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GEAP 10028-32
AEC RESEARCH AND
DEVELOPMENT REPORT
NOVEMBER 1969

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**SODIUM-COOLED REACTORS
FAST CERAMIC REACTOR
DEVELOPMENT PROGRAM
THIRTY-SECOND QUARTERLY REPORT
AUGUST-OCTOBER 1969**

U.S. ATOMIC ENERGY COMMISSION
CONTRACT AT (04-3)-189
PROJECT AGREEMENT 10

BREEDER REACTOR DEVELOPMENT OPERATION

GENERAL ELECTRIC

SUNNYVALE, CALIFORNIA

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SODIUM-COOLED REACTORS
FAST CERAMIC REACTOR DEVELOPMENT PROGRAM

Thirty-Second Quarterly Report

August-October 1969

Approved:

W. E. Baily

W. E. Baily, Project Engineer
Fast Ceramic Reactor Development Program

Approved:

E. L. Zebroski

E. L. Zebroski, Manager
Sodium Reactor Technology

Prepared for the United States Atomic Energy Commission
Under Contract No. AT(04-3)-189
Project Agreement No. 10

*Printed in U.S.A. Available from the
Clearing House for Federal Scientific and Technical Information
National Bureau of Standards, U.S. Department of Commerce
Springfield, Virginia
Price: \$3.00 per copy*

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1. INTRODUCTION

The Fast Ceramic Reactor Development Program is an integrated analytical and experimental program directed toward the development of fast reactors employing ceramic fuels, with particular attention to mixed plutonium-uranium oxide. Its major objectives follow:

- Development of a reliable, high performance fast reactor having nuclear characteristics which provide stable and safe operation
- Demonstration of low fuel cycle cost capability for such a reactor, primarily through achieving high

burnup of ceramic fuels operating at high specific power.

Progress during the period of August 1 through October 1969 on the currently active tasks of this program is described in subsequent sections.

This is the thirty-second in a series of quarterly progress reports written in partial fulfillment of Contract AT(04-3)-189, Project Agreement No. 10, between the United States Atomic Energy Commission and the General Electric Company. Prior progress reports to the Commission are listed in the Appendix.

2. SUMMARY

2.1 TASK B—DEFECTED FUEL BEHAVIOR

Examination of the fuel pins of forced convection capsule B9A was completed. Visual examination of the fuel cross sections indicated that sufficient fuel swelling occurred during irradiation to crack the cladding of both fuel pins two additional inches on each side of the originally fabricated notch in the cladding. An average diametral increase of 23% was observed in the defect area of the top pin [fuel oxygen-to-metal ratio (O/M) 1.99], and an average diametral increase of 7%, in the defect area of the bottom pin (fuel O/M 1.95).

Three additional SODA POP tests were completed. No damage was sustained by the mocked-up reactor pressure vessel and core components during the tests. However, forces were detected that caused control rod movement in the SODA POP configuration. These forces appear to be unique to SODA POP. Based upon these data, sodium inventories of 1.5 kg appear acceptable.

Design of forced convection capsule B9C was initiated. A system to permit detection of fuel pin failure is being incorporated in the capsule design.

Development of an "on demand" cladding defecting mechanism for the forced convection capsule continued. A "meltable plug" type defecting mechanism protected from the sodium by a thin nickel plate is presently being tested.

The current status of the in-pile program of task B is summarized in table 2-1.

2.2 TASK C-1—SAFETY EXPERIMENTATION (TRANSIENT OVERPOWER)

The first two EBR-II specimens were removed from their capsules. They appear in excellent condition. TREAT (Transient Reactor Test Facility) capsule fabrication is in progress.

Destructive examination was completed on the two molten fuel movement experiments, which were preirradiated to $\approx 20,000$ MWd/Te and then pulsed in TREAT. Observations tend to confirm that cladding failure in specimen C5A resulted from a combination of high internal pressure and weakened cladding (caused by high transient temperatures). A final report is in draft.

Inner capsule assembly was completed for the first multipin (side-by-side) TREAT assembly to permit direct comparison of sodium and gas bonded fuel specimens under identical conditions.

The current status of the task C-1 irradiation test program is given in table 2-2.

2.3 TASK C-2—SAFETY EXPERIMENTATION (LOSS OF FLOW)

Preliminary planning for this new task was completed and contract work scopes and cost estimates were prepared.

The principal work in progress on this task during the period was the fabrication and assembly of an out-of-pile loss-of-flow test, Mockup 1A. Instrumentation on this test will include flow, pressure, and temperature sensors, as well as accelerometers to detect acoustic signals during sodium voiding.

Mockup 1A is scheduled for its initial operation in early November.

2.4 TASK E—FUEL MATERIAL PROPERTIES AND PERFORMANCE EVALUATION

Initial measurements of compressive creep on mixed oxides show that $(\text{Pu}_{0.2} \text{U}_{0.8}) \text{O}_{1.98}$ has an activation energy of 110 kcal/mole at a stress of 8000 psi and temperatures of 1450° and 1550°C. This is a comparable value to $\text{UO}_{2.00}$. Creep rates of the mixed oxide, however, are higher than for $\text{UO}_{2.00}$ under the same conditions.

Preliminary determinations of the fraction of fuel melted for mixed oxides having O/M ratios of ≈ 1.94 , 1.97 and 2.00 indicate a trend toward lower integrated thermal conductivity values for material of lower O/M. This trend will be confirmed by additional tests.

Elemental standards have been prepared for use in conjunction with electron probe microanalysis. Examination of an irradiated mixed oxide powder fuel pin ($\approx 28,000$ MWd/Te), SOV-6, from Argonne National Laboratory (ANL), confirmed the presence and magnitude of a plutonium concentration gradient within the restructured region. Cesium was detected at the outer periphery of the vibro-packed fuel and in cladding grain boundaries. The type 304 stainless steel cladding showed a higher concentration of silicon at grain boundaries than within grains.

The current status of the task E irradiation test program is given in table 2-3.

2.5 TASK F—FAST FLUX TESTS AND SUPPORT

The post-irradiation examination of four fuel pins from group 6 revealed that one pin had failed. These pins contained mixed oxide fuel of 97% to 98% theoretical density and were irradiated to a calculated burnup of 61,500 MWd/Te at linear powers of ≈ 10 to 11 kW/ft. The failed pin (FOM) was one of two Incoloy 800 clad pins in this group.

Seven group 8A capsules were removed from EBR-II after reaching a calculated burnup goal of 54,800 MWd/Te. Neutrographs revealed no indications of failure.

An interim examination was completed on the sixteen F3A unencapsulated fuel pins at a calculated burnup of $\approx 36,000$ MWd/Te. Based on this examination the reinsertion of the fuel pins for continued irradiation

TABLE 2-1
TASK B—SUMMARY OF CURRENT MIXED OXIDE
CAPSULE IRRADIATIONS

Design Parameters													
Capsule Designation	Purpose and Major Parameters	Fuel							Operating Conditions				
		Diameter (inch)	Length (inch)	Density (Smear) (% TD)	Composition			Cladding Material	Irradiation Facility	Cladding Surface Temp (°F)	Peak Rating (kW/ft)	Burnup (MWd/Te)	Status
					Fuel-Cladding Gap (in.-diam)	PuO ₂ (%)	O/M						
B3D	Vented Fuel. Power profile simulates fuel in central core position. Natural convection cooling design.	0.217	20	85	0.006	25	1.97	type 316 stainless steel	V-Raft GETR	1100	18	700	Stored
B3E	Vented Fuel. Power profile simulates fuel in peripheral core position. Natural convection cooling design.	0.213	23	83	0.008	25	1.98	type 316 stainless steel	V-Raft GETR	1100	18	8,700	Stored
B3F	Vented Fuel. Power profile simulates fuel in peripheral core position. Natural convection cooling design.	0.212	23	84	0.008	25	1.98	type 316 stainless steel	V-Raft GETR	1100	15	23,500	Examination completed
B4D	Vented Fuel. Power profile simulates fuel in central core position. Natural convection cooling design.	0.217	20	84	0.007	25	1.98	type 316 stainless steel	V-Raft GETR	1100	15	47,500	Examination completed
B8B (2 pins)	Fuel-sodium compatibility. sodium-bonded fuel central temperature measured versus exposure time.	0.216	2	80	0.010	25	2.00	type 347 stainless steel	V-Raft GETR	800	14	14,500	Examination completed
		0.216	2	83	0.010	25	1.93						
B8C (2 pins)	Fuel-sodium compatibility versus burnup. Fission gas release versus burnup. Fuel-sodium-clad compatibility.	0.144	2.2	86	0.006	25	1.97	type 316 stainless steel	V-Raft GETR	1100	21	113,500 57,000	Examination Initiated
		0.204	2.25	84	0.017 ^{ac}	25	1.97						
B8D (3 pins)	Fuel-sodium compatibility versus density and stoichiometry and Pu content of fuel	0.214	1.75	80-88	0.0065	40	1.97	type 316 stainless steel	V-Raft GETR	950	17.5	41,000 43,500 41,400	Examination Initiated
		0.214	1.75	80-88	0.0065	25	1.97						
		0.214	1.75	80-88	0.0065	25	1.94						
B9A (2 pins axially aligned)	Defected fuel pin. Fuel swelling versus stoichiometry in flowing sodium.	0.216	5	89	0.005	25	1.99	type 316 stainless steel	Special V-Raft GETR	1019	25.6	9,300	Examination initiated
		0.216	5	89	0.005	25	1.95			971	25.6	9,300	

* Large gap required to accommodate sampling device

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Table 2-2
TASK C—CURRENT EXPERIMENTAL TEST SERIES

Design Parameters																		
Capsule	Test Objective	Diameter (inch)	Fuel Length (inch)	Fuel Density (% TD)		Fuel Composition				Cladding Material	Fabri- cation	Pre-Irra- diated		TREAT- Transient Irradiation		Nondestructive Exam†		Destruc- tive Exam
				Pellet	Smeared	% UO ₂	% PuO ₂	% U-235	O/M			Yes	No	Calib- ration	Test	Before Trans.	After Trans.	
C4A	Axial restraint at Fuel melting (≈5000°F).	0.2159	14.23	93.8	90.4	80	20	0	1.983	316SS	C		X	C	C	C	C	C
C4B	Axial restraint similar to C4A; test to identify percent melting necessary to cause cladding failure.	0.2155	14.21	94.5	91.2	80	20	0	1.983	316SS	C		X	C	C	C	C	C
C4C	Axial restraint similar to C4A; test to observe effect of fuel density on incipient failure point identified in C4A.	0.2164	14.19	83.1	80.4	80	20	0	1.983	316SS	C		X	C	C	C	C	C
C4D	Axial restraint designed at ≈3000°F - simulating average FCR fuel temperatures; test to determine if cladding deformation occurs in transient to fuel melting (≈5000°F).	0.2154	14.22	83.7	80.1	80	20	0	1.983	316SS	C		X	C	C	C	C	C
C4E	Axial restraint designed to simulate the restraint of C4A after steady-state irradiation; specific test goals to be determined after nondestructive examination of pin following removal from EBR-II.	0.2159	13.46	83.2	80.5	72	28	92.7	1.974	316SS	C	I*		P	P	P	P	P
C4F	Identical to C4E (transient magnitude will be varied).	0.2160	13.51	84	80.5	72	28	92.8	1.975	316SS	C	I*		P	P	P	P	P
C4G	Axial restraint similar to C4A. This test (and C4H) will be compared to C4A and B to determine the effect of burnup on failure threshold.	0.2154	13.50	94.4	90.5	72	28	89.3	1.973	316SS	C	C*		P	P	C	P	P
C4H	Identical to C4G (transient magnitude will be varied).	0.2152	13.45	94.4	90.4	72	28	89.3	1.976	316SS	C	C*		P	P	C	P	P
C4I	Typical powder fuel specimen. Test to determine failure threshold relative to pellet pins.	0.2204	13.48	powder	83.5	80	20	0	1.973	316SS	C		X	C	C	C	C	C
C4J	Identical to C4I (transient magnitude will be varied).	0.2205	13.45	powder	83.5	80	20	0	1.973	316SS	C		X	C	C	C	C	C
C4K	This test (and C4L) will be compared to C4I and J to determine effect of burnup on failure threshold of powder fuel.	0.2205	13.50	powder	84.1	72	28	93.0	1.972	316SS	C	I*		P	P	P	P	P
C4L	Identical to C4K (transient magnitude will be varied).	0.2206	13.47	powder	84.2	72	28	93.0	1.972	316SS	C	I*		P	P	P	P	P
C5A	Test to study mobility of transient melted fuel; investigate potential for fuel relocation under prototypical conditions of length, restraint, and fission gas.	0.214	24	95	91.5	100	0	12	2.000	316SS	C	C**		C	C	C	C	C
C5B	Identical to C5A; except pressure relief provided by hole through blanket.	0.214	24	95	91	100	0	12	2.000	316SS	C	C**		C	C	C	C	C
C6A,-1	Test to evaluate transient performance of defected or sodium bonded fuel; determine relative performance limits and failure mechanisms of pellet and powder fuel under transient irradiation conditions. Sodium loaded powder fuel is to maximize fuel-sodium heat transfer.	0.212	24	86	80	100	0	10	2.000	316SS	NC		X	P	P	P	P	P
-2		0.2203	24	powder	80	100	0	10	2.000	316SS								
-3		0.212	24	86	80	100	0	10	2.000	316SS								
C6B,-1	Identical to C6A (transient magnitude will be varied).	0.212	24	86	80	100	0	10	2.000	316SS	I		X	P	P	P	P	P
-2		0.2203	24	powder	80	100	0	10	2.000	316SS								
-3		0.212	24	86	80	100	0	10	2.000	316SS								

P — Planned
I — Initiated
NC — Nearly Completed
C — Completed

* EBR-II fast flux, burnup goal ≈50,000 MWd/Te
** GETR Thermal flux, burnup goal ≈20,000 MWd/Te
† Consists of x-rays, neutron radiographs, deencapsulation, cladding, diametral measurements, gamma transmission scan

Table 2-3
TASK E—SUMMARY OF CURRENT MIXED OXIDE CAPSULE IRRADIATIONS

Capsule	Description			Design Parameters							Operating Conditions					Status	
				Diameter (inch)		Length (inch)	Fuel		Composition			Cladding Material	Irradiation Facility	Cladding Surface Temp (°F)	Peak Power (kW/ft)		Burnup (MWd/Te)
							Pellet	Smeared	PuO ₂ (%)	O/M							
E1AA	Continuous conductivity.	in-pile	thermal	0.219	6.0	92.0	91.0	20	1.97	type 316 stainless steel.	GETR	1300	15	0	report in process		
E1E	Relative smeared conductivity at			0.218	1.9	94.7	94.5	20	2.00	type 347	MTR	875	20	85,000	report in process		
E1F	high burnup irradiation to high burnup and rabbit test E1F.			0.218	1.9	94.6	94.8	20	2.00	stainless steel	MTR	654/875	20/28	77,000			
E1J-1	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.94	type 316 stainless steel	GETR	1100	24	2,000	irradiated		
E1J-2	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.94	type 316 stainless steel	GETR	1100	24		awaiting irradiation		
E1J-3	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.94	type 316 stainless steel	GETR	1100	24		awaiting irradiation		
E1J-4	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.97	type 316 stainless steel	GETR	1100	24		awaiting irradiation		
E1J-5	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.97	type 316 stainless steel	GETR	1100	24	2,000	irradiated		
E1J-6	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.97	type 316 stainless steel	GETR	1100	24		awaiting irradiation		
E1J-7	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	2.00	type 316 stainless steel	GETR	1100	24	2,000	irradiated		
E1J-8	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	2.00	type 316 stainless steel	GETR	1100	24		to be fabricated		
E1J-9	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	2.00	type 316 stainless steel	GETR	1100	24		awaiting irradiation		
E2C-1	High burnup. Reference fuel			0.218	5	84.5	82.2	25	2.00	type 347	GETR	1060	20	121,000	report in process		
-2	irradiated at near reference thermal conditions.			0.218	5	94.3	91.4	25	2.00	stainless steel	GETR	1060	20	113,000			
E2E	Fuel swelling. Four separate pins			0.100	3	98.5	95.4	25	2.00	type 304	MTR	785	11.6	199,000	report in process		
F	at three power levels.			0.100	3	96.9	95.1	25	2.00	stainless steel	MTR	655	9.4	172,000			
G				0.100	3	97.0	95.2	25	2.00	steel	MTR	560	7.9	255,000			
H				0.100	3	96.6	94.8	25	2.00		MTR	785	11.6	330,000			
E2J-1	High burnup. Annular versus solid pellet.			0.218	5	90.4	89.5	25	1.98	type 316 stainless steel	GETR	1100	21	124,000	report in process		
-2				0.218	5	96.0	88.5	25	1.98	stainless steel		1100	21	125,000			
E5A-1	Powder and pellet fuel comparison			0.218	4.9	86.8	84.3	25	2.00	type 347	GETR	1100	27	2,000	report in review		
-2	at low burnup.			0.218	4.9	84.1	84.1	25	2.00	stainless steel	GETR	1100	27	2,000			
E5B-1	Power and pellet fuel comparison			0.218	4.9	85.8	84.1	25	2.00	type 347	GETR	1400	20	138,000	report in review		
-2	at low burnup.			0.218	4.9	84.1	84.1	25	2.00	stainless steel	GETR	1400	20	138,000			

to 50,000 MWd/Te was approved. These pins have been reinserted in subassembly XO40A and the subassembly has been loaded into the EBR-II core.

Evaluation of burnup analysis techniques for Nd-148 indicates that calculated burnups based on EBR-II data may be 13% to 20% low.

Post-irradiation examination of capsules F12C, F12D, and F12E (E5C, D, E) was initiated. These pins were irradiated in GETR to a burnup of $\approx 130,000$ MWd/Te.

Fuel pins F12P-Q (E2P and Q) were de-encapsulated, examined, fuel pin diameter measurements obtained, reencapsulated, and reinserted in the GE Test Reactor (GETR). At 20,000 MWd/Te, maximum diameter increase was significantly greater for the 10-mil-thick type 316 stainless steel cladding than for the 15-mil cladding.

The current status of the in-pile test program of task F is summarized in table 2-4 and 2-5.

2.6 TASK G-6—CORE DESIGN ANALYSIS

Analytical models for predicting fuel pin thermal performance are being correlated with the data from 21 task F pins, irradiated in EBR-II to burnups of 74,000 MWd/Te.

An assessment of the information required from steady-state and defected-fuel-irradiation tests in order to support the design and operation of LMFBR fuel assemblies was initiated.

Detailed planning is in progress on several subtasks which are beginning.

2.7 TASK G-10—KINETICS ANALYSES

The sensitivity of energy release in hypothetical LMFBR (Liquid Metals Fast Breeder Reactor) flow blockage accidents to coolant voiding dynamics is being studied with voiding rates and fraction of channels blocked as parameters.

Some preliminary results from the VOIDUM code are presented. The enclosed work is a summary of a paper "The Effects of Reactor System Design on Sodium Voiding Accidents," which will be presented at the 1969 winter meeting of the American Nuclear Society.

A study is in progress, using FORE II to evaluate the effects of various parameters on the calculated behavior of a reactor.

The FREADM-1 code is in the final checkout phase.

2.8 TASK L—CLADDING DEVELOPMENT

Flux wire analyses of the L-10 capsules confirm that the axial flux profile was asymmetric for subassembly (S/A) XO14. The average total fluence for the subassembly based on the flux wire analyses was 2.4×10^{22} n/cm², or 14% lower than that predicted from physics calculations. A two-dimensional heat transfer analysis, which considered axial heat transfer and geometric variation within the L-10

capsules, showed that the peak temperature of core test sections could have been as high as 1750°F and that the temperature could vary as much as 400°F axially along the specimen.

Volume swelling of type 304 and type 316 stainless steel can be reasonably estimated with the bounds of existing data by the equation:

$$\Delta V/V = 3.14 \times 10^{-4} F^{1.81} (T-360)^{0.683} (775-T)^{0.577}$$

where F is the fluence/10²², E > 0.1 MeV, and T is the temperature of irradiation.

Irradiated fast-flux fuel cladding has been cleaned of residual alpha contamination demonstrating that irradiated fuel cladding can be made available for testing in beta-gamma cells.

Theoretical differential cross sections for natural iron and nickel were normalized to measured concentrations of helium in commercially pure samples which had been irradiated in EBR-II. The resulting cross sections are lower than other recent measurements, but provide good agreement between calculated helium concentrations and independent measurements of helium in stainless steel irradiated in the EBR-II core.

An assessment of the effect of surface defects on the burst properties of seamless type 316H stainless steel showed that longitudinally oriented defects strongly influence the mode of failure, but the degradation in hoop strength is small.

A summary of the current task L in-pile experiments are summarized in table 2-6.

2.9 TASK N—CORE PHYSICS ANALYSIS

Evaluation of the Pu-239 cross sections in the unresolved resonance range for use in ENDF/B, version 2, has been completed. Corresponding statistical resonance parameters have been generated and excellent cross section representation is obtained without the use of a smooth background, providing energy dependent reduced neutron widths for s-wave resonances are used.

Checkout of the FUMBLE code has progressed to the successful calculation of fuel parameters over six refueling intervals during startup in a representative LMFBR.

Two modifications for the second version of the ENDRUN code have been completed and a third specified. These changes improve the input formats and resolved resonance averaging procedure and allow for some expected changes in ENDF/B, version 2. The flux solutions for the TDOWN-2 code have been specified.

Four topical reports have been completed and are in reproduction. These reports describe the computer codes DOT2DB and BICYCL, a perturbation module for the BISYN code, and studies of the effects of computational approximations on physics parameters. An all-FORTRAN version of the BISYN code was sent to the Argonne Code Center.

