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FAST CERAMIC REACTOR DEVELOPMENT PROGRAM

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Sodium Reactor Technology

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TABLE OF CONTENTS

	Page
1. Introduction	1
2. Summary	1
2.1 Task B - Defected Fuel Behavior	1
2.2 Task C-1 Safety Experimentation (Transient Overpower)	1
2.3 Task C-2 - Safety Experimentation (Loss of Flow).	1
2.4 Task E - Fuel Material Properties and Performance Evaluation	1
2.5 Task F - Fast Flux Tests and Support	5
2.6 Task G-6 - Core Design Analysis	5
2.7 Task G-10 - Kinetics Analyses	5
2.8 Task L - Cladding Development	10
2.9 Task N - Core Physics Analysis	10
2.10 Task P - Fuel Element Design Analysis	10
3. Task B - Defected Fuel Behavior	10
3.1 General	10
3.2 Defected Fuel Testing	12
3.3 Sodium Fuel Cladding Compatibility Experiments.	19
3.4 Fission Product Distribution	22
4. Task C-1 Safety Experimentation (Transient Overpower)	28
4.1 General	28
4.2 High Burnup Fuel Experiments	28
4.3 Axial Fuel Relocation Experiments	28
4.4 Defected or Sodium-Bonded Fuel Experiments	28
4.5 Mark II Treat Integral Sodium Loop	28
5. Task C-2 - Safety Experimentation (Loss of Flow)	32
5.1 General	32
5.2 Mockup 1A	32
5.3 Capsule C10X	32
5.4 Capsule C10A/C10B	32
5.5 Planning and Design	32
6. Task E - Fuel Material Properties and Performance Evaluation	39
6.1 General	39
6.2 Material Property Studies	39
6.3 Center Temperature Limits and Fuel Behavior	39
6.4 Cesium-Fuel-Cladding Reactions	46
6.5 Fuel-Sodium Reactions and Plutonium and Fission Product Redistribution	60
7. Task F - Fast Flux Tests and Support	67
7.1 General	67
7.2 Medium Power, Long Term Irradiations	68
7.3 Low Power Fuel Swelling Irradiations	68
7.4 High Power Intermediate Burnup Irradiations	78
7.5 Elevated Cladding Temperature Tests	78
7.6 Unencapsulated Fuel Pin Irradiations	79
7.7 Pre-Irradiation Transient Tests	79
7.8 Capsules F12C, F12D, and F12E (E5C, E5D, and E5E) Comparison of Long Columns of Mixed Oxide Fuel in the Form of Solid Pellets, Annular Pellets, and Powder	80

TABLE OF CONTENTS (Continued)

	Page
7.9 Capsule F12B, In-Pile Axial Expansion and Gas Release of Mixed Oxide Fuel	80
7.10 Capsules F12P and F12Q (E2P and E2Q) Effect of Radial Restraint on Fuel Swelling	80
8. Task G-6 - Core Design Analysis	80
8.1 General	80
8.2 Fuel Support System	86
8.3 Steady-State Irradiation Evaluation	86
8.4 Fuel Failure Effects	92
8.5 Coolant Boiling and Operating Safety Limits	97
9. Task G-10 - Kinetics Analysis	97
9.1 General	97
9.2 Model Development	97
9.3 Sensitivity Studies	101
10. Task L - Cladding Development	110
10.1 General	110
10.2 Materials Irradiation Performance	110
10.3 Clad Alloy Swelling	113
10.4 Clad Alloy Mechanical Properties	117
10.5 Materials Management	117
11. Task N - Core Physics Analysis	122
11.1 General	122
11.2 Compilation and Distribution of Evaluated Data	122
11.3 Improvements of Fuel Cycle and Management Schemes	126
11.4 Improvements of Existing Methods for Generating Group Constants	126
11.5 Sensitivity Studies	127
11.6 Improvement of Existing Transport Theory Codes	128
12. Task P - Fuel Element Design Analysis	129
12.1 General	129
12.2 Experimental Data Analysis	129
12.3 Performance and Failure Mode Studies	129
12.4 Integral Performance and Failure Modeling	135
Appendix A — Prior Reports	A-1
Contributors	-1-
Distribution	-3-

LIST OF ILLUSTRATIONS

Figure	Title	Page
3-1	Cross Section of B9C Test Section (10X)	13
3-2	Mockup Test of B9C Defect Actuator System	14
3-3	Details of Attachment of Actuator Wire to B9C Fuel Pin and Defect Plugs	15
3-4	B9C Defect Test No. 7	17
3-5	B9C Flowmeter Calibration	18
3-6	Schematic of the De-Entrainment Test Apparatus	20
3-7	De-Entrainer Efficiency as a Function of Flow Rate	21
3-8	Swelling Data from E1J-B2 and B9A	22
3-9	Fuel Pin B8D-1--Pre- and Post-Irradiation Profilometry Traces	23
3-10	Fuel Expansion Measured for Capsule B8C and B8D Fuel Pins (Sodium Bonded)	24
3-11	Appearance of Fuel in Capsule B8C Fuel Pins	25
3-12	Appearance of Fuel in Capsule B8D Fuel Pins	26
3-13	Pre- and Post-Test Cladding Diametral Measurements of Miniloop Fuel Sample	27
4-1	Correlation of Measured and Calculated Sodium Temperatures in Capsule C6B-1	29
4-2	Correlation of Measured and Calculated Sodium Temperatures in Capsule C6B-2	30
4-3	Correlation of Measured and Calculated Sodium Temperatures in Capsule C6B-3	31
5-1	Mockup 1A, Schematic	33
5-2	Measure Versus Calculated Temperatures for the Mockup 1A Loss-of-Flow Test	34
5-3	Calculated Mockup 1A Bulk Coolant Temperature at Boiling Inception (2.7 sec after Pump Scram)	35
5-4	Calculated Mockup 1A Radial Temperature Profiles at Boiling Inception (2.7 sec after Pump Scram--Top of Heated Region)	36
5-5	Assembly Drawing of C10X	37
6-1	Comparison of Creep Rates for 2.00 and 1.95 Nominal Stoichiometries	40
6-2	Comparison of the Creep Rate Versus Density Relationship for Mixed Plutonium-Uranium Oxide and UO_2	41
6-3	Effect of Burnup on Swelling of Mixed Oxide	45
6-4	Effect of Burnup on the Xe + Kr Inventory in the Fuel	46
6-5	Section Diagram Showing the Location of the Three Samples of F2H Along with Pre-Irradiation $\text{Pu}/(\text{Pu} + \text{U})$ Ratios	47
6-6	Plot of $\text{Pu}/(\text{Pu} + \text{U})$ Ratios Measured for the Three Sections Taken Along the Length of the Fuel Pin F2H Including Calculated End-of-Life Average Values	49
6-7	Montage of Fuel-Cladding Reaction Zone along with Radial Crack Examined in Section 6A1	50
6-8	Comparison of Specimen Current Images with Pd and Cs X-ray images in Radial Crack of Section 6A2 (See Figure 6-7 for Metallographic View of Area)	51
6-9	X-ray Images for Mo, I, Te and Ru in Radial Crack of Section 6A1. This is at the same location as Figure 6-8. See Figure 6-7 for Metallographic View of Area.	52
6-10	Specimen Current Image and X-ray Images of Pd and Mo Showing Fission Products in Radial but not in Circumferential Cracks in Section 6A1 (near the cladding). A Metallographic View is Shown in Figure 6-7.	53
6-11	Specimen Current Image and X-ray Images of Pd and Mo Showing Fission Products in Horizontal but not in Circumferential Cracks in Section 6A1. A Metallographic View is Shown in Figure 6-7.	54
6-12	Specimen Current Image and X-ray Images for Re, I and Mo near Bright Area in Section 8B	55
6-13	Specimen Current Image and X-ray Images for Cs, U and Fe at Bright Area in Section 8B	56
6-14	Typical Fuel-Clad Interaction Zone in Section 10A Showing a Specimen Current Image along with the X-ray Pulse Images for Cs, Fe and Te	57

LIST OF ILLUSTRATIONS (Continued)

