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MECHANICAL BEHAVIOR OF FAST REACTOR FUEL PIN CLADDING SUBJECTED TO SIMULATED OVERPOWER TRANSIENTS

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Hanford Engineering Development Laboratory

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G. D. Johnson

C. W. Hunter

June 1978

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ABSTRACT

Cladding mechanical property data for analysis and prediction of fuel pin transient behavior were obtained under experimental conditions in which the temperature ramps of reactor transients were simulated. All cladding specimens were 20% CW Type 316 stainless steel and were cut from EBR-II irradiated fuel pins. It was determined that irradiation degraded the cladding ductility and failure strength. Specimens that had been adjacent to the fuel exhibited the poorest properties.

Correlations were developed to describe the effect of neutron fluence on the mechanical behavior of the cladding. Metallographic examinations were conducted to characterize the failure mode and to establish the nature of internal and external surface corrosion. Various mechanisms for the fuel adjacency effect were examined and results for helium concentration profiles were presented. Results from the simulated transient tests were compared with TREAT test results.

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MECHANICAL BEHAVIOR OF FAST REACTOR FUEL PIN CLADDING
SUBJECTED TO SIMULATED OVERPOWER TRANSIENT (FF020)

SUMMARY

In-reactor measurement of cladding stress and strain on fuel pins undergoing an actual overpower transient is not currently possible. This situation necessitates the testing of fuel pin cladding under simulated transient conditions to obtain mechanical property data. The general approach of simulated transient testing of cladding specimens is to define the failure stress and strain of the cladding for the temperature range of 1000⁰-2500⁰F (811-1644 K) at transient heating rates of 10 F⁰/s (5.6 K/s) and 200 F⁰/s (111 K/s). These rates were selected as being representative of the range of interest of transient overpower conditions for the FFTF.

Simulated transient tests were conducted on 131 cladding specimens from EBR-II irradiated fuel pins. These fuel pins contained mixed oxide fuel, and they were clad with 20% CW Type 316 stainless steel. Irradiation of these fuel pins in EBR-II resulted in burnups of 11 to 59 MWd/kg. Corresponding neutron fluences of the cladding specimens ranged from 0.3 to 5.8×10^{22} n²/cm² (E > 0.1 MeV).

Results of the simulated transient tests showed that irradiated cladding specimens exhibited reduced ductility and failed at lower temperatures during the transient tests than unirradiated specimens. Under the constant pressure conditions of the test, a decrease in failure temperature of the irradiated specimen below the failure temperature of the unirradiated specimen represents a reduction in the failure strength. It was determined that specimens from the fuel column exhibited lower failure strengths than specimens from the plenum or below the fuel column, and also, this effect was greater for specimens tested at 10 F⁰/s (5.6 K/s).

The effects of reactor exposure on the mechanical properties of the cladding were described in a quantitative manner. Failure strengths were analyzed with the Larson-Miller Parameter (LMP). Separate LMP correlations were obtained for different fluence regions for the irradiated specimens. Examples were provided to show that these correlations could be used with the life-fraction rule to predict the time-temperature-stress conditions which will produce cladding failure. Failure strain data for each heating rate were described as functions of neutron fluence and failure temperature. It was observed that the failure strains of fuel column specimens decrease with increasing fluence and eventually exhibited a saturation.

Although the effect of reactor exposure could be empirically described, it is of importance to conduct various metallurgical examinations in an attempt to evaluate which environmental factors were responsible for the observed mechanical behavior. Metallographic examinations were conducted to evaluate the depths of wear marks, to measure the depths of inner and outer surface corrosion, and to examine the fracture mode. If wear marks were present on the specimens, the failure site was usually located in the wear mark. From the measured depths of the wear marks, it was shown that accounting for the effect of wear marks on a net-section basis was conservative for design purposes. Many cladding specimens had slight amounts of inner surface corrosion, with maximum depths of attack being on the order of 0.001 in. (0.025 mm). It was determined that the inner surface corrosion had little effect on the mechanical behavior of the cladding. Very little outer surface corrosion existed in any of the specimens. Metallographic analysis of the failure sites of the specimens revealed that the specimens that exhibited the poorest mechanical behavior failed in an intergranular mode, while the specimens that exhibited the best behavior failed transgranularly. Various fracture criteria were examined for their applicability to the intergranular failures. None were found to be acceptable for describing the effects of temperature and strain rate.

Cladding specimens from the fuel column region exhibited lower strength than specimens that were not adjacent to the mixed oxide fuel. Many of the

fuel column specimens that failed in the region of 1100-1600⁰F (866-1144 K) exhibited intergranular failures. The observation of the lower strength of fuel column specimens coupled with an intergranular failure mode was termed the "fuel adjacency effect". Mechanisms proposed for this effect were enhanced helium embrittlement, in-reactor steady-state stress, fuel-cladding mechanical interaction, residual stress, radial cladding temperature gradients, internal surface corrosion, and grain boundary embrittlement. Analysis showed that none of these factors, with the possible exception of grain boundary embrittlement, were primarily responsible for the fuel adjacency effect. It is proposed that the fuel adjacency effect was a result of impurities absorbed or segregated on the cladding grain boundaries.

Results from the simulated transient tests were used to estimate the cladding stress and strain included during TREAT test HUT 3-5B. Good agreement was obtained between the measured strain for the fuel pin and the calculated strain obtained from the simulated transient test results. Simulated transient tests were conducted on specimens from two TREAT-tested fuel pins, HOP 3-2A and HOP 3-2B, to assess prior damage. These pins were subjected to simulated FFTF 3 \$/s overpower transients terminated by the Plant Protective System (PPS). Results from the transient tests conducted on cladding specimens from these pins show that little, if any, damage was introduced into the cladding. This demonstrates that PPS termination of a 3 \$/s hypothetical transient not only precludes cladding breach but even prevented significant pin damage.

I. INTRODUCTION

Cladding mechanical property data for analysis and prediction of fuel pin transient behavior were obtained under experimental conditions in which the temperature ramps of reactor transients were simulated using the Fuel Cladding Transient Tester (FCTT). The general approach of simulated transient testing of fuel cladding specimens consisted of determining the failure strength and ductility for cladding in the temperature range of 1000-2500°F (811-1644 K) at heating rates of 10°F/s (5.6 K/s) and 200°F/s (111 K/s). These heating rates were selected as being applicable to the range of interest for FFTF transients.

An earlier report⁽¹⁾ presented FCTT data for 49 cladding specimens that were irradiated at temperatures from 700-1000°F (644-811 K) to peak neutron fluences of 4×10^{22} n/cm² ($E > 0.1$ MeV). This report presents the data obtained on an additional 131 specimens which were irradiated at temperatures from 700-1300°F (644-977 K) to peak fluences of 5.8×10^{22} n/cm² ($E > 0.1$ MeV). Test methods and procedures have been described or referenced in the previous report.⁽¹⁾ Figures and correlations presented in this report are based on the total data base of 180 specimens.

In this report major emphasis is placed on failure analysis and correlations for failure temperatures and failure strains. A detailed discussion of the various fracture modes is presented together with some metallographic observations of the fractures. It is shown that corrosion and wear marks have little effect on overall cladding behavior. Specimens, which have been adjacent to the fuel, experience a considerable reduction in strength; the mechanisms for this "fuel adjacency effect" are reviewed. The behavior of specimens from breached fuel pins and TREAT-tested fuel pins were also evaluated with the FCTT. It was determined that high irradiation temperatures can alter the strength and ductility of the cladding. Finally, it is shown how the FCTT data may be applied in analyzing TREAT test results.

II. DESCRIPTION OF FUEL PIN IRRADIATIONS

The FCTT specimens were obtained from fuel pins that were irradiated in 37- and 61-pin subassemblies in EBR-II as part of the HEDL Steady-State Irradiation Testing Program.⁽²⁾ Table 1 summarizes the irradiation history of the fuel pins used for the FCTT specimens. The fuel pins contained fuel of 25 wt% PuO₂ + 75 wt% UO₂ clad with 20% CW Type 316 stainless steel. All of these pins had a nominal outside diameter of 0.230 in. (5.84 mm) with a cladding wall thickness of 0.015 in. (0.38 mm). The majority of the fuel pins used N-lot tubing (Heat 87210) for the cladding. Some of the P-23A fuel pins used Lots O and T (Heats 88229 and 87210) tubing in addition to the N-lot tubing. The fuel pin subassemblies were unencapsulated, thereby allowing sodium to flow past all of the pins.

After EBR-II irradiation fuel pins P-23A-27 and P-23A-30 were tested in TREAT as part of the HOP test series and were designated HOP 3-2A and HOP 3-2B, respectively. These pins were subjected to transient overpower conditions which simulated FFTF 3\$/s transients terminated by the FFTF Plant Protective System (PPS).

TABLE 1

FUEL PIN IRRADIATION HISTORY

<u>Fuel Pin</u>	<u>Subassembly</u>	<u>Burnup, Mwd/Kg</u>	<u>Pin Power KW/Ft</u>	<u>Maximum Midwall Cladding Temp, °F (K)</u>	<u>Maximum Neutron Fluence, 10²² n/cm² (E > 0.1 MeV)</u>
P-23A-21	X-138	44.2	11.2	1285 (969)	4.10
P-23A-25	X-138	44.5	11.5	1337 (998)	4.13
P-23A-27	X-138	22.7	11.7	1320 (989)	1.96
P-23A-30	X-138	45.9	10.7	1264 (957)	4.10
P-23A-31	X-138	21.9	11.3	1309 (982)	1.96
P-23A-32	X-138	21.7	11.6	1288 (971)	1.94
P-23A-59C	X-138	10.8	11.7	1264 (957)	0.94
P-23A-60D	X-138	11.6	12.1	1305 (980)	1.08
P-23B-2A	X-181	53.5	11.3	1330 (994)	4.81
P-23B-14A	X-181	51.6	10.9	1270 (961)	4.75
P-23B-35B	X-181	51.9	11.0	1290 (972)	4.81
P-23C-3A	X-203	11.6	11.2	1114 (874)	1.06
P-23C-25B	X-203	11.4	11.0	1109 (871)	1.05
P-23C-29B	X-203	11.4	11.0	1108 (871)	1.05
P-23C-49C	X-203	11.6	11.2	1114 (874)	1.06
P-23C-71G	X-203	11.4	11.1	1198 (921)	1.05
NUMEC E-029	X-096	23.0	12.0	1139 (888)	1.76
NUMEC E-049	X-096	22.7	11.9	1135 (886)	1.73
NUMEC E-067	X-191	37.2	12.0	1114 (874)	3.16
NUMEC E-122	X-191	37.2	11.9	1111 (872)	3.09
PNL 9-13	X-087	43.8	5.6	990 (805)	5.35
PNL 9-30	X-087	45.6	5.7	1010 (816)	5.59
PNL 9-60	X-087	48.7	5.9	920 (766)	6.05
PNL 10-14	X-193	58.8	8.7	1020 (822)	4.96
PNL 10-15	X-193	58.3	8.7	1030 (827)	4.90
PNL 11-37	X-107	45.4	11.5	1010 (816)	4.17
PNL 11-48A	X-107	44.5	11.3	1015 (819)	4.02

III. TEST RESULTS

A. DATA TABLES

Summaries of the irradiation conditions and test results from simulated transient tests conducted on 131 specimens from 28 different fuel pins are presented in Tables 2 through 6. The irradiation temperatures are the average midwall temperatures along the specimen and the neutron fluence listed is the average value. Both fluence and temperature were obtained from the HIST version of SIEX.⁽³⁾ Diametral failure strain values, reported in the tables, were calculated from pre- and post-test diameter measurements. The strength ratio is the failure strength for the irradiated cladding specimen divided by the failure strength of an unirradiated cladding specimen at the same failure temperature. The strength ratio provides a quantitative measure of the amount of cladding failure strength left after steady-state irradiation. The wear mark depths listed in the data tables were obtained from metallographic sections.

Hoop stresses used in subsequent figures and discussions were calculated for the inner surface using the elastic thick-walled equation:

$$\sigma = \frac{(D_o^2 + D_i^2)}{(D_o^2 - D_i^2)} \cdot P \quad (1)$$

Where D_o is the outside diameter, D_i is the inside diameter, and P is the internal gas pressure. The cladding used for all of the fuel pins listed in Table 1 had initial dimensions of $D_o = 0.230$ in. (5.84 mm) and $D_i = 0.200$ in. (5.08 mm). Substitution of these values into Equation (1) gives $\sigma \cong 7.2 P$ (or $\cong 49.64 P$, MPa).

B. GENERAL TRENDS FOR FAILURE STRENGTH

The data from Tables 2 through 6, combined with the previously reported data⁽¹⁾ are presented in Figures 1 through 6 showing the failure strength as a function of temperature for irradiated fuel cladding. As described previously,^(1,4) the mechanical properties of cladding are quite different for various regions of the fuel pin. In particular, fuel column specimens have the poorest properties and the specimens from below the fuel column have the best. Therefore, the failure strength results were separated according to axial position in the fuel pin, as well as for transient heating rate. Figures 1 and 2 give the failure temperatures for fuel column specimens at $10\text{F}^0/\text{s}$ (5.6 K/s) and $200\text{F}^0/\text{s}$ (111 K/s), respectively. Similarly, Figures 3 and 4 are for plenum specimens and Figures 5 and 6 are for specimens from below the fuel column.

Under the constant-pressure conditions of an FCTT test, the irradiation effect on the cladding strength is exhibited by changing the failure temperature of the irradiated specimen, relative to the failure temperature of an unirradiated specimen. In Figures 1 through 6, data points to the left of the unirradiated cladding line indicate decreased hoop stress at failure, while points to the right indicate increased failure strength. Figures 1 through 6 clearly show that the fuel column specimens exhibit the lowest failure strengths.

As described previously,^(1,4) the strength degradation is greatest between approximately $1000\text{--}1600\text{F}$ ($811\text{--}1144\text{ K}$). At the $10\text{F}^0/\text{s}$ (5.6 K/s) rate, the transition from high strength to embrittled lower strength behavior occurs at 1000F (811 K), while at the $200\text{F}^0/\text{s}$ (111 K/s) rate, it is at $1150\text{ to }1200\text{F}$ ($894\text{--}922\text{ K}$). As in Reference 1, this transition is associated with the fracture mode transition from transgranular fracture at lower temperatures, to intergranular fracture, at higher temperatures. The higher strain rate of the $200\text{F}^0/\text{s}$ (111 K/s) tests raises the fracture mode transition temperature. The data points near or above the unirradiated

cladding curve at 100,000 psi (689 MPa) in Figures 1 and 2 demonstrate the high strength transgranular fracture behavior. At test temperatures above 1600°F (1144 K), the lower flow stress allows stress concentrations at grain boundaries to be blunted and relieved; consequently, intergranular embrittlement is not effective in degrading the strength (see Section IV, B).

Particularly for the $10F^0/s$ (5.6 K/s) data in Figure 1, specimens are shown to fail at 18,000 to 30,000 psi (124-207 MPa) over a wide temperature range. This temperature-independent failure strength behavior may result from critical fracture initiation stress, which is not as temperature dependent as the strength of the unirradiated material. This is discussed in Section IV.

At the lower fluences previously reported, the rate of degradation of strength of the $10F^0/s$ (5.6 K/s) tested specimens was consistently greater than for the $200 F^0/s$ (111 K/s) rate. The trend still exists; however, the highest fluence $200F^0/s$ (111 K/s) specimens, PNL-9-13-H and PNL-9-16-K, had a strength ratio of only 40% and failed by an intergranular pinhole through the cladding wall, which is typical of the poorer behavior during the $10F^0/s$ (5.6 K/s) test.

C. GENERAL TRENDS FOR FAILURE STRAIN

In the previous report⁽¹⁾ the effect of neutron fluence on the failure strain was described with the strain ratio. The strain ratio, ϵ_R , is the ratio of the irradiated cladding failure strain to the unirradiated cladding failure strain at the failure temperature for the irradiated specimen. A correlation was then obtained to define ϵ_R as a function of neutron fluence for each gas pressure and heating combination. These correlations were used to define the trend of failure strain with neutron fluence for any particular test condition; however, there was a minor problem with this approach. The actual failure temperature did not appear in the correlation for the

irradiated material. Thus, a generalized description of failure strain as a function of failure temperature and neutron fluence was not readily obtainable.

A new correlation was developed to show the relationship between these three variables. First, the failure strain data for all of the fuel column specimens at each heating rate were grouped into five regions of failure temperature, namely, 1000, 1200, 1400, 1600, and 1800°F (811, 922, 1033, 1144 and 1255 K). Data at each temperature actually represents a range of failure temperatures that are in a $\pm 100^\circ\text{F}$ (± 55.6 K) region of that temperature. These data are shown in Figure 7 and 8 for a heating rate of 10°F/s (5.6 K/s) and in Figures 9 and 10 for 200°F/s (111 K/s).

The correlation method consisted of first describing the unirradiated cladding failure strain, ϵ_u , as a function of temperature. A value for the irradiated specimen, ϵ_i , is obtained by multiplying the unirradiated cladding value by a fluence factor, F . Thus:

$$\epsilon_i = \epsilon_u \times F \quad (2)$$

For a heating rate of 10°F/s (5.6 K/s),

$$\begin{aligned} \epsilon_u = & -13.5473 + 6.24934 \times 10^{-2} T - 9.53733 \times 10^{-5} T^2 \\ & + 5.91599 \times 10^{-8} T^3 - 1.22263 \times 10^{-11} T^4 \end{aligned} \quad (3)$$

and
$$F_{10} = 0.93 \exp (-1.104 \phi t) + 0.07 \quad (4)$$

Where ϵ_u is in %, T in $^\circ\text{F}$, and ϕt is the neutron fluence in $(\text{n/cm}^2)/10^{22}$.