Table 2-4
SUMMARY OF TASK F FAST FLUX TESTING PROGRAM IN EBR-II

Group	Purpose and Major Parameters	S/A	Position	No. Pins	Exposure (MWd/Te X 10 ³)		Power kW/ft	Date In	Date Out	Irradiation	
					Target	Present				Finish	Status
F-0	Low power swelling rate of high density fuel at various burnup levels.	XG02	7A1	1	40	40	5-6	07/16/65	08/15/68	08/68	post exam in progress
		XG03	7D1	2	80	61.5	5-6	07/16/65	08/01/69	07/70	in reactor
		XG04	7B1	2	100	64.4	5-6	07/16/65		11/70	in reactor
F-1	Short time performance and proof test.	XG01	4F2	6	2	1.8	14-16	05/06/65	05/23/65	05/65	reported in GEAP-5570
F-2	Medium power scoping irradiations. Major parameters include smear density, stoichiometry, cladding-coolant interactions and burnup.	XG06	4E2	12	50	51.1	11-16	09/03/65	02/20/67	02/67	post exam completed
		XG05	4C2	5	75	74	11-16	09/03/65	03/06/68	03/68	post exam completed
		XO50	4C2	4	100	108.6	11-16	02/23/69		03/70	in reactor
F-3A	Bare pin irradiations to evaluate fuel cladding and cladding-cooling interactions.	XO40A	5B2	16	100	37.4	15-18	08/15/68 10/04/69	06/30/69	07/70	in reactor
F-3B	Density and stoichiometry effects for encapsulated fuel pins.	XO27	4B3	18	80 120	72.1	13-16	11/21/67		02/70 11/70	in reactor
F-4	Low density fuel behavior at high heat ratings.	XO11	2F1	2	50	35	15-18	05/09/66	06/28/67	06/67	post exam in progress
		XO39	3C2	7	80	50.5	15-18	05/08/68	12/24/68	05/70	discharged from EBR-II core 12/68 for interim exam.
F-5	Fuel performance at peak FCR cladding temperatures.	XO64	4C3	19	100	21.7	15-16	05/28/69		05/71	in reactor
F-6	Fuel-cladding interactions under accelerated swelling.	XO10	7F3	4	50	61.5	7-9	03/24/66	04/24/69	04/69	post exam in progress
F-7	Pre-irradiated axially restrained fuel prior to transient test. Main variable is fuel density.	XO15	4A2	2	50	59.8	12-15	12/23/67	04/03/69	04/69	post exam in progress
		XO50	4C2	4	50	33.4	12-15	02/23/69		04/70	in reactor
F-8A	Effect of density on fuel swelling rate.	XO19	6D2	7	50	54.8	10-11	01/13/67	08/02/69	08/69	removed from reactor for exam
		XO20	6B5	9	100	62.5	10-11	01/13/67		11/70	in reactor
F-8B	Effect of void deployment on fuel cladding interaction.	XO36	7E1	19	50	27.9	6-9	05/08/68		10/70	in reactor
F-9A	High power, high burnup, unencapsulated fuel pin irradiations.	XO43	4D2	37	100	34.0	11-16	02/20/69		01/71	in reactor
F-9B	Medium power, high burnup, unencapsulated fuel pin irradiations.	XO62	6F3	37	100	17.4	10-13	05/23/69		04/71	in reactor
F-9C	High power, high burnup, unencapsulated fuel pin irradiations.	XO56	5C2	37	100	28.1	13-16	04/03/69		03/71	in reactor
F-9D	Medium power, high burnup, unencapsulated fuel pin irradiations.	XO58	6F1	37	100	17.7	8-10	04/24/69		09/71	in reactor
F10B	Cladding performance at peak FCR cladding temperatures.	—	—	19	100	—	14-17	—			capsule fabrication in progress

Table 2-5
TASK F—SUMMARY OF CURRENT MIXED OXIDE CAPSULE IRRADIATIONS

		(Thermal Flux) Design Parameters							Operating Conditions				
		Fuel						Cladding Material	Irradiation Facility	Cladding Surface Temp (°F)	Peak Power (kW/ft)	Burnup (MWd/Te)	
		Diameter (inch)	Length (inch)	Density (% TD)		Composition							
Capsule	Description			Pellet	Smeared	PuO ₂ (%)	O/M						
F12C (E5C)	Powder, pellet and annular fuel comparison at high burnup.	0.215	16.0	87.5	82.9	25	1.96	type 316 stainless steel	GETR	1160	19.5	130,000	post irradiation exam initiated
F12D (E5D)	Powder, pellet, and annular fuel comparison at high burnup.	powder	16.0	81.8	81.8	25	1.97	type 316 stainless steel	GETR	1160	19.5	130,000	
F12E (E5E)	Powder, pellet, and annular fuel comparison at high burnup.	0.215 (annular)	16.0	90.5	83.3	25	1.96	type-316 stainless steel	GETR	1160	19.5	130,000	
F12A (E5F)	Linear fuel move- ment versus burnup.	0.214	10.0	92.0	87.1	25	1.98	type 316 stainless steel	GETR	850	17	44,000	
F12P (E2P)	Effect of radial restraint on fuel swelling. (15 mil tubing)	0.219	2.0	92.0	91.0	25	1.98	type 316 stainless steel	GETR	1160	15	20,000	in irradiation
F12Q (E2Q)	Effect of radial restraint on fuel swelling. (10 mil tubing)	0.229	2.0	92.0	91.0	25	1.98	type 316 stainless steel	GETR	1160	15	20,000	

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Table 2-6
SUMMARY OF TASK L MATERIALS IRRADIATED IN EBR-II

Series	Row-S/A	Capsules	Capsule Materials	Number of Specimens		Total Fluence October 1969			Projected Completion Date†
				Biaxial	Tensile	Target 10 ²³ nvt	10 ²³ nvt*	10 ²³ nvt**	
Irradiation Completed									
L-2'	4 - XG06	5	one each of types 304, 316, 321, and 347 stainless steels, and Incoloy 800.	15	80	0.38	0.34	0.25	Feb. 1967
L-4	4 - XO09	2	type 316 stainless steel.	10	20	0.20	0.19	0.15	Dec. 1966
L-4'	2 - XO14	5	one each of types 304, 321, and 347 stainless steels, and two of Incoloy 800.	25	50	0.20	0.17	0.10	April 1967
L-10	4 - XO16	10	one each of type 321 and 347 stainless steels; two of type 304 stainless steel; three each of type 316 stainless steel, and Incoloy 800.	70	60‡	0.20	0.29	0.22	Oct. 1968
L-14	4 - XO25	19	five each of type 316 stainless steel and Incoloy 800; four of type 304 stainless steel; three of type 321; two of type 347 stainless steel.	133	114‡	0.20	0.31	0.23	Dec. 1968
L-21	4 - XG05 3 - XO39	1	type 321	3	16	0.76	0.61	0.45	Dec. 1968
MT-3‡	4 - XA08	1	16 Incoloy 800, 16 Inconel 625 and 16 Hastelloy X tensile specimens.	None	48	1.00	0.71	0.53	June 1969
Irradiation in Progress									
L-2	4 - XG05 4 - XO50	4	one each of type 304, 316, and 347 stainless steels, and Incoloy 800.	12	64	3.2	0.63	0.47	1972
L-6	2 - XO18 4 - XO18A 4 - XO18B	9	one each of types 321 and 347 stainless steels; two each of type 304 stainless steel and Incoloy 800; three of type 316 stainless steel.	63	126‡	2.0	0.52	0.32	1971
L-15	3 - XO39	4	all five alloys.	None	0.288	0.30	0.10	0.10	1970
MT-4‡	4 - XA08	1	16 Incoloy 800, 16 Inconel 625 and 16 Hastelloy X tensile samples.	None	48	3.2	0.72	0.53	1972

* Fluence based on EBR-II core physics conversion factors.

** Fluence based on flux wire analysis conversion factors.

† Assuming EBR-II continues to operate at 45 MW with 50% load factor.

‡ Includes specimens of experimental alloy of base composition Fe, (9-14)Ni, (9-14)Cr, (0.004-0.05)C.

‡ Capsules from previous AEC program under Contract AT(04-3)-189, PA-29.

2.10 TASK P—FUEL ELEMENT DESIGN ANALYSIS

The Fuel Element Design Analysis effort within PA-10 is now being reported separately as a task, having previously been reported under tasks G and L.

Detailed analysis of the profilometry of 22 pins irradiated in EBR II has been completed. It was observed that irradiation induced cladding swelling probably accounts for all of the diametral increase in those pins with low density fuel or which ran at a low linear power.

A series of 1-mil deep circumferential benchmarks have been scribed on the cladding which will comprise six

of the fuel pins in the group 10A fuel assembly. After irradiation, the spacing of the marks will be microsurveyed to yield a direct measure of local axial strain in this cladding.

The calculation of structural stress and strain in members where both irradiation induced swelling and structural constraint are simultaneously involved has been accomplished.

3. TASK B—DEFECTED FUEL BEHAVIOR

3.1 GENERAL

The purpose of task B is to determine analytically and experimentally the manner and consequences of propagation of fuel defects, the behavior and performance of the fuel-cladding interface in contact with sodium, the distribution and rate of release of fission products from high performance mixed oxide fuel, and the identity of the fission product species which can escape to the primary coolant and cover gas.

The technical feasibility of fast reactors depends on quantitative knowledge of fission product release and on designs to limit the consequences of such release. It appears attainable to limit long-term activity release from vented fuel to essentially Kr-85 so that primary system maintenance conditions of vented designs are equivalent to (or possibly better than) nonvented designs.

The technologies of defected, vented, and sodium-bonded fuels are closely related. Although directed primarily at studying the behavior of defected fuel, task B also has included development of vented and sodium-bonded fuels.

3.2 DEFECTED FUEL TESTING

3.2.1 Forced Convection Capsule (B9A)

Disassembly of the capsule and metallographic examination of the fuel pins was completed. The plenum region of the capsule was sampled prior to disassembly. Table 3-1 lists the quantities of gases identified in two separate gas chromatographic analyses. Isotopic analysis of the xenon and krypton gases is presently in progress.

Table 3-1
B9A FISSION GAS ANALYSIS

Gas	Sample No. 1 (Vol %)	Sample No. 2 (Vol %)
O ₂	0.01	0.02
N ₂	26.3	26.9
H ₂	3.34	2.89
CO	Not Detected	Not Detected
CH ₄	Not Detected	Not Detected
CO ₂	Not Detected	0.06
Xe	2.29	2.38
Kr	0.02	0.02
Ar	0.08	0.07
He	68.7	67.5

Visual examination of the capsule fuel pins indicated extensive swelling had occurred in both fuel pins. The fully cold-worked cladding of both pins was found to be cracked an additional two inches beyond each side of the original

notch. The crack propagated from the notch in the defect area of each pin. The appearance of the capsule fuel pin after removal from the capsule is shown in figure 3-1. Neutron radiography of the top pin prior to capsule disassembly indicated that the fuel protruded from the defect area. During irradiation this fuel extended directly into the peak velocity region of the sodium coolant channel where sodium was flowing at approximately 14 ft/sec. It was inferred from the data, along with the capsule gamma scans, that no appreciable erosion of the fuel occurred because of flowing sodium.

Post-irradiation diametral measurements of the cladding of the top and bottom fuel pins were completed and are shown in figures 3-2 and 3-3. The top pin (fuel O/M = 1.99) showed substantially greater swelling than the bottom pin (fuel O/M = 1.95). Both pins showed the largest diametral increases in the purposely defected area. The top pin was elliptic in cross section in the defect area (major axis approximately 45° to the cladding defect). The maximum cladding outside diameter (o.d.) was 0.320 inch (initial o.d., 0.2495 inch). The average o.d. of the pin in the defect area was 0.308 inch which corresponds to a diameter increase of 23%. The cross section of the bottom pin was not elliptic and was round to within ±0.001 inch. The average o.d. of the bottom pin in the defect area was 0.269 inch (initial o.d., 0.250 inch). This corresponds to a diametral increase of 7%.

Sectioning and metallographic examination of the fuel pins were completed. Specimens were examined from the initially defected and initially nondefected areas of each pin. The top pin showed no center void in the defect area, while the bottom pin showed a small round center void 0.020 inch in diameter at the center of the pin. In the remaining portions of the fuel pins, the center void is generally offset from the center of the pin toward the side opposite the cladding defect. These data reflect the nonuniform heat transfer around the circumference of the pins caused by the presence of sodium after breach of the cladding. Figures 3-4 and 3-5 show beta-gamma autoradiographs and alpha autoradiographs of the fuel in the initially defected and initially nondefected areas of the fuel pins. The variation in the center void in the fuel pins described above can be observed in these views. A microprobe specimen from the top pin is presently being prepared for examination under task E.

Figure 3-6 shows the appearance of the fuel in the fabricated defect region of the top pin. The fuel in this region was difficult to retain during preparation as evidenced by the large areas of fuel fallout. Substantial amounts of reaction product can be observed at the periphery of the fuel although most was lost in specimen preparation. Although the fuel shows no evidence of columnar grain growth or of a center void, fuel swelling was sufficient in this region to crack the cladding directly

TOP END

(O/M 1.99)

(O/M 1.95)

BOTTOM END

Figure 3-1. Appearance of B9A Fuel Pins

Figure 3-2. Profilometer Data for B9A Top Fuel Pin (O/M 1.99)

Figure 3-3. Profilometer Data for B9A Bottom Fuel Pin (O/M 1.95)

Figure 3-4. Alpha and Beta-Gamma Autoradiographs of B9A Top Fuel Pin (O/M 1.99)

$\beta-\gamma$

α

FABRICATED DEFECT REGION

$\beta-\gamma$

α

INITIALLY NON-DEFECTED REGION

Figure 3-5. Alpha and Beta-Gamma Autoradiographs of B9A Bottom Fuel Pin (O/M 1.95)

UNETCHED

Figure 3-6. Appearance of Fuel in Fabricated Defect Region of B9A Top Pin (O/M 1.99)

opposite the initially fabricated defect. The cladding was fully cold worked with an ultimate tensile strength of approximately 160,000 psi.

Figures 3-7 and 3-8 show the appearance of the fuel in the bottom pin in the initially defected and nondefected regions. Unlike the top pin, the defect region shows a small center void and some columnar grain growth as well as reaction products at the periphery of the fuel. Two regions in the fuel metallographic specimens showed a pronounced tendency to "pull out" compared to the remaining portions. The radii of pronounced pull out were 0.070 inch and 0.095 inch from the fuel centerline. The initially nondefected region showed a large center void and a well developed columnar grain structure. The fuel in the initially nondefected region of the top pin is very similar in appearance. These data suggest that the initially nondefected regions of the pins operated at significantly higher temperatures at some time during irradiation compared to the initially defected regions of the pins. If this assumption is correct, then sodium does not immediately permeate throughout a fuel pin once the cladding is breached, but is initially confined to the defect and adjacent regions.

As can be observed in figure 3-8, some loss of fuel did occur in the initially nondefected regions of the bottom pin. This loss is evidenced by the shallow groove at the

periphery of the fuel in line with the crack in the cladding. This groove does not appear to be formed by erosion of the fuel by flowing sodium, since in many sections the groove is bridged by fuel at the surface.

3.2.2 SODA POP Tests

All repairs to the SODA POP* test equipment were completed, and the three planned capsule rupture tests (Nos. 4, 5, and 6) were conducted. During disassembly of test 4 some evidence of core component movement was observed. To establish core component movement, scribes were placed on selected core components in tests 5 and 6. Table 3-2 summarizes the significant parameters measured during these tests.

The tests showed good reproducibility, and no significant damage was sustained by the simulated core components or the pressure vessel during these tests. The small movement of the fuel element and filler piece are acceptable, because they cause negative reactivity insertions. However, motion of the control rod was noted in test 5, and modifications were added to test 6 to permit measurement of the force-time relationships on the control rod during the sodium-water

* The SODA POP test configuration is described in the 27th quarterly report, and test results are indicated in later quarterly reports (see appendix).

Table 3-2
SUMMARY OF SIGNIFICANT PARAMETERS DURING SODA POP TESTS 4, 5, and 6

Test	4	5	6
NaK Inventory (gm)	1500	1500	1500
NaK Temperature (°F)	1200	1200	1200
Reaction Orifice (in.)	1.25	1.25	1.25
Capsule Pressure (psig)	45	45	45
Reactor Pressure (psig)	135	135	135
Max Pressure on Bottom Head of Pressure Vessel (psi)	620	630	650
Max Pressure Vessel Support Loading (lb)			
up	4960	6080	5300*
down	7870	12,220	10,000*
Max Capsule Support Loading (lb)			
up	292	300	240
down	284	240	350
Max Guide Tube Horizontal Deflection (in.)	0.3	0.1	0.3
Grid Plate Vertical Motion (in.)	Not Measured	3	0.05**
Fuel Element Vertical Motion (in.)	Not Measured	0.2	0.8
Filler Piece Vertical Motion (in.)	Not Measured	1.3	0.9
Control Rod Guide Tube Vertical Motion (in.)	Not Measured	0.1	***
Control Rod Vertical Motion (in.)	Not Measured	1.9	***

* Based on west vessel support load only.

** Held down with 10,000 lb force to simulate flowing coolant.

*** Fastened to bottom of vessel. Strain gages used to measure force/time relationships.

Figure 3-7. Appearance of Fuel in Fabricated Defect Region of B9A Bottom Pin (O/M 1.95)

UNETCHED

Figure 3-8. Representative Appearance of Fuel in Initially Non-Defected Region of B9A Bottom Pin (O/M 1.95)

reactions. Preliminary analysis of these measurements suggest that the upward forces exerted on the instrumented control rod are peculiar to the SODA POP hardware and would not arise in a water-cooled test reactor, such as GETR. As a result of these tests, it appears that sodium inventories of 1.5 kg are acceptable and that the net effects of the release of this inventory do not interfere with the control of the reactor or the containment of accident products.

3.2.3 Forced Convection Capsule (B9C)

Design of forced convection capsule B9C was initiated. The capsule design incorporates a system to permit detection of fuel failure by tagging the capsule fuel pins with a distinctive gaseous isotope. Upon failure, the tagging gas will migrate to the plenum region of the capsule. A sample of the plenum gases will then be taken by means of a two-line carrier gas system. The presence of the tagging gas in the sample will indicate that fuel pin failure has occurred.

3.2.4 Defect Development

Work continued on the development of an "on demand" cladding defecting mechanism that will permit the forced convection capsule fuel pins to be brought to an appropriate power level prior to initiating failure. A "meltable plug" type defecting mechanism is presently being considered for capsule B9C. The defect is fabricated

by soldering shut a hole in the cladding using a solder with a melting temperature above the sodium coolant temperature expected during nonfailed operation, but below the temperature of desired cladding failure. The solder retains cladding integrity during capsule assembly and sodium fill. After the desired prefailure operation has been attained, the sodium coolant temperature is raised to exceed the remelt temperature of the solder plug, and the cladding fails.

A series of silver solders are presently in test. Tests show the remelt temperature of the solder plug in a 1/32-inch-diameter hole in type 316 stainless cladding is approximately 100°F above the initial melting point.

Since silver solders are quite reactive with sodium, the solder plugs are protected by electroplating a protective coating of nickel or chromium over the surface of the plug. Tests of 84-hours duration in sodium at 1000°F indicate good protection of the solder plug by this method. Figure 3-9 shows the appearance of one of the test specimens. The solder plug was effectively protected by a 0.0005-inch-thick coating of nickel throughout the duration of the test.

Tests to define the rupture pressure of nickel or chromium protective plating as a function of thickness and specimen hole size are in progress. Six cladding test specimens, with nickel plate thicknesses of 0.0005 inch to 0.00075 inch and 1/32-inch-diameter holes filled with solder, have all uniformly ruptured when subjected to pressures of 14 to 30 psi at 1250°F. Pressure differential across the cladding at time of failure in the forced convection capsule (FCC) will be > 30 psi.

Figure 3-9. Defect Test Area After 84-Hour Exposure to 1000°F Static Sodium

4. TASK C-1 SAFETY EXPERIMENTATION (TRANSIENT OVERPOWER)

4.1 GENERAL

The purpose of task C-1 is to establish the behavior of fuel and cladding under transient casualty and accident situations. Both analytical techniques and an experimental program are used to determine the response of fuel to severe transients. These investigations include a study of the transient performance of fuel and cladding as a function of axial restraint, density, condition (defected, high burnup), and fuel form (powder and pelleted fuel). Additional tests are conducted to determine the potential for movement of molten fuel and to observe the effect of the prototypic range of fuel and blanket lengths on movement.

Early in 1970, a final report will be issued on transient irradiation of zero-burnup, axially-restrained fuel as a function of density, and fuel form (pellet and powder). Design and fabrication of TREAT capsules for transient irradiation (axially-restrained fuel series) of EBR-II irradiated fuel specimens will be initiated.

Experiments designed to study relocation of molten fuel during transients in preirradiated long pins will be completed. Fabrication of capsules for transient tests of defected or sodium-bonded fuel will be initiated.

4.2 HIGH BURNUP FUEL EXPERIMENTS

4.2.1 EBR-II Irradiation

Fuel specimens C4G and C4H (two of six specimens undergoing EBR-II irradiation prior to TREAT transient irradiation) were removed from their EBR-II capsules (F7C and F7D respectively) after completing $\approx 57,600$ MWd/Te burnup in EBR-II. The remaining four specimens (C4E, F, K, and L) are still at EBR-II. The EBR-II fuel subassembly containing these latter four fuel specimens (in capsules F7A, B, E, and F respectively) has been removed from the EBR-II core (along with all other experimental sub-assemblies) at the end of cycle 37 (August 31, 1969). By the end of that cycle, the four specimens had accumulated $\approx 30,700$ MWd/Te burnup. These specimens are scheduled for reinsertion in EBR-II at the end of the 62.5 MW test at EBR-II.

4.2.2 TREAT Capsule Design and Fabrication

Design was finalized and fabrication has been initiated for fuel specimen C4X, a special test specimen containing highly enriched UO_2 fuel. This pin will be used to checkout the procedures involved in the remote capsule assembly followed by remote sodium fill operations required for reencapsulation into a TREAT test capsule. These checkout operations will be performed at a mockup remote handling station. Once encapsulation of C4X has been completed, the specimen and TREAT capsule will be irradiated at TREAT as a calibration and proof test of the capsule design.

Machining of parts is nearly complete for the inner capsule assemblies to be used for TREAT irradiation of fuel specimens C4X, C4G, and C4H. The final design of these TREAT capsules is very similar to the conceptual design reported in the 29th quarterly report (GEAP-5753).

4.3 AXIAL FUEL RELOCATION EXPERIMENTS

This test series involved two 24-inch-length, 0.250-inch o.d. oxide fuel specimens (designated C5A and C5B) which were irradiated under steady-state conditions in the General Electric Test Reactor (GETR) facility to burnup goal exposures of $\approx 20,000$ MWd/Te. The capsules were nondestructively examined to ascertain capsule integrity and then remotely reencapsulated for transient irradiation in the TREAT facility. The two specimens were identical except the 15-inch-length upper blanket of C5A was composed of solid pellets while the C5B blanket had annular (0.070-inch i.d.) pellets to provide a potential flow path for molten fuel. Specimen C5A failed, but C5B did not. The annular blanket configuration apparently was instrumental in preventing the failure of C5B. Destructive examination results and analysis for C5B were presented in the 28th, 29th, and 31st quarterly progress reports (GEAP's-5700,-5753,-10028-31).

4.3.1 Examination of Specimen C5A

The destructive examination of specimen C5A was completed. Preliminary results were presented in the 31st quarterly progress report. Figure 4-1 is the neutron radiograph (first shown in 28th quarterly report) of the fuel column and is included as a reference for the results shown in figures 4-2 through 4-5. Figures 4-2 through 4-4 show transverse sections from the upper failure region (see figure 4.1 for locations) that indicate the extent of melting of the cladding and the post irradiation condition of the fuel. The fuel appears intact (solid ring with molten core) in figure 4-2. In figures 4-3 and 4-4, the fuel is more porous and is partially dispersed in the coolant annulus. The fuel in this annulus was composed of very fine grains and, as seen in figure 4-4, was easily removed during the fuel etching process. The molten fuel centers in figures 4-2 and 4-3, by comparison, have large grains similar to that observed in specimen C5B. The fuel in the coolant annulus above the fuel-blanket interface was also composed of very fine particles and crumbled easily. Post-irradiation examination of the exterior surface of the cladding indicated no adverse effects as a result of contact or proximity of the fuel.

A transverse section from the lower end of the pin is shown in figure 4-5. The "necked-down" intergranular fracture at the unmelted end of the cladding suggests that the failure occurred as a result of excessive internal pressure and that one end of the cladding breach melted later as a result of hot fuel in the annulus.

Figure 4-1. Neutron Radiograph of Failed Specimen, C5A

Figure 4-2. Specimen C5A Transverse Section from Lower End of Upper Failure Region

ETCHED

NEG. No. A305-03

10X

ALUMINUM HEAT
SINK

GROUND

NEG No. A305-01

10X

Figure 4-3. Specimen C5A Transverse Section from Middle of Upper Failure Region

GROUND

NEG. No. A308-01 10x

ETCHED

NEG. No. A308-02 10x

Figure 4-4. Specimen C5A Transverse Section from Top End of Upper Failure Region

Figure 4-5. Specimen C5A Transverse Section from Lower Failure Region

The relationship or timing of the two failure areas could not be determined because of thermocouple malfunction and extensive melting of the cladding which effectively erased all traces of the initiating failure. It is postulated, however, that both failures resulted from a combination of high internal pressure and weakened cladding (because of high transient temperatures). This is in general agreement with conclusions from previous zero burnup tests in which cladding "burn through" was discounted as a primary failure mechanism.

4.4 DEFECTED OR SODIUM-BONDED FUEL EXPERIMENTS

This test series involves two three-pin capsules to investigate the transient performance of defected or

sodium-bonded fuel. These experiments incorporate sodium-filled pins (24-inch-length fuel, 15-inch-length upper blanket) using both pellet and powder fuel. The three pins in each capsule are separately encapsulated and are arranged side by side in a triangular array.

The three inner capsules for C6A have been completed and are being prepared for preirradiation neutron radiographs prior to installation in the TREAT pressure vessel.

At the request of TREAT operations, the hazards analysis for these series 6 experiments has been expanded in specific areas related to capsule design and maximum pressure generation. A revised draft has been prepared and is in final review.

5. TASK C-2—SAFETY EXPERIMENTATION (LOSS OF FLOW)

5.1 PROGRAM PLANNING

Preliminary planning was completed for a new task (task C-2) based on the near-term objective of an in-pile loss-of-flow experiment. Contract work scope and cost estimates were prepared for inclusion in a formal proposal to cover the contract period beginning October 1, 1969.