Figure	Title	Page
6-15	Intergranular Attack of CsOH on Type 316 Tubing in Creep Rupture Testing at 1200°F	59
6-16	Cross Sections of Irradiated Fuel Examined by Electron Microprobe	62
6-17	Variation of the Intensity Ratios for U, Pu and Pu/U + Pu as a Function of the Radial Position for Sample F20	63
6-18	Comparison of the Specimen Current Image of a Na-Fuel Particle to the Na X-ray Intensity in Sample F20	64
6-19	Comparison of the Specimen Current Image of a Na-Fuel Particle with the X-ray Pulse Image in Sample F20	65
6-20	Examination of Metal Phase Slightly Removed from Cladding in Sample F20	66
7-1	Power History of Group O Fuel Pin FOE	69
7-2	Sections from the Fueled Region of Failed Group 6 Fuel Pin FOM	71
7-3	Variation in Maximum Diameter over Fueled Length of Pin FOM	72
7-4	Profilometer Traces of Group 8A Fuel Pin F8A Compared to Pre-Irradiation Fuel Pellet Lengths and Diameters (Note: Fuel Lineup was Random)	73
7-5	Profilometer Traces of Group 8A Fuel Pin F8C Compared to Pre-Irradiation Fuel Pellet Lengths and Diameters	74
7-6	Profilometer Traces of Group 8A Fuel Pin F8E Compared to Pre-Irradiation Fuel Pellet Lengths and Diameters	75
7-7	Profilometer Traces of Group 8A Fuel Pin F8G (Note: Pin F8G Contained Vibratory Compacted Powder)	76
7-8	Metallographic Sections Taken from Peak Flux Regions of Group 8A Fuel Pins. Fuel Burnup is ~50,000 MWd/Te	77
7-9	Radial Montages of Fuel Sections Cut from Peak Flux Region of Fuel Pins F12C, -D, and -E	81
7-10	Radial Montage of Section from Pin F12C Showing (a) Region of Fuel which Operated in Molten State, (b) High Magnification (500X) Photograph of Region of Fuel Clad Reaction Area Marked by Rectangle in (a), (c) same as (b) except Cladding has been Etched (500X)	82
7-11	Photomicrograph of Typical Cladding Grain Boundary Attack Observed in Fuel Pin F12D	83
8-1	Grid Spacer Bundle	
8-2	Pressure in 1000°F Sodium 1 Microsecond after Contact with 6800°F Slab of UO ₂	84/85 93
8-3	Voiding One Channel Causes Many Cracks, Large Area or Burst Failures in a Few (Many) Sound Bundles	95
8-4	Initial Superheat or Transient Temperature Increase	96
9-1	FREADM Approximation Compared with Radial Void Worth Calculation	99
9-2	FREADM Void Worth Calculation	100
9-3	Fuel Failure Model Results	102
9-4	Fuel Failure Model Results	103
9-5	Fuel Failure Model Results	104
9-6	Fuel Failure Model Results	105
9-7	Assumed Failure Model	106
9-8	Idealized Stress-Strain Assumption	107
9-9	Assumed Transient Power Input	109
10-1	Temperature Profile for MT-3 Tensiles and Holding Strip	112
10-2	L-21 Section 2, 321 Stainless Steel	114
10-3	L-21 Section 3, 321 Stainless Steel	115

LIST OF TABLES

Table	Title	Page
2-1	Task B—Summary of Current Mixed Oxide Capsule Irradiations	2
2-2	Task C—Current Experimental Test Series	3
2-3	Task E—Summary of Current Mixed Oxide Capsule Irradiations	6
2-4	Summary of Task F Fast Flux Testing Program in EBR-II	7
2-5	Summary of Task F Thermal Flux Irradiations	9
2-6	Summary of Task-L Materials Irradiated in EBR-II	11
3-1	Summary of B9C Defect Mockup Tests	16
3-2	B9D Fuel Pin and Capsule Design Parameters	19
3-3	Average Diameter Increase Measured for Capsule B8C and B8D Fuel Pins	22
6-1	Fabrication and Operating Parameters of the E1J Capsule Series	42
6-2	Summary of Neutron Radiography Parameters Being Studied in E2R Program	42
6-3	Fuel Swelling Capsule Irradiation Design Data	43
6-4	Fuel Swelling Capsules Post-Irradiation Conditions	44
6-5	Summary of Fuel Burnup and Fission Gas Release from Fuel Swelling Capsules	44
6-6	Fuel Pin F2H-Data on Three Sections Taken for Microprobe Analysis	48
6-7	Cesium—Fuel—Cladding Program	60
6-8	Isothermal Creep Rupture Testing with Fission Product Elements or Compounds Added to Capsules	61
7-1	Burnup Values for Group F6 Fuel Pins	70
7-2	Fission Gas Release Fractions for Group F6 Fuel Pins	70
7-3	Volume of Gas Released from Group F8A Fuel Pins	78
7-4	Burnup Analyses Results for Fuel Pins F12C, -D, and -E	78
8-1	Typical EBR-II and LMFBR Environmental Conditions	87
8-2	EBR-II and LMFBR Comparative Temperature Gradients	87
8-3	Acoustic Energy in Wave 1 μ second After Start of Propagation from Slab Source (UO_2 Source @ 6800°F. Sodium @ 1000°F)	94
8-4	Acoustic Total Energy—as Function of Time UO_2 @ 6800°F. Sodium @ 1000°F.—Slab Source	94
9-1	Summary of Flow Blockage Triggers	98
10-1	Radiation-Induced Density Change in Type 321 as Determined by Immersion Density Measurement	113
10-2	Vendor-Certified Chemical Composition of the 5H090G Material (Ladle Analysis).	117
11-1	Energy Dependent Unresolved Resonance Parameters Used in Analysis of Pu-239 for ENDF/B, Version II	123
11-2	Uncertainty Ranges in Predicted Physics Parameters of Fast Breeder Power Reactors Due to Nuclear Data Uncertainties	128

LIST OF ILLUSTRATIONS (Continued)

Figure	Title	Page
10-4	L-21 Volume Change	116
10-5	Stress-Rupture Behavior of Annealed and Cold Worked Tubing of Type 304H Stainless Steel Test Temperature 1300°F	119
10-6	Typical Stress-Rupture Failure at 1300°F. Type 316H Annealed X10	120
10-7	Influence of Cold Working on the Ductility of Austenitic Stainless Steel Tubing	121
11-1	Comparison of Pu-239 α in ENDF/B, Version II with Experimental Data	124
11-2	Comparison of Pu-239 σ_f in ENDF/B, Version II with Experimental Data	125
12-1	Mean Fuel Diametral Increase Predicted by Start versus Peak Linear Power	130
12-2	Tangential Stress Distribution in Fuel and Cladding	132
12-3	Model of Fuel Pin Diametral Increase	133
12-4	Mean Pin Diameter Increase from Profilometry versus Predicted Mean Diameter—Increase at Startup Plus Calculated Cladding Swelling	134
12-5	Computer Code BEHAVE Fuel Element Performance Simulation	136
12-6	Assumed Strain Distributions from Thermal Loading Mechanisms	138
12-7	Examples of Assumed Strain Distribution from Local Loading Mechanisms	139

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 February 1970 through April 1970 on the currently active tasks of this program is described in subsequent sections.

This is the thirty-fourth 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

Mockup tests of the B9C mechanical defect and actuator device confirmed that typical fuel specimens were reliably defected with this system. Fuel specimen fabrication and calibration of the capsule flowmeter were completed. Final capsule assembly is in progress.

Design and development continued on forced convection capsules B9B and B9D. Gas entrainment tests of conditions expected in B9B indicate that the development of a more efficient gas de-entrainer may be required for this experiment.

The defecting of notched, cold worked clad by differential expansion of fuel and clad was confirmed at 19 kW/ft. Irradiation of the failed specimen was continued in order to determine short-term swelling kinetics of ruptured mixed-oxide fuel. Diametral increases were observed to vary logarithmically with time. Preliminary testing of an electric arc clad defecting mechanism indicated feasibility for application in forced convection capsules.

Capsules B8C and B8D fuel pins show a fuel expansion rate of $1.4\% \Delta V/V$ per 10,000 MWd/Te burnup between 40,000 MWd/Te and 110,000 MWd/Te. Diametral expansions of <0.005 in. and no clad attack at 1100°F were also observed.

A 3% fuel volume increase (approximately 0.004 in. average diametral increase) was measured for the miniloop fuel sample.

The current status of the Task B irradiation test program is given in Table 2-1.

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

The four remaining EBR-II fuel specimens (C4E, F, K, and L) have accumulated $\sim 53,000$ MWd/Te peak burnup, and will be removed from the EBR-II core at the end of cycle 42.

Final assembly is $\sim 90\%$ complete for the TREAT capsules containing the C4X, G, and H fuel specimens.

Analysis of calibration transient data indicates satisfactory performance of the first of two capsules (C6A and C6B) to investigate the transient performance of defected or sodium-bonded fuel.