Failure strains calculated from Equations (2) through (4) are shown in Figures 7 and 8 as solid curves.

At $200\text{F}^0/\text{s}$ (111 K/s),

$$\begin{aligned}\epsilon_u = & -11.6204 + 4.5342 \times 10^{-2} T - 6.03138 \times 10^{-5} T^2 \\ & + 3.33067 \times 10^{-8} T^3 - 6.2612 \times 10^{-12} T^4\end{aligned}\quad (5)$$

and
$$F_{200} = 0.915 \exp (-B \cdot \phi t) + 0.085 \quad (6)$$

The fluence coefficient, B , is given as:

$$\begin{aligned}B = & -1.0275 + 7.0398 T' - 13.7034(T')^2 + 10.3822(T')^3 \\ & - 2.5729 (T')^4\end{aligned}\quad (7)$$

where $T' = T/1000$. Failure strains calculated from Equations (2), (5), (6), and (7) are shown in Figures 9 and 10 as the solid curves. The correlations for the irradiated cladding failure strain at both heating rates are restricted to failure temperatures in the region of $900\text{-}1900^\circ\text{F}$ ($755\text{-}1311\text{ K}$). Data from P-23B pins, shown in Figure 10, displayed higher ductilities and were not included in the correlation for $200\text{F}^0/\text{s}$ (111 K/s). These data will be discussed in Section IX.

A three-dimensional representation of the dependence of cladding failure strain on both fluence and failure temperature is shown in Figure 11 for the $10\text{F}^0/\text{s}$ (5.6 K/s) data. At fluences greater than $3 \times 10^{22}\text{ n/cm}^2$ and at failure temperatures less than 1400°F (1033 K), low failure strains ($\sim 0.1\%$) occur. Figure 11 also shows the high strains for low-fluence material at high failure temperatures, as well as the constant failure strain for fluences greater than $3 \times 10^{22}\text{ n/cm}^2$.

A similar figure for the $200\text{F}^0/\text{s}$ (111 K/s) data was constructed and is shown in Figure 12. The region of low strain values occurs for fluences greater than $3 \times 10^{22}\text{ n/cm}^2$ and at failure temperatures of $1200\text{-}1600^\circ\text{F}$.

(922-1144 K). This shift of the minimum strains to higher temperatures for the 200°F/s (111 K/s), in comparison to the 10°F/s (5.6 K/s) data, is a result of the higher strain rate. The failure strains for irradiated specimens are also greater; however, the failure strains for unirradiated specimens are lower at 200°F/s (111 K/s).

TABLE 2

FUEL CLADDING TRANSIENT BURST TEST RESULTS FOR CLADDING SPECIMENS
FROM P-23A-21, 25, 26, 31, 32, 59C, 60D and NUMEC E-029, 049

Pin	Spec.	IRRADIATION CONDITIONS				TEST CONDITIONS				TEST RESULTS					
		Distance Above Fuel Bottom		Avg. Midwall Temp		Avg. Neutron Fluence 10 ²² n/cm ² (E > 0.1 MeV)	Trans. °F/s	Rate K/s	Gas Pressure		Failure °F	Temp K	Diametral Failure Strain %	Strength Ratio %	Type Of Failure
		In.	mm	°F	K				psi	MPa					
P-23A-21:	M	0.6-3.0	15.2-76.2	815	708	3.35	10	5.6	4,000	27.58	1440	1055	0.22	49.4	Pinhole
	Q	4.0-6.5	101-165	964	791	4.01	10	5.6	2,500	17.24	1195	919	0.11	21.3	Pinhole
	T	7.4-9.9	188-251	1105	869	3.94	200	111	4,000	27.58	1790	1250	0.41	93.5	Burst
	V	10.3-12.8	262-325	1186	914	3.28	200	111	4,000	27.58	1770	1239	0.52	88.3	Burst
	CC	14.8-17.3	376-439	1246	947	1.85	10	5.6	4,000	27.58	1695	1197	1.19	92.9	Small Crack
	SS-1	23.6-26.2	599-665	1246	947	0.61	200	111	2,500	17.24	1860	1289	0.65	73.2	Burst
	SS-2	26.2-28.8	665-731	1246	947	0.45	10	5.6	2,500	17.24	1785	1247	0.56	77.6	Burst
P-23A-25:	JJ	15.3-17.8	389-452	1297	976	1.66	200	111	4,000	27.58	1775	1241	0.46	89.6	Burst
	WW-2	25.6-28.1	650-714	1297	976	0.46	200	111	2,500	17.24	1935	1330	1.39	98.6	Burst
P-23A-26:	K	0.8-3.3	20-84	835	719	1.53	10	5.6	10,000	68.95	1180	911	0.13	84.1	Pinhole
	FF	19.3-21.7	490-551	1300	977	0.51	10	5.6	4,000	27.58	1540	1111	0.92(a)	61.3	Burst
	KK	22.89-25.5	581-648	1300	977	0.32	10	5.6	2,500	17.24	1775	1241	0.48	75.3	Small Crack
P-23A-31:	J	0.8-3.3	20-84	830	716	1.61	200	111	6,000	41.37	1610	1150	0.35	88.2	Burst
	L	3.6-6.1	91-155	956	786	1.92	200	111	4,000	27.58	1800	1255	0.70	96.0	Burst
	S	10.0-12.5	254-318	1231	939	1.58	10	5.6	2,500	17.24	1765	1236	0.71	72.6	Burst
	W-1	13.9-16.4	353-417	1269	960	0.97	200	111	4,000	27.58	1765	1236	0.90	87.3	Burst
P-23A-32:	J	0.8-3.3	20-84	827	715	1.61	200	111	14,300	98.6	1125	880	0.13(b)	107.9	Burst
	L	3.6-6.1	91-155	946	781	1.88	10	5.6	2,500	17.24	1830	1272	1.30	91.8	Burst
	T-1	13.8-16.3	351-414	1248	949	1.00	200	111	6,000	41.37	1700	1200	1.35	109.6	Burst
	T-2	16.3-18.8	414-478	1248	949	0.74	10	5.6	2,500	17.24	1740	1222	0.56(a)	67.4	Small Crack
	T-3	18.8-21.3	478-541	1248	949	0.56	200	111	4,000	27.58	1770	1239	0.85	88.6	Burst
P-23A-59C:	J	0.8-3.3	20-84	827	715	0.79	200	111	4,000	27.58	1805	1258	1.13	97.6	Burst
	L	3.8-6.3	97-160	955	786	0.91	10	5.6	4,000	27.58	1325	991	0.13	40.1	Pinhole
	O	7.3-9.8	185-249	1106	870	0.89	200	111	6,000	41.37	1585	1136	0.35	83.4	Burst
	X	14.8-17.3	376-439	1234	941	0.39	200	111	2,500	17.24	1870	1294	1.05	75.0	Burst
P-23A-60D:	L	0.8-3.3	20-84	834	719	0.89	10	5.6	13,500	93.08	1170	905	0.46	112.4	Burst
	N	3.6-6.1	91-155	954	785	1.04	200	111	10,000	68.95	1420	1044	0.61	101.4	Burst
	Q	7.1-9.6	180-244	1106	870	1.05	10	5.6	6,000	41.37	1280	966	0.12	56.8	Pinhole
	S	10.2-12.7	259-323	1236	942	0.87	10	5.6	4,000	27.58	1665	1180	0.83	85.7	Burst
	Y	14.8-17.3	376-439	1264	957	0.44	10	5.6	4,000	27.58	1605	1147	1.35	72.5	Burst
N-E-029:	D	1.0-3.5	25-89	820	711	1.49	200	111	13,500	93.08	1115	875	----	101.3	Burst
	M	13.4-15.9	340-404	1114	874	0.94	200	111	2,500	17.24	1770	1239	0.56(a)	55.4	Burst
	P-1	18.9-21.4	480-544	1092	862	0.43	10	5.6	2,500	17.24	1790	1250	0.61	79.6	Small Crack
	P-2	21.4-24.0	544-610	1092	862	0.31	200	111	4,000	27.58	1815	1264	0.87	100.7	Burst
N-E-049:	D	1.0-3.5	25-89	819	710	1.47	10	5.6	14,300	98.6	1010	816	0.17	105.1	Burst
	E-2	4.0-6.5	102-165	909	760	1.69	200	111	13,500	93.08	1020	822	0.52	96.9	Burst
	K	9.3-11.8	236-300	1060	844	1.48	200	111	10,000	68.95	1185	914	0.56(c)	78.4	Burst
	L	11.8-14.3	300-363	1060	844	1.13	200	111	4,000	27.58	1600	1144	0.15	57.6	Burst, in wear mark

(a) Failure near swagelok, strain value may be erroneously low.

(b) Extensive tear failure, strain value may be erroneously low.

(c) Specimen failed at Wear Mark (Depth = 1.5 mils).

Neg 7806187-2

TABLE 3

FUEL CLADDING TRANSIENT BURST TEST RESULTS FOR CLADDING SPECIMENS FROM PNL-10-14,
PNL-10-15, NUMEC E-067, NUMEC E-122, P-23A-27 AND P-23A-30 TESTED AT 10F°/s (5.6 K/s)
AND 4000 psi (27.6 MPa) GAS PRESSURE

		IRRADIATION CONDITIONS				TEST RESULTS			DATA ANALYSIS	
Fuel Pin	Spec.	Distance Above Fuel Bottom		Average Midwall Temp		Average Fluence 10 ²² n/cm ² (E > 0.1 MeV)	Failure Temp	Diametral Failure Strain	Strength Ratio	Type Of Failure
		in.	mm	°F	K					
PNL-10-14	B	(-4.0)-(-1.5)	(-102)-(-38)	700	644	2.24	1681	1189	1.96	Burst
	C	(-1.5)-1.0	(-38)-25	730	661	3.19	1634	1163	1.26	Pinhole
	V	22.3-24.8	566-630	990	805	0.86	1706	1203	0.56	Burst
PNL-10-15	B	(-2.8)-(-0.3)	(-71)-(-8)	700	644	2.66	1690	1194	2.00	Burst
	C	(-0.3)-2.2	(-8)-56	740	666	3.65	1643	1168	1.15	Burst
	D	2.2-4.7	56-119	815	708	4.48	1382	1023	0.24	Pinhole
	E	4.7-7.2	119-183	870	739	4.83	1375	1019	0.13(a)	Pinhole
	P1	11.9-14.2	302-361	995	808	3.32	1341	1000	0.17(b)	Burst
	P2	14.3-16.6	363-422	995	808	2.43	1576	1131	1.57(c)	Burst
	V	19.3-21.8	490-554	1000	811	1.28	1638	1165	0.48(d)	Burst
	Z	23.7-26.2	602-665	1000	811	0.68	1664	1180	0.35(e)	Burst
NUMEC E-067	C	2.4-4.9	61-124	850	727	2.91	1465	1069	0.17	Pinhole
	D	4.9-7.4	124-188	925	769	3.12	1561	1122	0.26	Pinhole
	M	10.1-12.6	257-320	1060	844	1.50	1540	1111	0.74	Burst
	Q	14.4-16.9	366-429	1070	850	1.50	1696	1197	1.00	Burst
	R	16.9-19.4	429-493	1070	850	1.10	1647	1170	0.39	Burst
	W	20.4-22.9	518-582	1070	850	0.73	1727(f)	1215	1.00	Burst
NUMEC E-122	C	2.0-4.5	51-114	840	722	2.79	1409	1038	0.15	Pinhole
	D	4.5-7.0	114-178	910	761	3.04	1300	977	0.10	Pinhole
	L	9.4-11.9	234-302	1040	833	2.64	1536(g)	1109	0.34	Burst
	N	15.5-18.0	394-457	1065	847	1.26	1616	1153	0.37	Burst
	V	20.6-23.1	523-587	1065	847	0.68	1729	1216	0.83	Burst
	W	23.1-25.7	587-650	1065	847	0.49	1693	1196	0.61	Pinhole
P-23A-27 (HOP-3-2A)(h)	C	3.8-6.3	97-160	960	789	1.91	1486	1081	0.22	Pinhole
P-23A-30 (HOP-3-2B)(h)	D	3.7-6.2	94-157	950	783	3.98	1221	934	0.13	Pinhole

Notes:

- (a) Specimen failed at edge of wear mark; depth = 1.8 mils (0.05 mm).
 (b) Specimen failed in wear mark; depth = 4.9 mils (0.12 mm).
 (c) Specimen failed in wear mark; depth = 3.9 mils (0.1 mm).
 (d) Specimen failed in wear mark; depth = 1.3 mils (0.03 mm).
 (e) Specimen failed in wear mark; depth = 1.2 mils (0.03 mm).
 (f) Specimen tested to 1306°F with no failure; specimen then re-tested.
 (g) Specimen tested to 1346°F with no failure; specimen then retested.
 (h) Fuel pin subjected to terminated 3S/s TREAT transient.

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TABLE 4

FUEL CLADDING TRANSIENT BURST RESULTS FOR CLADDING SPECIMENS FROM
FUEL PINS PNL-9-13, PNL-9-30, PNL-9-60, PNL-11-37 AND PNL-11-48A

Fuel Pin	Spec.	IRRADIATION CONDITIONS				TEST CONDITIONS				TEST RESULTS							
		Distance Above Fuel Bottom		Average Midwall Temp		Average Neutron Fluence 10 ²² n/cm ² , E _n >0.1 MeV	Heating Rate		Gas Pressure		Failure Temp		Diametral Failure Strain, %	Strength Ratio %	Type of Failure	Depth of Wear Mark	
		In.	mm	°F	K		F°/s	K/s	psi	MPa	°F	K				In.	mm
PNL-9-13	B	(-7.8)-(-5.3)	(-198)-(-135)	700	644	1.46	10	5.6	4,000	27.6	1677	1187	1.74	84.3	Burst		
	C	(-5.3)-(-2.8)	(-135)-(-71)	700	644	2.10	10	5.6	14,300	98.6	1198	921	0.30	126.5	Burst		
	D	(-2.8)-(-0.3)	(-71)-(-7)	700	644	2.98	200	111	14,300	98.6	1246	947	0.17	121.7	Burst		
	G	1.0 - 3.5	25 - 89	769	682	4.51	10	5.6	4,000	27.6	1135	886	0.05	33.1	Pinhole		
	H	3.5 - 6.0	89 - 152	820	711	5.15	200	111	4,000	27.6	1380 ^(a)	1022	0.04	40.4	Pinhole		
	L	9.5 - 12.0	241 - 305	940	777	4.51	200	111	4,000	27.6	1546 ^(a)	1114	0.10	52.8	Pinhole		
	M	12.0 - 14.5	305 - 368	965	791	3.52	10	5.6	2,500	17.2	1251 ^(b)	950	0.08	23.6	Pinhole	0.0030 ^(d)	0.076
	N	14.5 - 17.0	368 - 432	968	793	2.58	10	5.6	4,000 ^(e)	27.6	1662 ^(b)	1179	0.67	81.0	Burst	0.0023	0.058
	O	17.0 - 19.5	432 - 495	968	793	1.90	--	---	13,800 ^(e)	95.2	687	637	1.87	92.9	Burst		
PNL-9-30	C	(-6.9)-(-4.4)	(-175)-(-112)	700	644	1.67	10	5.6	6,000	41.4	1547	1115	1.48	92.4	Burst		
	H	0.6 - 3.1	15 - 79	762	679	4.55	200	111	6,000	41.4	1414	1041	0.07	63.7	Burst		
	J	3.9 - 6.4	99 - 163	835	710	5.44	10	5.6	6,000	41.4	1141 ^(e)	889	0.08	49.9	Pinhole		
	O	9.4 - 11.9	239 - 302	953	785	4.73	10	5.6	4,000	27.6	1311 ^(b)	984	0.02	40.8	Small Crack	(f)	
	S	14.6 - 17.1	371 - 434	989	805	2.53	10	5.6	2,500	17.2	1792 ^(b)	1251	0.47	75.0	Small Crack	0.0032	0.081
	U	17.9 - 20.4	455 - 518	989	805	1.74	10	5.6	6,000 ^(e)	41.4	1502 ^(b)	1090	0.44	84.3	Burst	0.0029	0.074
	V	20.4 - 22.9	518 - 582	989	805	1.27	--	---	12,700 ^(e)	87.6	686 ^(b)	636	1.15	85.5	Burst	0.0021	0.053
PNL-9-60	C	(-5.3)-(-2.8)	(-135)-(-71)	700	644	2.21	10	5.6	6,000	41.4	1418	1043	0.39	72.3	Pinhole		
	D	(-2.8)-(-0.3)	(-71)-(-8)	700	644	3.18	200	111	6,000	41.4	1624	1157	0.66	92.1	Burst		
	G	1.0 - 3.5	25 - 89	762	679	5.06	200	111	14,300	98.6	1156	897	0.22	110.5	Burst		
	K	7.0 - 9.5	178 - 241	863	735	5.82	200	111	2,500	17.2	1665	1180	0.10	41.9	Pinhole		
	L	9.5 - 12.0	241 - 305	903	757	5.09	10	5.6	6,000	41.4	1010	816	0.08	44.4	Pinhole		
	M	12.0 - 14.5	305 - 368	920	766	3.93	200	111	10,000	68.9	1100	866	0.28	73.3	Burst		
	N	14.5 - 17.0	368 - 432	920	766	2.79	200	111	10,000	68.9	1421	1045	0.75	107.2	Burst		
	O	17.0 - 19.5	432 - 495	920	766	1.98	10	5.6	4,000	27.6	1687	1192	0.83	86.6	Burst		
PNL-11-37	B	(-6.5)-(-4.0)	(-165)-(-102)	700	644	1.30	10	5.6	4,000	27.6	1698	1199	2.05	89.4	Burst		
	C	(-4.0)-(-1.5)	(-102)-(-38)	700	644	1.90	10	5.6	4,000	27.6	1697	1198	2.18	89.1	Burst		
	D	(-1.5)-1.0	(-38)-25	736	664	2.76	10	5.6	4,000	27.6	1215	930	0.13	36.1	Pinhole		
	G	3.0 - 5.5	76 - 140	847	726	3.97	10	5.6	6,000	41.4	1106	870	0.03	48.2	Pinhole		
	L	8.8 - 11.3	224 - 287	977	798	3.72	10	5.6	4,000	27.6	1245 ^(b)	947	0.08	37.4	Pinhole		
	R	15.7 - 18.2	399 - 462	1002	812	1.60	200	111	4,000	27.6	1805 ^(b,c)	1258	0.79	95.3	Burst	0.0009	0.023
	S	18.2 - 20.7	462 - 526	1002	812	1.21	200	111	4,000	27.6	1754 ^(b,c)	1230	0.61	83.0	Burst	0.0010	0.025
PNL-11-48A	C	(-6.5)-(-4.0)	(-165)-(-102)	700	644	1.15	10	111	10,000	68.9	1328	993	0.74	104.6	Burst		
	D	(-4.0)-(-1.5)	(-102)-(-38)	700	644	1.77	200	5.6	14,300	98.6	1248	949	0.21	122.0	Burst		
	E	(-1.5)-1.0	(-38)-25	734	663	2.63	10	111	14,300	98.6	1010	816	0.07	105.9	Burst		
	H	3.0 - 5.5	76 - 140	847	726	3.83	10	111	2,500	17.2	1353	1007	0.03	27.1	Pinhole		
	N	10.8 - 13.3	274 - 338	980	800	3.59	200	5.6	6,000	41.4	1545	1114	0.10	79.0	Pinhole		
	R	14.7 - 17.2	373 - 437	1007	815	1.82	10	111	6,000	41.4	1537	1109	0.53	90.5	Burst		