The test program as outlined, is aimed specifically at accident conditions involving rapid reductions in sodium flow, such as might be caused by loss of pumps or sudden blockage of a subassembly. It is recognized that gradual blockage may also occur because of fuel swelling or a slow accumulation of some material which chokes off flow over a period of time. It was assumed, however, in formulating these plans that these events would culminate in channel voiding and that the remainder of the accident progression would be similar. The purpose of this task, then is not to determine whether flow starvation will occur, but to specifically cause it (under postulated conditions) and to observe the sequence of events that follow.

In order to focus these experiments on a realistically limited objective, the loss-of-flow accident can be thought of in several postulated steps:

- Flow is reduced while power remains constant.
- Temperature increases and boiling (voiding) is initiated at some liquid superheat.
- Vapor expands as channel dries out. Core materials heat up during initial voiding.
- Coolant re-enters active fuel zone.
- Secondary reactions and voiding occur.

In general, it is estimated that pressures associated with the voiding in step c. will be of the order of 100 to 200 psi, assuming superheats $< 100^{\circ}\text{F}$. In steps d. and e., however, there exists a potential for much higher pressures, coupled with destructive energy release which could lead to rapid propagation of damage. It is this phenomenon of "coolant re-entry," to which the proposed experimental program is primarily directed, to provide experimental data to guide analytical efforts in this area. Here again, the purpose is not to determine whether re-entry will occur, since this may depend upon a prototypic simulation of a great many unknown parameters. Instead, it is proposed to specify re-entry as the initial condition and to attempt to determine the upper boundary of the resulting damage. The objective is to demonstrate with phenomenological tests that re-entry results in pressures and energies which can be reasonably absorbed in a typical structure or that the time scale of such events will allow detection and remedial action.

The development of a specific testing plan was based on a rationale involving three basic points:

- Since study of coolant re-entry demands a continued heat source after channel voiding, in-pile experiments are required.
- The forced convection capsule (FCC) provides the highest potential for early in-pile experiments involving transient flow and steady-state power.
- Since an in-pile loss-of-flow experiment would be unique, a step-wise progression of tests is necessary to minimize the size of extrapolations required for analysis of the hazards of the test itself.

Having set the initial goal of a loss-of-flow experiment using an in-pile capsule (C10A), the remainder of the near-term program is based on a need to predetermine information in two basic areas as follows to aid in the analysis of hazards:

- Determination of transient flow characteristics, instrumentation response, and demonstration of a reliable scram signal on initial voiding.
- Demonstration of maximum pressures and energy potential associated with rapid interaction of hot fuel and cladding with sodium.

The first area will be investigated with Mockup 1A which comprises an out-of-pile simulation of a loss-of-flow capsule instrumented to measure flow, temperature, pressure, and vibration (accelerometers). It is planned that the second area be covered with a small, instrumented capsule in the GETR trail-cable facility (C10X). This high strength capsule would be designed to sense pressure and show internal deformation in response to rapid overpower and contact of sodium with hot cladding and/or fuel. These results would be used to determine whether the in-pile capsule has sufficient pressure and energy absorption capability to be suitable for re-entry experiments.

5.2 MOCKUP 1A

The first test to be performed under the task C-2 loss-of-flow program is a flow blockage in an out-of-pile loop designated Mockup 1A. Mockup 1A is an electrically heated loop with a geometry similar to that planned for subsequent in-pile loss-of-flow capsules. The objectives of Mockup 1A follow:

- Determine which of the potential instrumentation outputs provides the most distinct voiding signal.
- Investigate instrumentation response for adequacy for in-pile capsules.
- Examine loop behavior to aid in eliminating anomalies introduced by the loop geometry.

In addition, the results of this test will be examined in an attempt to characterize the initial voiding sequence and to indicate the potential for re-entry and superheat.

The instrumentation which will be used to follow the loss-of-flow test in Mockup 1A includes an electromagnetic flowmeter, a pressure transducer, thirteen thermocouples, and six piezoelectric accelerometers. The flowmeter is located downstream of the test section, and the pressure transducer is near the entrance. Thermocouples are located at both exit and entrance and on the test section wall ≈ 8 inches below the exit of the heated zone. The accelerometers are being installed in an attempt to detect and identify boiling or voiding noise. Their locations were selected to utilize two different acoustic transmission paths from the assumed noise source at the uppermost end of the

electrical heaters. One path is radially outward while the second is in the longitudinal direction and utilizes capsule components as wave guides. The output of the instrumentation will be recorded on both a high speed oscillograph and magnetic tape.

All major machining has been completed and the remaining effort will be directed to the final assembly of these components. Checkout and reactivation of the sodium fill loop has been initiated, and detailed operational procedures are being specified. Initial testing of Mockup 1A is scheduled for early November.

6. TASK E—FUEL MATERIAL PROPERTIES AND PERFORMANCE EVALUATION

6.1 GENERAL

The purpose of this task is to establish the physical, mechanical, and thermal properties of ceramic fuels and fuel elements which will be effective in a fast reactor under both steady-state and casualty conditions. Ex-reactor experiments are conducted (as well as in-reactor) to provide property measurements or observations which aid in the interpretation of the behavior of irradiated specimens. Detailed examinations are made in which electron probe microanalyzer, x-ray diffraction, and other analytical tools are used to identify reaction or impurity phases that may be present in irradiated fuel specimens.

6.2 HIGH BURNUP IRRADIATIONS

The following capsules have been renumbered and will be reported under task F in this and future progress reports:

Part Identification Number	New Identification Number
capsule E5F	capsule F12A
capsule E5C	capsule F12C
capsule E5D	capsule F12D
capsule E5E	capsule F12E
capsule E2P-Q	capsule F12P-Q

6.3 PHYSICAL PROPERTY STUDIES

6.3.1 Mechanical Properties

The compressive creep apparatus has been installed in a glove box in the Plutonium Laboratory and an initial series of calibrating tests were conducted on UO_2 . The equipment was found to be operating properly and test results were in good agreement with previously reported compression creep data for UO_2 .¹

Initial creep results were obtained on mixed oxide samples prepared from coprecipitated powders. The cold pressed and sintered cylindrical specimens had $(\text{Pu}_{0.2} \text{U}_{0.8}) \text{O}_{1.98}$ composition, a 95% theoretical density, and a 25 μgr size. Samples were tested at compressive stresses from 2000 to 10,000 psi in an atmosphere of N_2 -8% H_2 to maintain the initial stoichiometry of the samples. The activation energy for test temperatures of 1550° and 1450°C was about 100 kcal/mole at a stress of 8000 psi. Compressive creep rates for these samples are plotted versus applied stress in figure 6-1 and are compared with previously determined values for UO_2 using the same test techniques. At stresses greater than 6000 psi, the mixed oxide creep rate is about twice the UO_2 rate. The increased creep rates measured for the mixed oxide are postulated to be caused by either the presence of the plutonia or to the disordered state of the nonstoichiometric material.

Figure 6-1. Steady State Creep Rate of 95% Theoretical Density, 20 wt% Plutonium Samples

6.3.2 Mixed Oxide Thermal Conductivity

The preparation of samples for measuring the thermal conductivity of mixed oxide was initiated. Pellets of both coprecipitated and mechanically blended powders are being prepared to evaluate the effects of density, stoichiometry, and plutonium concentration on thermal conductivity.

The radial heat flow equipment in which the testing will be done has been installed and calibrated in a glove box in the Plutonium Laboratory at GE-VNC. To maintain the initial stoichiometry of the mixed oxide pellets, the radial temperature gradient will be small and the atmosphere within the apparatus will be controlled to prevent changes in the oxygen-to-metal ratio from occurring during testing. Thermal conductivity values will be calculated from measurements of black-body hole temperatures at various radii using an optical pyrometer. This technique and equipment have been used previously with UO_2 .²

6.4 CENTER TEMPERATURE LIMITS

6.4.1 Capsule Series E1J (Effect of Stoichiometry on Effective Thermal Conductivity of Restructured Fuel)

The results from the irradiation and examination of the first capsule of this series, with an O/M of 2.00, have been previously reported.³ Two additional fuel pins with O/M of 1.936 and 1.97 were examined, and integrated conductivity value $\int_{T_s}^{T_m} k d\theta$ is being determined from fuel microstructural features. All capsules had received the same programmed power time cycles during irradiation, a 64-hour restructuring period between 8 and 18 kW/ft to form a central void followed by 24 hours at 24 kW/ft to achieve center fuel melting. The three capsule irradiations are summarized in table 6-1. In all cases, the radius of melting was assumed to be the location in the high density

core at which porosity begins. Additional samples of all three stoichiometries will be irradiated to obtain confirmation of these results. Microstructures of transverse sections of the three fuel pins used to establish the radius of various microstructural features are shown in figures 6-2, 6-3 and 6-4. All fuel was of the same initial density, 92% TD. The appearance and density of fuel in the outer perimeter did not vary from the pre-irradiation structure. The size of the observed center holes varied in the three cross sections.

6.5 PLUTONIUM AND FISSION PRODUCT DISTRIBUTION

6.5.1 Electron Probe Microanalysis of SOV-6, ($\text{Pu}_{0.2} \text{U}_{0.8}$) O_2 , at 28,000 MWd/Te

This sample was obtained from Argonne National Laboratory (ANL) where it had undergone microanalysis,⁴ and is being examined by GE-BRDO as part of a sample exchange program. A radial plutonium gradient was observed with the highest Pu concentration at the center of the fuel sample. The concentration of Pu ranged from 24 to 27 wt% Pu, confirming the values obtained by ANL. Within the columnar grain region, the Pu concentration was 18.4 wt%, while the U concentration was 79 wt%.* Closer to the outer perimeter a small Pu-rich area was found and analyzed to be 44 wt% Pu and 44 wt% U. At the outer edge of the physically mixed Vipak fuel, a particle of 2.7 wt% Pu, 91 wt% U was analyzed. Sintering and homogenization occurred in the hotter center locations.

* The difference in concentration between 100% and the sum of these elements (97.4%) is believed to represent fission products.

Table 6-1
CHARACTERISTICS OF E1J CAPSULES IRRADIATED IN GETR

Capsule	E1J-1	E1J-5	E1J-7
O/M	1.936	1.969	2.000
Pellet Diameter (in.)	0.2180	0.2178	0.2177
Pellet Density	92.6	91.7	91.9
Fuel/Clad Gap (mils)	2.7	2.7	2.7
Operating Power*	22.6±1.7†	23.7±0.6	23.4±0.7
Microstructure			
Center Hole (r/r_o)††	0.072	0.153	—
Columnar Grains (r/r_o)††	0.743	0.773	0.813
Equiaxed Grains (r/r_o)††	0.825	0.879	0.896
Molten Boundary (r/r_o)††	0.336	0.432	0.489
Melting Temperature ($^{\circ}\text{C}$)§	2740±25	2780±25	2750±25

* Range of power during last 24 hours of operation as indicated by 3 thermocouples.

† One of the thermocouples indicated lower values and is the reason for the wider range of power reported.

†† r/r_o = fractional radius of microstructural feature.

§ Solidus temperature.

Figure 6-2. E1J-7 Fuel Microstructure, 2.000 O/M, 92% TD

Figure 6-3. E1J-5 Fuel Microstructure, 1.969 O/M, 92% TD

Figure 6-4. E1J-1 Fuel Microstructure, 1.936 O/M, 92% TD

Several fission products were found in the fuel and their concentrations* are listed in table 6-2.

The concentration of cesium at the outer fuel perimeter has been noted in other work.^{4,5}

Analysis of the type 304 cladding indicated that Si and Cs were present in grain boundaries. Si concentration was higher at the grain boundaries than within the grains.

6.6 SODIUM-FUEL REACTIONS

6.6.1 Electron Probe Microanalysis of Failed Specimens F20 and B9A

A sample from each of these failed pins is being prepared for microprobe examination. A description of the F20 fuel pin has been reported previously.³

The B9A fuel pin is described in section 3 of this report and in a prior progress report.⁶ To preserve the fuel-sodium reaction product in this specimen, the samples are

being prepared by special nonaqueous methods and in low oxygen environments. All grinding and polishing is being done under kerosene and polishing oil. Xylene or petroleum ether are used for sample cleaning and washing.

6.7 FISSION PRODUCT-CLADDING REACTION STUDY

Creep rupture tests of several pressurized type 316 stainless steel tubing samples filled with compounds of cesium and iodine were conducted to investigate intergranular attack. Premature rupture or excessive strain occurred in four samples loaded with CsOH or Cs₂O, while normal behavior was exhibited by capsules filled with I₂, HIO₃, CsCl and CsI. Test periods up to 400 hours at effective hoop stresses to 11,000 psi at 1200°F were used.

Table 6-2
FISSION PRODUCTS IN ANL SAMPLE SOV-6

	Element	Periphery	Location	
			Mid-Radium	Center
* These are relative intensities ($I^{\text{observed element}}/I^{100\% \text{ element}}$) which have been corrected for instrument factors, such as dead time, drift, and background. Correction has not been attempted for x-ray phenomena intrinsic to the material, such as absorption, fluorescence, and atomic number.	Nd	0.16 wt%	0.15 wt%	not detected
	La	not detected	0.35 wt%	not detected
	Cs	1.0 wt%	not detected	not detected

REFERENCES

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7. TASK F—FAST FLUX TESTS AND SUPPORT

7.1 GENERAL

The purpose of task F is to apply the more promising results of the basic fuel technology developments to the design, fabrication, and integral testing of fuel elements in fast fluxes. Important fuel performance characteristics, such as fission gas release, fuel-cladding interactions, and fuel thermal performance, are determined under conditions simulating, as closely as possible, those expected in a fast ceramic reactor. Testing of fuel pins in a fast flux, in principle, will permit testing of fuel bundles under fair approximation of prototypical conditions.

The test parameters for task F are selected by a combination of design analysis which relies on basic properties data and on the results of selected experiments (such as those in task E and task L). In addition, the results of prior fast-flux irradiation tests are interpreted as quantitatively as possible to guide the selection of future tests. The selection of test parameters is also influenced and guided by the work under task C, which focuses on the preferred range for safety considerations of LMFBR designs. Demonstration of high fuel burnup (50,000 to 100,000 MWd/Te) is required over a range of power levels (4 to 24 kW/ft). Fuel parameters that are under study in a fast environment include density, configuration, fabrication process history, stoichiometry, diametral gap, axial gap, and external restraint. Fast flux tests are performed with both encapsulated and unencapsulated pins.

Selected irradiation tests in thermal test reactors are being performed in support of the fast flux irradiations in EBR-II. The areas being explored are selected high burnup irradiations at high power levels which contribute to the understanding of mixed oxide fuel irradiation performance and cannot currently be performed in the EBR-II.

These tests include high risk tests, instrumented capsules, capsules operated at high power with fuel temperatures at or above melting, and a capsule designed to determine mixed oxide fuel swelling by measuring fuel pin diameters without the complications of cladding swelling.

The capsules transferred from task E to task F, which are part of this irradiation test support work, are listed in table 2-5.

7.2 MEDIUM POWER, LONG TERM IRRADIATIONS

Group 2 (Medium Power Scoping Irradiations)

The four remaining group 2 capsules being irradiated in EBR-II subassembly XO50 continued irradiation toward a target burnup of 100,000 MWd/Te (average). The calculated peak burnup accumulated on the fuel in these capsules is 108,600 MWd/Te. These fuel pins have obtained the highest burnup of any mixed oxide fuel irradiated in the EBR-II under the FCR Program.

7.3 LOW POWER FUEL SWELLING IRRADIATIONS

7.3.1 Group 0 (Low Power Swelling of High Density Fuel)

Approval-in-principle to continue irradiation of the two group 0 capsules in EBR-II subassembly XG03 to $\approx 80,000$ MWd/Te was granted by AEC's Division of Reactor Development and Technology. An addendum to the group 0 data package was prepared and submitted to the EBR-II project to support the continued irradiation of the capsules to calculated fuel burnups of 80,000 MWd/Te.

Peak calculated burnup on the fuel in the two capsules in subassembly XG03 is 61,500 MWd/Te.

Irradiation of the two group 0 capsules in subassembly XG04 continued. Calculated peak burnup on the fuel in these two capsules is 64,400 MWd/Te.

Peak fuel burnup analysis values for fuel pin FOE are 34,800 MWd/Te and 34,000 MWd/Te based on the analysis of two samples. The burnup value predicted by EBR-II for this fuel pin was 40,000 MWd/Te.

7.3.2 Group 6 (Fuel Cladding Interactions Under Accelerated Swelling)

The post-irradiation examination of the four capsules from subassembly XO10 continued. The calculated peak burnup is 61,500 MWd/Te. Design parameters for these pins are shown in table 7-1.

Neutrographs revealed that fuel pin FOM failed. The failed fuel pin cladding was Incoloy 800 and had a smeared fuel density of 96.3%. The failure of FOM is severe and fuel was observed in the sodium annulus over about three-fourths of the fuel length.

The gamma scans of the capsules, including the fuel pins, have been previously reported (31st quarterly report, GEAP-10028-31). The gamma scan of the FOM capsule showed no anomalies except for the bottom insulator pellet area where slight gamma peaking was noted.

Axial profilometer traces of the capsule tubes were obtained at two orientations and are shown in figures 7-1 thru 7-4. Maximum diameter increases in the fuel region (relative to the plenum) are listed in table 7-2. Axial locations on the tubes where ovality was observed may be caused by dents that occurred during handling.

The FOJ, FOK, and FOL capsule tubes were punctured and checked for fission gas. No Kr-85 activity was detected. These three sound pins were then removed from their capsules and cleaned.

Fuel pins FOJ and FOK were bowed similar to the bow of the containment capsules. Pin FOL, however, was not bowed. The bowing of the capsule tubes and pins is shown in figure 7-5.

Table 7-1
GROUP 6 TEST PARAMETERS

PIN	FUEL PARAMETERS*			
	% U-235	O/M	Pellet Density (% TD)	Smeared Density (% TD)
FOJ	93.8	2.000	97.4	94.00
FOK	93.6	2.000	97.8	95.30
FOL	92.7	1.980	97.5	94.60
FOM**	92.8	1.980	97.3	95.20

PIN	CLADDING PARAMETERS		
	Cladding Type	Initial Outside Diameter (inch)	Initial Fuel and Cladding Gap Range (mils)
FOJ	1-800	0.2518	2.2-5.7
FOK	347	0.2500	1.6-4.9
FOL	316	0.2480	1.8-4.6
FOM**	1-800	0.2520	1.6-4.2

PIN IRRADIATION PARAMETERS

	Calculated Peak Fuel Burnup ($\times 10^{-3}$ MWd/Te)	Peak Linear Power (kW/ft)
FOJ	61.5	11
FOK	61.5	10.7
FOL	61.5	10.8
FOM**	61.5	11.4

* Each pin contained 14.25 inches of solid $\text{Pu}_{0.2}\text{U}_{0.8}\text{O}_2$ fuel pellets in 0.015-inch-wall tubing.

** Failed

Profilometer traces of the fuel pins (figures 7-6 thru -8) indicate ridging at fuel-pellet interfaces. The maximum diameter increase observed is about 1% or 2.5 mils. The diameter changes for pins FOJ and FOL did not reflect the EBR-II axial flux profile. The diameter of pin FOJ gradually increased from the top to the bottom of the fueled region. An opposite trend is seen for FOL where the diameter increased going from bottom to top of the fueled region. Pin FOK exhibited a diametral profile similar to the EBR-II axial flux distribution. Regions of high fuel density and small diametral gap were observed to correspond to areas of maximum diameter increase. Continuous profile

measurements from four orientations on each pin were quite similar indicating uniform circumferential strain or swelling. Analysis of these data is continuing.

7.3.3 Group 8A (Effect of Fuel Density of Fuel Swelling Rate)

The seven group 8A capsules in EBR-II subassembly XO19 attained their burnup goal and were removed from the EBR-II core on August 2, 1969. Calculated peak burnup on the fuel in these capsules was 54,000 MWd/Te. The subassembly was removed from the reactor, disassembled, and the capsules returned to GE-Vallecitos (VNC) for post-irradiation examination. Neutron radiographs of the capsules showed no indications of failure. Irradiation of the nine capsules in EBR-II subassembly XO20 continued toward 100,000 MWd/Te. Peak calculated burnup for the fuel in these capsules is 62,500 MWd/Te.

7.3.4 Group 8B (Effect of Void Deployment of Fuel Cladding Interactions)

The Calculated peak burnup on the fuel in the group 8B capsules in EBR-II subassembly XO36 is 27,900 MWd/Te. Target burnups for these capsules is 50,000 MWd/Te.

Table 7-2
MAXIMUM CAPSULE DIAMETER CHANGE
OVER FUEL REGION
OF GROUP F6 CAPSULE TUBES

Pin	Orientation	Max Δ Diam (mils)
FOJ	0°	1.7
	45°	1.8
	90°	1.7
	135°	1.8
FOK	0°	0.6
	45°	1.0
	90°	1.3
	135°	1.2
FOL	0°	0.8
	45°	1.0
	90°	1.5
	135°	1.3
FOM	0°	1.0
	45°	1.0
	90°	1.7
	135°	1.4

Figure 7-1. Profiles of Capsule Tube FOJ

Figure 7-2. Profiles of Capsule Tube FOK

Figure 7-3. Profiles of Capsule Tube FOL

Figure 7-4. Profiles of Capsule Tube FOM

NOTE: ALTHOUGH THE FOL CAPSULE WAS BOWED THE FUEL PIN REMAINED STRAIGHT

Figure 7-5. Photograph of Fuel Pins FOJ, FOK, and FOL and Their Capsule Bodies

7.4 HIGH POWER INTERMEDIATE BURNUP IRRADIATIONS

7.4.1 Group 3B (Density and Stoichiometry Effects)

Irradiation of the eighteen group 3B capsules in EBR-II subassembly XO27 continued. Target burnup for these capsules is 120,000 MWd/Te. An interim examination is scheduled when a calculated burnup of $\approx 80,000$ MWd/Te is attained. Calculated peak burnup on the fuel in these capsules is 72,100 MWd/Te.

7.4.2 Group 4 (Low Density Fuel Behavior at High Heat Ratings)

An addendum to the group 4 hazards analysis was prepared and submitted to the EBR-II Project. The addendum was in response to a request to justify continued irradiation of the seven remaining group 4 capsules to a target burnup of 80,000 MWd/Te.

7.5 ELEVATED CLADDING TEMPERATURE TESTS

7.5.1 Group 5 (Fuel Performance at Peak FCR Cladding Temperatures)

Calculated peak burnup accumulated on the nineteen capsules in EBR-II subassembly XO64 is 21,700 MWd/Te. Target burnup for the fuel in the group 5 capsules is 100,000 MWd/Te.

7.5.2 Group 10A (Cladding Performance at Peak FCR Cladding Temperatures)

The group 10A encapsulated fuel pin test was approved in principle. Preparation of specifications and drawings for the nineteen group 10A fuel pins and capsules was completed and preparation of powder for fuel fabrication was initiated.

7.5.3 Group 10B (Cladding Performance at Peak FCR Cladding Temperatures)

Fabrication of the nineteen group 10B capsules continued. All fuel pins have been fabricated. Encapsulation of eleven fuel pins is complete. The eight remaining capsules were sodium filled but inspection of the capsules indicated voids in the bottom of the capsules. These eight fuel pins were deencapsulated, cleaned, and prepared for reencapsulation and sodium filling.

7.6 UNENCAPSULATED FUEL PIN IRRADIATIONS

7.6.1 Group 3A (Unencapsulated Fuel Pin Irradiation to Evaluate Fuel-Cladding and Cladding Coolant Interactions)

An interim examination of sixteen group 3A unencapsulated fuel pins was performed at the EBR-II site. Peak calculated burnup on the fuel pins at the time of the examination was $\approx 36,000$ MWd/Te.

Figure 7-6. Profilometer Traces of FOJ Fuel Pin

Figure 7-7. Profilometer Traces of FOK Fuel Pin

Figure 7-8. Profilometer Traces of FOL Fuel Pin

The interim examination included the following:

- visual examination
- neutron radiography
- measurements of cladding diameter at 0° and 90° orientations.
- measurement of fuel pin weights.

All sixteen fuel pins were examined as described above; in addition, cladding temperature measurements were made on two pins.