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)

The thermal analysis of Mockup 1A has been completed. The final design of capsule C10X is nearing completion and a detailed transient thermal analysis has been initiated. Action has been initiated to obtain the computer program THTE from the Argonne Code Center. Work has been initiated on a comparative study of the loss-of-flow testing capabilities of the ANL Mark II loop and the General Electric Forced Convection Capsule.

2.4 TASK E - FUEL MATERIALS PROPERTIES AND PERFORMANCE EVALUATION

Compressive creep testing of $(\text{U-20 w/o Pu})\text{O}_{2-x}$ continued over a range of temperatures from 1475 to 1625°C and at stresses of from about 1,000 to 8,000 psi. Calculated activation energies for the mixed oxide with an O/M ratio of 1.95 are comparable to those previously obtained on mixed oxide samples with an O/M ratio of 2.00; these in turn are comparable to those determined for UO_2 , indicating that similar mechanisms control the creep process. The creep rates of the stoichiometric mixed oxide are about five times greater than those measured for the hypostoichiometric mixed oxide compound. Samples of 95% of theoretical density creep at a lower rate than samples of 93% theoretical density.

Mixed oxide fuel swelling rates have been calculated for fuels irradiated to burnups as high as 79×10^{20} f/cc ($330,000$ MWd/Te) in a thermal flux (capsules E2E, -F, -G,

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

Capsule Designation	Purpose and Major Parameters	Design Parameters							Operating Conditions					Status
		Diameter (inch)	Length (inch)	Fuel				Cladding Material Stainless Steel Type	Irradiation Facility	Cladding Surface Temp (°F)	Peak Rating (kW/ft)	Peak Burnup (MWd/Te)		
				Fuel-Cladding Gap (in.-diam)	Density (Smear) (% TD)	PuO ₂ (%)	O/M							
B3D	Vented Fuel. Power profile simulates fuel in central core position. Natural convection cooling design.	0.217	20	0.006	85	25	1.97	316	V-Raft GETR	1100	18	6,700	Stored	
B3E	Vented Fuel. Power profile simulates fuel in peripheral core position. Natural convection cooling design.	0.213	23	0.008	83	25	1.98	316	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	0.008	84	25	1.98	316	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	0.007	84	25	1.98	316	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 0.216	2 2	0.010 0.010	80 83	25 25	2.00 1.93	347	V-Raft GETR	800	14	14,500	Examination completed	
B8C (2 pins)	Fuel-sodium compatibility versus burnup. Fission gas release versus burnup. Fuel-sodium-clad compatibility.	0.144 0.204	2.2 2.25	0.006 0.017*	86 84	25 25	1.97 1.97	316	V-Raft GETR	1100	21	113,500 57,000	Examination completed	
B8D (3 pins)	Fuel-sodium compatibility versus density and stoichiometry and Pu content of fuel	0.214 0.214 0.214	1.75 1.75 1.75	0.0065 0.0065 0.0065	80-88 80-88 80-88	40 25 25	1.97 1.97 1.94	316	V-Raft GETR	950	17.5	41,000 43,500 41,400	Examination completed	
B9A (2 pins axially aligned)	Defected fuel pin. Fuel swelling versus stoichiometry in flowing sodium.	0.216 0.216	5 5	0.005 0.005	89 89	25 25	1.99 1.95	316	Special V-Raft GETR	1019 971	23.4 23.4	8,600 8,600	Examination completed	
E1J-B1	Test Fuel-Clad Differential Expansion Defect Mechanism	0.220	5	<0.001	92	25	2.00	316 (0.012 notch)	Trail Cable GETR	840	17	100	Examination Completed	
E1J-B2	Test Fuel-Clad Differential Expansion Defect Mechanism	0.220	5	<0.001	92	25	2.00	316 (0.012 notch)	Trail Cable GETR	915	19	800	Examination in Progress	

* Large gap required to accommodate sampling device

Table 2-2
TASK C—CURRENT EXPERIMENTAL TEST SERIES

Capsule	Test Objective	Design Parameters											TREAT-						Destruc- tive Exam	Final Report
		Diameter (inch)	Fuel Length (inch)	Fuel Density (% TD)		Fuel Composition				Cladding Material	Fabri- cation	Pre-Irra- diated		Calib- ration	Test	Nondestructive Exam†				
				Pellet	Smeared	% UO ₂	% PuO ₂	% U-235	O/M			Yes	No			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	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	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	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	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	C*		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	C*		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	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	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	C*		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	C*		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	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	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 logged powder fuel is to maximize fuel-sodium heat transfer.	0.212	24	86	80	100	0	10	2.000	316SS	C		X	C	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	C		X	C	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

and -H). These range from 0.10 to 0.14%

$$\frac{\Delta V}{V} / 10^{20} \text{ f/cc}$$

assuming that the measured fuel dimensions did not reflect any decrease in initial fuel void volume during the irradiation. The swelling rates would range from 0.12 to 0.23%

$$\frac{\Delta V}{V} / 10^{20} \text{ f/cc}$$

if the available initial void volume had been filled prior to the fuel undergoing a dimensional increase.

Electron probe microanalysis of three samples from fuel pin, F2H, irradiated to ~64,000 MWd/Te in EBR-II, indicated that the plutonium concentration at the fuel center was slightly higher than the average radial concentration at that position. The variation of the plutonium concentration at the fuel center from the average radial concentration was greater in the cooler operating sample than in the sample at the highest flux position. Pd, Cs, Te and Mo were concentrated at the fuel-cladding interfaces of these sections. Fe, Ni and Cr were found to have migrated into the fuel region. Fe was found to migrate further into the fuel than Ni. Very little migration of Cr was observed.

The out-of-pile testing for cesium migration and cladding attack in the presence of mixed oxide fuel (performed in thermal gradient capsules) has demonstrated that intergranular attack of Type 316 cladding occurs at cladding temperatures of from 1200°F (650°C) to ~1400°F (760°C). The migration of oxygen in mixed oxide fuel down the thermal gradient is enhanced by the presence of cesium.

Out-of-pile creep-rupture testing of pressurized annealed Type 316 stainless steel tubing with additives of cesium, iodine and their compounds revealed that Cs₂O and CsOH cause premature failure due to rapid intergranular attack.

The current status of the Task E Irradiation Test Capsule program is given in Table 2-3.

2.5 TASK F – FAST FLUX TESTS AND SUPPORT

The four Group F2 capsules continued their irradiation toward a target fuel burnup in excess of 100,000 MWd/Te (11.19 a/o). The average burnup of the highest burnup pin was 100,017 MWd/Te (11.9 a/o) at the end of EBR-II run 42. Peak fluence was 0.98×10^{23} nvt total (0.90×10^{23} nvt for E>0.1 MeV). These capsules have accumulated the highest fuel burnup and fast neutron fluence of any mixed oxide irradiations in the EBR-II under the FCR Program.

Post irradiation examination of the Group F6 and Group F8A fuel pins was completed. The fuel pins in each of the groups operated at comparable powers to 50,000 and 55,000 MWd/Te fuel burnup respectively. The post irradiation data indicate that the diameter changes of the

Group F8A pins was significantly less than that measured for the Group F6 pins. The fuel pin diameter differences are attributed to smeared density differences between the pins. The F6 pins had smeared fuel densities of from 93.9 to 95.4% of theoretical, while the F8A pins had smeared fuel densities from 84.2 to 87.3%.

Fabrication of the elevated cladding temperature tests for insertion in the EBR-II continued. The last five of the Group F10A fuel pins were completed and final inspection of eighteen out of nineteen pins was completed. Twelve of the pins have been encapsulated and filled with sodium. Fabrication and inspection of the six additional Group F10B pins was completed. Thirty-five of the thirty-seven Group F11A fuel pins were loaded with fuel and prepared for xenon tagging.

A request for approval-in-principle for the Group F9E test was submitted to AEC-RDT. The proposed EBR-II irradiation test will be a thirty-seven fuel pin bundle with grid spacers providing intermediate support for the pins. This is the first grid spacer type subassembly designed by General Electric.

The current status of the Task F irradiation program is summarized in Tables 2-4 and 2-5.

2.6 TASK G-6 - CORE DESIGN ANALYSIS

Modifications to the COBRA flow mixing code are complete and sodium test problems are now being run. A study was conducted to determine how well a grid spacer subassembly experiment in EBR-II would simulate loading conditions expected in an early LMFBR. Major loads and stresses due to fuel pin bowing were found to be comparable.