(a) Thermocouple came off during test; specimen breached 0.35 seconds after TC came off.

(b) Failed in a wear mark.

(c) TC came off at 1695°F; test re-run.

(d) Specimen did not fail in the wear mark.

(e) Specimen burst while being pressurized at the initial temperature.

(f) Specimen had wear marks; did not fail in wear marks.

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TABLE 5

FUEL CLADDING TRANSIENT BURST TEST RESULTS FOR CLADDING SPECIMENS FROM
FUEL PINS P-23B-2A, P-23B-14A AND P-23B-35B TESTED AT 200 F°/s (111 K/s)

Fuel Pin	Spec.	IRRADIATION CONDITIONS					TEST CONDITIONS		TEST RESULTS				
		Distance Above Fuel Bottom		Avg. Midwall Temp		Avg. Neutron Fluence 10 ²² n/cm ² (E>0.1 MeV)	Gas Pressure		Failure Temp		Diametral Failure Strain, %	Strength Ratio %	Type of Failure
		In.	mm	°F	K		psi	MPa	°F	K			
P-23B-2A	T	4.0-6.5	102-165	981	800	4.69	10,000	68.9	1230	939	0.28	83.4	Burst
P-23B-14A	L	1.2-3.6	30-91	830	716	4.01	13,365	92.1	1096	864	0.17	97.5	Burst
	M	3.6-6.1	91-155	937	776	4.58	13,250	91.4	762	679	>1.0	90.4	Burst
	U	8.2-10.7	208-272	1129	882	4.41	6,000	41.4	1588	1137	1.02	85.4	Burst
	W	11.2-13.6	284-345	1235	941	3.43	6,000	41.4	1650	1172	0.70	96.9	Burst
P-23B-35B	O	4.0-6.5	102-165	964	791	4.69	2,500	17.2	1794	1252	0.25	57.3	Burst
	R	7.3-9.8	185-249	1106	870	4.62	4,000	27.6	1784	1246	0.21	89.3	Burst
	U	10.5-13.0	267-330	1230	939	3.75	4,000	27.6	1808	1260	0.83	95.4	Burst
	Z	17.0-19.5	432-495	1256	953	1.55	6,000	41.4	1644	1169	1.19	95.7	Burst

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TABLE 6

FUEL CLADDING TRANSIENT BURST TEST RESULTS FOR CLADDING SPECIMENS FROM FUEL
PINS P-23C-3A-49C, -71G, -25B AND -29B TESTED AT 200F %s (111K/s)

Fuel Pin	Spec.	IRRADIATION CONDITIONS				TEST CONDITIONS			TEST RESULTS				
		Distance Above Fuel Bottom		Avg Midwall Temp		Avg Neutron Fluence 10 ²² n/cm ² (E > 0.1 MeV)	Gas Pressure		Failure Temp °F K	Diametral Failure Strain %	Strength Ratio %	Type of Failure	
		In.	mm	°F	K		psi	MPa					
P23C-3A	C	(-1.0)-1.5	(-25)-38	743	668	0.70	13,500	93.1	1170	905	0.23	105.9	Burst
	D	1.5-4.0	38-102	822	712	0.93	10,000	68.9	1354	1007	0.75	97.2	Burst
	E	4.0-6.5	102-165	895	752	1.04	10,000	68.9	1195	919	0.17	80.3	Burst
	G	7.0-9.5	178-241	982	801	1.03	10,000	68.9	1384	1024	0.99	101.4	Burst
P23C-49C	F	4.0-6.5	102-165	896	753	1.04	6,000	41.4	1592 ^(b)	1140	0.30 ^(b)	106.4	Burst
	H	7.0-9.5	178-241	983	801	1.04	6,000	41.4	1533	1107	0.21	77.1	Burst
P23C-71G	E	1.6-4.1	41-104	840	722	0.92	12,000	82.7	1242	945	0.39	101.5	Burst
	F	4.1-6.6	104-168	930	772	1.03	9,000	62.1	1430	1050	0.40	97.6	Burst
	I	7.6-10.1	193-257	1053	840	1.00	8,000	55.2	1480	1077	0.91	93.9	Burst
	J	10.1-12.6	257-320	1083	857	0.85	5,000	34.5	1732	1217	0.26	97.5	Burst
P23C-25B(a)	C	(-1.0)-1.5	(-25)-38	743	668	0.70	13,500	93.1	1182	912	0.33	107.0	Burst
	D	1.5-4.0	38-102	821	711	0.92	10,000	68.9	1380	1022	0.58	100.8	Burst
	E	4.0-6.5	102-165	886	747	1.03	10,000	68.9	1370	1017	0.75	99.4	Burst
	F	6.5-9.0	165-229	958	787	1.03	10,000	68.9	1422	1045	1.14	107.1	Burst
	G	9.0-11.5	229-292	1032	829	0.93	4,000	27.6	1853	1285	1.3	109.0	Burst
	I	12.0-14.5	305-368	1072	851	0.66	4,000	27.6	1892	1306	1.87	123.5	Burst
P23C-29B(a)	C	(-1.9)-0.6	(-48)-15	722	656	0.62	6,000	41.4	1660	1177	1.48	99.0	Burst
	E	1.1-3.6	28-91	812	706	0.88	6,000	41.4	1406	1036	1.34	62.8	Burst
	F	3.6-6.1	91-155	884	746	1.01	6,000	41.4	1710	1205	1.26	111.0	Burst
	G	6.1-8.6	155-218	952	784	1.04	6,000	41.4	1695	1197	0.94	107.1	Burst
	H	8.6-11.1	218-282	1020	822	0.96	6,000	41.4	1606	1147	1.24	88.5	Burst
	K	12.1-14.6	307-371	1073	851	0.67	6,000	41.4	1682	1190	0.93	104.0	Small Crack

(a) Ti-Mod. Cladding; strength ratios are relative to unirradiated N-lot cladding.

(b) Test terminated at 1363 F with 0.08% strain; rerun, strain value of 0.30% is the sum of both tests.

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FIGURE 1. Failure Temperatures for Irradiated Cladding Specimens Taken from the Fuel Column and Tested at 10°F/s (5.6 K/s).

HEDL 7802-54.1

FIGURE 2. Failure Temperatures for Irradiated Cladding Specimens Taken from the Fuel Column and Tested at 200°F/s (111 K/s).

HEDL 7703-172.20

FIGURE 3. Failure Temperatures for Irradiated Cladding Specimens Taken from the Plenum and Tested at 10°F/s (5.6 K/s).

FIGURE 4. Failure Temperatures for Irradiated Cladding Specimens Taken from the Plenum and Tested at 200F°/s (111 K/s).

HEDL 7703-172.18

FIGURE 5. Failure Temperatures for Irradiated Cladding Specimens Taken from Below the Fuel Column and Tested at 10°F/s (5.6 K/s).

HEDL 7703-172.17

FIGURE 6. Failure Temperatures for Irradiated Cladding Specimens Taken from Below the Fuel Column and Tested at 200°F/s (111 K/s).

HEDL 7802-55.19

FIGURE 7. Diametral Failure Strains for Specimens Tested at 10°F/s (5.6 K/s).

HEDL 7802-55.6

FIGURE 8. Diametral Failure Strains for Specimens Tested at 10F°/s (5.6 K/s).

HEDL 7802-55.20

FIGURE 9. Diametral Failure Strains for Specimens Tested at 200F°/s (111 K/s).

HEDL 7802-55.18

FIGURE 10. Diametral Failure Strains for Specimens Tested at 200F°/s (111 K/s).

FIGURE 11. FCTT Failure Strain at 10^6 /s (5.6 K/s).

HEDL 7703-172.9

FIGURE 12. FCTT Failure Strain at 200F°/s (111 K/s).

HEDL 7703-172.8

IV. FAILURE ANALYSIS

A. LARSON-MILLER PARAMETER (LMP) FAILURE STRENGTH CORRELATION

A correlation was developed for the transient failure strengths shown in Figures 1-6 that is able to account for both the strengthening and degradation effects exhibited. The correlation allows for interpolation of the failure strength data to other rates and temperatures than those tested. The correlation employs the life fraction rule to obtain a relationship between stress and the Larson-Miller Parameter (LMP) over the stress and temperature range of the tests.

1. Unirradiated Material Correlation

This correlation was first developed for unirradiated 20% CW 316 Type stainless steel specimens and was based on test results from stress rupture, tensile, tube burst and transient data.⁽⁵⁾ The Larson-Miller Parameter is given by:

$$LMP = T(\log t_r + C) \quad (8)$$

where T = absolute temperature, $^{\circ}\text{R}$

t_r = time to rupture, hours

C = constant, usually on the order of 20.

Calculation of rupture times for the stress-rupture, tensile and burst tests was straightforward. Conversion of transient test data into LMP values required an integration with the life fraction rule of the temperature-time path during the test to the point of failure. Stress values used in the LMP correlation were modified to account for the variation in elastic modulus with temperature.

Failure strength data for tensile, stress-rupture, burst and transient tests were reduced to a single curve described by $LMP = f(\sigma)$. The equation for the unirradiated material given in Reference 5 was:

$$\begin{aligned} LMP = & 4.862 - 7.782 \times 10^{-2} \sigma + 2.458 \times 10^{-3} \sigma^2 \\ & - 4.862 \times 10^{-5} \sigma^3 + 4.746 \times 10^{-7} \sigma^4 \\ & - 1.865 \times 10^{-9} \sigma^5 \end{aligned} \quad (9)$$

where LMP is the Larson-Miller Parameter time 10^{-4} , and σ is the modulus modified stress in ksi. The modified stress was given as:

$$\sigma = \sigma_T \left(\frac{E_{1400}}{E_T} \right) \quad (10)$$

where σ_T = stress at temperature T

E_{1400} = elastic modulus at 1400°F

E_T = elastic modulus at temperature T in °F.

The modified stress values for the unirradiated material in Equation (9) were restricted to 2 to 90 ksi (13.8 to 621 MPa).

2. Irradiated Cladding Correlation

The same procedure was used to calculate Larson-Miller Parameter values for the test data on irradiated specimens. Separate equations for $LMP = f(\sigma)$ were determined for different fluence regions for the irradiated specimens from fuel column, plenum, and below the fuel column locations. A multi-linear regression computer code was employed to fit the data for each case. Table 7 presents a summary of the equation forms, regression coefficients and the significant statistical quantities. Figures 13 through 26 compare the LMP-stress correlations with the data points for the various cases.

For the lowest fluence region, 0 to 1×10^{22} n/cm², there is little difference in behavior among cladding specimens from various regions of the fuel pins; thus the same correlation was used for all specimens. In addition, data for irradiated cladding tubing (structural) specimens⁽⁶⁾ were included in the correlation. All of the data for the various types of cladding specimens for this fluence region are compared in Figure 13 with the solid curve calculated from the regression equation given in Table 7.

LMP correlations for fuel column specimens are given in Figures 14 through 18 for five fluence ranges from 1 through 6×10^{22} n/cm². Except for the highest fluence region, different correlations were formulated for the two heating rates. For the two cases, 2 to 3×10^{22} n/cm² at 200F⁰/s (111 K/s), and 4 to 5×10^{22} n/cm² at 10F⁰/s (5.6 K/s), no correlation is presented because there are insufficient data.

At LMP values of 27,000 to 35,000 (corresponding to failure temperatures from 1000-1600⁰F (811-1144 K), fuel column cladding specimens with fluences greater than 2×10^{22} n/cm² generally fail in a low-ductility intergranular mode when tested at 10F⁰/s (5.6 K/s), while the 200F⁰/s (111 K/s) rate produced a mixed fracture mode. The difference in strength resulting from (or coincidental with) the different fracture modes is clearly evident in Figure 16 for fluences of 3 to 4×10^{22} n/cm². However, as noted in the previous section and evident in Figure 18, the two highest-fluence 200F⁰/s tests in this LMP range (PNL-9-13-H and PNL-9-60-K) exhibited strength, ductility and failure modes similar to that of the 10F⁰/s (5.6 K/s) tests.

The correlation for the 10F⁰/s (5.6 K/s) case at a fluence of 3 to 4×10^{22} n/cm² may not provide the best description of the data, as evidenced by the low correlation coefficient in Table 7. An equation in which LMP is the independent variable yields a better fit to the data, but stress must be the independent variable for application of the life fraction rule. Additional data at higher and lower LMP values are required to establish the proper trend.

The effect of neutron fluence on the transient burst strength of fuel column specimens is summarized in Figure 19 for specimens tested at $10F^0/s$ (5.6 K/s) and in Figure 20 for specimens tested at $200F^0/s$ (111 K/s). Both figures show that, in general, for increasing values of neutron fluence there is a decrease in strength. At low LMP values there is a slight strengthening due to irradiation hardening; this strengthening is most evident at fluences below $3 \times 10^{22} \text{ n/cm}^2$, but may also continue at higher fluences. At $10F^0/s$ (5.6 K/s), the strengths at most fluence levels are lower than those for $200F^0/s$ (111 K/s), but at the highest fluence, the faster rate appears to have been degraded to strengths similar to the $10F^0/s$ (5.6 K/s) rate. At LMP values of $2.8\text{--}3.2 \times 10^4$, the data at $10F^0/s$ (5.6 K/s) have about the same values over the fluence range of 3 to $6 \times 10^{22} \text{ n/cm}^2$. It is possible that this behavior is an indication of a saturation in the decrease of strength with increasing fluence.

LMP correlations for specimens from below the fuel column are shown in Figures 21 through 23. For each fluence region, data for both heating rates can be described by the same correlation. Figure 24 compares the strengths at the different fluence levels. At low LMP values, the increase in strength above the unirradiated values is due to irradiation hardening and the decrease in strength at the higher LMP values is due to helium embrittlement.⁽¹⁾

Data from plenum specimens, together with data from structural specimens,⁽⁶⁾ at a fluence of 1 to $2 \times 10^{22} \text{ n/cm}^2$ are compared in Figure 25 with the LMP regression equation. There are no data for plenum specimens at higher fluences.

Figure 26 presents a comparison of the strengths of cladding specimens taken from different axial locations in the fuel pin. Specimens from below the fuel column exhibit the best properties while fuel column samples, particularly those tested at $10F^0/s$ (5.6 K/s), have the lowest strength.

3. Application of LMP Equations

The equations in Table 7, which were derived from the results of the cladding specimen tests, can be used to predict the time-temperature-stress conditions which will produce cladding failure. Failure is determined from the life fraction rule when:

$$\int_0^{t^*} \frac{dt}{t_r(\sigma, T)} = 1 \quad (11)$$

where t^* is the failure time and t_r is the stress-rupture life, σ is the stress, and T is the temperature. For a given stress and thermal ramp rate, life fractions can be calculated and summed up by numerical integration. The appropriate stress rupture life, t_r , can be determined from Equation (8) and the appropriate LMP equation in Table 7. When the sum of the life fractions equal one, failure occurs, thus yielding a failure time and temperature.