7.6.1.1 Fabrication Parameters

The fuel pins in this subassembly contain either annular or solid mixed oxide fuel pellets with nominal plutonium content of 25 wt%. The cladding composition was type 316 and 304L stainless steel of 0.290 inch outside diameter with a 0.020 inch wall. Nominal fuel stoichiometry was 1.98 and 2.00. The as-fabricated fuel-cladding gaps and smeared fuel densities ranged from 0.0041 to

0.0069 inch and 81.2 to 89.8% of theoretical, respectively. Fuel and cladding parameters for each fuel pin are summarized in table 7-3.

7.6.1.2 Irradiation Parameters

The loading diagram for the XO40 subassembly prior to the interim examination is shown in figure 7-9. Calculated linear powers based on the fission rates reported for the EBR-II in the experimenters guide* are also listed in table 7-3 (These values may be 13% to 20% high; see subsection 7.8). Axial temperature profiles of the Na coolant and cladding i.d. and o.d. for the peak power pin in this subassembly is shown in figure 7-10.

* "Guide for Irradiation Experiments in EBR-II," Rev. 3, Argonne National Laboratory, July 1969.

Table 7-3
GROUP F3A FABRICATION AND TEST PARAMETERS

FUEL						CLADDING		MEASURED DURING INTERIM EXAM		
Fuel Pin Designation	Peak Linear Power (kW/ft)	Type	O/M	Density		Type	Diam Gap (mils)	Fuel ΔL (%)	Clad Max ΔD (mils)	
				Pellet (%)	Smeared (%)				0°	90°
F3B-1	16.25	AP	2.000	—	88.3	316	4.7	1.60	0.9	1.0
F3B-2	16.63	SP	1.979	90.5	87.4	316	4.3	1.59	0.8	0.8
F3B-3	17.13	AP	2.000	—	87.5	316	4.8	1.78	1.0	0.8
F3B-4	16.62	SP	2.000	91.3	87.9	316	4.8	1.78	0.9	0.6
F3B-5	15.35	SP	1.979	85.8	82.7	316	4.6	1.25	0.5	0.5
F3B-6	15.28	SP	1.999	84.4	81.2	316	4.7	1.08	1.0	1.1
F3B-7	16.90	AP	1.978	—	86.4	304	6.9	1.76	1.0	1.5
F3B-8	17.02	SP	1.981	90.1	86.0	304	5.5	1.85	1.3	1.7
F3B-9	17.09	AP	2.000	—	85.6	304	5.9	1.90	1.4	1.9
F3B-10	17.31	SP	1.999	90.3	86.1	304	5.8	1.52	1.3	1.9
F3B-11	16.07	SP	1.997	85.7	81.3	304	6.4	1.46	0.7	0.9
F3B-12	16.53	AP	1.978	—	88.4	316	4.8	1.27	1.4	0.8
F3B-13	16.73	SP	1.981	92.8	89.8	316	4.1	1.53	0.4	1.0
F3B-14	17.19	AP	2.000	—	87.9	316	4.2	1.42	1.1	1.3
F3B-15	16.85	SP	1.999	89.4	86.1	316	4.5	2.01	1.1	1.0
F3B-16	15.39	SP	1.980	85.5	82.5	316	4.4	1.25	0.7	0.7

NOTES:

Fuel column length 13.47 to 13.53 inches

AP - Annular Pellets

SP - Solid Pellet

Cladding 0.290-inch diameter with 0.020-inch wall thickness

All fuel contains 24.9 wt% Pu, fully enriched Uranium (92.4 to 92.6 wt% U-235) and was fabricated by coprecipitation.

Peak neutron fluence 2.4×10^{22} nvt (total)

GROUP:	F3A
REACTOR LOCATION:	5B2
SUBASSEMBLY:	XO40
ORIFICE PLATE:	NONE
FLOW:	68 gpm at 800°F
LOADED:	8/8/68

Figure 7-9. Loading Diagram for Subassembly XO40 in EBR-II

Figure 7-10. Axial Temperature Profile for Hottest 316 Clad Fuel Pin in Group F3A

7.6.13 Cleaning

Subassembly XO40 was cleaned in the EBR-II fuel transfer coffin using wet argon to react the sodium followed by demineralized water flushing and argon drying. Completion of sodium removal was assumed when the flush effluent reached a very low electrical conductivity.

7.6.1.4 Visual Examination

The cladding generally appeared similar to the pre-irradiation condition. Some superficial brownish spots were observed which had the appearance of a slight residue from the drying of water droplets. Spiral spacer wires had loosened and were separated from the cladding in some areas. All welds appeared in excellent condition. Slight bowing was observed on several of the pins.

7.6.1.5 Weighing

The fuel pins were weighed with the spiral wire wraps in place. The pre-irradiation weights were taken without the wire; therefore, a direct comparison was not possible. However, a tabulation of the weights, presented in table 7-4, indicates that the difference between the pre- and post-irradiation weights varies between 19.2 and 20.2 grams except for pin F3A-16, where the value is 37.7 grams. The post-irradiation value for F3A-16 is higher than the others while the pre-irradiation value appears normal. EBR-II is presently rechecking the post-irradiation weight of this pin.

7.6.1.6 Neutronradiography

Neutronradiographs showed the presence of center voids in all fuel pins. Fuel length measurements were taken from the neutrograph negatives and compared with the as-fabricated fuel lengths. Measured length increases ranged from 1.08% to 2.01%. No correlation was found between fuel length increase and fuel smeared density versus diametral gap. A plot of fuel ΔL versus peak linear power is shown in figure 7-11. In general the observed length increases were greater in the higher power pins.

7.6.1.7 Diameter Measurements

Fuel pin diameter measurements were made at 0° and 90° orientations at intervals of one to two inches as permitted by the spiral wire wrap. The instrument utilized for these measurements had an accuracy of ± 0.0001 inch. Pre- and post-irradiation diameter measurements are plotted in figures 7-12 and 7-13 for the type 316 and type 304L clad pins which operated at the highest linear powers.

A tabulation of the maximum diametral change measured on each fuel pin is listed in table 7-4. The maximum diametral change values are plotted in figure 7-14 as a function of peak linear power with cladding and fuel type noted. The greatest diameter increases occurred on four of the five type 304L clad fuel pins even though several pins clad with type 316 stainless steel ran at equivalent linear

Table 7-4
POST-IRRADIATION WEIGHTS
OF F3A FUEL PINS AT 3 AT.% BURNUP

Fuel Pin Designation	Weight* (grams), Post-Irradiation with Wire Wrap	Weight (grams), Pre-Irradiation Without Wire Wrap
F3B-1	446.2	426.4
F3B-2	444.4	425.2
F3B-3	444.3	425.1
F3B-4	445.2	425.7
F3B-5	439.2	419.9
F3B-6	437.4	417.9
F3B-7	439.1	419.4
F3B-8	441.1	421.9
F3B-9	437.6	418.7
F3B-10	442.0	422.3
F3B-11	436.0	416.5
F3B-12	445.6	426.2
F3B-13	444.0	424.8
F3B-14	444.5	425.3
F3B-15	444.3	424.1
F3B-16	457.5	419.8

* Weights accurate to ± 0.1 gram

powers. A plot of increase in cladding diameter as a function of as-fabricated diametral gap is shown in figure 7-15.

7.6.1.8 Temperature

Temperature measurements were taken 17 days after irradiation was completed on two fuel pins by pressing a shielded thermocouple bead against the cladding. A maximum temperature of 58°C was recorded.

The interim examination of this subassembly did not uncover any potential failures or anomalous fuel pin behavior, and the fuel pins were approved for reinsertion for further irradiation. The fuel pins were loaded in subassembly XO40A and reinserted on October 4, 1969. Peak calculated burnup on the fuel in this subassembly is 37,400 MWd/Te.

7.6.2 Group 9A (High Power, High Burnup, Unencapsulated Fuel Pin Irradiations)

Irradiation of the 37 unencapsulated group 9A fuel pins in EBR-II subassembly XO43 continued. Approval to continue irradiation of the group 9A fuel pins to an average burnup of $\approx 85,000$ MWd/Te was granted by the EBR-II project provided that the group 3A fuel pins are intact after a burnup of $\approx 60,000$ MWd/Te. Current calculated peak fuel burnup is 34,000 MWd/Te.

Figure 7-11. Peak Linear Powers Versus Percent ΔL for F3A Fuel at 3 At. % Burnup

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Figure 7-12. Cladding Diameter Measurements, Fuel Pin F3B-10

Figure 7-13. Cladding Diameter Measurements, Fuel Pin F3B-14

Figure 7-14. Peak Linear Power Versus ΔD for F3A Fuel Pins at 3 At.% Burnup

Figure 7-15. Fuel Clad Gap Versus ΔD for F3A Fuel Pins at 3 At.% Burnup

7.6.3 Group 9B (Medium Power, High Burnup, Unencapsulated Fuel Pin Irradiations)

The irradiation of the 37 fuel pins of group 9B continued in subassembly XO62. Calculated peak burnup on the fuel in the subassembly is 17,400 MWd/Te, compared to a target burnup of 100,000 MWd/Te.

7.6.4 Group 9C (High Power, High Burnup, Unencapsulated Fuel Pin Irradiations)

Irradiation of the 37 group 9C fuel pins was continued. They now have a calculated peak burnup of 28,100 MWd/Te, compared to a target burnup of 100,000 MWd/Te.

7.6.5 Group 9D (Medium Power, High Burnup, Unencapsulated Fuel Pin Irradiations)

The group 9D fuel pins continued their irradiation in subassembly XO58. The present calculated peak burnup is 17,700 MWd/Te. Target burnup for the group 9D fuel pins is 100,000 MWd/Te.

7.7 PRE-IRRADIATION TRANSIENT TEST

Group 7 (Effect of Irradiation, Density and Axial Restraint)

Calculated peak burnup on the fuel in the four group 7 capsules in subassembly XO50 is 33,400 MWd/Te.

7.8 BURNUP ANALYSIS OF MIXED OXIDE FUEL

Recent review and revision of methods of converting radiochemical and heavy element mass spectrograph data to fuel burnup has resulted in greater self-consistency and increased confidence in the validity of the experimentally determined burnup results. There still remain substantial differences in the calculated versus experimental burnup values (i.e., linear power) for test fuel pins irradiated in EBR-II (burnup analyses are up to 20% lower than the calculated EBR-II burnups).

Experimentally determined burnup values (based on Nd-148 to heavy element ratios) included wide variations in the results on nearly identical samples. Recent revisions in the method used to convert radiochemical and mass spectrograph analysis data to burnup values has decreased the spread in the burnup values. The validity of the analyses as a measure of burnup has been improved. (ASTM round robin exchanges have placed GE Nd-148 analysis within a maximum deviation of 1.2% from other laboratories.)

Calculated burnups are based on EBR-II fission rates. The fission rates are in turn based indirectly on fast fission nuclide yields. The nuclide yields used by EBR-II tend to overestimate burnup in experimental assemblies. In addition, the EBR-II calculations do not consider the effect of depletion of fissile material caused by fission and capture in high burnup experiments.

The EBR-II fission rate values are based on a yield for Cs-137 of 5.8% and for Tc-99 of 5.4% in a fast spectrum.

Recent data suggest that these yield values should be revised upward to 6.5% for Cs-137 and to 6.13% for Tc-99. If the new data are correct, estimates of burnup will be reduced 13%.

The computational technique used to convert Nd-148 radiochemical analysis and heavy element mass spectrograph analysis of irradiated fuel samples to fission burnups has recently been revised for use on (Pu, U)O₂ fuel samples containing highly enriched uranium using U-235 as a base. (The original technique was developed for water reactor fuels having high U-238 contents and made use of the U-238 content as a base.) This modification decreases the sensitivity of the results to possible U-238 contamination or inaccuracies in initial and final isotopic analysis for U-238.

The burnup values for task F fuel pins irradiated in EBR-II are listed in table 7-5. This table includes the calculated EBR-II values and GE-VNC analysis values based on U-235 and U-238. The calculated burnups based on EBR-II fission rates are plotted versus burnups from GE-VNC analyses in figure 7-16. This comparison shows that all the higher experimentally determined burnups are below the calculated burnups indicating a consistent bias. The line in this figure marked "13% low" indicates the correspondence that would occur if fission yield corrections alone were applied to the EBR-II fission rate calculational basis. The EBR-II calculated values do not account for depletion caused by fissile atom fission and capture. Fission depletion alone, at 100,000 MWd/Te, will drop the average burnup by ≈5%. The effect of correcting the EBR-II calculated values by 5% is shown by the line marked "18% low" in figure 7-16. The effect of correcting the EBR-II calculated values for depletion is shown by the line marked "depletion correction" in figure 7-16.

In addition to differences related to EBR-II fission rates derived from nuclide fission yields and depletion corrections, local flux perturbations in experimental assemblies containing less fuel than the surrounding driver fuel could also affect fuel burnup rates. All fuel rods analyzed for burnups by GE to date were irradiated in experimental fuel assemblies containing 19 fuel samples or less.

A sample exchange is being arranged whereby EBR-II driver fuel will be analyzed by various laboratories for Cs-137, Tc-99, total rare earths, and for Nd-148. This will be the first time that the same fast reactor fuel has been analyzed by several laboratories. The results will hopefully clarify fast fission yields of Cs-137, Tc-99, and Nd-148 relative to one another.

7.9 F12A, (E5F) IN-PILE AXIAL EXPANSION MEASUREMENTS

The linear variable differential transducer which was used to measure relative fuel expansion in capsule F12A

Table 7-5
SUMMARY OF FUEL BURNUP RESULTS OBTAINED BY CALCULATION
FROM EBR-II DATA AND FROM BURNUP ANALYSIS

Based on EBR-II Data				General Electric Chemical Analysis	% Difference at Position of Burnup Specimen		
Sample	Peak (at. %)	At Position of Burnup Sample (at. %)*	Corrected for Depletion (at. %)	Nd-148	No Depletion Correction	With Depletion Correction	Axial Correction for Position of Burnup Sample from Peak
				per Heavy Element (at. %)*			
F2A	5.26	5.24	5.07	4.39	19.4	15.5	0.997
F2B	5.34	5.34	5.17	3.89	37.2	32.8	0.999
F2E	5.45	5.43	5.24	4.70	15.6	11.5	0.997
F2N	5.53	5.52	5.33	4.74	16.5	12.5	0.999
F2P	3.27	3.26	3.16	2.60	25.5	21.7	0.998
F2Q	5.33	5.32	5.15	4.22	26.0	22.0	0.998
F2S	5.60	5.60	5.41	4.72	18.6	14.6	1.000
F2U	5.75	5.74	5.54	4.48	28.2	23.7	0.999
F2W	5.12	5.11	4.95	3.99	28.2	24.2	0.999
F2Y	5.00	4.99	4.84	3.76	32.8	28.9	0.999
F2Z	5.69	5.69	5.49	4.68	21.5	17.2	0.999
F2C	7.41	7.25	7.04	6.32	14.7	11.4	0.978
F2H	7.53	7.52	7.30	6.75	11.4	8.1	0.999
F2R	7.25	7.14	6.94	6.50	9.9	6.8	0.985
F2T	7.63	7.59	7.38	7.08	7.3	4.2	0.995

* Data plotted in Figure 7-16.

(E5F) was removed from the fuel pin and checked for electrical integrity. The resistances across the transducer coils were found to be identical to those of a duplicate transducer which was not irradiated. The irradiated transducer was calibrated against the control transducer by moving the transformer core in and out of the transducer with a micrometer allowing position calibration of the core to be made to the nearest 0.0001 inch. As the micrometer settings were changed, the electrical output was monitored using the same instrumentation utilized during the irradiation. The irradiated transducer calibration is shown in figure 7-17. The curve shows that the transducer was unaffected by neutron irradiation during the lifetime of the experiment.

7.10 F12C, F12D, AND F12E, (E5C, E5D, E5E), COMPARISON OF 16-INCH-LONG COLUMNS OF SOLID PELLET, ANNULAR PELLET, AND POWDER FUEL

The post-irradiation examination of capsules F12C, F12D, and F12E (E5C, E5D and E5E) was initiated. These capsules contain 16-inch lengths of physically blended mixed oxide fuel and were irradiated in GETR to peak

burnups of about 130,000 MWd/Te. Peak linear power was 19.5 kW/ft, and fuel center temperatures near the melting point were calculated. Parameters for these pins are listed in table 7-6.

Neutron radiographs indicated that center voids are present over the full length of the fuel in all three pins. No indications of cladding failure were observed on the neutrographs.

Gamma scans are shown in figures 7-18 thru 7-20. These pins were irradiated on a V-RAFT and were translated vertically in order to maintain the axial fuel center in the peak flux throughout GETR cycles. It was anticipated that the axial gamma activity distribution would coincide with the average axial neutron flux distribution in GETR, i.e., a chopped cosine curve with an end-to-peak ratio of about 0.8. The gamma scans are unexpectedly flat over the fuel length with a reduction in activity of ≈ 0.8 at the top of the fuel column relative to the bottom of the fuel column. Several regions of higher activity can be seen from 3 to 6 inches above the bottom of the fuel in all three pins. Destructive examination of these pins is in progress.

Figure 7-16. Comparison of EBR-II Physics Burnups with GE Chemistry Burnups

Figure 7-17. Calibration of Transducers for Capsule F12A (E5F) (50 mV Recorder Scale)

Table 7-6
F12C, -D, -E, FUEL PIN SPECIFICATIONS (E5C, -D, and -E)

	F12C (E5C)	Capsule F12D (E5D)	F12E (E5E)
Fuel Type (physically blended)	solid pellet	powder	annular pellet
Pellet Density (% TD)	89	N.A.	95
Smeared Density (% TD)	83	82	83
Oxygen-to-Metal Ratio	1.96	1.97	1.96
Pu/(Pu + U)	0.25	0.25	0.25
U-235/U	0.10	0.10	0.10
Fuel-Cladding Diametral Gap (in.)	0.0038-0.0060	N.A.	0.0045-0.0064
Cladding Type	type 316 SS	type 316 SS	type 316 SS
Cladding o.d. (nominal, in.)	0.250	0.250	0.250
Cladding i.d. (nominal, in.)	0.220	0.220	0.220
Fuel Length (in.)	16	16	16
Bottom Blanket Length (in.)	1	1	1
Top Blanket (in.)	11	11	11
Blanket Material		Natural UO ₂	

7.11 F12P-Q (E2P-Q) EFFECT OF RADIAL RESTRAINT ON FUEL SWELLING

Fuel pins F12P-Q (E2P and E2Q) underwent another interim examination and were subsequently reinserted into a GETR pool position. The interim examination included neutron radiography, gamma scans, and profilometer measurements followed by reencapsulation of the pins in new capsule bodies. Both the 0.010- and 0.015-inch-thick type 316 stainless steel clad fuel pins were intact after attaining fuel burnups of $\approx 20,000$ MWd/Te. The positions of the fuel pins were reversed when they were reencapsulated to ensure equal power and burnup accumulation for the two fuel pins over the total span of the irradiation test.

Diameter profiles for the 0.010-inch and 0.015-inch wall cladding are shown in figure 7-21 for 0° and 90°

orientations. The 0.010-inch-thick cladding exhibited a maximum diametral increase of 0.005 inch while the maximum increase for the 0.015-inch cladding was less than 0.003 inch. Figure 7-22 shows a comparison of the diameter changes for the two pins at burnups of $\approx 10,000$ and 20,000 MWd/Te. The diameter increase for the 0.010-inch cladding was about 0.001 to 0.0015 inch between 10,000 and 20,000 MWd/Te. The 0.015-inch-thick cladding diameter increase was not significant during this period.

The legend for figure 5-4 in the 30th quarterly report (GEAP-10028) is in error. The diameter increases reported for E2P and E2Q at 10,000 MWd/Te are reversed.

Figure 7-18.
Gamma Scan of Capsule F12C (E5C)

Figure 7-19.
Gamma Scan of Capsule F12D (E5D)

Figure 7-20.
Gamma Scan of Capsule F12E (E5E)

Figure 7-21. Comparison of Diameter Changes of Fuel Pins F12P and F12Q after 20,000 MWd/Te Burnup

Figure 7-22. Profilometer Traces of Fuel Pins F12P and F12Q at 10,000 and 20,000 MWd/Te Burnup

8. TASK G-6—CORE DESIGN ANALYSIS

8.1 GENERAL

The purpose of task G-6 (Core Design Analysis) is to contribute to the understanding of fuel performance, core design, and thermal hydraulic problem areas, to develop and use analytical and design tools to aid in this work, and to provide guidance for effective planning and coordination of experimental programs. Task G-6 is a new designation, covering the same scope of core design analysis and surveillance as was formally covered under task G. The results of studies provide direction for further development of analytical methods and form a basis for establishing current design and safety criteria which, in turn, define the range of reactor characteristics and test parameters upon which the experimental programs will focus.

8.2 FUEL PIN THERMAL PERFORMANCE

The analytical models for predicting fuel pin thermal performance is being correlated with the data from 21 task F pins, irradiated in EBR-II to burnups of 74,000 MWd/Te.* Among the parameters being investigated are the size of the central void, gap conductance, and grain growth boundaries, their associated temperatures and densities, and the influence of burnup.

A mass balance was performed on the fuel in an attempt to determine the extent of densification occurring within the fuel during operation. Preliminary results indicate the densification in the columnar grain growth region to be dependent on the initial fuel density and to a lesser extent on burnup.

In the region where columnar grains grow, two-thirds of the initial porosity appears to migrate toward the center of the pin to form a void at 50,000 MWd/Te. The fuel in this region exhibits conductivity characteristics which are better than fresh fuel having a density equal to the smear density of this region. This is because most of the remaining porosity aligns itself in a radial orientation, and therefore, does not interfere with the flow of heat. Insufficient data exists to evaluate the influence of burnup on fuel densification in the columnar grain growth region.

Outside of the columnar grain growth region, the fuel appears to decrease in density and conductivity during life. This is probably the result of a buildup of fission gas within the fuel matrix.

Considering the fact that no fuel melted in any of the pins being considered, it was possible to determine the temperature at which columnar- and equiaxed grain growth occurred. Preliminary indications are that columnar grain growth occurs at no more than 2050°C and equiaxed grain growth, at no more than 1500°C.

Gap conductivity was found to be a function of linear power and initial gap size early in life. After reaching a maximum value at a burnup of less than 50,000 MWd/Te, the gap conductivity appears to decrease as more fission gas is released at higher burnups.

The validity of these conclusions has not been tested for operating conditions outside the experimental range. Some of the operating parameters of the pins used in this evaluation are shown in table 8-1.

Table 8-1

Pin Size	
Outside Diameter (in.)	0.250
Fuel Length (in.)	14.2
Peak Linear Power (kW/ft)	9.9 to 17.2
Initial Fuel Density (TD)	0.830 to 0.975
Cladding Inside Surface Temperature (°F)	1115
Initial Diametral Gap (in.)	0.052
Burnup (MWd/Te)	1500 to 74,000

8.3 STEADY-STATE IRRADIATION EVALUATION

A program was initiated which is aimed at assessment of information required from steady-state and defected fuel irradiation tests in order to support the design and operation of LMFBR fuel assemblies. The data requirements will be assessed and weighed on the basis of their impact on core design, safety, and fuel cycle economics. Based on the results of this work, recommendations for specific experiments and observations will be made.

The work which has been accomplished on this program to date follows:

- Data sheets for recording pre- and post-irradiation information from fuel pins have been prepared and an attempt is being made to complete at least one data sheet from each of the major experimenters.
- An assessment was made of available fuel design tools.
- The post-irradiation observations of available GE fast flux irradiated fuel pins were reviewed and areas were identified which require additional experiments or observations to properly understand fuel pin thermal performance.
- The economic significance of varying several of the fuel design parameters was studied for the early core designs as well as for large commercial plants.
- A review of the existing testing program of fast flux irradiation is in progress.
- Recommendations for specific experiments which would yield information useful in the design of fuel is being assembled from people involved in fuel testing, analysis, and design.

* Burnup based on EBR-II predictions

8.4 PROGRAM PLANNING

Work plans are being developed for a subtask which will design and evaluate alternate systems for fuel pin support. Primary consideration will be given to grid type

systems. Detailed planning is also in progress on two subtasks which will assess the information which is needed on the effects of fuel pin failure and coolant boiling on the design of LMFBR fuel assemblies.