The problem definition phase and state-of-the-art review of fuel failure effects was completed, forming the basis for selection of key areas to be analyzed in the sensitivity study.

A survey and study of the transmission, reflection and absorption of a shock impulse incident on a pin or channel wall was completed. The results were applied to understanding an ANL channel explosion test.

Formulation and coding was completed on a computer program to calculate the approximate deformation, strain and energy absorption of a hexagonal duct in the plastic distortion regime.

A state-of-the-art survey of analytical and experimental programs related to thermal interaction was completed. The FFTF thermodynamic analysis of fuel-clad-sodium thermal interaction was reviewed and a one-dimensional dynamic analysis of acoustic wave propagation was completed.

2.7 TASK G-10 - KINETICS ANALYSIS

Additional triggering options for initiating flow blockages were added to the FREADM code, and a heat balance was incorporated into the steady-state temperature calculations. Voiding reactivity effects calculated with

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

Capsule	Description			Design Parameters							Operating Conditions					Status
				Fuel				Cladding Material	Irradiation Facility	Cladding						
				Diameter (inch)	Length (inch)	Density (% TD)				Composition	Surface Temp (°F)	Peak Power (kW/ft)	Burnup (MWd/Te)			
						Pellet	Smeared	PuO ₂ (%)	O/M							
E1AA	Continuous	in-pile	thermal	0.219	6.0	92.0	91.0	20	1.97	316	GETR	1300	15	0	report in review	
E1E	Relative smeared conductivity at high burnup irradiation to high burnup and rabbit test E1F.			0.218	1.9	94.7	94.5	20	2.00	347	MTR	875	20	85,000	report in review	
E1F				0.218	1.9	94.6	94.8	20	2.00		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	316	GETR	1100	23	2,000	examination complete	
E1J-2	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.94	316	GETR	1100	23		examination in progress	
E1J-3	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.94	316	GETR	1100	23		awaiting examination	
E1J-4	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.97	316	GETR	1100	23		awaiting examination	
E1J-5	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.97	316	GETR	1100	24	2,000	examination complete	
E1J-6	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	1.97	316	GETR	1100	23		examination in progress	
E1J-7	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	2.00	316	GETR	1100	24	2,000	examination complete	
E1J-8	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	2.00	316	GETR	1100	23		awaiting irradiation	
E1J-9	Effect of restructuring on smeared thermal conductivity.			0.217	5.0	92.0	90.5	25	2.00	316	GETR	1100	23		examination in progress	
E2C-1	High burnup. Reference fuel irradiated at near reference thermal conditions.			0.218	5	84.5	82.2	25	2.00	347	GETR	1060	20	121,000	report in review	
-2				0.218	5	94.3	91.4	25	2.00		GETR	1060	20	113,000		
E2E	Fuel swelling. Four separate pins at three power levels.			0.100	3	98.5	95.4	25	2.00	304	MTR	785	11.6	199,000	report in review	
F				0.100	3	96.9	95.1	25	2.00		MTR	655	9.4	172,000		
G				0.100	3	97.0	95.2	25	2.00		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	316	GETR	1100	21	124,000	report in review	
-2				0.218	5	96.0	88.5	25	1.98			1100	21	125,000		
E2R	Center void formation rate at lower burnup			0.216	10	94.0	91.0	25	1.98	316	GETR	1000	20	2,000	fabrication in progress	
E5A-1	Powder and pellet fuel comparison at low burnup.			0.218	4.9	86.8	84.3	25	2.00	347	GETR	1100	27	2,000	report in review	
-2				0.218	4.9	84.1	84.1	25	2.00		GETR	1100	27	2,000		
E5B-1	Power and pellet fuel comparison at low burnup.			0.218	1.9	85.8	84.1	25	2.00	347	GETR	1400	20	138,000	report in review	
-2				0.218	4.9	84.1	84.1	25	2.00		GETR	1400	20	138,000		

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

GROUP	PURPOSE AND MAJOR PARAMETERS	TOTAL NUMBER OF PINS IN GROUP	DATE IN	PROJECTED DATE OUT	ACTUAL DATE OUT	NUMBER OF PINS IN S/A	S/A	POSITION	RANGE OF AVERAGE (1) PIN POWERS kW/ft	TARGET BURNUP At. %	MWd/T _e	AVERAGE CALC. BURNUP (2)		PEAK CALC FLUENCE (3) (nvt × 10 ⁻²³)		STATUS
												At. %	MWd/T _e	TOTAL	E>.1 MeV	
F-0	Low power swelling rate of high density fuel at various burnup levels	5	07/16/65	—	08/15/68	1	XG02	7A1	8.71	3.91	35,000	3.88	34,702	0.322	0.2963	Post exam complete
			07/16/65	11/20/71	—	2	XG03	7D1	5.44-5.89	8.95	80,000	6.10	54,489	0.522	0.4799	In reactor
			07/16/65	08/07/72	—	2	XG04	7B1	5.40-6.21	11.18	100,000	6.78	60,608	0.586	0.4613	In reactor
F-1	Short time performance and proof test	6	05/06/65	08/07/72	—	2	XG01	4F2	12.17-14.31	0.22	2,000	0.21	1,904	0.017	0.0159	Reported in GEAP-5570
F-2	Medium power scoping irradiation. Major parameters include smear density, stoichiometry, cladding-coolant interactions and burnup.	21	09/03/65	—	03/06/68	9	XG05	4C2	11.28-13.74	8.39	75,000	6.69	59,800	0.569	0.5237	Report in publication
			09/03/65	—	02/20/67	12	XG05	4E2	8.34-14.65	5.59	50,000	5.02	44,897	0.422	0.3879	Report in review
			02/23/60	05/15/70	—	4	XO50	4C2	10.57-12.68	11.19	100,000	11.19	100,017	0.981	0.9021	In reactor
F-3A	Bare pin irradiation to evaluate fuel cladding and cladding-coolant interaction	16	08/15/68	—	06/30/69	16	XO40	5B2	12.59-14.32	5.62	50,000	2.88	25,601	0.236	0.2174	Interim exam
			10/04/69	07/24/70	—	16	XO40A	5B2	12.33-14.01	5.62	50,000	4.64	41,324	0.386	0.3547	In reactor
F-3B	Density and stoichiometry effects for encapsulated fuel pins.	18	11/21/67	06/19/70	—	18	XO27	4B3	11.28-12.58	8.99	80,000	8.27	73,527	0.700	0.6443	In reactor
F-4	Low density fuel behavior at high heat ratings.	9	05/09/66	—	06/28/67	9	XO11	2F1	13.57-14.30	5.64	50,000	3.64	32,255	0.298	0.2741	Post exam complete
			08/15/68	—	12/24/68	7	XO39	3C2	13.12-13.55	9.03	80,000	4.85	42,964	0.400	0.3679	Interim exam
			01/10/70	08/05/70	—	7	XO70	3E1	12.75-13.32	9.03	80,000	6.34	56,155	0.528	0.4853	In reactor
F-5	Fuel performance at peak FCR cladding temperatures.	19	05/28/69	01/01/72	—	19	XO64	4F2	12.54-13.35	11.25	100,000	3.83	34,106	0.315	0.2902	In reactor
F-6	Fuel-cladding interactions under accelerated swelling	4	03/24/66	—	04/24/69	4	XO10	7F3	8.45-99.17	5.60	50,000	5.71	51,014	0.480	0.4416	Post exam complete
F-7	Pre-irradiated axially restrained fuel prior to transient test. Main variable is fuel density.		12/23/67	—	12/23/67	2	XO15	4A2	—	—	—	—	—	—	—	Removed from reactor
			01/23/69	—	02/23/69	4	XO50	4C2	—	-5.48	50,000	4.50	41,040	—	—	In reactor
F-8A	Effect of density on fuel swelling rate	16	01/13/67	—	08/02/69	7	XO19	6D2	8.36- 8.91	5.63	50,000	4.97	44,161	0.412	0.3791	Post exam complete
			01/13/67	11/15/71	01/31/70	9	XO20	6B5	8.20- 8.91	11.26	100,000	6.15	54,615	0.524	0.4816	Interim exam in progress
F-8B	Effect of void deployment on fuel cladding reaction.	19	07/25/68	03/18/71	—	19	XO36	7E1	4.64- 6.54	5.63	50,000	3.62	32,213	0.305	0.2806	In reactor
F-9A	High power, high burnup, unencapsulated fuel pin irradiations.	37	02/20/69	10/09/71	—	37	XO43	4D2	11.86-13.14	11.26	100,000	4.95	43,988	0.411	0.3777	In reactor
F-9B	Medium power, high burnup, unencapsulated fuel pin irradiations.	37	05/23/69	09/22/72	—	37	XO62	6F3	8.95-10.62	11.26	100,000	2.80	24,927	0.229	0.2107	In reactor
F-9C	High power, high burnup, unencapsulated fuel pin irradiations.	37	04/03/69	02/17/72	—	37	XO56	5C2	10.43-11.47	11.24	100,000	4.18	37,221	0.345	0.3174	In reactor
F-9D	Medium power, high burnup, unencapsulated fuel pin irradiations.	37	04/24/69	01/17/73	—	37	XO58	6F1	7.46- 8.88	11.25	100,000	3.02	26,874	0.250	0.2296	In reactor
F-9E	High power, high burnup,	37	—	—	—	37	—	—	—	—	—	—	—			Design in progress
F-10A	Cladding performance at peak cladding temperatures.	19	—	—	—	19	—	—	—	—	—	—	—			Fabrication in progress
F-10B	Cladding performance at peak cladding temperatures	19	—	—	—	19	—	—	—	—	—	—	—			Fabrication in progress
F-10C	Fuel pin performance at	19	—	—	—	19	—	—	—	—	—	—	—			Design in progress