During the testing of four specimens, the thermocouples came off. Under fail-safe closed-loop temperature control, the induction heating power was turned off and the specimens cooled quickly; thus, the tests were terminated prior to failure. All specimens were subsequently retested to failure. An analysis was conducted to determine if any mechanical damage was produced by the terminated test. The analysis method employed consisted of calculating the accumulated life fraction incurred at the maximum temperature of the terminated transient. As shown in column three of Table 8, the life fraction values of the terminated transients for three specimens were small enough that no damage should have occurred. Also, the actual failure temperatures during the subsequent transients agreed quite well with the predicted failure temperatures. The life fraction of the fourth specimen, PNL-11-37-S, was only marginally significant, since a life fraction of 0.24 would produce only a $10F^0$ decrease in the failure temperature, which is certainly less than the scatter in the data. Calculation of the predicted failure temperatures, as well as the terminated transient life fraction,

were based on a life fraction calculation using the Larson-Miller Parameter for the appropriate fluence.

B. FRACTURE MODE

1. Stress x Strain as a Measure of Toughness

It has been previously shown that cladding with the lowest failure strains exhibited the largest decreases in failure temperature.⁽¹⁾ This type of behavior occurred under conditions of partial or total intergranular fracture. It was also shown that specimens, which exhibited transgranular failure, exhibited an increase in failure temperature. The current data exhibit the same trend. The specimens showing a large increase in failure temperature failed in a transgranular mode indicating that radiation hardening had occurred. Specimens showing a large decrease in failure temperature failed by intergranular fracture.

The two types of fracture, intergranular and transgranular, require large differences in the energy absorbed before failure occurs. In a standard tensile test, the total area under the stress-strain curve is indicative of the toughness of the material. Also, this area is a measure of the amount of work done on the specimen per unit volume. Similarly, in an FCTT test, the product of the hoop stress and failure strain, $\sigma \cdot \epsilon$, is also a measure of toughness. In a cladding transient test, the hoop stress is essentially constant throughout the test; thus, the product $\sigma \cdot \epsilon$ is a measure of the toughness and is analogous to the work done in a tensile test.

Values for the product $\sigma \cdot \epsilon$, at a heating rate of $10F^0/s$ (5.6 K/s), are shown in Figure 27 as a function of neutron fluence. Similarly, Figure 28 depicts the toughness for $200F^0/s$ (111 K/s) tests. For both figures, the data points represent the total data field to date, excluding specimens that

failed in wear marks. Fracture modes are indicated in the Figures for those specimens which were examined metallographically. The product $\sigma \cdot \epsilon$ is affected more by failure strain than by hoop stress, since the ϵ values vary by a factor of 20 while σ values vary by only a factor of 7.

In Figure 27, the lower edge of the data field is bounded by specimens exhibiting an intergranular fracture of the "pinhole" type. Data points, with higher $\sigma \cdot \epsilon$ values, are for intergranular fracture occurring at higher failure temperatures (under these conditions the failure strain was much greater) or for mixed mode failure (intergranular changing to transgranular). The general trend of the data field is toward lower values of $\sigma \cdot \epsilon$ at higher fluences. Comparison of Figure 28 with Figure 27 reveals three major differences exhibited by the data obtained at the 200F⁰/s (111 K/s) rate. First, the number of pinhole failures greatly decreased. Next, the lower and upper limits of the data field lie at higher values of toughness. Finally, the specimens exhibiting the higher $\sigma \cdot \epsilon$ values developed a transgranular fracture. These differences can be attributed to the difference in strain rates associated with the heating rates. The strain rates for the specimens heated at 10F⁰/s (5.6 K/s) were on the order of 10^{-4} to 10^{-3} sec⁻¹, while at 200F⁰/s (111 K/s) the strain rates were 10^{-2} to 10^{-1} sec⁻¹.⁽⁴⁾ As pointed out by Dieter,⁽⁷⁾ decreasing the strain rate increases the tendency for intergranular fracture. Thus, at the slower heating rate, there were many pinhole intergranular fractures with no specimens (metallographically examined) exhibiting transgranular fracture. However, at the higher heating rate, the higher strain rate nearly eliminated pinhole fractures and favored transgranular fractures.

The results in Figures 27 and 28 again indicate that the cladding properties are degraded by increasing fluence. Also, the irradiation-produced decrease in ductility is not offset by an increase in strength, since the product $\sigma \cdot \epsilon$ is seen to decrease with fluence.

2. Intergranular Fracture

From Figure 27, it is evident that the pinhole type of failure is characterized by a low toughness ($\sigma \cdot \epsilon$) value. This low $\sigma \cdot \epsilon$ value results from the low value of failure strain. At a heating rate of $10\text{F}^0/\text{s}$ (5.6 K/s), the failure strains of the pinhole-failure specimens range from 0.04% to 0.22%. A metallographic section of a typical pinhole failure is shown in Figure 29 for specimen P-23A-59C:L. The fracture is not wide and it is totally intergranular. The narrow width of the fracture is consistent with the low failure strain.

The essential feature of the low ductility intergranular fracture is that the intergranular cracking process occurs normal to the applied tensile stress. Thus, if the intergranular cracking or fracture stress is less than the flow stress, the specimen can fail without significant amounts of plastic strain. Therefore, the critical parameter to the degraded behavior is actually the intergranular cracking stress. In this section, several fracture analysis concepts and criteria are examined to help clarify the understanding of cladding behavior.

a. Analysis of Crack Initiation

Various fracture initiation criteria were reviewed by Petch.⁽⁸⁾ These criteria were based on dislocation mechanisms which produce crack initiation. The stress required for crack initiation is produced by a pile-up of dislocations. Although several models were examined, they all yield approximately the same result, namely:

$$\tau = c \left(\frac{G \cdot \gamma}{d} \right)^{1/2} \quad (12)$$

where τ = net shear stress compressing the dislocation pile-up

G = shear modulus

γ = surface energy

d = grain diameter

c = constant (ranging from 1.83 to 2.34, depending upon the model)

Using 1100°F (866 K) as a representative temperature, Equation (12) can be used to calculate the fracture initiation shear stress. A shear modulus value of 8.32×10^6 psi (575 GPa) was used for 316 Type stainless steel at 1100°F and the constant, C , was assumed to be 2. Values for γ were assumed to be on the order of 1000-1500 erg/cm² (1-1.5 J/m²). The 20% CW Type 316 stainless steel cladding on current HEDL mixed oxide fuel pins has an ASTM grain size in the range of 7 to 9. For these grain sizes and surface energies of 1000- 1500 ergs/cm² (1-1.5 J/m²), the shear stress values calculated from Equation (12) range from 14,635 to 17,924 psi (101-124 MPa). Conversion of these shear stresses to maximum principal stresses, using the maximum shear-stress theory, results in stresses of 33,990 to 41,630 psi (234-287 MPa) for crack initiation. The nature of the fracture shown in Figure 29 suggests that once the crack has formed, propagation of the crack along the grain boundaries may readily occur. These calculated stresses are of the right order of magnitude for the results shown in Figure 1. For the specimens that failed with a pinhole, however, Equation (12) cannot predict the temperature or rate dependence.

b. Griffith Fracture Equation

Griffith's criterion for propagation of a crack has been used for many years to evaluate the stress required to cause a crack to propagate in a brittle material. Even though this criterion is applicable only to a perfectly brittle material, it is useful to determine the magnitude of this stress for comparative purposes. Griffith's equation for the stress, σ_F , required to propagate a crack in a brittle material as a function of the size of the crack is:⁽⁹⁾

$$\sigma_F = \left(\frac{2E\gamma}{\pi a} \right)^{1/2} \quad (13)$$

where E is the elastic modulus, and a is the crack length. Evaluation of Equation (13) at 1100°F gives the following results. For a surface energy of 1000 ergs/cm² and a crack length of 0.0001 in. (0.0025 mm), the fracture stress is 28,000 psi (193 MPa), and for a surface energy of 1500 ergs/cm² with the same crack length, the fracture stress is 34,360 psi (237 MPa). These stresses for typical crack lengths are comparable to the stresses that produced pinhole failures, but the Griffith criterion is not adequate since it cannot predict the temperature or rate dependence.

c. Fracture Mechanics Analysis

Application of fracture mechanics to a tubular specimen with a flaw is complex. It is not the intent of this section to evaluate this situation, but rather to examine some recent data on irradiated notched stainless steel specimens and apply a simple fracture mechanics analysis to obtain values of fracture stresses for cladding specimens. Fish⁽¹⁰⁾ evaluated the effects of notch geometries on the tensile properties of irradiated 304 Type stainless steel. Critical fracture-toughness stress intensity values, K_{IC} , were calculated for a single-edge cracked strip subjected to tension using the approach of Paris and Sih⁽¹¹⁾ from the 304 Type stainless steel data.⁽¹⁰⁾ The following K_{IC} values were obtained for 1100°F:

Strain Rate, s ⁻¹	K_{IC} , psi $\sqrt{\text{in.}}$
4.45x10 ⁻⁵	12,898
4.45x10 ⁻²	24,292

Application of these K_{IC} values to a semi-elliptical crack in a plate yields fracture stresses, which are well above the ultimate tensile strength of 20% CW 316 Type stainless steel even for crack depths of 0.005 in. (0.127 mm). A second approach using a single-edge notch geometry, yields similar results. The results are shown in Figure 30 for different crack depths and both strain rates. One advantage of the fracture mechanics prediction based on the 304 Type stainless steel data is that it demonstrates a strain-rate dependence. The 304 Type stainless steel fracture data are obtained with machined notches; possibly, lower fracture-toughness values would have been obtained with sharper cracks, which would yield closer agreement with the FCTT results.

Results from the Griffith equation are also shown in Figure 30. Stresses, which resulted in pinhole failure, are generally shown to be in a region that is between the results of the fracture mechanics analysis and Griffith analysis. The Griffith Theory can predict very low fracture strength values since fracture resistance is by surface energy rather than plastic strain. This situation is also true for Petch's Equation ⁽¹²⁾. The results of the fracture mechanics analysis show that the absolute depths of possible cracks in thin cladding are small enough that the possible values of applied stress intensity are relatively small. The observed internal surface cracks range to 0.0002 in. (0.005 mm); therefore, if utilization of a fracture mechanics approach is appropriate for this material, the fracture toughness values must be an order of magnitude lower than those extracted from the notched 304 Type stainless steel tests. Further related discussion on the intergranular failure mode is presented in Section VII.

3. Transgranular Fracture

Specimen P-23A-32:J was tested at a heating rate of 200F⁰/s (111 K/s) with a gas pressure of 14,300 psi (98.6 MPa). Failure occurred at 1125⁰F (880 K), which is about 200⁰F (111 K) greater than the failure temperature for unirradiated material. It was irradiated at an average temperature of

827°F (715 K) to an average neutron fluence of 1.6×10^{22} n/cm² (E > 0.1 MeV). As discussed previously, this irradiation produced increase in failure strength is typical of cladding loading at high enough strain rates and low enough temperatures to produce transgranular fracture. A detailed analysis of the microstructure and fracture mode of this specimen was conducted through optical, Scanning Electron Microscope (SEM) and Transmission Electron Microscope (TEM) metallography to confirm the understanding of this type of behavior.

Figure 31 shows the as-burst specimen. The failure consists of a single large tear with the remaining portion of the specimen exhibiting very little strain. Sections of the "flap" shown in the figure were cut for SEM and TEM specimens. The remainder of the sample was mounted for optical metallographic observation. Figure 32 shows the transgranular fracture of the specimen, and the localized wall thinning and necking occurring before and during the specimen fracture. Intergranular crack propagation is not stable under these conditions so high strength and toughness is exhibited.

A section, about 0.1-inch (2.5-mm) length was mounted for SEM observation. Figure 33 shows a low magnification view of the fracture surface. The area near region B in Figure 33 is similar to the transverse section shown in Figure 32. The two areas circled in Figure 33 are shown at higher magnifications in Figures 34 and 35. Both of these figures show that plastic dimpling has occurred. These dimples are microvoids, which were formed at interfaces between the matrix and particles such as carbides and at imperfections. These voids grow by shear deformation processes until they coalesce to give the hemispherical cavities or dimples shown in the 3000X views in Figures 34 and 35. The voids producing the dimples are not related to voids responsible for swelling in irradiated material.

Typical structures for P-23A-32:J observed with the TEM are shown in Figures 36 and 37. The structure in Figure 36 consists of a coarse structure of microtwins, stacking faults and deformation bands, with a dislocation network distributed through the balance of the matrix. Also, there is a high density of Frank-faulted loops as shown in Figure 37. The structure shown in these Figures is very similar to that observed by Brager and Straalsund⁽¹²⁾ for samples of 20% CW 316 Type stainless steel irradiated to 0.6×10^{22} n/cm² at 842°F (723 K). Virtually no (swelling) voids were found in this sample, which is consistent with the fluence-temperature relationship for void formation established by Brager.⁽¹³⁾ An important observation from the TEM examination is that the structure in this sample is essentially the same after transient testing as that found at the irradiation temperature. Heating the samples at 200°F/s (111 K/s), from 700°F (644 K) to the failure temperature, 1125°F (880 K), did not change the structure to any appreciable extent. That is, Frank loops did not unfault and the dislocation density did not decrease noticeably. The high density of the irradiation-induced Frank loops increased the cladding strength and failure temperature. Slower heating rates or holding at temperature prior to testing will allow the loops to unfault.

The important consequence of transgranular fracture is that the requisite shear deformation blunts any crack or reduces stress concentrations. Consequently, the cladding will possess its full strength, whereas under intergranular fracture the failure strength of the cladding can be significantly reduced. As discussed in Reference 1, transgranular fracture occurs below 1000-1200°F (811-922 K) (depending on strain rate), so fuel pin failure in cladding below this temperature would require very high loads.

C. SIZE OF BREACH

Various models have been proposed to describe the motion of fuel or fission gas through a breach in the cladding of the fuel pin.⁽¹⁴⁻¹⁷⁾ In each of these models, a certain "hole size" has been assumed for the breached area. Results from the simulated transient tests may provide a better estimate of the size of the breach. Figure 38 shows a typical opening in a cladding specimen after being tested with the Fuel Cladding Transient Tester (FCTT). Measurements of the width and length of the FCTT specimen breaches have been made for all of the specimens tested. Figures 39-50 present the results of these measurements. In each figure, the width and length of the breach are shown as a function of neutron fluence ($E > 0.1$ MeV) for both heating rates used with the FCTT. For those specimens that exhibited a pinhole-type failure, the width and length are both plotted as a zero value.

Figures 39 through 50 show that for pressures less than 10,000 psi (68.9 MPa), the cladding breach does not increase in size with increasing fluence. The width and length either remain constant or decrease in magnitude with increasing fluence. The breach shown in Figure 38 may be approximated in shape by two equal-sized circular segments. The area, A , of such a shape is given by:

$$A = \frac{w^3}{8\ell} + \frac{2\ell w}{3} \quad (13)$$

where w = width and ℓ = length. This formula was used to calculate the area of the breach openings of the FCTT specimens. Figures 51-56 show the breach area as a function of neutron fluence for gas pressures from 2500 to 13,500 psi (17.2 to 93.1 MPa), respectively. These figures show the same fact as the width and length figures; namely, the size of the breach either remains constant or decreases with increasing fluence for pressures less than 10,000 psi (68.9 MPa). The irradiation-produced decrease in the size of the breach may result from the propensity for

pinhole failures and the cladding ductility decrease exhibited by higher-fluence specimens.

It is our opinion that gas pressures of 6000 psi (41.4 MPa) or less provide stresses that are representative of the cladding stresses provided during transient overpower accident (TOP) events. (Failure temperatures for unterminated TREAT-tested pins are in the range of 1400-1700°F (1033-1200 K).⁽¹⁸⁾ Gas pressures required to produce failure in this temperature range are on the order of 4000 to 6000 psi (27.6 to 41.4 MPa), and they can be determined from Figures 1 and 2. For loss-of-flow transients, lower pressures are appropriate.)

Some caution must be used in the application of the breach sizes from FCTT results to fuel pin behavior during a transient, because the FCTT does not correctly duplicate all aspects of cladding, thermal and loading conditions. Nevertheless, it appears that the area of the breach decreases with increasing fluence for pressures that are representative of TOP conditions.