9. TASK G-10—KINETICS ANALYSIS

9.1 GENERAL

The purpose of task G-10 (Kinetic Analysis) is to contribute to the understanding of fast reactor safety, to develop and use analytical tools to aid in fast reactor safety evaluation, and to provide guidance for effective planning and coordination of experimental programs. Task G-10 is a new designation, covering the same scope of safety analysis and related surveillance as was formally covered under task G. The results of studies provide direction for further development of analytical methods, and form a basis for establishing current design and safety criteria which, in turn, define the range of reactor characteristics and test parameters upon which the experimental programs will focus.

9.2 REACTOR ACCIDENT ANALYSIS

9.2.1 Sensitivity Studies—FREADM

Combined reactor kinetics and calculations are being performed for a 1000 MWe LMFBR to study the effects of sodium voiding dynamics on the work energy released during a hypothetical disassembly accident. Tables of void position versus time are coupled to a multichannel heat transfer calculation, a point kinetics model, and a modified Bethe-Tait calculation to determine the sensitivity of available work energy to the number of channels blocked and the rate of voiding of an individual channel.

The reactor selected for the calculations is a 1000 MWe oxide-fueled fast breeder reactor with a core height of 30 inches and a diameter of ≈ 8 feet.¹ Power distribution and local Doppler coefficients were computed using the two dimensional multigroup synthesis code, BISYN.² Perturbation theory results for the local sodium-in Doppler coefficient were normalized to give $(T \, dk/dT) = -0.006$. Computations of the transients required for the initial stages of the hypothetical accident are being performed with the multichannel accident code FREADM.³ These results are used to set initial conditions for the disassembly calculation in the MARS code.⁴ Isentropic expansion of the fuel vapor to 20°C and 1 atm is assumed in computing available work.

Parabolic fits to the results of precalculated diffusion theory problems have been used for the reactivity effect of local coolant voiding in the FREADM code. The coolant voiding rate, by input tables of upper and lower void boundary position versus time, is initiated in a coolant channel when the local bulk coolant temperature reaches or exceeds 1620°F. Coupling of flow between the various channels is neglected. Accidents are initiated by simultaneous total flow blockage of a fraction of the coolant channels in a cylindrical core region about the core center axis. Voiding is assumed to be symmetrical about the core midplane, with the void volume increasing linearly with

time. Times assumed for voiding the blocked channels are 0.01, 0.1, and 1.0 second. The maximum reactivity worth of voiding a single channel is ≈ 6 cents. Voiding of the axial blanket region is not being considered.

Preliminary results indicate for this hypothetical accident the two cases below:

- Significant violent energy release is obtained only for very low probability accidents (simultaneous flow blockage to $>20\%$ of the core).
- Even if large and very low probability blockages are considered, the energy release is manageable.

For blockages involving a high fraction of the core, the differences in power level between the coolant channels results in an effective time delay for void initiation so that energy release is insensitive to the fraction of channels blocked. This effect is shown in figure 9-1, where are shown the patterns generated for the case of full core blockage and the assumed 0.1 second voiding time per channel.

9.2.2 Effects of Primary Coolant System on Voiding Accidents

Previous analysis of sodium voiding from a reactor channel has shown that the flow resistance of the entire primary coolant system is important in determining the voiding rate and pattern (21st quarterly report, GEAP-5433). In order to calculate the dynamic response of the primary system to accident conditions, each flow path must be described mathematically and the resulting equations solved simultaneously as a function of time. To do this, a subroutine package entitled VOIDUM has been developed and incorporated into the FREADM accident analysis code.³ Typical accidents which can be investigated are pipe breaks, flow coastdowns, power excursions, and flow blockages.

The primary coolant system considered is illustrated in figure 9-2 and is typical of a pot-type LMFBR (future extension of the analysis to loop systems is contemplated). The system is divided into control volumes, and a momentum conservation equation is written for each control volume. Mass continuity equations are written at the interfaces between control volumes. The resulting 48 equations, defining the complete primary coolant system accelerations and pressure drops, are solved simultaneously at each time step during the transient.

The most important assumptions used in the model follow:

- A slug pattern of voiding occurs in each reactor channel.
- Voiding in a channel is initiated when
 - specified fuel temperature limit is exceeded in the bundle, or
 - the required superheat is reached in the channel.

Figure 9-1. Coolant Void Patterns for Total Flow Blockage with Void Time of 0.1 Seconds for a Single Channel

- Once voiding is initiated, the vapor pressure is defined
 - by the saturation pressure of sodium at the insulated cladding surface temperature where voiding is initiated, or
 - as a time-dependent input to the code.
- When the top or bottom liquid slug has voided out of a channel, any further consideration of that channel is neglected (i.e., the phenomenon of liquid re-entry into a channel is not yet treated in the model).
- Coolant pressure drops are caused by frictional losses, entrance and exit losses, orifice losses, elevation differences, and momentum change losses.

To date, two fast reactor accidents have been analyzed with the VOIDUM code. These analyses were done before the heat transfer subroutine had been coupled with VOIDUM; therefore, the analysis is not fully consistent. These preliminary studies are summarized here to illustrate the subroutines capabilities.

The first accident is a "guillotine" failure of the piping connecting one pump with the reactor inlet plenum. The pipe rupture produces a large-scale bypass of sodium into the primary tank. The resulting depressurization of the inlet plenum (figure 9-3) rapidly reduces the flow through the core to about 25% of full flow (figure 9-4). The subsequent propagation of channel voiding is illustrated in figure 9-5, assuming 100°F superheat. Because the time between boiling initiation in adjacent channels is comparable to the time required for the expanding voids to reach the inlet

plenum, the voiding of one channel does not significantly influence the time and location of vapor formation in other channels.

The second accident is core voiding following a power excursion which results in massive fuel pin failure and fuel-sodium interaction. Figure 9-6 shows the pressure response of the system following the generation of high pressure vapor in the peak power channels (because the present state of understanding does not enable one to define the vapor pressure-time relationship in such a channel, the pressure is assumed fixed at 250 psi). The resulting flow response is illustrated in figure 9-7.

Because the time between void initiation in different channels is short relative to the time to completely void a channel, pressurization of the inlet plenum occurs. This, in turn, retards the expulsion of liquid in the upstream direction and results in vapor venting to the outlet plenum. It should be noted that this interaction between channels would not normally be considered in single-channel accident analyses.*

* The above work has been incorporated into the paper "The Effects of Reactor System Design on Sodium Voiding Accidents," by W. G. Meinhardt, D. J. Bender, and P. M. Magee, for presentation at the American Nuclear Society 1969 Winter Meeting.

Figure 9-2. Primary Coolant System

Figure 9-3. Pressure Response in Reactor-Pipe Break Accident

Figure 9-4. System Flow Response—Pipe Break Accident

Figure 9-5. Core Voiding Pattern Versus Time—Pipe Break Accident

9.2.3 Sensitivity Study—FORE-II

A study is in progress, using FORE-II to evaluate the effects of certain parameters on calculated behavior of a reactor. The objective of this work is to determine the effects of possible variations in various physical, nuclear, and mechanical properties on the response of a typical fast reactor to accidents such as large flow transients or reactivity insertions. Parameters varied included reactivity feedback coefficients, fuel specific heat, and fuel-cladding gap conductivity behavior. Results indicate that in some areas, improved data would show significant reductions in calculated accident severity, while in others, more severe accidents are possible.

9.2.3.1 Flow Reductions

The model used to determine the gap coefficient ($\text{Btu/h-ft}^2\text{-}^\circ\text{F}$) between the fuel and the cladding appears to be the largest significant factor encountered in the flow reduction analyses. If a plausible variable gap model is used in which the cladding can grow away from the fuel during the transient and reduce the gap coefficient, the calculated rate of the sodium temperature rise is appreciably lower

than if a fixed gap coefficient model is used. A direct comparison was made of maximum cladding surface temperature following a 67.5% step flow reduction, assuming a fixed gap thermal conductivity in one case and a variable gap model in the other. The latter case resulted in a peak surface temperature some 120°F below that of the fixed gap coefficient case.

In these cases, the calculation included a reactor scram, because the flow reductions were for the whole core, and easily detected. A short series of computer runs were then made for single channel flow blockage, without feedback or scram, to determine whether the choice of the fuel-cladding gap heat transfer model made a significant difference in the available thermocouple response time. The flow reductions were all made in steps. As expected, the coolant temperature rise is less rapid in the variable gap cases than in the fixed gap cases.

In table 9-1 the time to reach boiling in the peak channel is shown for several flow blockages with the gap heat transfer coefficient—fixed (column 3) and with the variable gap model employed (column 2). The difference in the time is shown in column 4.

Figure 9-6. Pressure Response—Power Excursion

Figure 9-7. System Flow Response—Power Excursion

Table 9-1
SINGLE CHANNEL FLOW BLOCKAGES

Time to reach the boiling point as a function of blockage.

Flow Reduction (%)	Time-Variable (sec)	Time-Fixed (sec)	Delta-Time (sec)
100	0.67	0.56	0.11
80	0.92	0.76	0.16
65	2.50	2.01	0.49
60*	2.25	1.76	0.49

* The final equilibrium coolant temperature is 1712°F, which is less than the boiling point. These data are for 1650°F coolant temperature.

It appears that the required thermocouple response time is only increased to about 120 to 125% of the original response time if the variable gap model is correct.

9.2.3.2 Reactivity Insertions

In the case of a 1.25-dollar step reactivity insertion, several parameters have significant effects on the resultant transient:

- The use of new specific heats for the mixed oxide, which are based on the GE Nuclear Materials and Propulsion Operation (NMPO) and ANL measurements for UO_2 , reduces the rate of fuel melting for a given reactivity insertion. If the initial fuel failure mechanism is the pressure pulse caused by the rapid release of entrained gaseous fission products from the melting fuel, calculated fuel failure is delayed by the use of the new specific heats.
- The use of a variable gap model appears to increase the rate at which heat is transferred to the sodium. The oxide heats up faster than the cladding, reducing the gap size, and increases the gap thermal conductivity. The sodium temperature rise is more rapid, and the probability of sodium boiling increases.
- The assumption is made that during the transient, the mixed oxide is restrained from growing axially by the fuel cladding. If the oxide-cladding "bond" is weak in the axial direction during the transient and the oxide can grow axially to some extent because of its own temperature rise, the resultant negative feedback is large enough to cause a considerable reduction in the severity of the transient.
- Use of calculated Doppler coefficients and calculated sodium density coefficients, rather than 80% and 120% of the (respective) values for "conservatism," result in less severe transients.
- The effect of gross radial expansion caused by the structural temperature rise is more important than a 20% variation in the Doppler coefficient. If the fuel channels are free standing and are not clamped together over the active core length, the effective fuel

bundle pitch does not change if the channel wall expands; all that happens is that the sodium between the channels is displaced. In this case, with no gross radial feedback, the transient is far more severe than if there is gross radial core expansion.

Several FORE-II calculations were made using ramp, rather than step, reactivity insertions. The first three cases, 1.25-dollars inserted at 5\$/sec, yielded the same differences due to choice of specific heat and/or gap model that the step insertions indicated. An earlier study⁵ had indicated that the amount of reactivity that could be safely inserted into the core before the control rods take effect was independent of how it was added. As the three ramp insertions were less severe than the corresponding step insertions, it appeared that the assumption may have been in error. Three ramp cases were run at 6.50\$/sec; 1.25\$, 1.45\$, and 1.65\$. The 1.25-dollar case was more severe than when it was run in at 5\$/sec. The other two cases would have caused considerable damage, and were definitely unsafe. There is apparently only a few cents difference between what can be handled safely in a step and in a ramp that terminates before the scram becomes effective.

A final report on this work is being prepared. The following aspects appear to merit greatest attention:

- fuel-cladding interference or restraint in axial direction during transient
- fuel-cladding gap thermal conductivity model
- specific heat of the mixed oxide at high temperatures
- core mechanical design in respect to gross radial core expansion during transient

9.3 MODEL DEVELOPMENT

9.3.1 FREADM Code

The primary coolant loop flow and core voiding mode (VOIDUM) has been combined with the coupled thermo-hydraulics and point-kinetics calculations in the FREADM code. Final checkout of this first version is in progress.

9.3.2 Channel Voiding

Work is in progress on an improved core heat transfer and voiding subroutine to be used in conjunction with VOIDUM (and FREADM). The model of liquid metal bubble generation, growth, and transport conforms as closely as possible to recent physical observations of channel voiding. When the bubble is generated, a thin liquid film remains on the wall. Subsequent bubble growth is caused primarily by evaporation from this film. At the same time, growth is retarded by vapor condensation as the void extends axially into cooler regions. The model calculates the pressure-temperature history of the void as well as the axial distribution of heat transfer from fuel rods. Simplified equations have been developed to describe this behavior, and a computer subroutine has been prepared. Work is progressing on the integration of this subroutine into the VOIDUM package.

9.3.3 Fuel Melting, Slumping, and Freezing

A study has been initiated of fuel melting, slumping, and freezing in a fast reactor under accident conditions.

Particular effort will be placed on creating realistic models which agree with the available in-pile and out-of-pile experimental findings. The objectives of the study are to improve the models being used in the FREADM code and to determine what experimental work might be needed to better understand fuel movement.

The initial effort has been directed at development of a model which accurately predicts the rupture of a fuel pin during a transient as a function of burnup, fuel density, duration of the transient, and cladding temperature.

Heat transfer calculations have been carried out using the general heat transfer code THTD. Using punch card output from THTD, a time share code was written which determines cladding strains for a given fuel melt fraction.

The first set of results obtained shows that the most significant variable is fuel burnup. As an example, at a burnup of 70,000 MWd/Te, the predicted cladding breach was at a melt fraction of 35%, while at zero burnup, the cladding remained intact at 80% melt fraction, although some deformation is expected.

A parameter study of pin failure thresholds for selected accidents is in progress.

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10 TASK L—CLADDING DEVELOPMENT

10.1 GENERAL

The technical and economic feasibility of ceramic-fueled fast reactors depends on the attainment of high performance of core structural members and the fuel system. It depends especially on the survival of the cladding at temperatures and specific powers of practical interest. The present status of testing and analysis of potential core structural and cladding materials, with regard to alloys and environmental variables, indicates good prospects that adequate initial performance of structural members and cladding for fast reactor fuel elements can be obtained by using carefully specified materials within the range of compositions of commercial processing methods. No material, however, has yet been tested under fully typical conditions. Data are not yet available to establish optimum compositions or structures, although some tentative trends have been suggested. This lack of test experience to support the rational selection of preferred material specifications is regarded as a major remaining obstacle to the attainment of the reliable operation of an LMFBR core.

The general objectives of the core materials development program for the next several years are to contribute to the attainment of the desired levels of fast reactor fuel element performance which, in turn, are required by the desired fuel-cycle economics. The objectives of specific activities follow:

- Determine the performance characteristics—including mechanical properties, fracture characteristics, radiation damage, and structural stability—of the most likely cladding material candidates and core structural materials under test conditions simulating the LMFBR environment.
- Investigate the rate of alloy swelling and the environmental and metallurgical factors which influence and control it in LMFBR primary alloys and those which are potential candidates.
- Determine the minimum performance criteria and the quality control techniques to measure performance against these criteria, required to establish recommended specifications for nuclear quality materials for LMFBR systems.
- Perform systematic data analysis of the response of candidate alloys to test environments which simulate reactor applications, on which to base selection of a preferred alloy for near-term LMFBR applications.
- Interpret the in-depth behavior of core materials under simulated LMFBR conditions and recommend specific material applications and design limits for LMFBR use.

10.2 MATERIALS IRRADIATION PERFORMANCE

10.2.1 EBR-II Materials Capsules: Series L-2, L-6, and L-15

The series L-2 (four capsules), L-6, and L-15 materials capsules are at EBR-II in various stages of their scheduled irradiations. The L-2 capsules (mark A-19 type) are in row 4 subassembly (S/A) designated XO50, which is continuing irradiation to early 1970. The target total fluence for these capsules was upgraded to 3.2×10^{23} n/cm² which will require further irradiation beyond that scheduled for XO50. The four L-2 capsules have accumulated 6.9×10^{22} n/cm² total fluence.*

The L-6 capsules (mark B-7 type) were reconstituted into a new row 4 subassembly designated XO18B. These capsules have accumulated a total fluence of 6.1×10^{22} n/cm² and will continue a scheduled irradiation to 1×10^{23} n/cm² in XO18B. The target fluence of these capsules has also been upgraded to 3.2×10^{23} n/cm² which will require further irradiation beyond that presently scheduled for S/A XO18B.

The L-15 materials capsules (mark A-19 type) previously irradiated in S/A XO39 to 1.0×10^{22} n/cm² total fluence are still waiting reinsertion into another mark A-19 subassembly for further irradiation. Target total fluence for the L-15 is 3.0×10^{22} n/cm².

10.2.2 Series L-10 EBR-II Materials Capsules

A description of 10 L-10 capsules was presented earlier (30th quarterly report, GEAP-10028). These capsules were irradiated in row 4 (S/A XO16) of EBR-II for ≈ 175 EFPD to an estimated peak total fluence of 2.8×10^{22} n/cm², at a peak temperature during irradiation of approximately 1750°F.

Iron flux wires from capsules A and C were analyzed for Mn-54 activity, and flux and fluence values were calculated using methods described in reference (1). The reaction cross section was varied to account for axial spectrum changes in neutron flux. The results (table 10-1) confirmed that the axial flux profile was asymmetric about the core midplane as previously shown in gamma scans of L-10 capsules tubes. The flux drop across the subassembly at the midplane was calculated to be 7%. The average total fluence at the midplane for the S/A XO16 was calculated to be 2.4×10^{22} n/cm², 14% lower than the total fluence predicted by EBR-II physics considerations, which yielded 2.8×10^{22} n/cm².

A further study of the heat transfer calculations for the L-10 capsules has been initiated to better define the temperature of irradiation for these materials. The

* Unless otherwise noted, all fluences reported in section 10 are based on EBR-II physics calculations.

Table 10-1
FLUXES AND FLUENCES FOR L-10 CAPSULES A AND C
BASED ON Fe-54 (n,p) Mn-54 REACTION

Distance from Flux Midplane (inches)	Activity on 5/20/69 (10 ⁹ dis/sec)	Reaction Cross Sections for Flux above Stated Energy (mb)			Neutron Flux above Stated Energy (10 ¹⁴ n/cm ² - sec)			Neutron Fluence above Stated Energy (10 ²¹ n/cm ²)		
		>0	>0.1 MeV	>1.0	>0	>0.1 MeV	>1.0	>0	>0.1 MeV	>1.0
Capsule A										
10.5	0.305	6.1	8.0	61	5.4	4.1	0.54	7.9	6.0	0.79
9.0	0.617	8.2	10	68	8.1	6.7	0.98	12	9.8	1.4
6.0	1.58	14	16	73	12	11	2.3	18	16	3.4
3.0	2.64	17	20	74	17	14	3.9	25	21	5.7
0	2.79	19	21	75	16	14	4.0	23	21	5.9
3.0	2.81	17	20	74	18	15	4.1	26	22	6.0
6.0	1.99	14	16	73	15	13	2.9	23	20	4.3
9.0	0.848	8.2	10	68	12	9.2	1.3	16	13	2.0
12.0	0.301	4.8	6.1	57	6.8	5.3	0.57	9.9	7.8	0.84
Capsule C										
0	2.60	19	21	75	15	14	3.8	22	20	5.5

two-dimensional heat transfer analysis considered the effects of axial heat transfer and geometric variations within the capsule. The nodal model extended over one test section including the umbilical tubing on either end. Assuming a heat generation rate of 3.2 W/gm for stainless steel and an emissivity of 0.5 resulted in a maximum calculated temperature of $\approx 1750^\circ\text{F}$ for the core specimens. These calculations also show that the temperature could vary axially by 400°F along the center core specimens. The calculations confirm the earlier hypothesis that the temperature during irradiation was above 1200°F for the L-10 capsules and could have been as high as 1750°F .

10.2.3 Series L-14 EBR-II Materials Capsules

A description of the 19 L-14 capsules has also been presented earlier (31st quarterly report, GEAP-10028-31). These capsules were irradiated in the S/A XO25, which received 8,399 MWd of EBR-II exposure, or approximately 3.1×10^{22} n/cm² total fluence in the peak core position. The total exposure time for XO25 was 187 EFPD (≈ 4500 hours). The post-irradiation examination of these capsules will be completed by the end of January 1970.

10.2.4 MT-3 and MT-4 Materials Capsules

The MT-3 and MT-4 materials capsules were transferred to the FCR Development Program from a previous AEC program at GE under contract AT(04-3)-189,

PA-29. These capsules are of the mark A-19 type and were irradiated in S/A XOA8 to 19,278 MWd in row 4 of EBR-II, which is equivalent to 7.2×10^{22} n/cm² total fluence. The exposure time was ≈ 428 effective full power days at 45 MW, ($\approx 10,280$ hours). After discharge of XOA8, MT-3 was returned to GE-VNC for post-irradiation examination, and MT-4 is being retained at EBR-II for further irradiation to higher fluences in a new mark A-19 subassembly.

Both capsules are identical and each contains 48 tensile specimens of Incoloy 800, Inconel 625 and Hastalloy X fastened to a type 304 stainless steel strip which traverses the entire length of the capsule. Although the capsules were originally designed as vehicles for irradiating mechanical property specimens (flat sheet tensiles); these specimens and the stainless steel strip holder can also be used for swelling studies of cladding. A detailed description of these capsules can be found in an earlier report² describing the MT-1 and MT-2 irradiation results.

10.2.5 Series L-16 Materials Capsules

The new group of materials capsules designated L-16 capsules is being designed to provide quantitative data on the swelling behavior of candidate clad alloys type 304 and type 316 as a function of temperature of irradiation, metallurgical condition, and fluence. Because temperature is known to be a strong variable which influences swelling,

the axial and radial heat transfer aspects of L-16 are undergoing detailed design analysis to ensure proper temperature distributions. Only one heat of each alloy (designated specialty tubing) will be evaluated, heat No. 04200 for type 304 and heat No. M2783 for type 316. This tubing, which includes annealed as well as cold-worked structures, is being characterized in depth.

The capsule design will permit the irradiation to be conducted in the mark A-19 subassembly, or with slight modification, the mark-B19 subassembly. The design concept is directed toward assurance of control of all test parameters, temperature, flux, flux energy spectrum, and fluence, to the highest level of confidence possible within the degree of uncertainty associated with EBR-II operating characteristics.

10.2.6 Cladding Swelling

The objective of this program is to monitor cladding alloy materials and subassembly hardware swelling in available PA-10 irradiated specimens to determine the relationship between swelling and fluence, and irradiation temperature, alloy microstructure, and stress history. The techniques used to measure cladding swelling are liquid immersion density, dimensional analysis, and transmission electron microscopy.

10.2.6.1 Empirical Relationship of Swelling with Temperature of Irradiation and Fluence

Equations relating swelling to irradiation temperature and fluence have been developed by empirical means. The method involved using a computer curve-fitting routine which determines the constants of an equation that best fits the experimental data. Since annealed 304 and 316 stainless have exhibited similar swelling characteristics, all the available data on these two materials are being used in the correlation.

To use the curve-fitting routine, it is necessary to choose an equation form. In the present analysis a form of beta distribution was used because the shape of such a curve was similar to the trend of the data. In using a beta distribution it is necessary to choose a low and upper threshold temperature for swelling. From the trend of data and theoretical considerations, the lower threshold for swelling was estimated at 360°C and the upper threshold at 775°C.

