, is

$$\dot{\epsilon}_s = A\sigma^n,$$

where

$$A = 2.5 \exp(-89,000/RT), \text{ and}$$

$$n = 4.$$

Another equation, which assumes softening at all stress levels and strength decreasing to that of modified type 316L stainless steel, is

$$\dot{\epsilon}_s = A(\sinh \alpha\sigma)^n,$$

where

$$A = 1.3 \times 10^{17} \exp(-83,000/RT),$$

$$\alpha = 1/15,800, \text{ and}$$

$$n = 3.9.$$

The Search for the Mitra-McLean Effect in Type 316 Stainless Steel (R. W. Swindeman)

We have prepared a number of creep curves for annealed stainless steel that show the performance after reductions in stress to establish whether significant recovery occurs as described by Mitra and McLean¹⁴ for Ni and Al. This effect has also been observed¹⁵ for W.

Figure 4.13 shows schematically what we would expect if recovery occurred after a stress reduction. A period, Δt , should occur during which there should be no creep. Since, within the limits of sensitivity of our extensometer, no such effect was observed, there was either very rapid recovery or very sluggish recovery. We are inclined to believe that recovery was sluggish. In either event, primary creep is not pronounced for this specific heat of material, and recovery is of little

Fig. 4.13. Illustration of the Mitra-McLean Effect.

practical importance. This is not necessarily true for cold-worked type 316 stainless steel. Even other heats of type 316 stainless steel, in the annealed condition, could behave differently. We consider it prudent to continue such studies on other materials, preferably LMFBR-grade tubing, since the existence of such a phenomenon could have a pronounced effect on the performance of fuel pins during power cycling.

Notes

1. F. Anselin and W. E. Baily, Trans. Am. Nucl. Soc. 10(1), 103-104 (1967).
2. E. L. Zebroski et al., "Review of Status of Technology of Fast Reactor Fuels," pp. 2-63-2-80 in Fast Reactors National Topical Meeting, San Francisco, April 10-12, 1967, ANS-101, American Nuclear Society, San Francisco, 1967.
3. G. W. Greenwood and M. V. Speight, J. Nucl. Mater. 10(2), 140-144 (1963).
4. I. Goldberg, L. L. Lynn, and C. D. Sphar, FIGRØ - FORTRAN IV Digital Computer Program for the Analysis of Fuel Swelling and Calculation of Temperatures in Bulk Oxide Cylindrical Fuel Elements, WAPD-TM-618 (December 1966).

5. C. M. Cox, R. B. Fitts, and F. J. Homan, Fuels and Materials Development Program Quart. Progr. Rept. March 31, 1969, ORNL-4420, pp. 33-39.
6. R. C. Daniel et al., Effects on High Burnup on Zircaloy-Clad, Bulk UO₂, Plate Fuel Element Samples, WAPD-263 (September 1962).
7. G. Karsten et al., Fabrication of Fast Reactor Fuel Pins for Test Irradiations, KFK-577 (April 1967).
8. Thomas R. Padden, Polyvinyl Chloride Replica Technique for High Burnup Reactor Fuels, WAPD-T-1595 (August 1963).
9. F. J. Homan, W. E. Stillman, and B. R. Dewey, Fuels and Materials Development Program Quart. Progr. Rept. Dec. 30, 1969, ORNL-4520, pp. 85-91.
10. W. K. Appleby et al., Oxide Fuel Element Development Quart. Progr. Rept. June 30, 1969, WARD-3791-39, p. 4.
11. E. E. Bloom, Oak Ridge National Laboratory, personal communication, February 1970.
12. A. Biancheria et al., Oxide Fuel Element Development Quart. Progr. Rept. Sept. 30, 1969, WARD-4135-1, pp. 6-9.
13. E. E. Bloom and J. O. Stiegler, Fuels and Materials Development Program Quart. Progr. Rept. Sept. 30, 1969, ORNL-4480, pp. 76-85.
14. C. M. Cox and F. J. Homan, Fuels and Materials Development Program Quart. Progr. Rept. Sept. 30, 1969, ORNL-4480, pp. 63-64.
15. Pipes and Tubes for Elevated Temperature Service, USS Bull. 26, ed. 59, p. 62, National Tube Division, United States Steel Corporation, Pittsburgh, Pennsylvania, 1959.
16. T. Lyman (ed.) Metals Handbook, 8th ed., p. 423, vol 1, American Society for Metals, Metals Park, Novelty, Ohio, 1961.
17. E. C. Bishop et al., Stainless Steel Cladding Development Quart. Progr. Rept. June 30, 1969, WARD-3791-41, p. 13.
18. A. Biancheria et al., Oxide Fuel Element Development Quart. Progr. Rept. Sept. 30, 1969, WARD-4135-1, p. 19.
19. W. E. Bailey, Sodium-Cooled Reactors Fast Ceramic Reactor Development Program, Thirty-First Quarterly Report, May-July 1969, GEAP-10028-31. pp. 7-22-7-23.
20. W. H. Bridges, The Physical and Tensile Properties of Type 316 Stainless Steel, ORNL-TM-3037, in press.