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TABLE 7
SUMMARY OF REGRESSION EQUATIONS FOR LARSON-MILLER CORRELATIONS

Cladding Type	Fluence Range, 10 ²² n/cm ² (E>0.1 MeV)	Heating Rate, F°/s	Number of Tests	Equation Form	A	B	C	D	S _B	S _C	S _D	$\bar{X}_1$	S _{$\bar{X}_1$}	$\bar{X}_2$	S _{$\bar{X}_2$}	$\bar{X}_3$	S _{$\bar{X}_3$}	$\bar{Y}$	S _{$\bar{Y}$}	S.E. _Y	r^2	C.C.	Limits of Applicability For σ_m , ksi
All	> 0, ≤ 1	10 & 200	77	1	4.6402	-5.1218x10 ⁻²	7.0417x10 ⁻⁴	-4.1349x10 ⁻⁶	7.9312x10 ⁻³	2.1536x10 ⁻⁴	1.6175x10 ⁻⁶	33.669	20.582	1551.7	1776.8	86563	1.4785x10 ⁶	3.6505	0.43878	0.16428	1.9702	0.930	5-90
Plenum/Structural	> 1, ≤ 2	10 & 200	23	1	4.2463	-1.937x10 ⁻²	0	0	1.8469x10 ⁻³	---	---	42.368	11.747	---	---	---	---	3.4257	0.2483	0.10176	0.21745	0.916	18-80
Fuel Column	> 1, ≤ 2	10	5	2	0.42798	-3.7206	-	-	0.38874	---	---	2.8576x10 ⁻²	1.6611x10 ⁻²	---	---	---	---	0.32166	6.2808x10 ⁻²	1.2915x10 ⁻²	5.0039x10 ⁻⁴	0.984	23-90
Fuel Column	> 1, ≤ 2	200	7	1	4.2281	-2.0469x10 ⁻²	0	0	3.8435x10 ⁻³	---	---	69.069	21.269	---	---	---	---	2.8143	0.47218	0.20024	0.20048	0.922	25-92
Fuel Column	> 2, ≤ 3	10	10	2	0.43661	-3.5455	-	-	0.79212	---	---	2.6097x10 ⁻²	9.8449x10 ⁻³	---	---	---	---	0.34409	4.1290x10 ⁻²	2.3395x10 ⁻²	4.3786x10 ⁻³	0.845	23-90
Fuel Column	> 3, ≤ 4	10	11	1	3.4402	-1.5134x10 ⁻²	0	0	1.0339x10 ⁻²	---	---	29.208	7.427	---	---	---	---	2.9982	0.25631	0.24281	0.53062	0.439	20-40
Fuel Column	> 3, ≤ 4	200	10	1	4.3049	-2.4167x10 ⁻²	0	0	4.9219x10 ⁻³	---	---	41.955	14.849	---	---	---	---	3.2910	0.41412	0.21925	0.38456	0.867	20-70
Fuel Column	> 4, ≤ 5	200	7	3	1.4057	-6.1434x10 ⁻³	-	-	1.0183x10 ⁻³	---	---	47.257	22.469	---	---	---	---	1.1154	0.14721	5.6046x10 ⁻²	1.5706x10 ⁻²	0.938	18-90
Fuel Column	> 5, ≤ 6	10 & 200	6	2	0.45240	-3.3271	-	-	0.85328	---	---	3.2465x10 ⁻²	1.4490x10 ⁻²	---	---	---	---	0.34438	5.4181x10 ⁻²	2.7647x10 ⁻²	3.0574x10 ⁻³	0.890	17-90
Below Fuel Column	> 1, ≤ 2	10 & 200	10	1	4.2048	-1.6362x10 ⁻²	0	0	6.0504x10 ⁻⁴	---	---	55.234	24.493	---	---	---	---	3.3010	0.40295	4.4457x10 ⁻²	1.5812x10 ⁻²	0.995	25-100
Below Fuel Column	> 2, ≤ 3	10 & 200	6	1	4.0269	-1.3301x10 ⁻²	0	0	1.9245x10 ⁻²	---	---	65.742	32.330	---	---	---	---	3.1525	0.44765	0.13912	7.7421x10 ⁻²	0.961	25-100
Below Fuel Column	> 3, ≤ 4	10 & 200	3	1	4.2276	-1.9360x10 ⁻²	0	0	6.9889x10 ⁻⁴	---	---	37.410	9.0935	---	---	---	---	3.5033	0.17616	8.9878x10 ⁻³	8.0781x10 ⁻⁵	0.999	25-50

SUMMARY OF EQUATION FORMS

1. $Y = A + BX_1 + CX_2 + DX_3$; $Y = LMP \times 10^{-4}$, $X_i = (\sigma_m)^i$
2. $Y = A + BX$; $Y = 1/(LMP \times 10^{-4})$, $X = 1/\sigma_m$
3. $Y = A + BX$; $Y = \ln(LMP \times 10^{-4})$, $X = \sigma_m$

LMP = Larson-Miller Parameter
 σ_m = Modulus Modified Stress, ksi
A, B, C, D = Regression Coefficients

NOTES:

S_B, S_C, S_D = Standard Deviations of B, C, and D
 $\bar{X}_i$ = Mean of X_i
S _{$\bar{X}_i$} = Standard Deviation of $\bar{X}_i$
 $\bar{Y}$ = Mean of Y
S _{$\bar{Y}$} = Standard Deviation of $\bar{Y}$
S.E._Y = Standard Error of Estimate of Y
 r^2 = Sum of the Square of the Residuals
C.C. = Correlation Coefficient

TABLE 8

LIFE FRACTION ANALYSIS OF TERMINATED FCTT TRANSIENTS

Specimen	Terminated Temp, °F(K)	Life Fraction Of Terminated Transient	Subsequent FCTT Failure Temp, °F(K)	Predicted FCTT Failure Temp, °F(K)
NUMEC E-067-W	1306 (981)	1×10^{-4}	1727 (1215)	1680 (1189)
NUMEC E-122-L	1346 (1003)	8×10^{-3}	1536 (1109)	1535 (1108)
PNL-11-37-S	1695 (1197)	0.24	1754 (1230)	1770 (1239)
P-23C-49C-F	1363 (1012)	1.9×10^{-3}	1592 (1140)	1600 (1144)

HEDL 7802-55.2

FIGURE 27. Failure Energy of Transient Specimens Tested at 10F°/s (5.6 K/s).

HEDL 7802-55.1

FIGURE 28. Failure Energy of Transient Specimens Tested at 200F°/s (111 K/s).

FIGURE 29. Transverse Section Through P-23A-59C:L; the Failure is Typical of the Intergranular Pinhole Failure Which is Exhibited by Many of the Specimens. Neg 764531-3

HEDL 7802-55.3

FIGURE 30. Effect of Crack Depth on Predicted Cladding Fracture Stress at 1100°F.

FIGURE 31. Specimen P-23A-32:J, Which was Typical of Specimens Tested at High Enough Strain Rate and Low Enough Temperature to Produce Transgranular Fracture and Exhibit an Irradiation-Produced Strength Increase.

FIGURE 32. Transverse Section Through the Failure Site in Specimen P-23A-32:J Showing Necking and Wall Thinning Associated with the Transgranular Fracture. Such deformation will blunt any stress concentration or crack and enable a high-strength, high-toughness fracture, in contrast to the intergranular fracture shown in Figure 29. Neg 764531-2

FIGURE 33. Fracture Surface of Specimen P-23A-32:J as Revealed by Scanning Electron Microscopy. Regions A and B are shown at higher magnification in Figures 34 and 35.

300X

A-1

3000X

Region A-1
from Above

FIGURE 34. Transgranular Plastic Dimpling Fracture in Region A of Figure 33. There is very little evidence of intergranular fracture.

300 X

B-1

3000 X

Region B-1
from Above

FIGURE 35. Transgranular Plastic Dimpling in Region B of Figure 33. The shear deformation processes, which produce the plastic dimpling fracture, relieve stress concentrations and blunt cracks so that the cladding can exhibit high strength.

FIGURE 36. Microstructure of P-23A-32:J as Revealed by Transmission Electron Microscopy. Microtwins, stacking faults and deformation bands are evident.

1000 Å

FIGURE 37. Frank Loops Were Still Dense in the Sample Following the Transient to 1125°F (880 K). These loops were responsible for the strength increase produced by irradiation.

FIGURE 38. Typical Breach in an Irradiated Cladding Specimen Produced from Simulated Transient Testing.

HEDL 7802-55.21

FIGURE 39. Size of the Breach for FCTT Specimens Tested at 10°F/s (5.6 K/s) and 2500 psi (17.2 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.22

FIGURE 40. Size of the Breach for FCTT Specimens Tested at $10\text{F}^\circ/\text{s}$ (5.6 K/s) and 4000 psi (27.6 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.7

FIGURE 41. Size of the Breach for FCTT Specimens Tested at 10F°/s (5.6 K/s) and 6000 psi (41.4 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.9

FIGURE 42. Size of the Breach for FCTT Specimens Tested at $10\text{F}^\circ/\text{s}$ (5.6 K/s) and 10,000 psi (68.9 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.10

FIGURE 43. Size of the Breach for FCTT Specimens Tested at $10F^\circ/s$ (5.6 K/s) and 13,500 psi (93.1 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.8

FIGURE 44. Size of the Breach for FCTT Specimens Tested at 10F°/s (5.6 K/s) and 14,300 psi (98.6 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.11

FIGURE 45. Size of the Breach for FCTT Specimens Tested at 200F°/s (111 K/s) and 2500 psi (17.2 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.12

FIGURE 46. Size of the Breach for FCTT Specimens Tested at 200F°/s (111 K/s) and 4000 psi (27.6 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.13

FIGURE 47. Size of the Breach for FCTT Specimens Tested at 200F°/s (111 K/s) and 6000 psi (41.4 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.14

FIGURE 48. Size of the Breach for FCTT Specimens Tested at 200F°/s (111 K/s) and 10,000 psi (68.9 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.15

FIGURE 49. Size of the Breach for FCTT Specimens Tested at 200F°/s (111 K/s) and 13,500 psi (93.1 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.17

FIGURE 50. Size of the Breach for FCTT Specimens Tested at 200F°/s (111 K/s) and 14,300 psi (98.6 MPa) Gas Pressure (Solid Symbols: Fuel Column; Open Symbols: Non-Fuel Column).

HEDL 7802-55.4

FIGURE 51. Area of the Breach for FCTT Specimens Tested at a Gas Pressure of 2500 psi (17.2 MPa).

HEDL 7802-55.5

FIGURE 52. Area of the Breach for FCTT Specimens Tested at a Gas Pressure of 4000 psi (27.6 MPa).

HEDL 7802-55.16

FIGURE 53. Area of the Breach for FCTT Specimens Tested at a Gas Pressure of 6000 psi (41.4 MPa).

HEDL 7802-56.17

FIGURE 54. Area of the Breach for FCTT Specimens Tested at a Gas Pressure of 10,000 psi (68.9 MPa).

HEDL 7802-56.16

FIGURE 55. Area of the Breach for FCTT Specimens Tested at a Gas Pressure of 13,500 psi (93.1 MPa).

HEDL 7802-56.15

FIGURE 56. Area of the Breach for FCTT Specimens Tested at a Gas Pressure of 14,300 psi (98.6 MPa).

V. EFFECTS OF WASTAGE

A. CORROSION

Metallographic examinations were conducted on selected specimens from P23A, P23B and NUMEC E fuel pins for the purpose of evaluating the inner and outer surface corrosion. The metallographic examinations were conducted on transverse sections taken through the failure site. In some cases, numerous photos were taken to determine the wall thickness.

Table 9 summarizes the results of the metallographic examinations. The depth of the attack noted in columns 2 through 5 includes both matrix and intergranular attack into the cladding wall, but does not include the thickness of any corrosion product layer build-up on the cladding surface, as in the case of samples P-23A-21:T, V, and P-23A-31:S. All samples were examined in both the as-polished and etched conditions. The samples were etched electrolytically with 2% chromic acid.

Figure 57 shows the corrosion product layer, which was found on the cladding inner surface of specimens P-23A-21:T and V. The layer has partially separated from the cladding surface. This partial separation may have been promoted by the temperature and stress conditions of the transient test. The maximum thickness of the layer for both specimens was 0.8 mil (0.02 mm). The layer appears to be identical to that found in the post-irradiation examination of the P-23A fuel pins.^(2,19)

Intergranular attack was found on the inner surface of eight specimens. Typical attack is shown for two of these samples in Figure 58. The depth of attack shown in Figure 58 for specimens P-23A-32:J and NUMEC E-029:M is 0.4 mil (0.01 mm). It should be noted that the maximum value for P-23A-32:J was 0.8 mil (0.02 mm).

Specimen P-23A-21:V exhibited both inner and outer surface corrosive attack; both are shown on the unetched metallographic section in Figure 59. The maximum depth of intergranular penetration on the inner surface was 0.8 mil (0.02 mm) and the depth of attack on the outer surface was 0.2 mil (0.0051 mm). The corrosive attack for this specimen did not degrade the observed FCTT failure temperature or failure strain.

In general, the cladding wall thickness was quite uniform for all of the samples examined. However, one sample, P-23A-31:S, exhibited localized wall thinning at one location, and is shown in Figure 60. The minimum wall thickness shown here is 13.8 mils (0.35 mm); however, it should be pointed out that failure did not occur at this location. Measurements near the failure site revealed that the wall thickness was on the order of 14.6 to 14.8 mils (0.37 to 0.38 mm).

The corrosive attack shown on the outer surface in Figure 59 was also found on other samples. Figure 61 shows this type of attack for P-23A-21:CC. The attack did not exceed a depth of 0.2 mil (0.0051 mm). Details of this outer surface attack are shown at higher magnification in Figure 62. A fine surface layer is shown in view A. At another location, view B, the layer is associated with the sub-surface porosity, which is indicative of ferrite formation.

Specimen P-23B-14A-W was irradiated at an average midwall temperature of 1235°F (941 K). The inner surface of the specimen was characterized by intergranular attack coupled with a porous layer, as shown in Figure 63. The average depth of the intergranular attack was 0.7 mil (0.017 mm), with the maximum value being 1.1 mils (0.028 mm). These results were discussed earlier and were summarized in Table 9. The outer surface of the cladding was characterized by a thin ferrite layer, with thicknesses up to 0.24 mil (0.006 mm).

The data in Table 9 were analyzed to determine if wastage produced an effect on the mechanical properties of the cladding. Two approaches were employed: (1) computation of the effects of wastage in elevating the net-section stress; and (2) correlation of depth of attack with the extent of degradation of mechanical properties.

The first approach is based on the premise that wastage lowers the nominal failure strength of the cladding by elevation of the net-section stress, since wastage has reduced the thickness of the cladding wall and its capability of bearing load. Comparison of changes in the computed values of the strength ratio, due to consideration of cladding wastage, is a convenient means to test the above premise. A strength ratio of the irradiated material failure stress to the unirradiated material failure stress, at the failure temperature of the irradiated specimen, provides a normalized indication of how the cladding failure strength has been changed by irradiation. The strength ratio for each of the specimens in Table 9 was recalculated considering wastage and the results are shown in Table 10. The nominal strength ratio, σ_r , in column 3 of Table 10, was calculated from:

$$\sigma_r = \left(\frac{\sigma_{\text{irradiated}}}{\sigma_{\text{unirradiated}}} \right) \quad (14)$$

with $t_{\text{irradiated}} = t_{\text{unirradiated}} = 15$ mils (0.38 mm) wall thickness. The strength ratio based on the minimum uniform wall thickness, σ_{rm} , in column 4 of Table 10, was calculated from:

$$\sigma_{rm} = \sigma_r \left(\frac{15}{t_{\text{irradiated}}} \right) \quad (15)$$

with $t_{\text{irradiated}}$ = minimum uniform wall thickness in column 7 of Table 9. The strength ratio considering wall attack, σ_{ra} , in column 5 of Table 10, was calculated from:

$$\sigma_{ra} = \sigma_r \left(\frac{15}{t_{\text{irradiated}}} \right) \quad (16)$$

with $t_{\text{irradiated}}$ = average uniform wall thickness - (maximum ID + maximum OD attack).

The expected strength ratios in column 6 of Table 10 were calculated from the LMP equations given in Table 7.

The results given in Table 10 show that only four of the twelve samples had strength ratios less than the expected values. For these samples, an elevation in the strength ratio based on minimum wall and/or attacked wall did not greatly raise the nominal value.

It may be possible that the extent of wastage correlates with the amount of degradation in mechanical properties. From a plot of diametral failure strain versus fluence, it is possible to define the observed strain behavior in a qualitative manner as being better, average, or worse by comparison with the trends shown by the data for all specimens at a particular gas pressure. The results of this definition of behavior were plotted in Figure 64 versus the maximum depth of inner surface attack. The failure strengths are shown in the same manner. If the attack were to have an effect, there should be a trend towards worse than average behavior with increasing depth of attack. Clearly, this is not the case in Figure 64. Thus, the amounts of wall thinning and cladding attack listed in Table 9 do not correlate with the extent of mechanical property degradation.

B. WEAR MARKS

Significant wear marks were developed during the irradiation of certain 61-pin subassemblies.⁽²⁾ Performance analysis of fast reactor fuel pins requires a quantitative description of the effects of cladding wastage on the mechanical properties of the cladding.

The effect of wear marks on the transient burst strength of the cladding was evaluated for unirradiated cladding specimens.⁽²⁰⁾ Test results for FCTT tests are summarized in Table 11 and the results are shown in Figure 65, where the net-section stress is given as a function of failure temperature for heating rates of $10\text{F}^0/\text{s}$ (5.6 K/s) and $200\text{F}^0/\text{s}$ (111 K/s). The net-section stress was computed by assuming that the wall thickness was uniformly reduced by an amount equal to the depth of the wear mark. The results shown in Figure 65 clearly indicate that the net-section stress adequately describes the effect of wear marks on the cladding strength.

Numerous FCTT specimens from various fuel pins had breaches associated with wear marks. Figure 66 shows the wear mark on specimen PNL-10-15-P1 before the test and the failure in the same wear mark after the FCTT test. Typical metal flashing or flow associated with a wear mark is shown in Figure 67 for specimen PNL-10-15-Z. Metallographic sections similar to that in Figure 67 were used to measure the depth of the wear marks for each specimen.