The curve-fitting was done using both linear and nonlinear regression analysis. While the equation form was not linear, it could be made linear by using a logarithmic transformation so that linear regression analysis could be used. By taking the constants determined by linear regression as initial estimates, the nonlinear analysis could be performed. It is felt that the nonlinear analysis produces

an equation that best fits the data, as evidenced by a higher correlation coefficient. The following equations were produced by the two methods:

$$\Delta V/V = 5.4(10)^{-4} (F)^{1.27} (T-360)^{0.603} (775-T)^{0.669} \quad (1)$$

$$\Delta V/V = 7.53(10)^{-5} (F)^{1.62} (T-360)^{0.898} (775-T)^{0.711} \quad (2)$$

where:

$\Delta V/V$ = % volume change

F = fast fluence/ 10^{22} n/cm² ($E > 0.1$ MeV)

T = temperature of irradiation (°C)

Since fluence is the variable presently having the greatest uncertainty, it was thought that a curve-fit using fluence as the dependent variable would be valuable. When converted back to the form of the above equations, the constants were somewhat different, but the equations were still a good representation of the experimental results. With fluence as the dependent variable the following equations were produced:

$$\Delta V/V = 4.6(10)^{-4} (F)^{1.71} (T-360)^{0.629} (775-T)^{0.584} \quad (3)$$

$$\Delta V/V = 3.14(10)^{-4} (F)^{1.81} (T-360)^{0.683} (775-T)^{0.577} \quad (4)$$

Figure 10.1 shows the resultant curves for changes in volume of type 304 and 316 stainless as a function of temperature and fluence as predicted by this equation (3).

10.2.6.2 Immersion Density Measurements

Using a previously described procedure (31st quarterly), immersion density measurements have continued on test sections from the L-10 materials capsules. Initially, measurements have been confined to the tubular test sections irradiated in the peak flux region of EBR-II. Table 10-2 summarizes the density measurements made on L-10 materials to date.

As can be seen from this table, the measured changes in density are relatively small if one considers that the variation in density in the same heat of material has been found to be as great as $\pm 1.5\%$ (31st quarterly). In some cases the conclusion is that there is no measurable density change. With the exception of L-10 A and D, there appears to be a trend of greater density change at the lower temperature. This would seem to indicate that the samples irradiated at 760°C are near the upper end of the temperature range in which swelling occurs. It may be noted that type 304 and 316 stainless irradiated at 660°C to an equivalent fluence exhibited approximately five times as much density decrease.³

Figure 10-1. Change in Volume in Annealed 304 and 316 as a Function of Fluence and Temperature of Irradiation as Predicted by Empirical Equation 4

Table 10-2
IMMERSION DENSITY RESULT FROM FCR CLADDING ALLOY SAMPLES
IRRADIATED AT 2.4×10^{22} n/cm², E > 0.1 MeV.
(L-10 Capsules)

Sample	Material	$\Delta D/D$	Mean Density	% Density Change	Irradiation Temp (°F)
JA4A	I-800 AR	0	7.9450	+0.12	1750
JA4C	I-800 AR	0	7.9516	+0.20	1400
JE4A	316 AR	0	7.9720	-0.03	1750
JE4C	316 AR	0	7.9515	-0.29	1400
JF4A	316 SA	5.2	7.9579	-0.36	1750
JF4C	316 SA	0	7.9379	-0.61	1400
JH4A	304 AR	0	7.9065	+0.16	1750
JH4C	304 AR	0	7.8709	-0.29	1400
JI4A	347 AR	2.8	7.8920	-0.13	1750
JI4C	347 AR	0	7.8747	-0.35	1400
JJ4A	321 AR	4.4	7.8478	-0.45	1750
JJ4C	321 AR	0	7.8336	-0.63	1400
JD4A	316 AR	8.9	7.9286	-0.30	1750
JD4C	316 AR	0	7.9417	-0.13	1400

NOTES:

AR means as received.

SA means Solution Annealed at 2200°F/24 h

JA4A means L-10A, test section, 4, sample A

Mean Fluence is 2.0×10^{22} n/cm² E > 0.1 MeV

Further measurements of density changes of irradiated austenitic stainless steel are in progress. Materials available are L-10 core sections, L-14 test sections, MT-3 capsule materials, and hex flats from EBR-II subassemblies XA08 and XO18.

10.2.6.3 Alpha Cleaning Process

The purpose of developing a process for removing alpha emitters from oxide fuel cladding is to provide irradiated cladding that can be removed from the alpha cell for density change measurement and destructive mechanical testing. A general outline of the process has previously been reported (31st quarterly).

To test the process, the 2-inch sections of fuel pin F2T were mechanically reamed and ultrasonically cleaned and removed from the alpha cell. Samples of this material were taken for residual fuel analysis along with an unirradiated control sample that had been placed in the alpha cell. The levels of residual alpha contamination after mechanical cleaning are shown in table 10-3. Other pieces of cladding were electropolished on the inside surface to remove residual fuel particles imbedded in the wall. Residual fuel analyses of samples after electropolishing are shown in table 10-4. Comparison of these tables indicates that after the electropolishing process, the Pu-239 concentration of fuel cladding has been reduced to the level of unirradiated tubing exposed to the environment of the alpha cell. This

level of contamination is adequate to allow use of this material for immersion density measurements in a beta-gamma cell and probably low enough for destructive mechanical tests, such as ring ductility and burst tests.

Pieces of tubing subjected to the entire cleaning process were also metallographically examined. Figure 10.2 shows a representative sample of reamed and electropolished tubing. Examination showed no fuel imbedded in the cladding and no evidence of intergranular attack on the electropolished surface. Approximately 0.005 inch of cladding was removed from the inside of the tubing wall by the combined reaming-electropolishing process.

10.2.7 Helium Generation

Theoretical differential cross sections for natural iron and nickel were normalized to measured concentrations of helium in commercially pure samples (99.5% wt%) which had been irradiated in EBR-II. The resulting cross sections are lower than other recent measurements^{4 5} but provide good agreement between calculated helium concentrations and independent measurements of helium in stainless steel irradiated in the EBR-II core.

The iron and nickel wires were irradiated in the EBR-II row 2 subassembly XO14 for a 3,674 MWd reactor exposure. Both wires were located 0.5 inch from the flux midplane and received a total fluence of about 1.8×10^{22} n/cm². The concentrations of helium in these wires were

SAMPLE: F2T-14A

NEG. No. A325-05
11.2 mils

250x

CLADDING THICKNESS* (mils)

1. 9.4
2. 10.8
3. 9.2
4. 11.2

SPREAD - 2.0 mils

SAMPLE: F2T-14B

o.d.

i.d.

NEG. No. A327-04
10.0 mils

250x

CLADDING THICKNESS (mils)

1. 10.7
2. 8.9
3. 10.0
4. 9.7

SPREAD - 1.8 mils

*** THICKNESS MEASUREMENTS FROM FOUR MICROGRAPHS, EQUALLY SPACED AROUND THE CLADDING SAMPLE**

Figure 10-2. Typical Photographs of Transverse Cladding Samples (F2T)

Table 10-3
CONTAMINATION LEVELS OF F2T FUEL CLADDING
AFTER MECHANICAL DECONTAMINATION

	F2T-3A	F2T-3B	Control*
Smearable Contamination**			
cpm (a) $\beta\gamma$	20,000	5,000	5,000
dpm (b) α	2,000	<200	<200
Total $\beta\gamma$ cpm/g of S.S.	5.4×10^{10}	3.9×10^{10}	2.5×10^6
Total α dpm/g of S.S.	2.6×10^7	1.6×10^7	1.2×10^4
μCi of Pu-239/g of S.S.	7.6	4.7	3.8×10^{-3}

* Unirradiated equal length of type 347 stainless steel tube from clad supplied by BRDO.

** Smears were taken from a 1/8 inch length and read with a portable survey meter.

a. counts per minute

b. disintegrations per minute

Table 10-4
CONTAMINATION LEVELS OF F2T FUEL CLADDING
AFTER ELECTROPOLISHING

	F2T-14A	F2T-14B
Total $\beta\gamma$ cpm/g of S.S.	9.8×10^7	1.2×10^8
Total α dpm/g of S.S.	4.5×10^4	3.9×10^3
μCi of Pu-239/g of S.S.	8.6×10^{-3}	3.7×10^{-4}

measured using a high-sensitivity gas mass spectrometer system developed at Atomics International.⁶ Absolute uncertainties determined from cumulative uncertainties have been estimated to be < 3%.

The calculated helium concentrations were based on two-dimensional, multigroup neutron fluxes computed at GE-BRDO for the EBR-II reactor. The energy dependence of the (n, α) cross sections are those recommended by Schmidt,⁷ and are based on theoretical calculations of Bullock and Moore⁸ for iron and Büttner, et al.⁹ for Nickel-58. Helium production from impurities of boron and nitrogen was included in all calculations.

Table 10-5 shows both the computed and measured helium concentrations for the iron and nickel samples and also for five type 304 stainless steel samples taken at various axial positions along the hexagonal channel tubing from the EBR-II row 4 subassembly XG05. This channel was irradiated for 12,640 MWd to a peak total fluence of about 5.3×10^{22} n/cm². The agreement shown in table

10-5 for the iron and nickel results was forced by normalization of the cross sections to the measured helium concentrations. Use of these normalized cross sections gives computed helium concentrations in the stainless steel samples which are 10% to 20% lower than measurements for core positions. The differences for stainless steel in table 10-5 are well within the range of values obtained by considering the uncertainties in exact sample position, content of boron and nitrogen, and local and gross flux distributions in EBR-II.

The normalized differential cross sections for iron and nickel were also averaged over a U-235 fission neutron spectrum for comparison with values reported by Ljungberg⁴ and Birss.⁵ Comparison of these values, in table 10-6 indicates that the differential results normalized to helium concentrations may be low. It is expected that consideration of flux depression in the test assembly will improve the comparison. New flux levels in the test assemblies are now being generated, as described below.

Table 10-5
HELIUM CONCENTRATIONS IN IRRADIATED IRON, NICKEL,
and TYPE 304 STAINLESS STEEL

Sample No.	Material	Sample Distance From Flux Midplane (in.)	Weight (mg)	Concentration of ⁴ He (Atomic ppm)	
				Calculated	Measured
LSA	Nickel	0.5 (core)	2.67	14.5*	14.4
LSB	Nickel	0.5 (core)	2.80		14.3 (14.5 ave.)
LSE	Nickel	0.5 (core)	3.35		14.7
LXA	Iron	0.5 (core)	1.49		2.28
LXB	Iron	0.5 (core)	1.74	1.72*	1.73 (1.72 ave.)
LXE	Iron	0.5 (core)	2.32		1.16
HA-2	304	-10 (blanket)	2.24	1.94	3.82
HA-5	304	- 4 (core)	1.62	7.5	8.66
HA-6	304	0 (core)	2.64	8.9	10.19
HA-9	304	+6 (core)	3.49	5.6	6.75
HA-14	304	+15 (blanket)	1.93	0.90	0.78

* Agreement shown between calculated and measured values was forced by normalization of cross sections to measured helium concentration.

Table 10-6
SPECTRUM AVERAGED (n,a)
CROSS SECTIONS FOR Fe AND Ni

	σ (N,a), (mb)	
	Fe	Ni
U-235 Fission Spectrum		
Normalized Differential Data	0.21	3.10
Ljungberg's Values	0.4	4.1
Birss' Values	0.23	4.5

10.2.8 Neutron Flux

Several sets of calculated EBR-II fluxes, for a 91 subassembly core, are being evaluated and compared with dosimetry measurements.^{1,10} The calculations were performed by the following organizations using the indicated techniques:

1. GE-BRDO DOT-2DB,¹¹ a 10-energy-group transport-theory analysis in two-dimensional RZ geometry.
2. NSP** 2DF,¹² a 16-energy-group transport-theory analysis in two-dimensional RZ geometry.

* Mynatt, F. R. "A User's Manual for DOT: A Two-Dimensional Discrete Ordinates Transport Code with Anisotropic Scattering," Oak Ridge National Laboratory (K-1694), in draft form.

** Nuclear Systems Programs, Cincinnati, Ohio

3. NSP a 40-energy-group Monte Carlo Evaluation.¹³
4. ANL DIFF-2D,¹⁴ a 22-energy-group diffusion theory analysis in two-dimensional RZ geometry.

Figures 10.3 and 10.4 show the integral flux per MW, in rows 2 and 4 of EBR-II, respectively. The calculations are in good agreement with each other. Also shown in these figures are integral fluxes greater than 1 MeV which were derived from GE-BRDO dosimetry data. The dosimetry results are $\approx$ 5% to 10% below the calculations. This difference may be accounted for by flux depressions which exist in nonfueled experimental subassemblies.

Work is continuing to determine energy dependent flux levels for GE-BRDO experimental subassembly positions in EBR-II through the correlation of experimental data and detailed physics calculations. Local flux depression in the experimental subassemblies is included as a significant effect in this analysis.

10.3 MATERIALS MANAGEMENT

The materials management program is charged with the responsibility of assuring that the highest quality material (consistent with need) is provided for cladding of LMFBR fuel. The program has concentrated heavily on the detailed characterization of tubing. The program is aimed toward the establishment of a basis for assessing the capability of industry to provide a quality of tubing that

Figure 10-3. Integral Flux Per MW in Row 2 at Midplane for EBR-II - 91 Subassembly Core

Figure 10-4. Integral Flux Per MW in Row 10.4 at Midplane for EBR-II - 91 Subassembly Core

exceeds standard ASTM specifications. This will permit experimenters to work with well-characterized tubing materials.

10.3.1 Quality Control and Vendor Assessment

The quality characterization of the two groups of annealed seamless cladding tubes of austenitic stainless steel currently being used on PA-10 programs has been incorporated in a topical report¹⁵ slated for distribution early in 1970.

A comprehensive mechanical properties test program was completed on the nine heats of Assessment-of-Vendors tubing and the five KO666-G tubing heats. Tube tensile tests were performed at room and elevated temperatures (700°, 900°, 1100° and 1300°F). The room temperature mechanical properties data showed extremely close agreement with similar data provided by the several vendors and readily conformed to the properties range allowed by GE-BRDO. The mechanical properties at elevated temperature are somewhat better than current specifications and suggest that an upward adjustment of 5,000 to 10,000 psi in yield and ultimate strengths might be prudent. A summation of the tensile data collected on the five alloys comprising the KO666-G order is shown in figure 10.5. Note that the room temperature yield and ultimate strengths on all materials lie well within that specified.

10.3.2 Characterization of Specialty Tubing

Complete quality characterization of the eight lots of recently procured "specialty tubing" has been initiated concurrent with its utilization in fast reactor irradiation programs. This specialty tubing, so-called because of extremely close fabrication and handling control, consists of approximately 1600 feet of type 304H and a like quantity of type 316H. The material was provided in four degrees of cold work—10%, 20%, 30%, and 50%—as measured by the percent reduction in cross-sectional area occurring on the final draw pass. The procedure followed in the characterization of this material is based on that established for the quality assessment of the KO666-G tubing which was discussed in detail in an earlier report (28th quarterly report, GEAP-5700).

10.3.3 Tube Defect Study

10.3.3.1 Short-Term Testing

A number of thin-wall tubes of seamless type 316H stainless steel containing machined defects were burst

tested to corroborate earlier data on welded-and-drawn tubing. In that work (27th quarterly report, GEAP-5677), it was shown that tubes containing surface defects running in the axial direction invariably failed at and along the defect during short-term pressure testing at 1300°F. To assure that this mode of failure was not caused by, or influenced by, the presence of the seam weld, seamless tubes with similar defects ranging in depth from 5% to 20% of the wall were tested under identical conditions. Of the six defected tubes so tested, all failed at the defect. Further observed was the fact that all tubes with defect depths greater than 10% of the wall failed cleanly, with little deformation, in a lenticular pattern along the defect. Two tubes with defect depths less than 10% of the wall failed catastrophically, with severe transverse tearing. The appearance of the failure regions in these specimens was identical to those of the control specimens. All defects were oriented longitudinally, along the tube axis, and were one-eighth inch long.

The effect of such defects as described above on the 1300°F hoop strength of seamless 316H was found to be small: a maximum reduction of not more than 8 percent from average control values was measured.

These data provide clear and ample support to the work and conclusions previously advanced (27th quarterly) and confirm the relative acceptability of both welded-and-drawn tubing and seamless tubing in applications in which high strain rate loadings are anticipated.

10.3.3.2 Long-Term Testing

One of the principal reasons for out-of-pile testing of tubing containing surface defects is to provide empirical data that will allow establishment of a realistic reject basis for cladding tubes. This requires not only that we test defects representative of, or correlatable to, the type of defect encountered in service, but that we apply as closely as possible the type of loading that an actual tube experiences as a fuel cladding. That loading pattern is best simulated by long-term low-strain-rate conditions such as those imposed in stress-rupture testing.

A series of specimens containing artificially-produced longitudinal defects was prepared for stress-rupture testing. The specimens were selected from tubing that had been characterized in the earlier quality assessment program to assure that the magnitude of such critical variables as local wall thinning and eccentricity could be minimized. The initial test program, consisting of 400-, 1000-, and 4000-hour runs at 1300°F, is designed to provide the empirical guidance necessary for planning a large-scale test program. The experimental phase of this work is underway.

Figure 10-5. Tensile Properties of K0666-G Tubing - Annealed

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11. TASK N—CORE PHYSICS ANALYSIS

11.1 GENERAL

The purpose of task N is to contribute to the development of improved physics computational tools for the LMFBR program. Task N is a new designation, covering the same scope of neutronic analysis and code development which has been carried out as part of task G. To accomplish this work, analytical methods and computer codes are specified and written, and evaluations are performed on the basic nuclear data. Progressively improved methods and data are used in analytical studies to determine their effects on LMFBR neutronics, design, fuel costs, and the reactivity aspects of reactor safety. The results of these studies, together with results from associated reactor design and development activities, provide direction for further development of physics analytical methods and form a basis for establishing current design and safety criteria.

11.2 COOPERATIVE EVALUATION

Cross sections for U-238, Pu-239 and Pu-240 which are based upon the current GE 60-group data files have been put into the ENDF/B format. These cross sections are essentially those used with the CROFO4 code, although testing has shown small differences when the group values are generated from the pointwise ENDF/B format. The unresolved resonance parameters for Pu-239 are those which have been generated for ENDF/B, version 2, and are described below. They produce essentially the same average cross sections as in the current GE 60-group file.

The ENDRUN code is being used to generate a 60-group, generalized cross-section set based on these data and the ENDF/B version 1 data for other materials. This file will be used in the evaluation of the effects of group structure, flux weighting, and interpolation methods on the

accuracy of the rapid cross-section adjusting technique, as well as used to provide a file for general reactor computations.

Evaluation of the Pu-239 neutron cross section data in the unresolved resonance region was completed and a set of recommended values sent to the National Neutron Cross Section Center for version 2 of the ENDF/B nuclear data file. This new evaluation was undertaken to include recent σ_f and alpha measurements and to include higher order angular momentum states, specifically, p-wave parameters. The unresolved region extends from 300 eV to 25 keV. Special emphasis was placed on the region from 1 to 3 keV, where higher resolution was found desirable.

The following procedure was used to derive pointwise parameters from the evaluated cross sections:

- A p-wave strength function of $1.5 \times 10^{-4} \text{ eV}^{-1/2}$ was chosen with associated parameters from Otter¹ and from extending Greebler's² s-wave parameters. These parameters are listed in table 11-1.
- The s-wave strength function was adjusted to give Uttley's³ σ_T values when combined with the p-wave contribution. The reduced neutron widths, listed in table 11.2, were taken to vary with energy and were obtained from the formula:

$$\langle \Gamma_n^0 \rangle_{\ell J} = S_{\ell} \langle D \rangle_{\ell J}$$

where:

S_{ℓ} = Strength Function
 $\langle D \rangle_{\ell J}$ = Mean Spacing

- The energy dependent $\langle \Gamma_f \rangle_{\ell=0}^{\ell=9}$ was adjusted to give an "eye-average" of experimental alpha and σ_f

Table 11-1
UNRESOLVED RESONANCE PARAMETERS FOR Pu-239
FOR USE IN ENDF/B VERSION 2

ℓ	J	(eV)	Γ_f (eV)	Γ_{γ} (eV)	Fission Degrees of Freedom	Neutron Degrees of Freedom
0	0	8.78	1.5	0.0416	2	1
0	1	3.12	energy dependent	0.0416	1	1
1	0	8.78	0.0	0.0416		1
1	1	3.12	1.0	0.0416	2	2
1	2	2.123	1.0	0.0416	3	1

NOTES:

p-wave strength function = $0.15 \times 10^{-3} \text{ eV}^{-1/2}$

s-wave strength function = energy dependent

Penetration factor constant = 0.001985

- values, using the constant parameters described in step (a) and the reduced neutron widths generated in step (b). Alpha measurements were obtained from preliminary data of the U.K., of Gwin at ORNL, and of Czirr at LRL with higher energy alpha values from de Saussure.⁴ Experimental values of σ_f from James,⁵ Shunk,⁶ and Patrick,⁷ with preliminary values from Gwin⁸ were also considered. Table 11-2 lists the resulting values of $\langle \Gamma_f \rangle_{j=1}^{l=0}$.
- (d) In the region from 1 to 3 keV, where detail is particularly important, higher resolution was used in determining $\langle \Gamma_f \rangle_{j=1}^{l=0}$. Preliminary data with energy resolution of 1/8 lethargy unit, or less, were obtained from both R. Gwin of ORNL and B. Czirr of LRL. These sets were normalized to the evaluated

alpha average values (over 1 keV ranges) and used to obtain a more detailed description in this region. There were some indications that finer variations in the strength function should be considered; however, the finer resolution Uttley data was not available.

- (e) Comparison of the resulting computed σ_f with experimental values is shown in figure 11-1.

The 30 pointwise values of $\langle \Gamma_n^0 \rangle_{j=0,1}^{l=0}$ and $\langle \Gamma_f \rangle_{j=1}^{l=0}$ given in table 11-2 are the exact values which will be used in ENDF/B, version 2. Since the present ENDF/B format does not allow energy dependent reduced neutron widths, two sets of Pu-239 cross sections will be available, one with constant values of $\langle \Gamma_n^0 \rangle_{j=0,1}^{l=0}$ and one with variable values in the approved new format. Additional smooth data will be necessary if the constant $\langle \Gamma_n^0 \rangle_{j=0,1}^{l=0}$ is used, to arrive at the evaluated cross sections. It is anticipated that no smooth background will be necessary with the variable reduced neutron widths.

Table 11-2
ENERGY DEPENDENT PARAMETERS

Energy (eV)	$\langle \Gamma_f \rangle_{j=1}^{l=0}$ (eV)	$\langle \Gamma_n^0 \rangle_{j=0}^{l=0}$ (eV $\times 10^{-3}$)	$\langle \Gamma_n^0 \rangle_{j=1}^{l=0}$ (eV $\times 10^{-3}$)
300	0.0191	1.137	0.404
310	0.0081	1.096	0.389
340	0.0084	0.936	0.333
365	0.0667	0.845	0.300
475	0.3253	0.581	0.207
550	0.0550	1.688	0.600
580	0.0352	1.649	0.586
610	0.0063	0.716	0.255
665	0.0063	0.711	0.253
725	0.0290	0.730	0.259
825	0.0440	0.734	0.261
870	0.0250	0.724	0.257
930	0.0610	1.223	0.435
975	0.0660	1.172	0.417
1050	0.0800	0.966	0.343
1075	0.0466	0.800	0.284
1225	0.0436	0.831	0.295
1325	0.2121	0.847	0.301
1550	0.0156	0.882	0.313
2000	0.0131	0.939	0.334
2100	0.0001	0.942	0.335
2200	0.0007	0.944	0.335
2500	0.0202	0.939	0.334
2800	0.0511	0.924	0.328
3500	0.0210	0.935	0.332
6500	0.0166	0.966	0.343
8500	0.0521	0.944	0.335
10000	0.0340	0.869	0.309
17000	0.0330	0.838	0.298
25000	0.0689	0.818	0.291

11.3 COMPILATION AND DISTRIBUTION OF EVALUATED DATA

A representative from GE-BRDO participated in the meeting of the Cross-Section Evaluation Working Group. September 24-26, and the task force for the reevaluation of fissile and fertile isotopes. Activities associated with these groups included the reevaluation of Pu-239 unresolved resonances (described above) and review of the proposed ENDF/B procedures manual and data testing.

11.4 SUPPLEMENTARY ANALYTICAL INTERPRETATION OF INTEGRAL DATA

Discussions were held on October 9, with personnel from Argonne National Laboratory concerning the program for fast critical experiments in ZPR-3, ZPR-6, ZPR-9, and ZPPR. This meeting was informative regarding the capabilities of these assemblies and provided a means for stressing the importance of resolving problems encountered in the analysis of these critical experiments.