* All calculated power, burnup, and fluence numbers are based on the fission rates provided in the “Guide for Irradiation Experiments in EBR-II”.

1. Values are the calculated axial average powers. Ratio of peak to average power is 1.125.

2. Burnup values are axial average values for the pin in the subassembly with the highest fuel burnup regardless of position in subassembly. All values are corrected for fuel depletion. Burnup values in MWd/T_e are based on a value of 192 MeV/fission.

3. Fluence values reported are calculated for the peak axial flux for the pin in the subassembly located closest to the center of reactor core.

Table 2-5
SUMMARY OF TASK F THERMAL FLUX IRRADIATIONS

Capsule		Description	Design Parameters							Operating Conditions					Status
			Diameter (inch)	Fuel Length (inch)	Density (%TD)		Composition		Cladding Material	Irradiation Facility	Cladding Surface Temperature (°F)	Peak Power (kW/ft)	Burnup (MWd/Te)		
					Pellet	Smeared	PuO ₂ (%)	O/M							
New No.	Former No.														
F12A	(E5F)	Axial fuel movement versus burnup	0.214	10.0	92.0	87.1	25	1.98	Type-316 stainless steel	GETR	850	17	44,000	REPORT IN PROCESS	
F12B	—	Axial fuel movement and fission gas release versus burnup	0.214	10.0	92.0	87.1	25	1.97	Type-316 stainless steel	GETR	1200	20	100,000*	IN FABRICATION	
F12C	(E5C-1)	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	EXAMINATION COMPLETE	
F12D	(E5D-1)	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-1)	Powder, pellet, and annular fuel comparison at high burnup	0.215 (Annular)	16.0	95.0	83.3	25	1.96	Type-316 stainless steel	GETR	1160	19.5	130,000		
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	40,000	INTERIM EXAMINATION	
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	40,000		

* Target

FREADM were compared to static 2-D flux synthesis methods (BISYN).

Work continued on the space dependent kinetics models (GAKIN) and the few-group reactor kinetics methods. Instability problems in the single channel voiding model were corrected.

A sensitivity study was performed using a fuel pin failure computer model, based on pressure rise due to entrapped fission gas during fuel melting. Transient failure threshold was found to be sensitive to burnup, initial fuel density, rate of energy input, and cladding temperature. Major assumptions involved cladding strength and ductility, gas retention and location, and relative time scale for energy input.

2.8 TASK L - CLADDING DEVELOPMENT

Irradiation of four L-2 materials capsules continued in S/A X050. Irradiation of three L-6 series capsules is continuing in S/A X018B. The L-15 series capsules are waiting for reinsertion into a new subassembly. The total fluences are 8.5, 6.7, and 0.94×10^{22} n/cm² for the L-2, L-6, and L-15 series, respectively.

Design of the L-16B capsules has been completed and fabrication of 19 capsules initiated. All of the capsule hardware is on hand and ready for assembly. Design of the L-16A capsules has been initiated.

A thermal analysis of MT-3 has been performed and a peak operating temperature of 1280 to 1315°F has been established for the Type 304 holding strip and tensile specimens. The calculated axial temperature is in close agreement with the temperatures established by low melting alloys.

Immersion density measurements have been completed on Type 321 irradiated at 538 to 638°C to a peak fluence of 4.0×10^{22} n/cm² $E_n > 0.1$ MeV. Peak swelling measured was 1.7% which occurred at the higher fluence levels. The swelling of Type 321 in the above temperature range appears to be more fluence dependent than temperature dependent.

The status of the current Task L irradiation program is summarized in Table 2-6.

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 intimate 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.

2.9 TASK N - CORE PHYSICS ANALYSIS

Cooperative activities involving the nuclear data file ENDF/B, Version II, included: a) Regeneration of unresolved resonance parameters for Pu-239, b) Phase I data testing of data for iron, and c) Generation of multi-group data for Phase II testing.

Detailed checkout of the first version of the nuclear data processing code ENDRUN was completed. Modifications were made to Version II to conform to ENDF/B format and procedural changes. Modifications to the TDOWN-1 code are in progress in order to be more consistent with the elastic removal cross section generated by ENDRUN when narrow energy groups are being used.

A study of the effects of current data uncertainties on fast reactor design was performed.

Modification of the DOT2DB code to common data interfaces is nearing completion.

Topical reports on the BICYCL and DOT2DB codes have been distributed and reports on the ENDRUN-1 code and Pu-239 data evaluation for ENDF/B have been completed.

2.10 TASK-P - FUEL ELEMENT DESIGN ANALYSIS

Differential thermal expansion between the fuel and clad during the startup of fresh fuel was considered in detail. The effects of radial cracks, plastic fuel densification and fuel extrusion are included in this model (START). The results of this work indicate that the value of the contact conductance at the fuel-clad interface is important. Predictions of this startup model were compared with the observed diametral increase of 18 mixed oxide pins irradiated in EBR-II to a burnup ranging from 2,000 to 74,000 MWd/Te. Close agreement was found between the experimental data and the START model when a contact conductance value of 2500 Btu/hr-ft²-°F was assumed.

All of the diametral deformation of the EBR-II pins which have been examined can be attributed to initial startup interference and subsequent clad swelling. No evidence of steady state fuel swelling was observed so far.

The BEHAVE computer code is now being constructed for the purpose of predicting nominal pin performance. The STATUS computer code is being developed to predict cladding local strain levels and remaining clad life.

The technical feasibility of fast reactors depends on quantitative knowledge of such 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 are equivalent to (or possibly better than) nonvented designs.

The technologies of defected, vented, and sodium-bonded fuels are closely related. Although directed

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

Series	Row-S/A	Capsules	Capsule Materials	Number of Specimens		Target 10 ²³ nvt	Total Fluence		Projected Completion Date**
				Biaxial	Tensile		March, 1970		
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 – X009	2	Type 316 stainless steel	10	20	0.20	0.19	0.15	Dec., 1966
L-4'	2 – X014	5	One each of Types 304, 321, and 347 stainless steels; and two of Incoloy-800.	25	50	0.20	0.17	0.10	Apr., 1967
L-10	4 X016	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 X025	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 – X039	1	Type 321	3	16	0.76	0.57	0.42	Dec., 1968
MT-3‡	4 – XA08	1	16 Incoloy-800, 16 Inconel-625 & 16 Hastelloy-X tensile specimens	None	48	1.00	0.69	0.51	June, 1968
Irradiation in Progress									
L-2	4 – XG05 4 – X050	4	One each of Types 304, 316, and 347 stainless steels, and Incoloy-800.	12	64	3.2	0.75	0.56	1972
L-6	2 – X018 4 – X018A 4 – X018B	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	0.59	0.37	1971
L-15	3 – X039	4	All five alloys	None	288	0.30	0.14	0.10	1970
MT-4‡	4 – XA08	1	16 Incoloy-800, 16 Inconel-625 & 16 Hastelloy-X Tensile Samples	None	48	3.2	0.71	0.53	1972

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

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

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

‡ Fluence based on flux wire analysis conversion factors.

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

primarily at studying the behavior of defected fuel, Task B also has included development of vented and sodium-bonded fuels. Although vented pins are a prospective reference design for 300 Mw and 1000 Mw plants, funding limitations have made it necessary to suspend this aspect of the work.