All FCTT specimens tested to date, which have breached in wear marks, are listed in Table 12 with the failure data, wear mark depth and strength ratios. The effect of the wear marks on the mechanical behavior can be determined by examining the strength numbers in Table 12. The actual strength ratio for each specimen was less than the expected value at the fluence value for each specimen. However, the net-section strength ratios are all essentially equal to or greater than the expected value. This behavior is consistent with the results for the unirradiated specimens.

The depth of the wear mark appears to have a small effect, as shown in Figure 68 where the ratio of the actual strength ratio to the expected strength ratio is plotted as a function of wear depth. Although the average value of the ratio is less than one, there is no obvious trend with the wear depth.

A similar plot is shown in Figure 69, where the actual strength ratio has been replaced by the net-section strength ratio. Since the ratio increases with wear depth, it is obvious that accounting for the effect of wear marks on net-section basis is conservative for design purposes.

TABLE 9

SUMMARY OF METALLOGRAPHIC OBSERVATIONS

SPECIMEN	AVERAGE DEPTH OF ATTACK				MAXIMUM DEPTH OF ATTACK				UNIFORM WALL THICKNESS				MISCELLANEOUS
	ID		OD		ID		OD		AVERAGE		MINIMUM		
	mils	mm	mils	mm	mils	mm	mils	mm	mils	mm	mils	mm	
P-23A-21:Q T	0.0 0.4	0.0 0.01	0.0 0.0	0.0 0.0	0.0 0.9	0.0 0.02	0.0 0.0	0.0 0.0	15.0 14.8	0.38 0.38	15.0 14.3	0.38 0.36	Reaction layers 0.8 mils (0.02 mm) thick
V	0.4	0.01	0.2	0.005	0.8	0.02	0.2	0.005	14.7	0.37	14.5	0.37	reaction layers 0.8 mils (0.02 mm) thick
CC SS-1	0.0 0.0	0.0 0.0	0.2 0.0	0.005 0.0	0.0 0.0	0.0 0.0	0.2 0.2	0.005 0.0	14.8 ---	0.38 --	14.6 --	0.37 --	
P-23A-26:K	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0	14.9	0.38	14.7	0.37	
P-23A-31:S	0.4	0.01	0.0	0.0	0.6	0.02	0.0	0.0	14.7	0.37	13.8	0.35	Reaction layers 0.5 mils (0.01 mm) thick
P-23A-32:J	0.4	0.01	0.0	0.0	0.8	0.02	0.0	0.0	---	--	--	--	
P-23A-59C:J L	0.0 0.0	0.0 0.0	0.0 0.0	0.0 0.0	0.5 0.0	0.01 0.01	0.0 0.0	0.0 0.0	15.0 ---	0.38 --	14.8 --	0.38 --	
N-E-029:M	0.0	0.0	0.0	0.0	0.4	0.01	0.3	0.008	15.1	0.38	14.8	0.38	
N-E-049:D K	0.0 0.0	0.0 0.0	0.0 0.0	0.0 0.0	0.0 0.5	0.0 0.01	0.0 0.0	0.0 0.0	--- 14.7	-- 0.37	-- 14.6	-- 0.37	
P-23B-14A:W	0.7	0.018	--	--	1.1	0.028	0.24	0.006	15.0	0.38	--	--	Reaction layers 1.2 mils (0.03 mm) thick

SPECIMEN P-23A-21:T
300X

1 Mil

SPECIMEN P-23A-21:V
300X

1 Mil

FIGURE 57. Corrosion Product Layer on the Cladding Inner Surface of Specimens P-23A-21:T and P-23A-21:V. The maximum thickness of this layer was 0.8 mils (0.02 mm).

SPECIMEN P-23A-32:J
750X

1 Mil

SPECIMEN NUMEC E-029:M
750X

1 Mil

FIGURE 58. Inner Surface Intergranular Penetration or Cracking on Specimens P-23A-32:J and Numec E-029:M. The depth of penetration shown here was 0.4 mils (0.1 mm) for each sample.

1 Mil

225X

FIGURE 59. Corrosive Attack Found on Specimen P-23A-21:V. Depth of attack on the inner surface was up to 0.8 mils (0.02 mm) and the depth of attack on the outer surface was 0.2 mils (0.0051 mm).

1 Mil

225X

FIGURE 60. Localized Wall Thinning in Specimen P-23A-31:S. The minimum wall thickness is 13.8 mils (0.35 mm).

1 Mil

225X

FIGURE 61. Corrosive Attack on the Cladding Outer Surface of Specimen P-23A-21:CC. Maximum depth of attack was 0.2 mils (0.0051 mm).

A

750X

1 Mil

B

750X

1 Mil

FIGURE 62. Corrosive Attack on the Outer Surface of Specimen P-23A-21:CC. The layer is assumed to be associated with ferrite formation.

Inner Surface
(As Polished)

750X

1 Mil

Outer
Surface
(Etched)

750X

1 Mil

FIGURE 63. Metallographic Section of Specimen P-23B-14A(W). Corrosion at the inner surface consists of a layer plus intergranular attack, with a combined depth of 2 mils (0.05 mm). The ferrite layer on the outer surface is $\sqrt{0.2}$ mils (5.6 μm) thick.

TABLE 10
EFFECT OF CLADDING WALL THINNING AND ATTACK ON STRENGTH RATIO CALCULATIONS

Specimen Identification	Fluence 10^{22} n/cm ² (E > 0.1 MeV)	Strength Ratio $\sigma_r\%$ (a)	Strength Ratio (Minimum Wall) $\sigma_{rm}\%$ (b)	Strength Ratio (Attacked Wall) $\sigma_{ra}\%$ (c)	Expected Strength Ratio $\sigma_{re}\%$ (d)
P-23A-21:Q	4.0	21.3	21.3	21.3	29.2
:T	3.9	93.5	97.8	100.6	65.7
:V	3.3	88.3	91.1	96.5	65.7
:CC	1.9	92.9	95.5	95.5	80.5
:SS-1	0.6	73.2	--	73.3	60.3
P-23A-26:K	1.6	84.1	85.7	--	82.3
P-23A-31:S	1.6	72.6	78.3	76.6	82.9
P-23A-32:J	1.6	107.9	--	113.9	98.8
P-23A-59C:J	0.8	97.6	97.6	101.0	91.3
N-E-029:M	0.9	55.4	55.6	57.2	60.3
N-E-049:K	1.5	78.4	80.6	82.9	85.6
P-23B-14A:W	3.4	96.9	--	96.9	70.9

a. $\sigma_r = \left(\frac{\sigma_{\text{irradiated}}}{\sigma_{\text{unirradiated}}} \right)$; wall thickness $t_{\text{irradiated}} = t_{\text{unirradiated}} = 15$ mils.

b. σ_{rm} , same as σ_r , except $t_{\text{irradiated}} = \text{Minimum Uniform Wall Thickness from Table 9.}$

c. σ_{ra} , same as σ_r , except $t_{\text{irradiated}} = \text{Average Uniform Wall Thickness} - \text{Maximum Attack (ID \& OD) from Table 9.}$

d. Calculated strength ratio based on LMP equations.

TABLE 11

TRANSIENT TEST RESULTS

Specimen Number	Heating		Wear Mark Dimensions				Failure		Nominal Hoop		Net Section	
	Rate, F°/s	(K/s)	Depth, in. (mm)		Length, in. (mm)		Temp, F° (K)		Stress, ksi (MPa)		Hoop Stress, ksi (MPa)	
W4	200	(111)	0.003	(.076)	1.0	(25.4)	1145	(891)	72.0	(496)	90.0	(621)
W15	200	(111)	0.001	(.025)	1.0	(25.4)	1370	(1016)	72.0	(496)	77.1	(532)
W16	200	(111)	0.003	(.076)	0.25	(6.4)	1310	(983)	72.0	(496)	90.0	(621)
W3	10	(5.6)	0.003	(.076)	1.0	(25.4)	1240	(944)	72.0	(496)	90.0	(621)
W8	10	(5.6)	0.001	(.025)	1.0	(25.4)	1290	(972)	72.0	(496)	77.1	(532)
W10	10	(5.6)	0.003	(.076)	0.25	(6.4)	1255	(952)	72.0	(496)	90.0	(621)
W13	10	(5.6)	0.001	(.025)	0.25	(6.4)	1315	(986)	72.0	(496)	77.1	(532)

HEDL 7802-56.13

FIGURE 65. Effect of Wear Marks on Transient Burst Strength. Solid curves are for specimens with no defects. Data points are for specimens with wear marks.

PRE-TEST

POST-TEST

FIGURE 66. Wear Mark on FCTT Specimen PNL-10-15-P1.

300X

FIGURE 67. Metallographic Section of Specimen PNL-10-15-Z Showing Failure Through the Center of a Wear Mark. The metal flow, or buildup, on the cladding outer surface is typical of a wear mark.

TABLE 12

SUMMARY OF THE EFFECT OF WEAR MARKS ON FCTT SPECIMEN BEHAVIOR

Specimen	Neutron Fluence 10 ²² n/cm ²	Heating Rate		Gas Pressure		Failure Temp.		Wear Mark Depth		Strength Ratio %	Expected ^(a) Strength Ratio %	Net-Section ^(b) Strength Ratio %	
		F°/s	(K/s)	psi	(MPa)	°F	(K)	In.	(mm)				
PNL-9-13-N	2.58	10	(5.6)	4,000	(27.6)	1662	(1179)	0.0023	(.058)	81.0	60.0	95.7	
PNL-9-20-S	2.53	10	(5.6)	2,500	(17.2)	1792	(1251)	0.0032	(.081)	75.0	75.0	95.3	
	-U	1.74	10	(5.6)	6,000	(41.4)	1502	(1090)	0.0029	(.074)	84.3	78.0	104.5
	-V	1.27	--		12,700	(87.6)	686	(636)	0.0021	(.053)	85.5	----	99.4
PNL-11-37-R	1.60	200	(111)	4,000	(27.6)	1805	(1258)	0.0009	(.023)	95.3	86.0	101.4	
	-S	1.21	200	(111)	4,000	(27.6)	1754	(1230)	0.0010	(.025)	83.0	95.0	88.9
PNL-10-15-P1	3.32	10	(5.6)	4,000	(27.6)	1341	(1000)	0.0049	(.125)	42.6	48.0	63.3	
	-P2	2.43	10	(5.6)	4,000	(27.6)	1576	(1131)	0.0039	(.099)	65.6	70.0	88.7
	-V	1.28	10	(5.6)	4,000	(27.6)	1638	(1165)	0.0013	(.033)	76.1	83.7	83.3
	-Z	0.68	10	(5.6)	4,000	(27.6)	1664	(1180)	0.0012	(.031)	81.1	91.2	88.5
PNL-10-23-S	2.09	200	(111)	6,000	(41.4)	1550	(1116)	0.0044	(.112)	79.3	78.0	112.2	
PNL-10-63-P	2.08	200	(111)	10,000	(68.9)	1270	(961)	0.0019	(.048)	86.8	90.0	99.4	
	-Q	1.43	10	(5.6)	6,000	(41.4)	1485	(1080)	0.0021	(.053)	81.5	82.0	94.8
NUMEC E-049-L	1.13	200	(111)	4,000	(27.6)	1601	(1145)	0.0015	(.038)	57.6	93.0	64.0	
NUMEC F-027-H	1.98	200	(111)	10,000	(68.9)	1265	(958)	0.0022	(.056)	84.7	91.0	99.3	
NUMEC F-070-N	1.90	200	(111)	10,000	(68.9)	1070	(850)	0.0039	(.099)	71.3	91.0	96.4	

(a) Calculated from appropriate LMP equation.

(b) Calculated for wall thickness equal to 15 mils minus wear depth.

HEDL 7802-56.9

FIGURE 68. Effect of Wear Depth on Cladding Strength.

HEDL 7802-56.10

FIGURE 69. Effect of Wear Marks on Cladding Strength.

VI. METALLOGRAPHIC OBSERVATIONS

As mentioned earlier, metallographic sections are normally made at the failure site for measurement of the depth of wear marks or to observe the mode of fracture. In some specimens, additional features were observed and they are reported in this section.

Specimen PNL-10-15-E failed with a pinhole fissure, with the failure being adjacent to a wear mark. The sample was sectioned and incrementally ground to locate the failure site. Before the failure was located, a fairly large area of attack was found on the inner surface. This area was examined with the shielded electron microprobe.

Results of the microprobe analysis showed that some areas exhibited a small increase in molybdenum. Other areas exhibited small increases in chromium and iron content. Palladium was detected on the internal surface of the cladding, but no cesium or tellurium could be located. Iron and nickel showed small concentration decreases near the corroded grain boundaries. Some of the corroded areas contained a phase that was not positively identified, but specimen current images indicated that it was probably an oxide.

After the microprobe analysis was completed, the sample was ground incrementally until the corroded area disappeared. It was determined that this region extended about 40 to 50 mils (1.02 to 1.27 mm) in the axial direction, 28 mils (0.71 mm) in the circumferential direction and 3.3 mils (0.084 mm) into the wall.

Continued incremental grinding revealed the pinhole failure, which was a narrow intergranular crack extending across the wall while being contiguous with a wear mark. The appearance of the failure was the same as other intergranular failures found in many of the fuel column specimens previously tested. In the same plane, another corroded area, similar to the

first, was found in a location remote from the failure site. This area is shown in Figure 70. It was determined that this region was 13 mils (0.33 mm) in length, 10 mils (0.25 mm) wide, and extended into the cladding to a depth of 2.8 mils (0.071 mm).

A metallographic section of plenum specimen PNL-10-15-Z is shown in Figure 71. The layer shown on the inner surface of the cladding was remote from the FCTT failure site. This layer is 0.001-inch (0.0025-mm) thick and extends around 10% of the internal circumference. The structure of this layer is similar to that found in the flash or metal buildup associated with external wear marks. In addition to the thick layer, there is a thin intermediate phase layer between the cladding and the 1-mil (0.0025-mm) thick region. This sample was adjacent to the Inconel reflector, and the layers in Figure 71 are probably a result of combined mechanical and diffusive interaction with the reflector. The layer apparently had no deleterious effect on the cladding properties.

A metallographic section of specimen P-23B-14A-M is shown in Figure 72 in both the as-polished and etched conditions. A deposit or layer on the inner surface of the specimen appears to be tightly bonded to the cladding. The layer was apparently brittle, due to the cracks produced during the FCTT test. This same layer was found in fuel pin section N, which was a transverse metallographic section. The layer found in section N did not have any radial cracks. It is not possible, at this time, to determine if the layer affected the flow behavior of the cladding during the test. An analysis of the layer with the Scanning Electron Microscope showed that the layer was composed primarily of palladium with small amounts of barium and neodymium and a trace of strontium.

Specimen P23C-29B-E exhibited the lowest strength ratio of all of the specimens from this fuel pin. Its strength ratio of 62.8% is quite

low for a fluence of $0.88 \times 10^{22} \text{ n/cm}^2$. A metallographic section of the failure site revealed a thinned wall 90° away from the failure site. Figure 73 shows this region. The minimum wall thickness is 8.6 mils (0.218 mm).

750X

FIGURE 70. Metallographic Section of Specimen PNL-10-15-E Showing an Inner Surface Corroded Area.

1 Mil 750X

FIGURE 71. Metallographic Section of Plenum Specimen PNL-10-15-Z Showing a Build-Up of Material on the Internal Cladding Surface.

(a) As-Polished

1 Mil 300X

(b) Etched

FIGURE 72. Metallographic Section of Specimen P-23B-14A(M). Failure occurred at 762°F (679 K). The cracks in the surface layer were created during the test.

FIGURE 73. Metallographic Section of Specimen P23C-29B-E. The minimum wall thickness is 8.6 mils (0.218 mm).

VII. FUEL ADJACENCY EFFECT

Cladding specimens, from the fuel column region of fuel pins, show lower strength than specimens that have not been adjacent to the fuel. This effect is shown in Figure 74 for specimens tested with the FCTT at $10\text{F}^\circ/\text{s}$. The irradiation conditions for fuel pin samples and structural samples were similar. Unfueled samples show about a 20% reduction in strength, while the fueled samples exhibit more than a 50% reduction in strength. All of the fuel pin samples that have shown this large reduction in strength have failed in an intergranular mode (see Figure 29).

The fuel adjacency effect is also evident when examining the behavior of specimens from different regions of a fuel pin. A fuel pin may be divided into three regions: (1) fuel column, (2) plenum (i.e., above the fuel column), and (3) below the fuel column.

FCTT test results at $10\text{F}^\circ/\text{s}$ (5.6 K/s) and 4000 psi (27.6 MPa) gas pressure give a large enough data base to examine the effect of axial location on the mechanical properties of the cladding. Values for failure temperature were plotted as a function of neutron fluence for each of these three regions. Curves were fit with a least-squares computer program to show the average behavior of these quantities as a function of neutron fluence. Figures 75, 76 and 77 show the failure temperatures as a function of neutron fluence for fuel column specimens, plenum specimens and specimens from below the fuel column, respectively. In each case, a decrease in failure temperature is observed for increasing fluence. The failure temperature trends of specimens from these three regions are compared in Figure 78. Specimens from the fuel column exhibit the worst properties while specimens from below the fuel column are the best; i.e., their failure temperatures are the highest at any value of fluence. The difference between the plenum and below the fuel column specimens is most likely due to the difference in irradiation temperatures.