11.5 IMPROVEMENTS IN FUEL CYCLE AND MANAGEMENT SCHEMES

The fuel cycle code FUMBLE has been used to carry out startup computations for a representative fast reactor. The startup approach to equilibrium was based upon six successive operating cycles with different fuel exposures, plant load factors, and operating times. In addition, the core volume was reduced after the third cycle to account for a planned increase in the fuel specific power. FUMBLE computations of the average core fissile enrichment, by batch, for the six operating cycles are given in table 11-3.

The startup computations included a checkout of the convergence options of the FUMBLE code based upon an adjustment of the beginning-of-cycle enrichments, the operating time, and the flux level. The convergence criteria

Figure 11-1. Fission Cross Sections In The Unresolved Resonance Range

Table 11-3
FUMBLE CODE CHECKOUT -
CORE FUEL ENRICHMENTS, BY BATCH,
FOR THE FIRST SIX OPERATING CYCLES OF A REPRESENTATIVE FAST REACTOR STARTUP SCHEME

Operating Cycle	Discharged Fuel Exposure (MWd/Te)	Average Power (MWt)	Plant Loan Factor	Operating Time (yr)	End of Cycle (k_{eff})	Core Volume* ($cm^3 \times 10^6$)	Av Core Enrichment (% Pu-239+Pu-241)**			
							Batch 1		Batch 2	
							Beginning of Cycle	End of Cycle	Beginning of Cycle	End of Cycle
1	33222	459	0.475	1.6	0.983	1.32	16.72	15.38	16.72	15.38
2	65583	459	0.750	1.07	0.981	1.32	15.38	14.28	18.56	16.90
3	74979	458	0.800	1.2	0.976	1.32	16.90	15.37	17.72	16.02
4	83829	456	0.80	1.2	0.978	1.204	16.02	14.49	19.08	16.60
5	95282	453	0.80	1.15	0.978	1.089	16.60	14.75	19.21	16.79
6	95256	453	0.80	1.15	0.979	1.089	16.79	14.95	19.07	16.62

* The core volume was reduced, in two steps, between the fourth and sixth operating cycle by replacing the discharged outer core fuel bundles with blanket fuel.

** Two fuel batches were used for all core zones. The source of the loaded fissile plutonium was discharged BWR fuel.

were ± 0.005 for the specified end-of-cycle multiplication constant, ± 2000 MWd/Te for the specified discharge fuel exposure and $\pm 2\%$ for the average power over the refueling interval. A FUMBLE option which permits the user to specify the relative power distribution of the zones for each operating cycle was also checked. The cost of these fuel cycle computations was about \$15 on the GE 635 computer.

Output from the FUMBLE code includes fuel cycle costs, fuel masses (weights) by zone, power distributions, and the important fuel cycle parameters (breeding ratio, doubling time, etc.) as well as the enrichments indicated in table 11-3. The code will permit the user to determine optimum approaches to the equilibrium fuel cycle and will disclose unrealistic fissile enrichment requirements or unsuitable power mismatches between fuel batches of a given zone.

Checkout and modifications to the FUMBLE code are continuing in order to improve the available output edits and to test the remaining important code options.

11.6 IMPROVEMENTS OF EXISTING METHODS FOR GENERATING GROUP CONSTANTS

Specifications for an improved version of the ENDRUN code, ENDRUN-2, are nearing completion and programming of the modifications has begun. Three major changes already incorporated will improve the code considerably. These are the following:

- new input format
- completely revised resolved resonance calculation
- additional capabilities in the handling of unresolved resonance data

The new input format (free-form type) should prove a great advantage to the user. The input deck for version 1 had been expanded as the code evolved and, as now documented, includes up to 21 different card types, over 10 different fixed formats, with complicated checks and semirepetitious input indices. To reduce this problem, the following major improvements have been made in the input for ENDRUN-2:

- Each card begins with an alpha-numeric heading which clearly specifies the information contained.
- Card headings are checked internally so that cards may appear in any order.
- An input check is automatically performed and warns the user of any apparent inconsistencies.
- An input deck submitted without file cards will only be edited and checked with no calculations performed. The new input has been programmed and checked out.

The resolved resonance averaging procedure performed in ENDRUN-1 has been thoroughly tested but has proven to be sensitive, in certain cases, to input choices, resulting from "over-flexibility." In ENDRUN-2, this averaging procedure has been replaced by an extrapolated Romberg integration technique. Although the user no longer specifies exactly the point at which calculations are performed, he does have the capability of changing the accuracy level and of inputting the number of coarse and fine groups. This new integration scheme has been programmed and checked out and has produced more accurate results at substantially less cost.

Unresolved resonance calculations are performed in ENDRUN-1 by averaging over a series of hypothetical resonances, representing chi-squared distributions of

Breit-Wigner parameters about the given average values. In version 1, this averaging was performed at up to 20 energy points which were automatically spaced at equal lethargy intervals over the unresolved energy range. Furthermore, only the fission width, $\langle \Gamma_f \rangle$, was allowed to be energy dependent in the ENDF/B file, and special input was required to provide the option of energy dependent $\langle \Gamma_n^o \rangle$ values. As ENDF/B materials have been updated, particularly Pu-239 (see subsection 11.2), this method of processing has proven limiting because rapidly varying strength functions cannot be treated correctly. Without changing the basic numerical techniques, several limitations has been lifted in ENDRUN-2. These are the following:

- Calculations may be made at up to 30 points, instead of 20.
- The energy points are individually specified as input, instead of being automatically spaced.
- The new LRF=2 type data on ENDF/B can be processed, thus allowing any or all parameters to be energy dependent.

These changes have been programmed and are in the process of being checked out.

Testing of the rapid cross-section adjusting technique in 60 groups necessitates the consideration of elastic scattering transfer across groups. Both the ENDRUN-1 and TDOWN-1 codes were modified to include an elastic

scattering matrix. A simple recipe is used in ENDRUN-1 to determine the probabilities for scattering from each coarse group to those below it. These probabilities are then used by TDOWN to compute the matrix by multiplying by the appropriate elastic removal cross section.

Corrections have been made to the TDOWN-1 code and specifications for inclusion of zero and one-dimensional diffusion theory solutions in the second version were completed. Correction of an error in the elastic removal cross section adjustment slightly improved agreement with results using the MC²-MACH-1 codes for ZPR-3, assembly 48.

11.7 CODE PACKAGING AND DOCUMENTATION

Topical reports describing the DOT2DB⁹ and BICYCL¹⁰ codes have been completed and are being prepared for submittal to the AEC. A topical report on the sensitivity of fast power reactor calculations to some computational approximations has been completed and is in reproduction.¹¹

The BISYN code, including read and write routines in FORTRAN as well as more efficient versions in GE 635 and UNIVAC 1108 machine language, have been sent, along with necessary documentation, to the Argonne Code Center. A topical report describing the perturbation module for the BISYN code¹² has been sent to the AEC for review.

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12. TASK P—FUEL ELEMENT DESIGN ANALYSIS

12.1 GENERAL

The objective of task P is the development of an integral fuel lifetime model. This modeling and analysis effort relates to other portions of the FCR Development Program in the following manner:

- Guidance is provided for fuel and cladding irradiations.
- Assistance is provided for the analysis and interpretation of experimental results from fuel and cladding irradiations.
- A performance and failure model will be developed for use in parametric design studies of the sodium cooled breeder reactor.

The Fuel Element Design Analysis Task is a new task under PA-10. This effort is being reported separately for the first time in this quarterly report, although it should be noted that this type of work has been going on within PA-10 for almost three years. For the benefit of the reader, these related activities are listed and categorized in table 12-1 along with a notation of the pertinent progress reports. An overview of this table reveals that a generally balanced program has already been underway except for the integral performance and failure modeling. Better understanding of the fuel performance modes is rapidly developing, and increased future effort on the fuel lifetime model and code development is therefore appropriate.

12.1.1 Planned Approach

The general approach within task P is to develop a realistic model of fuel performance and failure. This will be accomplished by parallel and continuous activities of data analysis of experimental fuel irradiations and by theoretical studies that explain relevant aspects of observed fuel performance. An integral performance and failure model will evolve as understanding of fuel performance and failure modes develops. An appropriate computer code will be developed for fuel design and other related uses.

12.2 EXPERIMENTAL DATA ANALYSIS

As part of this subtask, detailed data will be correlated for the task F fuel irradiations. The fuel pin profilometry, microstructure, and gas release observations will be correlated to fabrication parameters and operating variables. Variables considered include burnup, linear power, fuel density, cladding restraint (cladding thickness, temperature, and material) and stoichiometry. The swelling data from the Fast Ceramic Reactor Development Program will be compared with all available data (DFR, PNL, ANL, etc.) and differences will be analyzed.

In all of the data analysis, emphasis will be placed on relevance to fuel element modeling, and correlative

equations which most closely reflect physical relationships will be used instead of strictly empirical correlations. Where possible, the relevance of data uncertainty will be factored into the analysis.

The adequacy of current experimental techniques (e.g., pin profilometry) will be reevaluated as appropriate, and improvements to such procedures will be recommended. Work on this task had been reported under task G.

12.2.1 Fast Flux Fuel Pin Profilometry Analysis

The bank of data from which correlations are currently made consists of 22 pins: 15 F-2 pins, 6 F-1 pins and pin FOE. Thus, data is available for correlation and analysis covering a range in burnup from 2000 to 74,000 MWd/Te* and linear powers from 5 to 17 kW/ft* over a wide range of fuel densities, cold gaps, and other variables.

12.2.1.1 Analysis of Local Pin Diameter Increases

Exploitation of computer produced plotting permitted the production of several hundred graphs illustrating relationships between two or more variables. Some of the more noteworthy correlations found are discussed below:

- (a) The profilometer traces of certain pins are not smooth traces as one would expect if swelling were primarily related to local linear power or local cladding temperature. In addition to the ridging effect of the cladding which is apparently associated with the pellet interfaces, there are "regional" profilometry fluctuations extending over axial distances of 1 to 3 inches. These fluctuations may be related to those fabrication parameters which similarly fluctuate on such a regional basis, namely the as-fabricated pellet density and fuel-cladding gap.

To test this hypothesis, the profilometry data was examined on each pin for the mid range of the fuel where the power and burnup were known to be constant to within $\pm 5\%$. Correlations were found between fuel pin local diameter change (ΔD) and local fuel pellet density for pin F2Q and between ΔD and the initial fuel-cladding diametral gap (cold gap) in pin F2C. These relationships appear in figures 12-1 and 12-2. Similar correlations between these sets of variables, however, were not found for any other pins, leaving the interpretation of the results in doubt.

* Linear power and burnup values are based on EBR-II predictions (See section 7 for discussion of uncertainty in those values).

Table 12-1
SUMMARY OF PREVIOUSLY REPORTED FUEL ELEMENT DESIGN ANALYSIS

			ANALYSIS CATEGORY																		
PREVIOUSLY REPORTED FUEL ELEMENT DESIGN ANALYSIS	Task-G	Task-L	REFERENCED QUARTERLY PROGRESS REPORT AND TASK DESIGNATION	<div>Experimental Data Analysis</div> <div>data correlation</div> <div>understanding & improvement of data taking</div> <div>guidance of experimentation</div> <div>Studies of Performance and Failure Modes</div> <div>fuel swelling studies</div> <div>cladding swelling studies</div> <div>cladding response studies</div> <div>fuel cladding interaction</div> <div>sensitivity to material properties</div> <div>sensitivity to fabrication</div> <div>sensitivity to operation</div> <div>comparison to experimental observation</div> <div>Integral Performance and Failure Modeling</div> <div>critical mode evaluation</div> <div>lifetime code development</div> <div>parametric studies</div>																	
Categorizing cladding stress and strain patterns		19																			
Preliminary unrestrained fuel swelling estimates based on Bettis plate data	20,21																				
Importance of cladding strain analysis and design criterion	20																				
Predicting cladding stress from a deformation rate in bending stress patterns	20																				
Thermal stress in cladding due to circumferential temperature gradients	21																				
Multi-axial creep equations relating cladding stress, strain, and creep exponent	21																				
Analysis of barreling cylinder ring ductility test	22,26																				
Determination of cladding strain from profilometry data	22,23																				
Importance of fuel plasticity in a fission gas bubble growth model	23																				
Fuel growth due to fission gas bubble nucleation and growth including effects of viscosity	24																				
Nonuniform deformation of cladding under hydrostatic pressure due to specimen geometry changes	24																				
Development and application of microsurveying	25																				
Preliminary analysis of F2 profilometry data (to 57,000 MWd/Te)	27																				
Ductility penalty due to nonuniformity of cladding thickness	27																				
Cladding creep strength, determination of	28																				
Cladding ductility and the effect of specimen support	28																				
Refinement of fission gas bubble growth model including coalescence and movement to the grain boundary	29																				
Development of statistical treatment of profilometry data	29,30																				
Cladding swelling data correlation	30																				
Parametric studies using fuel swelling model and code	30																				
Cladding loading study and cumulative strain damage index as a rational approach to fuel lifetime expectation	30																				
Use of cladding swelling data in structural design	31

Figure 12-1. Fuel Pin Diameter Change Plotted Against Fuel Pellet Density - Pin F2Q (58,000 MWd/Te)

Figure 12-2. Fuel Pin Diameter Change Plotted Against Cold Fuel-Cladding Gap, Pin F2C (71,000 MWd/Te)

- (b) Plots of ΔD versus linear power for individual pins indicated that for four out of the sixteen pins with significant burnup, more swelling occurred at the bottom (colder) end of the pin than at the top (hotter). A particularly striking contrast is found between F2Q, in which the ΔD trace peaks ≈ 4 inches from the bottom of the pin, and F2S, in which ΔD peaks ≈ 10 inches from the bottom. (See figure 12-3 in which the local "ridging" has been purposely left out.) The major differences between the two pins are that F2Q had 15-mil cladding and the fuel was not axially constrained while F2S had 30-mil cladding and the fuel was axially constrained. It might be hypothesized that the axial restraint at the top of the fuel column in F2S prohibited axial fuel movement in the vicinity of the free surface. This would account for the greater radial swelling in the upper portion of F2S. However, other similarly axially restrained pins did not exhibit this pattern. If the proximity of a free axial surface is important in the prediction of pin diametral increase, it may partially explain the difference in performance of the short pins irradiated in a thermal flux and the long pins irradiated in a fast flux.
- (c) An empirical equation developed previously (see equation 7-5, 30th quarterly, GEAP-10028) for predicting swelling of types 304 and 316 stainless steel was used to calculate the pin diameter increase which would be attributed to cladding swelling alone. This calculation was then compared to pin swelling actually observed in the 15 F2 pins and FOE. The calculated cladding swelling appears to account for virtually all of the observed diametral increase in 5 of the 16 pins (see figure 12-4). These five pins were of either low linear power at the peak point (≈ 10 kW/ft) or low fuel density ($\approx 85\%$). If this interpretation is correct, it can also be inferred that type 347 stainless cladding swells an amount identical to that of type 316. The only pin with Incoloy 800 cladding, pin F2C, had an observed ΔD lower than that predicted by the equation. This may indicate that at these temperatures, I-800 swells substantially less than types 304 or 316, an observation at variance with some high temperature swelling data on I-800 previously reported in subsection 8.2.3.3 of the 27th quarterly report (GEAP-5677).
- (d) Graphs were plotted of fuel pin ΔD versus burnup in restricted ranges of cold gap and fuel pellet density (see figure 12-5). A set of such graphs with both cold gap and pellet density split into several increments forms a matrix of two-dimensional graphs, permitting consideration of the effects of three independent variables (in this case, burnup, cold gap, and pellet density) on ΔD . A fourth independent variable, the pin with which each pellet is associated, is denoted on the graphs by means of a letter or number. Generally,

graphs such as the ones shown in figure 12-5 indicate a reduction of ΔD with increasing cold gap and/or fuel porosity, but with a fair degree of scatter in the data.

- (e) Sets of graphs similar to the set described above were produced, this time plotting the observed fuel pin ΔD minus the ΔD calculated as being caused by cladding swelling versus linear power (see figure 12-6). This set of graphs is useful in testing the hypothesis that all the observed diametral increase is attributable either to the thermal interference between fuel and cladding at the initial startup or to swelling of the cladding. If the hypothesis were correct, a high degree of correlation should be found on this type of graph. The large amount of scatter in the results is evidence that the hypothesis is inadequate, although it could alternatively be an indication of incorrect accounting for cladding swelling.

12.2.1.2 Concluding Remarks Several observations made in the course of this analysis are at odds with interpretations made in a less detailed analysis reported in subsection 7.5.2 of the 27th quarterly report.

- The assertion that cladding thickness has an effect on cladding deformation appears now to be lacking in actual evidence, although the case has yet to be proven.
- No difference in pin swelling has been found in the present study between pins clad with type 316 stainless steel and those clad with type 347 stainless steel.
- A "secondary peak" in swelling at a linear power of ≈ 10 kW/ft observed in the previous work was not found in this much more detailed study.

In summary, based on the analysis of the profilometry for these pins with burnups up to 74,000 MWd/Te the following statements are made:

- (a) Fuel pin diameter change cannot, in general be well correlated as a simple function of only one other variable as was attempted with cold gap and fuel density, even when other variables are relatively constant.
- (b) The cladding swelling equation used predicts an amount of pin diameter increase equal to the full amount of pin deformation measured for five low-fuel-density or low-linear-power pins.
- (c) On the basis of interpretation of only fuel pin data, types 316 and 347 stainless steels appear to swell to the same extent. Incoloy 800 appears to swell considerably less than types 316 and 347. Both these observations are at odds with previously reported data (see subsection 8.2.3.3, 27th quarterly report).
- (d) Increased cold gap and decreased fuel density both correspond to a reduced cladding deformation.

PIN F2Q

PEAK BURNUP	58,000 MWd/Te
SMEAR DENSITY	94.9% TD
FUEL-CLADDING GAP	0.8 - 1.7 mils
O/M	2.00
CLADDING	S.S. 347, 15 mil, 1025°F (max)
AXIAL RESTRAINT - NO

PIN F2S

PEAK BURNUP	61,000 MWd/Te
SMEAR DENSITY	96.1% TD
FUEL-CLADDING GAP	0.5 - 1.4 mils
O/M	2.00
CLADDING	S.S. 347, 30 mil, 1045°F (max)
AXIAL RESTRAINT - YES	

Figure 12-3. Pin Diameter Change versus Local Linear Power.
Pin F2Q Compared to F2S.

Figure 12-4. Pin Diametral Increase Compared to Amount Predicted by Clad Swelling for Four Low Fuel Density or Low Linear Power Pins

LEGEND:

A-F2A
 B-F2B
 C-F2C
 E-F2E
 H-F2H
 N-F2N
 P-F2P
 Q-F2Q
 R-F2R
 S-F2S
 T-F2T
 U-F2U
 W-F2W
 Y-F2Y
 Z-F2Z
 0-F0E
 1-F1A
 2-F1B
 3-F1C
 4-F1D
 5-F1E
 6-F1F

Figure 12-5. Diametral Increase of Task F Pins versus Burnup in Restricted Regions of Cold Gap and Linear Power

LEGEND:

A-F2A
B-F2B
C-F2C
E-F2E
H-F2H
N-F2N
P-F2P
Q-F2Q
R-F2R
S-F2S
T-F2T
U-F2U
W-F2W
Y-F2Y
Z-F2Z
0-F0E
1-F1A
2-F1B
3-F1C
4-F1D
5-F1E
6-F1F

Figure 12-6. Local Pin Diametral Increase Minus Calculated Clad Swelling versus Local Linear Power

- (e) The hypothesis that all observed diametral increase is caused either by fuel-cladding interference on startup or by cladding swelling is not supported by the data.
- (f) There is not a pronounced peak in swelling at a position on the fuel pin corresponding to a linear power of 10 kW/ft.
- (g) There are no apparent differences in ΔD between pins with type 316 and 347 stainless steel cladding or between pins with different cladding thickness.

12.2.2 Microsurveying

In subsection 8.4.1 of the twenty-fifth quarterly report, methods were evaluated to determine experimentally local strains by measuring the distances between fiducial marks placed on fuel cladding. These methods called "microsurveying" were judged to be the most suitable for the measurement of local strain in reactor core structures. The development of such techniques was proposed for the purpose of gathering data for investigation of local cladding loading mechanisms. In the earlier work, fiducial marking consisted of microhardness marks and chemically etched grids. The more recent technique consists of scribing finely cut lines with a ruling engine. On the basis of a test with microhardness marks at 1300°F in flowing sodium, post-exposure mark quality was determined acceptable.

Patterns of marks about 1/16-inch long, 2-mils wide and 1-mil deep have been ruled on six pieces of cladding material and six alternate pieces to be used in assembly 10A (refer to section 7 of this report). Marking was done so spaces of 1-inch interval would occur over the completed fuel pin, with three patches within the fuel region containing marks 0.1-inch apart. The marks have been carefully checked and found to be accurately located to within ± 0.0002 inch of the desired spacing. This resolution can be used to establish the minimum spacing for a desired accuracy. If the resolution of in-cell measurement is assumed as good as the mark placement, then for gage lengths of 0.1 inch, strains can be obtained with a maximum error of ± 0.004 in./in. and for 1-inch spacing to within ± 0.0004 in./in. For nominal strains of 2%, the 0.1-inch spacing may be appropriate, however, to measure strains on the order of, for example, 0.1%, a larger gage length would be required, depending on the nominal strain involved.

This first in-reactor demonstration of microsurveying in assembly 10A is designed to provide information about

the distribution of axial strain in a fuel pin. The experiment should yield data on irradiation induced cladding swelling. The 0.1-inch gage lengths may provide the first experimental evidence concerning local axial effects of interaction between cladding and fuel pellets.

12.3 PERFORMANCE AND FAILURE MODE STUDIES

Under this subtask, investigation will continue on the magnitude and interactions of the various internal and external loading mechanisms on fuel cladding. These mechanisms will relate stresses and strains to pin fabrication and operating parameters.

A second objective of this subtask is the development of a model to predict the macroscopic interaction of fuel and cladding in a fast reactor due to the effects of fuel startup, differential thermal expansion, and a steady-state condition of fuel and cladding swelling. This model will help to identify the types of measurements and the precision required from irradiated fuel pin performance data that are needed to confirm or improve fuel models. Additional irradiation tests and fuel and cladding property measurements will be recommended where needed to provide a better basis for interpretation of fuel pin performance data.

12.3.1 Cladding Swelling Induced Stress Analysis

In section 7.5.2.1 of the 31st quarterly report, there was a brief discussion of the problem of strains in cladding and other core structure which are mechanically induced by nonuniform stainless steel swelling. The analogy between cladding swelling and transient thermal expansion was described along with approximate methods of estimating strains, strain rates, and stresses from an empirical swelling equation. It was found that inaccuracies involved in assuming a linear swelling rate in this previous work could be eliminated by a more rigorous approach suggested in the literature.¹ The elastic-plastic analysis of a solid cylinder with an axisymmetric temperature distribution previously done has been extended to the case of a hollow cylinder with swelling. The scope was also extended to handle viscoelastic deformation and a temperature dependent yield criterion. The reduction of the set of constitutive equations to computer solution is not planned in the immediate future and consequently, the equations required for solution are summarized in table 12-2 for interested users. All necessary parameters are functions of the deviatoric stresses S and S_z and the rates of thermal and swelling expansion $\alpha \dot{T}$ and $\beta \dot{\gamma}$ previously defined.