3.2 DEFECTED FUEL TESTING

3.2.1 Forced Convection Capsule (B9C)

B9C is designed to investigate the performance of defected hypostoichiometric mixed oxide fuel at prototypic LMFBR conditions in flowing sodium. This experiment will include an investigation of the effect of sodium purity on fuel specimens failed during full power operation. Fabrication and testing of capsule components were completed and final capsule assembly is in progress. Components fabricated and tested during the current reporting period include proof testing of the defect mechanism, gas tagging of the fuel pins and calibration of the flowmeter.

The defect mechanism in B9C will be the notched plug.⁽¹⁾ A cross-section of the test section showing the notched plug in the fluted liner of the test section is shown in Figure 3-1. Tests were conducted to determine the strength of the notched plug and reliability of the actuator mechanism. Development tests to date have included testing of individual components, both separately and together in complete system form.

Hydrostatic pressure tests were conducted on clad samples containing the notched plug. The tests also included a helium leak check at 500 psig of three specimens with no leaks detected. Two pressure tests of the 20% cold-worked cladding indicated burst pressures of 13,000 psi with the failures in the vicinity of the notched plug. Similar tests of the annealed tubing indicated rupture at 11,000 psi with the failure occurring away from the notched plug area. Testing was also continued (see 33rd Quarterly Report) to determine the forces necessary to pull the notched plugs from the clad. All tests conducted with temperatures ranging up to 1200°F showed a range of 4 to 8 pounds required to cause defecting.

Figure 3-2 shows a schematic of the test equipment mocking up the capsule B9C defect actuator system. The details of the attachment of the actuator wire (0.031 inches OD - annealed 304SS) to the fuel pin and defect plugs is shown in Figure 3-3. From the fuel pin, the actuator wire passes through an angled guide tube (7°) which mocks up the passage of the wire through the capsule gas de-entrainer. The actuator wire is then mechanically joined to a rod which connects to two spring bellows (spring constant 15 lb/in). The bellows are required to retain the gas tight

integrity of the capsule upper bulkheads. A silver solder joint connects the rod and an 8 foot section of 1/16 inch connector cable which is in turn mechanically connected to the Controlex cable. The connector cable is sheathed in a 1/4 inch O.D. stainless steel tube. The use of the smaller connector cable instead of Controlex cable for the full length is due to space limitations at the top of the capsule. The Controlex cable is connected mechanically to the actuator device.

The actuator device consists of a threaded rod which is displaced by screwing down on a threaded handle. A spring scale is placed in line with the threaded rod to give loading data at the actuator device. Also included are dial indicators, one at the actuator device and one by the spring bellows, to indicate displacement.

Individual component tests conducted prior to full mockup testing included tensile tests of the 0.31 inch actuator wire (annealed 304SS) at 1100 and 1200°F.

Results from these tests indicated a break strength of 50 to 55 pounds and 38 to 44 pounds, respectively. Tensile tests were also made on the silver braze joint between the 1/16 inch connector cable and the 1/8 inch stainless steel. The tests were conducted at room temperature (this joint will be at essentially room temperature because of the reactor cooling water) with resulting ultimate tensile forces of 136 to 146 pounds. These tests indicated that the strength of the silver solder joint exceeded that of the connector cable.

Seven full mockup tests were conducted in the furnace of the FCC cleanup and fill system. The tests were conducted with dummy fuel pins at 1200°F. The results of the tests are summarized in Table 3-1. Data from a typical test (No. 7) is shown in Figure 3-4. The force plotted as the ordinate is the force measured on the spring scale. The displacement plotted as the abscissa is the displacement measured by the dial indicator positioned at the spring scale. The test was conducted by displacing the threaded screw rod in 0.010 inch increments. The plot of the data shows six distinct regions which identifies the reaction of the system to gradual loading. The first region (a) shows the resistance due to the bellows (mockedup by springs) as slack is pulled out of the system between the bellows and the first defect plug. In region (b) the distinct change in the loading rate indicates the additional resistance of loading the notched plug. At a displacement of 0.34 inches and a load of 6 pounds on the notched plug, the plug failed. Region (c) corresponds to the slack in the wire between the two plugs and a partial straightening of the bend through the first plug. The free displacement due to region (c) allows the spring scale to drop in load slightly. In region (d) the second plug is being loaded and the bend in the wire at the first plug is completely straightened. At a displacement of 0.55 inches and 9 pounds force on the second plug an indication of the second plug giving way is seen. However,

(1) 33rd Quarterly Reports

Figure 3-1. Cross Section of B9C Test Section

Figure 3-2. Mockup Test of B9C Defect Actuator System

Figure 3-3. Details of Attachment of Actuator Wire to B9C Fuel Pin and Defect Plugs

Table 3-1
SUMMARY OF B9C DEFECT MOCKUP TESTS

Test No.	Temperature °F	Maximum Force Applied At Remote Actuator	Force To Fail First Plug	Force To Fail Second Plug	Location Of Fuel Pin In Fluted Liner	Comments
1	1200	26	5	6	No Liner	Successful.
2	1200	30	13	13	Centered in Liner	Test cancelled when total force reached 30 lb with no indication of defecting. (Examination indicated that first plug was defected and the second plug was 90% defected.)
3	1200	23	11	11	0.015 in. Offset	Successful. The test assembly was improved by increasing clearances in actuator system and offsetting dummy pins in fluted liner
4	1200	18	7.5	10	0.015 in. Offset	Successful. System included flowing N_2 gas to prevent oxidation of hardware.
5	1200	18	7.5	8.5-10	Centered in Liner	Successful. Fuel pin centered in fluted liner.
6	1200	18	10	7	Centered in Liner	Successful.
7	1200	20.5	6	9-12	Centered in Liner	Successful. N_2 gas was eliminated.

complete shearing of the plug has not taken place. At a displacement of 0.67 inches and 12 pounds force, the second plug completely sheared off. The region (e) corresponds to the 1/8 inch slack in the wire after failure of the second plug and before the wire is restrained by the bottom guide pin. In region (f) the wire is loaded to insure that all the slack is out of the system.

A failure detection system is being incorporated into this capsule to permit positive identification of fuel pin failure. This system will sample the capsule cover gas which will be subsequently analyzed for tag gases (Argon -36 and -40) originally loaded into the fuel pins. Tests were conducted to establish the amount of tag gas which could be injected into each pin prior to the final end plug weld. Dummy pins which duplicated the real pins except for the use of steel pellets were tagged with argon. Gas chromatographic analysis of the gas taken from dummy pins indicated that approximately 10% of the plenum volume was argon. This amount of tag gas released in the capsule would be diluted to approximately 30 ppm (total of 60 ppm for the two pin segments) in the capsule plenum. This is six times the limit of detection. Based on these results, the B9C fuel pins were tagged, and fabrication completed.

The B9C flowmeter was calibrated in the FCC cleanup and fill system. The flowmeter was put into a test stand which fully mocked up its location in the capsule on a one-to-one scale. Results from two calibration runs with sodium at 890°F and 1040°F are shown in Figure 3-5. Also

shown is the calculated output of the flowmeter based on the measured strength of the magnet.

3.2.2 Forced Convection Capsule (B9D)

Design of forced convection capsule B9D continued. This experiment will investigate the performance of defected mixed oxide fuel at prototypic LMFBR conditions in flowing sodium. This capsule will have essentially the same design as B9C but will be operated at 1200°F to determine the effect of sodium coolant temperature on the behavior of failed fuel. In addition, one of the two fuel specimens in B9D will be failed in the plenum region (rather than the fueled region) to determine the effect of defect location on failed fuel. Table 3-2 summarizes the design parameters of the fuel pin and capsule.

3.2.3 Forced Convection Capsule (B9B)

Design of forced convection capsule B9B continued. This experiment will investigate the performance of mixed oxide fuel which fails after reaching a burnup of 25,000 MWd/Te. The capsule will contain one mixed-oxide fuel specimen 24 inches in length topped by a 15 inch UO_2 blanket and fitted with continuous pressure sensing instrumentation. The fuel pin will be intentionally defected after an exposure of 25,000 MWd/Te and will be further irradiated (after defecting) to 30,000 MWd/Te burnup. This will permit comparison with previous specimens in the B9 series which have been defected at zero or low burnups (approximately 1000 MWd/Te). Continuous monitoring of fuel pin

Figure 3-4. B9C Defect Test No. 7

plenum pressure will permit estimation of the fission gas release rate from defected mixed-oxide fuel pins at high burnups while operating at near prototypic conditions.