A number of factors have been suggested as being responsible for the greater degradation of fuel column specimens. These factors are enhanced helium embrittlement, in-reactor steady-state stress, fuel cladding mechanical interaction (FCMI), residual stress, radial-cladding temperature gradients, internal surface corrosion and grain boundary embrittlement. These factors will be discussed in the next sections.

A. HELIUM EMBRITTLEMENT

One mechanism, proposed to explain the poorer behavior of fuel column cladding specimens, is that of enhanced helium embrittlement. The presence of a helium concentration gradient in the fuel pin cladding could possibly produce a severe degradation in the mechanical properties of the cladding. Kramer, et al.,⁽²¹⁾ have evaluated the effect of helium on the tensile properties of austenitic steels at 973 K. Helium was injected into miniature tensile samples using alpha-particle irradiation from a cyclotron. Figure 79 shows the effect of helium on the total elongation of several types of stainless steel. These data clearly show that increasing the helium concentration decreases the ductility of austenitic stainless steels. Also, the data do not indicate any saturation effects with increasing helium content. Thus, it is possible that fuel pin cladding, with a high helium concentration, may have much poorer ductility and strength permitting failure at lower temperatures and leading to the results given in Figure 74.

Samples from NUMEC E and PNL-9 fuel pin cladding and N-10 tubing specimens irradiated in Subassembly X-124 were sent to Atomics International for helium analysis.

Determination of the helium content by mass spectrometry was performed by Harry Farrar IV at Atomics International. Each specimen was vaporized under vacuum using a resistance-heated tungsten wire basket. After melting, the temperature of the tungsten was increased until the sample was

seen to vaporize; and finally, the tungsten itself disintegrated. Immediately before the heating operation, a precisely-known number of ^3He atoms were added to the vacuum enclosure. After mixing of the isotopes, the gas passed over getters directly into the mass spectrometric volume. A measurement of the $^4\text{He}:^3\text{He}$ ratio and a knowledge of the weight of the material then produced the helium concentration. The low-level ^3He spike was obtained by successive expansions through calibrated volumes. The mass spectrometer was repeatedly calibrated for mass fractionation during each series of runs by analyzing known mixtures of ^3He and ^4He . A sample was analyzed for ^3He without adding the ^3He spike, and the ^3He concentration was found to be a negligible ~ 0.96 appb (9.6×10^{-10} atom fraction). This could have been formed directly, but is more likely the product of a small amount of decayed tritium.

Absolute uncertainty, determined from the cumulative uncertainties of sample weight, isotope ratio measurement, and spike size are estimated to be less than 1.5% of the concentration value for this series of runs. The reproducibility between duplicate specimens of the inner surfaces average better than 2%. This uncertainty can probably be attributed to the flux gradients across the widths of the claddings.

Both bulk and incremental samples were analyzed to determine the average concentration and any existing gradient profiles across the wall. Duplicate mass spectrometric determinations were made for both the fuel pin and tubing samples. Table 13 summarizes the results of the bulk analyses. These results show that the samples from the fuel pin cladding contained about twice as much helium as the structural stainless steel sample when the values are adjusted for the fluence.

Figure 80 presents the results of the analyses of the incremental samples. The incremental samples were obtained by drilling a bulk sample with successively larger diameter drills. The structural sample shows a

relatively constant helium concentration across the wall; but, the PNL-9-30:M sample showed a high concentration for the first couple of mils on the inside surface.

Since the helium gradient shown in Figure 80 was not expected, additional samples were prepared for helium analysis to examine possible mechanisms for the higher helium content and gradient in the fuel pin cladding. These samples were selected to examine: (1) the effect of axial location (irradiation temperature) in the pin; (2) the effect of fuel composition; and, (3) the effect of the cladding composition. The fuel pin samples, chosen for the study, are listed in Table 14 with the irradiation conditions and axial location of the samples.

The samples from P-23B-2A were selected to provide information pertaining to the effect of axial position and irradiation temperature in the pin. Comparison of samples L2 and LL may provide data on the effect of nitrogen. If significant amounts of nitrogen from the fuel diffused into the cladding, thereby yielding a radial nitrogen gradient, then the helium profiles should be different due to the two different cladding temperatures 745°F vs 1305°F (669 K vs 980 K). Similarly, comparison with the NUMEC E-067 sample should show differences in helium profiles, since it had been already determined that the fuel had increased the bulk-cladding nitrogen in this pin to 300 ppm. Nitrogen has a very high (n,α) cross section and, therefore, its presence can result in appreciable helium production.

If nitrogen from the fuel were to produce a major effect on helium generation, the irradiation temperature should affect the resultant helium profile. Nitrogen profiles across the cladding were calculated for several temperatures assuming a constant inner surface composition of 2000 wppm and an outer surface value of 80 wppm. Results of the calculations for a time of 5000 hours are given in Figure 81. A steep gradient is shown for 800°F (700 K), while at 1400°F (1033 K) equilibrium has been attained. The

different profiles shown here would indicate that if a large amount of nitrogen had diffused into the cladding from the fuel, then samples from the bottom and top of the pin would have dissimilar nitrogen profiles, and therefore, dissimilar helium profiles.

Comparison of samples P-23A-21-M and P-23B-2A-L2 were chosen to provide information on the effect of cladding composition. Fuel pin P-23A-21 used O-Lot cladding which contained 240 ppm nitrogen and 9 ppm boron. Variations of these elements can affect the amount of helium generation. Samples P-23B-2A-K and -L1 were selected as controls, since they were not adjacent to the fuel.

In addition to the cladding samples, pieces of the wire wrap from each pin were analyzed for helium. This measurement would provide a direct comparison of stainless steel specimens at the same position in the core. The wire wrap specimens were cut as 10-mil (0.25-mm) thick discs. These wire wrap specimens were from the P-23B-2A and NUMEC E-067 samples.

Figures 82 and 83 present the results of helium analyses for each specimen. The helium concentration is shown as a function of the cladding wall thickness. All specimens show a similar profile, in that the helium content increases by about a factor of two during the first 2-3 mils (0.051-0.076 mm), and then shows a fairly constant concentration for the remaining wall thickness. The dashed curves, shown in Figures 80, 82 and 83, for each specimen are the predicted helium concentration profiles based on helium production from (n,α) reactions and ternary fission.⁽²²⁾ The amount of helium produced by (n,α) reactions in the stainless steel can be calculated based on the neutron fluence. Table 15 presents the as-received chemical composition for N-and T-lot 20% CW 316 Type stainless steel and the percent of helium produced by the elements in the stainless steel. Ternary fissions occur about one per 400 fissions in the fuel. From a knowledge of

the energy spectrum of these α particles from ternary fission, depth of penetration can be calculated. The dashed curves in Figures 80 and 82 represent the sum of the predicted helium produced by both mechanisms. Good agreement between experimental data and predicted concentrations were obtained for the four specimens. Thus, the results are consistent with the hypothesis that ternary fission is responsible for the helium profile in the fuel column specimens. The effect of cladding composition (O lot versus N and T lots), axial location and irradiation temperature of the specimens in the fuel column did not appear to have any effect, as shown by the similar profiles of P-23A-21-M, P-23B-2A-LL and L2 in Figure 82. The helium concentration gradients in Figure 82 for specimens L2 and LL from P-23B-2A are very similar, indicating that significant quantities of nitrogen did not diffuse from the fuel.

A summary of the measurements and calculations made on the wire wrap specimens are given in Table 16. Both the predictions and measurements for the bulk helium contents of the cladding and the adjacent wire wrap specimens are similar. The last column in the table gives the as-received nitrogen concentrations for the two materials. Based on the original nitrogen concentrations, it would be reasonable to expect that a difference in other elements (e.g., less boron and nickel) and lower fluence in the wire wrap specimens could offset the effect of increased nitrogen content. Therefore, the predicted helium concentrations were very similar for the cladding and wire wrap.

The results shown in Figure 83 were not expected. Both samples exhibited an enhanced helium content at the inner surface. Since the fuel pins contain helium gas, it may be that this gradient is a result of radiation-enhanced diffusion, but this has not been substantiated. Additional analyses would be required to understand this effect.

Figure 84 shows that the generation of helium on the cladding inner surface is roughly proportional to neutron fluence, and that a similar

relationship exists for the bulk of the material, which is labeled as the outer surface. The helium concentration data, from all specimens, may be normalized. The measured helium data were divided by the calculated helium concentrations that should have resulted from (n, α) reactions with the cladding constituents. Figure 85 shows there are no obvious differences between the data from the different specimens. If sample position, irradiation temperature or chemistry were to produce an effect, the data in Figure 85 would not be reduced to essentially the same profile.

In summary, it has been determined that the fuel pin cladding exhibits a radial helium gradient. Even though helium can produce an embrittlement, as shown in Figure 79, it appears that helium is not solely responsible for the fuel adjacency effect, since there was a gradient in specimens that were not adjacent to the fuel.

B. EFFECT OF IN-REACTOR STRESS

During reactor irradiation, fission gas accumulates in a fuel pin causing an internal pressure. The fuel pin cladding increases in diameter because of irradiation creep and swelling. A series of transient tests were conducted to determine if the strains produced by irradiation creep are detrimental to the cladding.⁽²³⁾ Pressurized tubes from the X-157 subassembly were subjected to a transient burst test at a heating rate of $10\text{F}^{\circ}/\text{s}$ (5.6 K/s) and at a gas pressure of 6000 psi (41.4 MPa). Specimens irradiated in the X-157 subassembly were subjected to various stresses during irradiation. The irradiation conditions and test results are summarized in Table 17. Control specimens, irradiated without stress, were also tested and the results are presented in Table 18. Data from these tests are compared to data from fuel pin specimens in Figure 86, where the strength ratio is shown as a function of neutron fluence. The data are grouped into two distinct behavior patterns, fueled and unfueled. From these results, it is obvious that diameter changes resulting from irradiation creep are non-damaging, and that the fuel adjacency effect is not related to in-reactor stress.

C. EFFECT OF FCMI, RESIDUAL STRESS, AND RADIAL CLADDING TEMPERATURE GRADIENTS

The effects of FCMI, residual stress and radial cladding temperature gradients have not been quantitatively evaluated. It was shown in Section IV, that the cladding specimens that exhibited poor strength and low ductility failed in an intergranular mode. Fuel column specimens exhibiting the poorest behavior always fail with a pinhole; i.e., in an intergranular mode. It is not reasonable to expect FCMI, residual stress or radial cladding temperature gradients to alter the cladding in such a manner as to produce an intergranular fracture in a temperature region where transgranular or mixed-modes of fracture are normally observed. Therefore, these items are assumed to have no bearing on the fuel adjacency effect.

D. INTERNAL SURFACE CORROSION

Internal surface corrosion would have an effect by thinning the wall and by producing small cracks that might propagate under applied stress. It was shown in Section V, that the depth of internal surface corrosion had no effect on the strength and failure strains for depths up to 0.001 in. (0.025 mm). Various theories for crack propagation were evaluated in Section VI and it was shown that stresses on the correct order of magnitude could be calculated by the Petch and Griffith equations, but these equations were not capable of describing temperature and rate effects. This observation is important, since the fuel adjacency effect is more prominent at the $10\text{F}^0/\text{s}$ (5.6 K/s) rate than at the $200\text{F}^0/\text{s}$ (111 K/s) rate. It is concluded that the effects of corrosion alone do not account for the fuel adjacency effect. It is possible that small cracks may interact with the embrittlement phenomenon discussed in the next section.

E. GRAIN BOUNDARY EMBRITTLEMENT

It is a well-known fact that intergranular fracture in alloy steels can be produced by the segregation of impurities to the grain boundaries.^(24,25) The segregation of impurities to the grain boundaries disturbs the interatomic bonding across the interface thereby allowing the material to fail in an intergranular mode. This segregation of impurities to the boundaries will result in a lowering of the surface energy, γ . This surface energy was used in the evaluation of fracture stresses in Equations (12) and (13) in Section IV. A reduction in γ would result in low fracture stresses calculated by either equation. It is currently proposed that the fuel adjacency effect is the result of certain impurities (yet to be identified) being absorbed on or segregated to the grain boundaries. This, coupled with the effects of helium, results in a material that is susceptible to embrittled intergranular fracture under the appropriate temperature and strain rate conditions.

HEDL 7802-54.2

FIGURE 74. Comparison of Fueled and Unfueled Specimen Transient Behavior for a Neutron Fluence of $3 - 4 \times 10^{22} \text{ n/cm}^2$ ($E < 0.1 \text{ MeV}$).

HEDL 7802-56.4

FIGURE 75. Failure Temperatures of Fuel Column Specimens Transient Tested at $10F^\circ/s$ ($5.6 K/s$) with a Gas Pressure of 4000 psi (27.6 MPa).

TABLE 13

HELIUM CONCENTRATIONS OF CLADDING SAMPLES

<u>HEDL Sample Name</u>	<u>Source of Sample</u>	<u>Fluence Above 0.1 MeV (n/cm²)</u>	<u>Average Helium Concentration (appm)</u>	<u>Ratio of Helium Concentration to Fluence (x 10²²)</u>
N-049:D	Fuel Pin	1.45 x 10 ²²	4.57	3.2
N-049:E2	Fuel Pin	1.7 x 10 ²²	5.16	3.0
PNL-9-30:M	Fuel Pin	5.6 x 10 ²²	13.7	2.4
Q-16(X124)	Structural	3.8 x 10 ²²	5.0	1.3

HEDL 7802-56.1

FIGURE 80. Helium Concentration Profiles in 20% CW Type 316 Stainless Steel Cladding.

TABLE 14
SPECIMENS FOR HELIUM ANALYSIS

<u>Specimen</u>	<u>Irradiation Temperature, °F (K)</u>		<u>Neutron Fluence 10²² n/cm² (E > 0.1 MeV)</u>	<u>Cladding Lot^(a)</u>	<u>Specimen Location, Distance Above Fuel Bottom, in. (mm)</u>	
P-23A-21-M	785	(691)	2.8	0	1.0	(25.4)
P-23B-2A-K	700	(644)	2.7	T	-0.6	(-45.7)
P-23B-2A-L1	700	(644)	2.9	T	-0.5	(-12.7)
P-23B-2A-L2	745	(669)	3.3	T	0.3	(7.6)
P-23B-2A-LL	1305	(980)	3.4	T	12.7	(323)
NUMEC E-067-D	950	(783)	3.1	N	7.0	(178)

(a) 0 lot is from Heat 88229; T and N are from Heat 87210.

TABLE 15
TYPICAL CONTRIBUTIONS TO HELIUM PRODUCED
IN STAINLESS STEEL

<u>Element</u>	<u>wt %</u>	<u>% of Helium(a)</u>
Fe	65.6	24
Cr	16.5	3
Ni	13.4	61
Mo	2.42	0.2
Mn	1.52	0.3
Si	0.44	3
Cu	0.07	---
C	0.056	0.3
P	0.012	0.1
N	0.007	4
S	0.006	1
B	0.0007	4

(a) Percentage of total helium produced by each element.

HEDL 7604-243.4

FIGURE 81. Calculated Nitrogen Profiles Through Cladding Wall After 5000 Hours, Assuming Inner and Outer Surface Concentrations of 2000 and 80 WPPM Nitrogen, Respectively.

HEDL 7802-56.12

FIGURE 82. Helium Profiles Through the Cladding Wall in Specimens from the Fuel Column Region of Fuel Pins. The inner surface of the cladding is at wall position "zero".

HEDL 7802-56.14

FIGURE 83. Helium Profiles Through the Cladding Wall in Specimens from Below the Fuel Column Region of Fuel Pins. The inner surface of the cladding is at wall position "zero".

TABLE 16

HELIUM ANALYSIS OF CLADDING AND WIRE WRAP

Sample	10^{22} Neutron Fluence n/cm^2 ($E > 0.1$ MeV)	Measured Helium Concentration, appm	Predicted Helium Concentration, appm	Nitrogen Concentration (a) wppm
NUMEC E-967-D	3.17	10.8	10.6	60(c)
NUMEC E-067-D (WW)(b)	3.09	10.2	11.6	530
P-23B-2A-L2	2.94	9.5	9.7	60
P-23B-2A-L2 (WW)	2.65	9.9	10.4	530
P-23B-2A-LL	2.88	8.7	9.2	60
P-23B-2A-LL (WW)	2.58	8.5	9.9	530

(a) As Received Composition

(b) WW = Wire Wrap

(c) 300 wppm After Irradiation

FIGURE 84. Neutron Fluence Dependence of Helium Concentration in Fuel Pin Cladding.

HEDL 7802-56.18

FIGURE 85. Normalized Helium Concentration Profiles Across the Cladding Wall. The inner surface is at position "zero".