Table 12-2(a)
SUMMARY OF EQUATIONS FOR STRESS ANALYSIS OF A HOLLOW CYLINDER,
ELASTIC-PLASTIC AND VISCOELASTIC IN NATURE
WITH AXISYMMETRIC SWELLING

Stress Rates

$$\dot{S}_z = 2G \left[\dot{\epsilon}_z - \alpha \dot{T} - \frac{\dot{\sigma}}{\beta \gamma} - \frac{\dot{\sigma}}{3K} - \left(\frac{1}{2\eta} + \{1 - g(r,t)\} \mu \right) S_z \right] \quad (1)$$

$$S = 2G \left[\xi_i - \left(\frac{1}{2\eta} + \{1 - g(r,t)\} \mu \right) S \right] \quad (2)$$

Strain Rate

$$\xi_i = 3 \left(\alpha \dot{T} + \frac{\dot{\sigma}}{\beta \gamma} + \frac{\dot{\sigma}}{3K} \right) - \frac{6}{r^2} \int_a^r \left(\alpha \dot{T} + \frac{\dot{\sigma}}{\beta \gamma} + \frac{\dot{\sigma}}{3K} \right) r dr \quad (3)$$

Mean Stress Rate

$$\begin{aligned} \dot{\sigma} = \frac{G}{1 + \frac{3K}{4G}} & \left[\frac{6}{b^2} \int_0^b \left(\alpha \dot{T} + \frac{\dot{\sigma}}{\beta \gamma} \right) r dr + \dot{\epsilon}_z - 4 \left(\alpha \dot{T} + \frac{\dot{\sigma}}{\beta \gamma} \right) \right. \\ & \left. - 2 \int_r^b \left(\frac{1}{2\eta} + (1 - g) \mu \right) \frac{S}{r} dr + \left(\frac{1}{2\eta} + (1 - g) \mu \right) (S - S_z) \right] \quad (4) \end{aligned}$$

Proportionality Factor

$$\mu = \frac{\dot{J}_2^V - 2k\dot{k}}{4Gk^2} \quad (5)$$

Axial Strain Rate

$$\dot{\epsilon}_z = \frac{2}{b^2 - a^2} \int_a^b \left[\alpha \dot{T} + \frac{\dot{\sigma}}{\beta \gamma} + \frac{\dot{\sigma}}{3K} + \left(\frac{1}{2\eta} + (1 - g) \mu \right) S_z \right] r dr \quad (6)$$

Yield Function

$$g(r,t) = 1 \text{ if } J_2 - k^2(T) < 0 \text{ or if } J_2 - k^2(T) = 0, \dot{J}_2^V - 2k\dot{k} < 0$$

$$g(r,t) = 0 \text{ if } J_2 - k^2(T) = 0, \dot{J}_2^V - 2k\dot{k} \geq 0 \quad (7)$$

Stress Invariant

$$J_2 = \frac{1}{4} (S^2 + 3S_z^2) \quad (8)$$

Viscoelastic Parameter

$$\dot{J}_2^V = G \left[S \xi_i + 3S_z \left(\dot{\epsilon}_z - \alpha \dot{T} - \frac{\dot{\sigma}}{\beta \gamma} - \frac{\dot{\sigma}}{3K} \right) - \frac{1}{\eta} (S^2 + 3S_z^2) \right] \quad (9)$$

Table 12-2(a)

(Continued)

SUMMARY OF EQUATIONS FOR STRESS ANALYSIS OF A HOLLOW CYLINDER,
ELASTIC-PLASTIC AND VISCOELASTIC IN NATURE
WITH AXISYMMETRIC SWELLING

Stress Equations

$$\sigma_r = \int_r^b \frac{S}{r} dr \quad (10)$$

$$\sigma_\theta = \sigma_r - S \quad (11)$$

$$\sigma_z = \frac{1}{2} [2 \sigma_r - S + 3S_z] \quad (12)$$

$$\sigma = \frac{1}{2} \left[2 \int_r^b \frac{S}{r} dr - S + S_z \right] \quad (13)$$

Table 12-2(b)
HOLLOW CYLINDER
NOMENCLATURE

σ_i	= stress, $i = R, \theta, z$
ϵ_i	= strain, $i = r, \theta, z$
σ	= $1/3 (\sigma_r + \sigma_\theta + \sigma_z)$, hydrostatic or mean stress
ϵ	= $1/3 (\epsilon_r + \epsilon_\theta + \epsilon_z)$, mean strain
S_i	= $\sigma_i - \sigma$, deviatoric stress
e_i	= $\epsilon_i - \epsilon$, deviatoric strain
S	= $S_r - S_\theta$ (defined for calculations)
ξ	= $\epsilon_r - \epsilon_\theta$ (defined for calculations)
α	= linear coefficient of thermal expansion
T	= temperature (above some reference)
μ	= proportionality factor (proportional to plastic power)
η	= viscosity
G	= $E/2(1+\nu)$, shear modulus
ν	= Poisson's ratio
K	= $E/3(1-2\nu)$, bulk modulus
k	= yield stress in shear
$\beta\gamma$	= linear structure swelling component (function of position and time)

12.3.2 Ductility Penalty for Cladding Wall Defects

In section 8.4.2 of the 27th quarterly report, the results of the tubing defect tests were reported. It was observed that no significant loss in rupture strength resulted for longitudinal defects which were less than 10% of the wall in depth. It is also interesting that a significant decrease in strength did not occur even with marks almost 25% of the wall in depth. It is very significant that the tubing diameters at failure were reduced by a factor as much as 0.6 by the presence of the deeper defects (table 12-3).

A theoretical study of the effect of wall thickness variations on the expected diameter of a tube at failure was also discussed in section 8.3.2 of the 27th quarterly report, and a theoretical model was presented for predictive purposes. This theoretical model has been found to provide a safe (conservative) prediction of the penalty in diametral ductility experienced in the defect tubing tests.

These were short term rupture tests where a catastrophic failure resulted. The rapid strain rates and distortion in the rupture area make post-test determination of rupture ductility of questionable value in relation to reactor design data. Since the defect groove was small in width compared to the circumference, the conclusion was made that the post test measurements of diameter at the defect and away from the defect just before rupture would be almost identical. The average measured penalty in ductility caused by the axial defects are presented in table 12-3 and plotted in figure 12-7. Also shown is the theoretical value predicted from the approximation for narrow defects. The strain hardening exponent is not well characterized for this material but previous tensile tests show it to be between about 0.125 and 0.25. Using these

figures, the theory predicts more penalty than measured. For defects less than 12% of the wall, there appears to be no significant detrimental effect on diametral ductility.

This conservative estimate is understandable from the differences between the assumed theoretical condition and the test. First it was assumed in theory that the defect was infinite in length. The short defects would understandably be strengthened by the nondefective regions on either end. Also, in the theory, it was assumed that stress in the defect would be uniaxial while in reality a triaxial condition exists which strengthens the defect area.

From the results of this review, it is concluded that longitudinal defects up to 10% of the wall thickness do not produce significant losses in either strength or diametral ductility for high temperature short term loading. Longitudinal defects up to 25% of the tube wall do not seriously affect short term rupture strength but produce serious penalties in diametral ductility. The analytical method of predicting the penalty in ductility imposed by longitudinal defects is a useful method and over the range of plausible values of the strain hardening exponent, the prediction is conservative. More accurate estimates may be possible provided the strain hardening characteristic is measured for the material in question.

The above conclusions should not be applied to loading of a creep nature. Such conclusions will necessarily await creep results under the task L defect testing program.

12.3.3 Fuel Cladding Mechanical Interaction Analysis

The conclusion resulting from analysis of experimental data carried out to date is that currently available models are incapable of predicting actually observed cladding deformations, and they are inconsistent with a number of

Table 12-3
DUCTILITY PENALTIES IN RUPTURED TUBING
WITH ARTIFICIAL LONGITUDINAL DEFECTS (1300°F)

Specimen	Defect Depth (% of wall thickness)	Failure Stress (psi)	Fraction of Diametral Ductility Lost in Relation to Tubing Without Defects	
			Measured Average	Theoretical Range*
LMD-L-3.7-A	24	40,500	0.59	0.67-0.80
LMD-L-2.7-B	18	40,850	0.46	0.55-0.69
LMD-L-3.4-C	23	41,150	0.37	0.65-0.78
LMD-C-1.3-A	7	42,500	0.04	0.24-0.36
LMD-C-1.8-C	12	43,100	-0.05**	0.40-0.54

* Based on equation of figure 12-6 with strain hardening exponent, M between 0.25 and 0.125.

** The unexpected ductility increase probably indicates the extent of data scatter or measurement error.

Figure 12-7. Diametral Ductility Penalty versus Depth for a Short Longitudinal Defect

experimentally observed phenomena.² Whether the assumptions are valid on which the models are commonly based is in considerable doubt. For example, in the observation discussed in section 12.2.1, the swelling in two fuel pins of very similar design peaked in one case at the top of the pin and in the other case toward the bottom; this is inexplicable by the conventional model of fuel swelling induced by the slow formation of fission gas.

In the belief that nothing should be taken for granted, the preliminary fuel-cladding interaction modeling analysis will be to formulate alternative hypotheses capable of being tested against experimental data. An example of the technique is the set of graphs referred to in subsection 12.2.1.1, in which fuel pin swelling minus cladding swelling is plotted against local linear power. If the hypothesis were correct that the diametral increase is caused by the interference on startup plus cladding swelling, then the data would be more closely grouped than it is found to be. Thus, there is reason to doubt the validity of that hypothesis. Those same graphs do, however, lend some support to the theory that most of the observed diameter change in low density or low linear power pins is caused by swelling of the cladding.

An activity carried out in the past (previously reported under task G) has been the development of a computer program TEMP, which calculates fuel temperatures and predicts the radii of equiaxed and columnar grain growth and the radius of the central void. Figure 12-8 shows a plot of the predicted radii as a function of length for pin F2T. Experimental data for central void radius, plotted on the graph, shows good agreement with the predicted value in this particular case. The point of interest in this comparison of experimental data is the dependence of axial location. The model definitely overpredicts the central void radius at the bottom and underpredicts it at the top. This axial asymmetry may be associated with axial fuel migration and

may be related to the axially asymmetrical pin profilometer traces discussed in subsection 12.2.1.1.

12.4 INTEGRAL PERFORMANCE AND FAILURE MODELING

Previous work reported in subsection 8.3.4.2 of the 30th quarterly report has led to the conclusion: with the aid of a computer, the local deformations of cladding can be predicted as a function of reactor operating conditions. It is planned to develop a computer code that uses fuel swelling and cladding swelling models and combines the effects of other cladding loading mechanisms to predict stress, strain, and diameter change in cladding as a function of time. A maximum local strain criterion will be developed to predict the end of life.

In order to make meaningful comparisons with the work of other AEC contractors, the following models will be reviewed and portions of them utilized where feasible and appropriate:

- SWELL and LIFE (ANL)
- BRASH and BRITL (GA)
- FMODEL (ORNL)
- DEFORM and PECT (PNL)
- CYGRO (Westinghouse)
- FTB and CLADRS (AI)

Codes (or portions which may be applicable) will be selected from this list, and comparisons will be made with the model developed in this program.

The earliest forms of the fuel lifetime model will use irradiated fuel pin performance data as direct inputs. In subsequent versions more fundamental stress analysis equations and material properties will be used as input. An appropriate computer program will be developed.

No work was carried out under subtask 12.4 during the last quarter.

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FUEL	$(U-0.2 P_u) O_2$
PELLET DENSITY	96.1% TD
SMEARED DENSITY	94.2% TD
O/M	2.00
GAP	0.9-4.9 mil
CLADDING	SS-347, 15 mil, 1080°F (max)
LINEAR POWER	17.3 kW/ft (max)
BURNUP	74,000 MWd/Te (max)

ALL RADII CALCULATED BY "TEMP"

Figure 12-8. Calculated Fuel Length Void and Grain Growth Temperature Contours for Pin F2T

APPENDIX

PRIOR REPORTS

Prior progress reports to the Commission under this contract include:

Monthly Progress Letters, Number 1-94, from July 1959 through October 1969.

The quarterly reports under the title "Sodium-Cooled Reactors Fast Ceramic Reactor Development Program" follow:

GEAP-3888
First Quarterly Report
October–December 1961.

GEAP-3957
Second Quarterly Report
January–March 1962

GEAP-3981
Third Quarterly Report
April–June 1962.

GEAP-4080
Fourth Quarterly Report
July–September 1962.

GEAP-4158
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October–December 1962.

GEAP-4214
Sixth Quarterly Report
January–March 1963.

GEAP-4300
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April–June 1963.

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April–June 1964.

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July–September 1964.

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October 1964–January 1965.

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February–April 1965.

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August–October 1965.

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May–July 1966.

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August–October 1966.

GEAP-5433
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October 1962 through April 1964.

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CONTRIBUTORS

TASK B DEFECTED FUEL BEHAVIOR

BRDO

N. W. Brown
K. E. Gregoire
R. E. Murata
P. E. Novak
H. C. Pfefferlen
D. E. Plumlee

IPO

D. L. Brown
R. J. Burrill
R. H. Eymann
F. J. Lamphere
E. D. Ormiston
D. C. Palmer
J. P. Peterson
G. L. Stimmell
E. E. Walker
L. W. Waters
D. L. Zimmerman

TASK E FUEL MATERIAL PROPERTIES
AND PERFORMANCE EVALUATION

GE-BRDO

R. R. Arnold
A. L. Barzin
P. E. Bohaboy
D. A. Cantley
R. N. Duncan
G. F. Melde
P. E. Ray
B. F. Rubin

MS&P

E. A. Aitkin
S. K. Evans
T. E. Lannin
T. E. Ludlow
H. S. Rosenbaum

IPO

W. L. Conant
R. H. Eymann
G. P. Ferguson
D. L. Zimmerman

TASK C-1 SAFETY EXPERIMENTATION
(Transient Overpower)

GE-BRDO

J. Field
G. Thomas
T. Hikido
D. Freeman
F. Aquino

NFD

D. Hafer
R. Eymann
M. Carino
G. Stimmell
L. Waters

NFD

M. Thompson
G. Berg

R. Smith
D. Orton
S. Lukezic
D. Zimmerman

TASK F FAST FLUX TESTS AND SUPPORT

GE-BRDO

A. L. Barzin
P. E. Bohaboy
J. S. Bromberg
D. A. Cantley
C. N. Craig
R. N. Duncan
R. N. Evatt
G. F. Melde
K. J. Perry
P. E. Ray
B. F. Rubin

GE-VAL

E. A. Aitken
W. L. Conant
J. E. Lewis
B. F. Rider
H. S. Rosenbaum
M. L. Thompson

IPO

R. H. Eymann
R. N. Irvine
G. P. Ferguson
R. E. Smith

TASK C-2 SAFETY EXPERIMENTATION
(Loss of Flow)

GE-BRDO

J. H. Field
M. I. Temme
H. C. Pfefferlen
D. L. Brown

IPO

G. W. Tunnell
D. E. Hafer
R. H. Eymann
M. E. Carino
G. C. Linder

TASK G-6 CORE DESIGN ANALYSIS

F. Aquino
J. H. Field
K. M. Horst
L. M. McWethy

W. G. Meinhardt
H. W. Moddison
L. L. Lee
W. A. Sangster

TASK G-10 KINETICS ANALYSIS

F. G. Aquino	G. R. Thomas
D. J. Bender	R. C. Thompson
J. H. Field	M. B. Goldstein
P. M. Magee	D. D. Freeman
W. G. Meinhardt	G. Leff
R. G. Stuart	J. T. Hitchcock

TASK N CORE PHYSICS ANALYSIS

S. Crick	L. Price
C. Cowan	R. Protsik
P. Greebler	R. Seymour
T. Hitchcock	J. Turner
B. Hutchins	P. Vaughan
M. Kelley	J. Douglass
P. Linder	A. Miller

TASK L CLADDING DEVELOPMENT

J. J. Albanese	D. H. Frisby
J. S. Armijo	B. A. Hutchins
R. R. Asamoto	R. M. Irvine
P. E. Bohaboy	T. A. Lauritzen
N. I. Brown	A. O. Montes
H. J. Busboom	R. T. Penrose
F. A. Comprelli	R. P. Rhodes
A. E. Conti	D. W. Sandusky
J. G. Douglass	J. E. Turner
R. N. Evatt	W. K. Wagner
R. H. Eymann	D. L. Zimmerman
J. P. Ferguson	

TASK P FUEL ELEMENT DESIGN ANALYSIS

C. F. Barrett	D. P. Hines
J. R. Cox	T. Oldberg
D. K. English	J. D. Stephen

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Director, Contracts Division U. S. Atomic Energy Commission San Francisco Operations Office 2111 Bancroft Way Berkeley, California 94704	2	Division of Reactor Development and Technology U.S. Atomic Energy Commission Washington, D. C. 20545 Attn: E. E. Sinclair Asst. Director for Reactor Tech. (Attn: J. M. Simmons; I. Zartman)	2
Chief, California Patent Group U. S. Atomic Energy Commission San Francisco Operations Office P. O. Box 808 Livermore, California 94551	1	RDT Site Office U.S. Atomic Energy Commission c/o General Electric Company 310 DeGuigne Drive Sunnyvale, California Attn: J. V. Levy	2
Division of Reactor Development and Technology U. S. Atomic Energy Commission Washington, D. C. 20545 Attn: Asst. Director for Reactor Engineering	2	LMFBR Program Office Argonne National Laboratory 9800 South Cass Avenue Argonne, Illinois 60439 Attn: A. Amorosi, Director	1
Division of Reactor Development and Technology U. S. Atomic Energy Commission Washington, D. C. 20545 Attn: M. J. Whitman Asst. Director for Program Analysis	1	LMFBR Program Office Argonne National Laboratory 9800 South Cass Avenue Argonne, Illinois 60439 Attn: L. R. Kelman	1
Division of Reactor Development and Technology U. S. Atomic Energy Commission Washington, D. C. 20545 Attn: A. Giambusso Asst. Director for Project Mgmt (Attn: R. Sweek, G. Wensch, J. Morabito)	3	Atomics International P.O. Box 309 Canoga Park, California 91304 Attn: S. Golan	2
Division of Reactor Development and Technology U. S. Atomic Energy Commission Washington, D. C. 20545 Attn: J. A. Lieberman Asst. Director for Nuclear Safety	1	Director Liquid Metals Information Center P. O. Box 309 Canoga Park, California 91305	2
Division of Reactor Development and Technology U.S. Atomic Energy Commission Washington, D. C. 20545 Attn: J. W. Crawford Asst. Director for Plant Engineering	1	The Babcock & Wilcox Company Atomic Energy Division Lynchburg, Virginia 24501 Attn: M. W. Croft	2
Division of Reactor Development and Technology U.S. Atomic Energy Commission Washington, D. C. 20545 Attn: R. H. Steel Division of Naval Reactors	1	Mr. L. W. Fromm, Manager 1000 MWe LMFBR Follow-On Study Project Building 208 Argonne National Laboratory 9800 South Cass Avenue Argonne, Illinois 60439	2

Mr. C. A. Anderson, Project Mgr 1000 MWe LMFBR Follow-On Study Westinghouse Electric Corporation Advanced Reactors Division Waltz Mill Site P. O. Box 158 Madison, Pennsylvania 15663	2	RDT Senior Site Representative U.S. Atomic Energy Commission P. O. Box 550 Richland, Washington 99352	1
FFTF Project P. O. Box 220 Richland, Washington 99352 Attn: Configuration & Data Management	15	RDT Site Representative U.S. Atomic Energy Commission Post Office Box 2108 Idaho Falls, Idaho 83401	1
W. P. Staker, Project Manager 1000 MWe LMFBR Follow-On Study Combustion Engineering, Inc. P. O. Box 500 Windsor, Connecticut 06095	2	RDT Site Representative U.S. Atomic Energy Commission United Nuclear Corporation Grasslands Road Elmsford, New York 10523 Attn: M. Napack	1
RDT Senior Site Representative Canoga Park Area Office P. O. Box 2325 San Diego, California 92112	1	RDT Site Representative U.S. Atomic Energy Commission United Nuclear Corporation Grasslands Road Elmsford, New York 10523 Attn: A Strasser	1
RDT Senior Site Representative Canoga Park Area Office P. O. Box 591 Canoga Park, California 91305	1	Argonne National Laboratory 9800 South Cass Avenue Argonne, Illinois 60439 Attn: J. H. Kittel	3
RDT Senior Site Representative U.S. Atomic Energy Commission Argonne National Laboratory 9800 South Cass Avenue Argonne, Illinois 60439	1	Argonne National Laboratory 9800 South Cass Avenue Argonne, Illinois 60439 Attn: R. C. Vogel	1
RDT Site Office U.S. Atomic Energy Commission Atomic Power Development Associates, Inc. 1911 First Street Detroit, Michigan 48226	1	Pacific Northwest Laboratory P. O. Box 999 Richland, Washington 99352 Attn: E. A. Eschbach	1
RDT Senior Site Representative Oak Ridge National Laboratory P. O. Box X Oak Ridge, Tennessee 37830	1	Pacific Northwest Laboratory P.O. Box 999 Richland, Washington 99352 Attn: E. A. Evans	2
The Babcock & Wilcox Company Atomic Energy Division Lynchburg, Virginia 24501 Attn: S. H. Esleeck	1	University of California Lawrence Radiation Laboratory P. O. Box 808 Livermore, California 94551 Attn: A. Rothman	1

Los Alamos Scientific Laboratory P. O. Box 1663 Los Alamos, New Mexico 87544 Attn: R. D. Baker	1	Dow Chemical Company Rock Flats Division P. O. Box 888 Golden, Colorado 80401 Attn: R. D. Forest	1
Los Alamos Scientific Laboratory P. O. Box 1663 Los Alamos, New Mexico 87544 Attn: D. B. Hall	1	U.S. AEC Scientific Representative United States Embassy Paris, France APO New York 09777	1
Los Alamos Scientific Laboratory P. O. Box 1663 Los Alamos, New Mexico 87544 Attn: J. C. Clifford		Senior U.S. AEC Representative U.S. Mission to the European Communities United States Embassy Brussels, Belgium	1
Scientific Laboratory P. O. Box 1663 Los Alamos, New Mexico 87544 Attn: Reports Librarian	1	Division of Technical Information Ext. U.S. Atomic Energy Commission P. O. Box 62 Oak Ridge, Tennessee 37831	50
Westinghouse Electric Corporation Bettis Atomic Power Laboratory P. O. Box 79 West Mifflin, Pennsylvania Attn: E. J. Kreh	1	Dr. John C. Woodhouse 21 Woodbrook Circle Wilmington, Delaware 19803	1
Oak Ridge National Laboratory P. O. Box X Oak Ridge, Tennessee 37830 Attn: J. E. Cunningham	1	Power Reactor Development Corp. 1911 First Street Detroit, Michigan 48226 Attn: W. J. McCarthy	1
Brookhaven National Laboratory Upton, New York 11973 Attn: O. E. Dwyer	1	Argonne National Laboratory Idaho Division P. O. Box 1096 Idaho Falls, Idaho 83401 Attn: F. W. Thalgott	1
Battelle Memorial Institute Columbus Laboratories 505 King Avenue Columbus, Ohio 43201 Attn: D. Keller	1	Detroit Edison Company 1911 First Street Detroit, Michigan 48226 Attn: A. S. Griswold	1
Atomics International P. O. Box 309 Canoga Park, California 91304 Attn: H. Pearlman	1	Atomic Power Development Associates 1911 First Street Detroit, Michigan 48226 Attn: A. A. Shoudy	1
Atomics International P. O. Box 309 Canoga Park, California 91304 Attn: FBR Project Manager (R. J. Deeley)	1	Combustion Engineering, Inc. Nuclear Division Prospect Hill Road Windsor, Connecticut 06095 Attn: W. P. Chernock	1

The Babcock & Wilcox Company
Atomic Energy Division
Lynchburg, Virginia 24501
Attn: H. S. Barringer

1

S. Visner
Nuclear Division
Combustion Engineering, Inc.
P. O. Box 500
Windsor, Connecticut 06095

2

General Atomic
P. O. Box 608
San Diego, California 92112
Attn: D. B. Coburn

1

W. B. Cottrell, Director
Nuclear Safety Program
Oak Ridge National Laboratory
P.O. Box Y
Oak Ridge, Tennessee 37830

1

The Babcock & Wilcox Company
Alliance Research Center
Alliance, Ohio 44601
Attn: D. Koch

1

Irradiations Coordinator
EBR-II Project
P. O. Box 2528
Idaho Falls, Idaho 83401
Attn: R. E. Neidner

1

Westinghouse Electric Corporation
Advance Reactors Division
P. O. Box 217
Cheswick, Pennsylvania 15024
Attn: W. E. Ray

1

Irradiations Coordinator
EBR-II Project
P. O. Box 2528
Idaho Falls, Idaho 83401
Attn: J. E. Case

1

Nuclear Materials & Equipment Corp.
Plutonium Laboratory
Leechburg, Pennsylvania 15656
Attn: William J. Ross

1

General Electric Company
Nuclear Systems Programs
P. O. Box 15132
Cincinnati, Ohio 45215
Attn: J. W. Stephenson

1

U. S. AEC Scientific Representative
United States Embassy, Box 40
FPO
New York, N. Y. 09510

1