Because of the possible sudden release of accumulated fission gas upon defecting from the long, high burnup fuel pin in capsule B9B, tests were conducted to determine the efficiency of the present gas de-entrainer design. Figure 3-6 shows a schematic of the test apparatus used. Water and air were used to simulate the sodium and fission gas.

As noted in Figure 3-7, the de-entrainer efficiency is very low at high flow rates. The results of the tests are in general agreement with expected results. The present de-entrainer depends essentially on the buoyancy of the

bubbles as the driving force for separation. At the high sodium velocities, the drag on the bubbles is large compared to their buoyancy and sweeps them past the de-entrainer. On the basis of these tests and the requirements of capsule B9B, design and testing have been initiated to develop a high capacity gas de-entrainment device.

3.2.4 Development of Fuel Pin Defecting Mechanisms Electric Arc Device

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 and burnup level prior to initiating failure. Preliminary testing of an electric arc as a means of

Figure 3-5. B9C Flowmeter Calibration

producing defects in the clad was completed. Thin foils (0.005 inches thick) of Type 316 stainless steel were successfully penetrated using a tungsten electrode and a one second burst of 18 ampere current in air and a one second burst of 30 ampere current in distilled water. Testing is continuing and is presently being refined to more fully simulate a test geometry compatible with FCC space limitations. In addition, the next series of tests will simulate the electrical and hydrodynamic conditions of sodium flow.

Differential Fuel/Clad Expansion

Modification, irradiation, and non-destructive examination was completed on capsule E1J-B2. This test was

conducted to further explore the differential expansion defect mechanism. In this defect mechanism, the clad is notched longitudinally on the outside surface. The fuel-clad gap is minimized, which results in large cladding stresses due to the thermal expansion of the fuel ($O/M = 2.00$) under full power conditions. The capsule was irradiated in the GETR trail cable facility for 66 hours at 15 KW/ft. It was then thermally cycled five times from 5 to 15 KW/ft and finally raised to 19 KW/ft and held for two minutes. Post-irradiation neutron radiography showed the fuel pin failed in the notched defect area with an average 0.004 inch diametral increase. Based upon data from (E1J-B1) which did not fail at 17.5 KW/ft(2), it is assumed that the high power in this experiment caused failure.

Table 3-2
B9D FUEL PIN AND CAPSULE DESIGN PARAMETERS

FUEL PIN	B9D-1 (Upper)	B9D-2 (Lower)
Fuel		
Material	Co-Precipitated (Pu, U) O ₂	
Ratio Pu/Pu + U	0.25	
Ratio U-235/U-235 + U-238	0.90	
Stoichiometry	1.97 ± .010	
Pellet Density (Theoretical)	94 ± 2%	
Pellet Dimensions:		
Outside Diameter (in.)	0.215 ± .001	
Length (in.)	0.250 Nom.	
Column Length (in.)	4.25 ± 0.125	
Cold Diametral Fuel-Clad Gap (in.)	0.005 Nom.	
Gas Plenum Volume	0.6 cc	
Gas Plenum Location	Above Fuel	Below Fuel
Gas Plenum Length (in.)	1-3/8	
Tag Gas	Ar-36	Ar-40
BLANKET AND INSULATOR		
Material	Natural or Depleted UO ₂	
Stoichiometry	2.00 ± 0.003	
Pellet Dimensions:		
Outside Diameter (in.)	0.215 ± 0.001	
Length (in.)	0.125 ± 0.0025	
Blanket Length (Above Fuel) (in.)	0.125	
Insulator Length (Below Fuel) (in.)	0.125	
CLAD		
Material	316 SS Tubing, 20% C.W.	
Outside Diameter (in.)	0.250 ± 0.001	
Wall Thickness (in.)	0.015 ± 0.001	
FUEL PIN DEFECT		
Type	Notched Plug	
CAPSULE OPERATION		
Sodium Coolant Exit Temperature	1200°F	
Sodium Coolant Purity	5 PPM	
Sodium Velocity Past Fuel Pins	25-30 ft/sec	
Coolant Temperature rise	50°F	
Peak Fission Power of Pins	18-20 Kw/ft	

After failure was observed, it was decided to reinsert E1J-B2 to obtain data on the short-term swelling kinetics of ruptured mixed-oxide fuel specimens. Capsule E1J-B2 was irradiated three additional times with irradiation periods of 1/2, 2, and 24 hours, respectively. The diametral expansion of the fuel pin was found to increase after each irradiation period (determined from neutron radiography). Fuel pin diameter change is summarized in Figure 3-8 along with the data from the high stoichiometry (O/M = 1.99) specimen of capsule B9A. A straight line with a slope of 1/4 was found to fit the data.

3.3 SODIUM/FUEL/CLADDING COMPATIBILITY EXPERIMENTS

3.3.1 Capsules B8C and B8D

Compatibility experiments are conducted to determine the effects of sodium in contact with mixed oxide fuel with particular concern for the sensitivity of the

parameters density, stoichiometry, burnup, and plutonium enrichment.

Post-irradiation diametral measurements of the fuel pins from capsules B8C and B8D were completed. The greatest cladding diametral increase was observed for fuel pin B8D-1 which contained fuel of 1.97 stoichiometry and 40 w/o PuO₂. Figure 3-9 shows the pre- and post-irradiation profilometry traces for fuel pin B8D-1. The greater diametral increase observed for the high density portion of the fuel column (bottom fuel segment) would appear to be due to the axial restraint imposed by the insulator pellet and the fuel above it. Table 3-3 lists the average diametral increases measured on the cladding of the five fuel pins.

Figure 3-10 shows the measured fuel pellet expansion with burnup for the capsule B8C and B8D fuel pins. From ~40,000 MWd/Te to ~113,000 MWd/Te burnup, the fuel shows an expansion rate $\Delta V/V$ of 1.4% per 10,000 MWd/Te burnup. Currently reported values for fuel expansion due to fission gas and solid fission product buildup in the fuel fall

Figure 3-6. Schematic of the De-Entrainment Test Apparatus

Figure 3-7. De-Entrainer Efficiency as a Function of Flow Rate

in the range of $0.25\% \Delta V/V$ per 10,000 MWd/Te.⁽³⁾ The greater volume change observed in the present sodium bonded pins is presumed due to the formation of low density sodium-fuel compounds.

On Figure 3-10 it is to be seen that the gross expansion for fuel pins B8D-1, B8D-2, and B8D-3 are similar and appear to be independent of the fuel stoichiometry, density, and Pu content of the fuel. These data are contrary to the results of forced convection capsule experiment B9A where more fuel expansion was observed for the 1.99 O/M fuel compared to the 1.95 O/M fuel. This suggests that the B8 specimens had limited sodium available to react with the fuel. It is also of interest that the gross

volume change of pin B8C-2 lies far from the curve but that its net change, that is the change when the change in the center void is taken into account, is impressively close to the curve.

Metallographic examination of the fuel pins from capsules B8C and B8D were completed. Figures 3-11 and 3-12 show the post irradiation appearance of the fuel in each pin. A grey sodium-fuel reaction phase was observed at the fuel-cladding interface in all pins. This reaction phase ranged in width from 0.002 to 0.010 inches. Metallographic examination of the fuel pins from capsule B8C which operated at 21 Kw/ft showed evidence of center line melting. Fuel pins in capsule B8D operated at 17 - 18 Kw/ft

Figure 3-8. Swelling Data from EIJ-B2 and B9A

Table 3-3

AVERAGE DIAMETER INCREASE MEASURED FOR CAPSULE B8C AND B8D FUEL PINS

Fuel Pin	O/M	W%/Pu	Fuel Burnup (MWD/Te)	Average Cladding Diametral Increase	
				Low Density Fuel ΔD	High Density Fuel ΔD
				in.	in.
B8C-1	1.97	25	113,000	—	0.0014
B8C-2	1.97	25	57,000	—	0.0007
B8D-1	1.97	40	41,000	0.0008	0.0034
B8D-2	1.97	25	43,500	0.0008	0.0018
B8D-3	1.935	25	41,000	0.0007	0.0013

and showed no evidence of melting. The cladding of all pins operated at an ID surface temperature of between 1000 and 1100°F and showed no evidence of attack.

3.4 FISSION PRODUCT DISTRIBUTION

3.4.1 Miniloop (Out-of-Pile Radioactive Fission Product/Sodium Loop)

Diameter measurements and sectioning of the fuel sample operated in the miniloop were completed. Metal-

lographic examination showed the fuel to be extruded into the slit area of the cladding (although not projecting beyond the cladding). Figure 3-13 shows the pre- and post-test diametral measurements of the fuel sample. The diametral changes correspond to a 3% volume increase of the fuel. Because of the observed fuel swelling, the extent of fuel loss during the test due to erosion by the sodium through the slit in the cladding could not be precisely determined. However, it appeared to be small.