TABLE 17

POSTIRRADIATION TRANSIENT RESULTS WITH PRESSURIZED TUBES TESTED AT 10F°/s
(5.6 K/s) WITH A GAS PRESSURE OF 6000 PSI (41.4 MPa)

Specimen	Fast Fluence 10 ²² n/cm ²	Irradiation Temp °F (K)		In-Reactor Stress, ksi	Nominal Total Measured % Strain ($\Delta D/D_0$)	Assumed Irradiation Creep Strain, %	Failure Temp °F (K)	Strength Ratio %
GW	0.89	710	(650)	10	.015	.032	1410 (1038)	71
GY	0.89	710	(650)	30	.041	.056	1510 (1093)	90
FO	0.87	710	(650)	40	.050	.064	1515 (1096)	91
FA	1.05	845	(725)	10.3	.00	.029	1555 (1118)	100
FC	1.05	845	(725)	30.9	.083	.094	1550 (1115)	100
LQ	2.05	930	(772)	23.9	.202	.201	1530 (1104)	95
GU	1.43	1035	(830)	9.6	.036	.040	1545 (1113)	98
GT	2.92	965	(792)	8.6	.072	.088	1515 (1096)	91
LG	4.27	787	(693)	58	1.040	1.040	1540 (1110)	97
HF	5.08	890	(748)	0	.063	----	1545 (1113)	98
KS	5.08	890	(748)	18.5	.380	.372	1500 (1087)	88
KR	5.08	890	(748)	27.7	.707	.660	1540 (1110)	97
IK	5.70	1084	(857)	2.1	.120	.081	1465 (1069)	81
BP	9.80	980 → 800	(800 → 700)	4.3	3.200	.633	1555 (1118)	100
BF	10.60	980 → 800	(800 → 700)	2.2	2.750	.358	1475 (1074)	83
AR	11.40	1080 → 830	(852 → 716)	0.8	1.980	.193	1455 (1063)	78

TABLE 18

POSTIRRADIATION TRANSIENT RESULTS FOR UNSTRESSED
SPECIMENS TESTED AT 10F°/s (5.6 K/s) AND 6000 PSI (41.4 MPa)

<u>Specimen</u>	<u>Irradiation Temp,</u> <u>°F (K)</u>		<u>Fluence, 10²² n/cm²</u>	<u>Failure Temp,</u> <u>°F (K)</u>		<u>Strength</u> <u>Ratio, %</u>
FF2	960	(789)	1.3	1520	(1100)	92
K1	1050	(838)	3.4	1460	(1066)	80
Q43	860	(733)	3.6	1475	(1075)	83
BM2	913	(763)	6.0	1495	(1086)	86
BV2	735	(663)	6.0	1515	(1097)	91
BV5	735	(663)	6.0	1525	(1103)	93

HEDL 7802-56.2

FIGURE 86. Comparison of Transient Failure Strength for Fueled, Unfueled and Pressurized Irradiated Cladding Tested at $10^\circ/\text{s}$ (5.6 K/s) with Gas Pressure of 6000 psi (41.4 MPa).

VIII. COMPARISON OF THE MECHANICAL PROPERTIES OF CLADDING FROM STEADY-STATE-IRRADIATED UNBREACHED AND BREACHED PINS

Fuel pins PNL-10-14 and NUMEC E-122 both developed a breach during EBR-II irradiation near the top of the fuel column after burnups of 58,830 and 37,200 MWd/MTM, respectively. PNL-10-14 failed in a wear mark which was 9 mils (0.229 mm) deep. Both pins failed in a benign manner, with the breach appearing as a pinhole. A comparison of the mechanical properties of specimens from a breached pin with specimens from an unbreached sibling is possible only for the NUMEC E pins, since there were no FCTT specimens from the fuel column of the breached PNL-10-14 pin. However, the cladding properties of the sibling PNL-10-15 may be compared with the property trends exhibited by pins that have not experienced a breach.

Figure 87 shows the failure temperatures and failure strains for specimens from NUMEC E-067 and E-122. The data trends, as a function of neutron fluence, were established from prior data on specimens from PNL-10, PNL-11, P-23A, NUMEC E and NUMEC F fuel pins. Failure strength and strain values of the specimens from these breached and sibling pins are very similar to those indicated by the trend lines. The average of the cladding data from the breached NUMEC E-122 pin is lower than that from the sibling pin; however, the results do fall in the trend lines. This result indicates that the steady-state breach observed in NUMEC E-122 was a local and specific phenomenon, rather than a general phenomenon occurring over the entire pin. If the same steady-state irradiation breach mechanism had been operating to the same extent over the entire pin, then all the specimens would have been on the verge of breach and would have exhibited poorer mechanical properties than those observed. As shown in Figure 88, the sibling PNL-10-15 results demonstrate that it also was not at incipient breach.

HEDL 7802-56.23

FIGURE 87. Comparison of Breached and Sibling Fuel Pin Mechanical Behavior. Specimens tested at 10°F/s (5.6 K/s) and 4000 psi (27.6 MPa) gas pressure.

HEDL 7802-56.24

FIGURE 88. Comparison of FCTT Data From PNL-10-15 Specimens with FCTT Data Trend of Fuel Column Specimens for a $10\text{F}^\circ/\text{s}$ (5.6 K/s) Heating Rate and 4000 psi (27.6 MPa) Gas Pressure.

IX. EFFECT OF HIGH IRRADIATION TEMPERATURE AND CLADDING ATTACK

It was previously determined that the presence of mixed oxide fuel and neutron fluence produced a much larger change in cladding properties than did variations in irradiation temperatures below 1000°F (811 K).⁽¹⁾ Examination of the data in Tables 2-6 show there is little effect on the failure strain for irradiation temperatures less than 1100°F (866 K). However, irradiation temperatures above 1100°F (866 K) may produce a much higher ductility, as shown by the P-23B data in Figure 10.

Specimens irradiated at temperatures above 1100°F (866 K) also possess superior strength properties, as indicated by their higher failure temperatures shown in Figures 89 and 90. In Figure 89, the four specimens with high irradiation temperatures have failure temperatures several hundred degrees higher than specimens irradiated at 800-1000°F (700-811 K). These four specimens were from P-23A and P-23B fuel pins. The specimens with the poorer strength, at the same fluence, were from PNL-9 and NUMEC E fuel pins. In Figure 90, a similar effect is observed. For the four tests above 3×10^{22} n/cm², the two stronger specimens (higher failure temperature) had irradiation temperatures above 1100°F (866 K) and were from P-23B fuel pins, while the two specimens with the lower failure temperatures were from PNL-9 and PNL-11 fuel pins.

The data in Tables 8 and 9 demonstrate that these high-irradiation temperature specimens, which exhibited good transient burst properties, did have significant amounts of inner surface corrosion. Therefore, the mere presence of fuel-cladding attack is not sufficient to significantly degrade mechanical properties. In fact, it is possible that the high-irradiation temperature conditions, which are necessary for cladding fission-product attack, may simultaneously decrease the crystal (matrix) hardness so that the specimen is not sensitive to low ductility intergranular fracture. In addition, the morphology of the high-temperature fission-product attack

results in crack starters, which are more rounded and blunt than the tight, sharp, short intergranular penetrations, which are often observed on the inner surface of lower temperature fuel column specimens.

However, it is definitely premature to conclude that irradiation temperatures above 1100°F (866 K) always produce greater cladding strength and ductility, as is indicated by Figures 89 and 90. Still higher irradiation temperatures may produce deleterious phases in the cladding. Also, the high irradiation temperature specimens have been tested only at the 200F°/s (111 K/s) rate; it could be presumed that the high strain rate diminished the effect of corrosion defects on the strength. This behavior has also been observed by Ring, Busboom and Spalaris.⁽²⁶⁾ They conducted burst tests (constant temperature, increasing pressure) at 1100°F (866 K) on fuel pin sections that had intergranular attack on the internal surface. Their results indicated that the burst strength had not been strongly affected by the intergranular attack, although the ductility was somewhat decreased. Additional FCTT tests, at slower rates, are required to determine the effect of high irradiation temperature.

HEDL 7802-56.19

FIGURE 89. Failure Temperatures for Fuel Column Cladding Specimens Tested with the FCTT at 200F°/s (111 K/s) and 4000 psi (27.6 MPa) Gas Pressure.

FIGURE 90. Failure Temperatures for Fuel Column Cladding Specimens Tested with the FCTT at 200F°/s (111 K/s) and 6000 psi (41.4 MPa) Gas Pressure.

X. LOW TEMPERATURE FAILURES

Three specimens listed in Tables 4 and 5 failed at fairly low temperatures. In each case, the specimens failed at temperatures less than their average steady-state irradiation temperature because of the very high gas pressures used for each test. Specimen PNL-9-30-V failed at 686°F (636 K). This specimen burst while pressurizing at the initial temperature. The failure site was in a wear mark, which was 2.1 mils (0.053 mm) deep. The nominal strength ratio was 85.5% and the net-section strength ratio was 99.4%. Specimen PNL-9-13-0 also failed before the test was initiated. It failed at 687°F (637 K) with a strength ratio of 92.9%. Based on the LMP correlation for plenum specimens, the strength ratio would have been only 84% had the specimens been tested with lower pressures which produce failures in the normal range of 1000-1600°F (811-1144 K). The slight strength reduction exhibited in the low-temperature test condition probably resulted from minor amounts of recovery of the cold work at the plenum irradiation temperature.

The third specimen to fail at a low temperature was P-23B-14A-M. Failure occurred at 762°F (679 K), which corresponds to a strength ratio of 90.4%. A metallographic section of this specimen showed that there was a layer or deposit on the inner surface, which apparently had low ductility and may have influenced the mechanical behavior of the specimen. Nevertheless, the expected strength ratio at 1200-1600°F (922-1144 K) failure temperatures, for this specimen, would have been only 54%. Therefore, the strength of this specimen at the low temperatures was much greater than would have been evident had the specimen been tested under conditions in which low-ductility intergranular fracture could have significantly degraded the strength.

All three of these specimens exhibited failure strains in excess of 1.0%. This fact, combined with the good strength ratios, indicates that

all of these specimens exhibited reasonably good behavior for the imposed test conditions. This behavior further reinforces the statements which have been made previously regarding the high strength and good ductility of cladding irradiated at temperatures below 1000⁰F (811 K) under transient loading conditions. Consequently, the lower portion of a fuel pin, which stays below 1000⁰F (811 K) in a transient, will not fail.

XI. EVALUATION OF TREAT-TESTED PINS

Simulated transient tests were conducted on specimens from two TREAT-tested fuel pins, HOP 3-2A and HOP 3-2B. These pins were subjected to TREAT transients, simulated FFTF 3\$/s overpower transients terminated by the Plant Protective System (PPS). A cladding sample was cut from the fuel column of each pin and tested at $10\text{F}^0/\text{s}$ (5.6 K/s) and 4000 psi (27.6 MPa) internal gas pressure. Figure 91 shows the failure temperatures of these specimens from the TREAT-tested pins, compared with the failure temperatures of specimens from steady-state irradiated pins, as a function of neutron fluence. The solid curve represents the average of specimens taken from steady-state irradiated fuel pins. Both samples from TREAT-tested pins exhibited failure temperatures slightly lower than the average curve, but both are within the scatter of the data field.

Pre- and post-TREAT test profilometry data for HOP 3-2A and HOP 3-2B were used to calculate the incremental TREAT test plastic strains produced in pin sections used for FCTT testing. Also, pre- and post-FCTT test diameter measurements were made in each transient specimen. Table 19 summarizes the strain data for these samples. Column 3 gives the total strain for each specimen and Column 4 gives the average strain for steady-state irradiated specimens tested at $10\text{F}^0/\text{s}$ (5.6 K/s) and 4000 psi (27.6 MPa). Total TREAT and FCTT strain values for both specimens compare well with expected failure strain values. These results in Figure 91 and Table 19 demonstrate that PPS termination of even a severe hypothetical 3\$/s transient not only precludes cladding breach, but also prevents significant pin damage.

Even though the cladding temperature is significantly elevated during a PPS-terminated hypothetical 3\$/s transient, very little cladding mechanical damage occurred. PPS termination of the transient prevented the high fuel temperatures that give rise to cladding loading from differential fuel-cladding thermal expansion, intragranular fission-gas-induced swelling, and transient release of the intergranular fission gases.^(27,28) For

example, the calculated cladding hoop stress at failure for the un-terminated hypothetical 3\$/s TREAT test HOP 3-3C was 50,000 to 60,000 psi (345-414 MPa),⁽²⁹⁾ which agrees very well with the failure stresses of 55,000 psi (379 MPa) inferred from FCTT results.⁽¹⁾ However, for the FFTF PPS-terminated hypothetical 3\$/s TREAT test, the maximum hoop stress calculated on the same basis was only 18,000 psi (124 MPa); therefore, little mechanical damage to the cladding is expected.

TABLE 19

CUMULATIVE STRAIN DURING TREAT AND FCTT TESTS

<u>Test</u>	<u>Fuel Pin</u>	<u>Incremental TREAT Test Strain</u>	<u>Incremental FCTT Test Failure Strain</u>	<u>Total TREAT and FCTT Strain</u>	<u>Average FCTT Failure Strain*</u>
HOP 3-2A	P-23A-27:C	0%	0.22%	0.22%	0.30%
HOP 3-2B	P-23A-30:D	0.04%	0.13%	0.17%	0.14%

*Tested at 10°F/s (5.6 K/s) and 4000 psi (27.58 MPa) gas pressure.

FIGURE 91. Comparison of FCTT Data for Specimens from TREAT-Tested Pins with Specimens from Steady-State Pins.

XII. EFFECT OF CLADDING COMPOSITION

Results given in Table 7 for specimens from P-23C fuel pins were for samples of two different types of cladding. Specimens from pins -3A, -49C and -71G were 20% CW 316 Type stainless steel N-lot cladding. This is the same type of cladding that was used for all of the specimens listed in Tables 2 through 5. Cladding for P-23C-25B and -29B pins was a titanium modified 20% CW 316 Type stainless steel.

Failure temperatures for the two types of cladding from the P-23C pins are shown in Figure 92. Except for specimen 29B-E, the Ti-modified cladding appears to have failure temperatures that are equal to or slightly better than unirradiated N-lot cladding. The N-lot specimens from P-23C pins have failure temperatures that are generally equal to or less than unirradiated material. It should be pointed out that this qualitative observation should not be used to infer behavior of the Ti-modified cladding at higher fluences. The maximum fluence given in Table 6 is only 1.04×10^{22} n/cm². Additional data would be required to obtain a proper comparison between the two types of cladding.

HEDL 7802-56.22

FIGURE 92. Failure Temperatures of Cladding Specimens from P-23C Fuel Pins Tested 200F°/s (111 K/s).

XIII. APPLICATION OF FCTT DATA

During an integral fuel pin transient test, differential fuel cladding thermal expansion, intragranular-fission-gas-induced fuel swelling, and transient release of the intergranular fission gases^(27,28) can generate high local cladding loadings. To formulate a fuel pin cladding integrity limit criterion from the fuel loading functions and the cladding mechanical properties requires detailed quantitative information for both the loading functions and cladding properties. A knowledge of the cladding deformation behavior and failure strength and ductility may be determined with FCTT testing under appropriate transient heating and loading test conditions. However, the fuel loading functions must be inferred or determined from TREAT tests. Nevertheless, a knowledge of the cladding properties will enable a more rational inference or extraction of the fuel loading function. The ductility exhibited in an integral fuel pin transient test should be similar to that exhibited by an FCTT specimen that experienced similar irradiation and transient conditions. Furthermore, the failure strength exhibited in the FCTT results may be employed to gain insight into the cladding loadings imposed by the various fuel behavior phenomena. For example, knowing the steady-state irradiation conditions and the transient heating rate and failure temperature for an integral fuel pin transient test will enable estimation from FCTT results of the stresses that must have been applied to produce cladding failure.

TREAT test HUT 3-5B was a 3\$/s unterminated overpower transient test on fuel pin PNL 9-54. Pin failure occurred at 7.035 seconds with the midwall cladding temperature reaching a maximum of 1541⁰F (1111 K) at the central portion of the pin. Figure 93 illustrates the cladding midwall temperature history. The average heating during the TREAT test was approximately 1700F⁰/s (944 K/s). From the LMP failure stress correlation given in Section IV, it is possible to estimate the stress in the cladding at failure. Fuel pin PNL 9-54 had a burnup of 48 MWd/Kg resulting in a maximum neutron fluence of 5.9×10^{22} n/cm². Using the LMP equation for

$\phi t = 5-6 \times 10^{22} \text{ n/cm}^2$ for fuel column specimens in Table 7, failure temperatures were calculated for heating rates of $200\text{F}^0/\text{s}$ (111 K/s) and $1700\text{F}^0/\text{s}$ (944 K/s). The results are summarized in Figure 94. For a cladding temperature of 1541F^0 (1111 K) and a heating rate of $1700\text{F}^0/\text{s}$ (944 K/s) the stress from Figure 94 is found to be ~ 27 ksi. The curve at $200\text{F}^0/\text{s}$ (111 K/s) is shown for comparative purposes. At this rate, the stress would be ~ 23.5 ksi (162 MPa).

Pre- and post-test diameter measurements were obtainable on a section adjacent to the failed region. Section C was located at a position which was from 2.5 to 5.0 inches (63.5-127 mm) above the bottom of the fuel column. The diameter measurements indicated that this section had strained 0.22% during the TREAT test at an average midwall temperature of 1525F^0 (1102 K). Equations (2) and (5) through (7) were used to calculate a failure strain of 0.15% for $T = 1541\text{F}^0$, (1111 K), $\dot{T} = 200\text{F}^0/\text{s}$ (111 K/s) and $\phi t = 5.9 \times 10^{22} \text{ n/cm}^2$. Only a minor change in this value is expected for $1700\text{F}^0/\text{s}$ (944 K/s). This value is in reasonable agreement with that for Section C, which was adjacent to the failed region.

HEDL 7802-56.25

FIGURE 93. Cladding Midwall Temperature During TREAT Test HUT 3-5B.

HEDL 7802-56.21

FIGURE 94. Transient Failure Temperatures for a Neutron Fluence of $5-6 \times 10^{22} \text{ n/cm}^2$.

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