Figure 3-9. Fuel Pin B8D-1—Pre- and Post-Irradiation Profilometry Traces

Figure 3-10. Fuel Expansion Measured for Capsule B8C and B8D Fuel Pins (Sodium Bonded)

FUEL PIN B8C-1

O/M 1.97
25 wt% PuO₂
BURNUP 113,000 MWd/T_e

FUEL PIN B8C-2

O/M 1.97
25 wt% PuO₂
BURNUP 57,000 MWd/T_e

Figure 3-11. Appearance of Fuel in Capsule B8C Fuel Pins

LOW DENSITY FUEL (85% THEO.)

FUEL PIN B8D-1

O/M 1.97
40 wt % PuO_2

FUEL PIN B8D-2

O/M 1.97
25 wt % PuO_2

FUEL PIN B8D-3

O/M 1.935
25 wt % PuO_2

HIGH DENSITY FUEL (94% THEO.)

Figure 3-12. Appearance of Fuel in Capsule B8D Fuel Pins

Figure 3-13. Pre- and Post-Test Cladding Diametral Measurements of Miniloop Fuel Sample

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 were 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.

4.2 HIGH BURNUP FUEL EXPERIMENTS

4.2.1 EBR-II Irradiation

The final four TREAT specimens to be irradiated in EBR-II (specimens C4E, F, K, and L in capsules F7A, B, E, and F respectively) have accumulated an average peak burnup of ~53,000 MWd/Te. The subassembly containing these specimens (X050) will be removed from the EBR-II core and disassembled at the completion of the present cycle (cycle 42). Following this disassembly, the fuel specimens will be returned to the Vallecitos Nuclear Center for nondestructive examination, and reencapsulation into TREAT capsules for transient irradiation.

4.2.2 TREAT Capsule Fabrication

Assembly of the TREAT capsules for the transient irradiation of the EBR-II preirradiated fuel specimens C4G and C4H, plus test specimen C4X (zero-burnup, highly-enriched UO_2 fuel), are 90% complete.

The capsule containing the prototype C4X specimen will be the first capsule of the series to undergo the complete procedure of assembly and irradiation in TREAT. As such, the C4X capsule will serve as a proof test of the remote procedures involved in the closure welding and sodium filling, the final insertion of the inner capsule into the pressure vessel of the TREAT capsule, and as a verification of the TREAT irradiation characteristics of the capsule design. The development of the remote reencapsulation procedures will permit TREAT testing of the high burnup specimens C4G and H, and eventually C4E, F, K, and L (all of which have 50-60,000 MWd/Te fast-flux burnup in EBR-II).

Currently, the C4X fuel specimen has been remotely inserted into the inner capsules, the closure weld completed, and the remote sodium fill operation checked out. All of these remote operations on the C4X capsule have been performed at a simulated remote station to better facilitate the establishment of procedures for performing the actual remote operations in a hot-cell environment

during the reencapsulation of the actual high burnup specimens.

It is expected that the assembly of all three initial capsules (C4X, G, and H) will be completed and that the C4X capsule will be subjected to transient irradiation during the next quarter.

4.2.3 Analytical Prediction of Experimental Failure Thresholds

The Task G-10 fuel failure model is being applied to the correlation of failure time and fuel melt volumes for the previously tested Series IV zero-burnup specimens C4A and C4B (both of which failed during TREAT transient irradiation). The initial results indicate a very promising failure prediction correlation.

4.3 AXIAL FUEL RELOCATION EXPERIMENTS

This test series has been completed, and a topical report (GEAP 13543) was issued in September 1969.

4.4 DEFECTED OR SODIUM-BONDED FUEL EXPERIMENTS

Final capsule assembly was completed on this test series which involves two three-pin capsules to investigate the transient performance of defected or sodium-bonded fuel. Each of these experiments incorporate two sodium-filled pins (24-inch length fuel, 15-inch length upper blanket), using both pellet and powder fuel, plus a normal gas-bonded pin. The three pins in each assembly are separately encapsulated and are arranged in a triangular array. The two capsule assemblies (C6A, C6B) were shipped to the TREAT facility and a calibration transient performed on Capsule C6B. Analysis of this calibration transient data indicates satisfactory performance. Figures 4-1 through 4-3 which show the sodium coolant temperatures during the transient, illustrate reasonable correlation between calculated and actual test data. The test transient for C6B is scheduled for late April. Since the two capsules are identical, a calibration transient will not be performed on C6A and the test transient is scheduled for early May.

The 12-channel oscillograph recorder purchased to augment existing instrumentation capabilities at TREAT was employed for this test. No difficulties were encountered.

4.5 MARK II INTEGRAL SODIUM LOOP

Detailed discussions on the Mark II loop design and its capabilities were held with Argonne National Laboratory

personnel. The meeting was held to supplement the preliminary evaluation initiated earlier (33rd Quarterly Report).

ANL has completed a preliminary study to determine the feasibility of modifying the loop to accommodate a

longer test section (at present they are limited to ≈ 13 in of active fuel, centered over the TREAT midplane) Although, it is apparently feasible, no detailed design effort is currently in progress for loop modification to test specimens approaching demonstration or FFTF pin lengths

Figure 4-1. Correlation of Measured and Calculated Sodium Temperatures in Capsule C6B-1.

Figure 4-2. Correlation of Measured and Calculated Sodium Temperatures in Capsule C6B-2

Figure 4-3. Correlation of Measured and Calculated Sodium Temperatures in Capsule C6B-3

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

5.1 GENERAL

Rapid loss (or reduction) of cooling to a localized region of a reactor core, is one of the major safety-related concerns for LMFBR and for FFTF. Such coolant starvation might be a result of local blockages, fuel distortion, debris, or failure of the pumps. The questions of fuel response to such starvation, potential for propagation to neighboring pins, and the time characteristics of signals such as temperature, flow, etc., are a primary concern in the design of the fuel bundles and instrumentation system. In addition, answers to these questions must be available to provide reliable input to accident analyses for licensing purposes.

Various theoretical studies have been conducted on the effects of loss of flow, ranging from estimates of core voiding reactivity, to the local heat transfer and dynamics of single channel boiling. Very little experimental data, however, are available on the local effects of transient undercooling and boiling in a confined channel. A need exists to perform experiments in-pile, with oxide fuel and flowing coolant, to verify models based on out-of-pile experiments and to assess secondary events produced by sodium reentry and subsequent contact with hot or molten fuel. The purpose of this task is to begin a program of such tests, utilizing the Forced Convection Capsule and/or the Mark II loop as the basic vehicle.

5.2 MOCKUP 1A

The thermal analysis of Mockup 1A (Figure 5-1), the first out-of-pile loss-of-flow experiment, has been completed. A comparison of the measured and calculated bulk coolant temperatures at various times is shown in Figure 5-2. In spite of some unexplained temperature oscillations prior to boiling inception, the agreement is quite good. In addition, the axial and radial temperature profiles at boiling have been generated and are shown in Figures 5-3 and 5-4. As can be seen from Figure 5-4, the radial temperature skewing caused by the premature loss of one of the heating elements is quite severe.

The radial temperature profile was calculated without considering the temperature skewing caused by the wire wrapped spacer. A separate calculation of the degree of temperature peaking under the spacer indicates that it should be less than 50°F. Since the exact location of the wire spacer will not be determined until the heater bundle is removed, it is difficult to establish the exact amount of wall superheat at boiling inception. Estimates at this time suggest that wall superheat at boiling inception is less than 25°F.

This information on the test section temperature distribution will be applied to an investigation of the observed bubble growth behavior. Since models pertaining

observed behavior will be evaluated to establish the relative

5.3 CAPSULE C10X

The experiment designed to establish the pressures and energies associated with molten fuel-sodium interactions (Capsule C10X) is in its final stage of design. The assembly drawing for C10X is shown in Figure 5-5.

A detailed thermal analysis of the C10X capsule has been initiated. The steady state portion of this analysis has been completed and work is in progress on the transient analysis. This analysis will provide detailed information on the temperature in the C10X test section when voiding is initiated. This temperature information will be used in conjunction with existing boiling codes to predict void growth behavior during the test.

5.4 CAPSULE C10A/C10B

The magnetic tapes containing the THTE program have been received and reassembled to the NED format. The program is an extended version of the THTD program with added capabilities for the computation of fluid flow and pressure drop as well as multichannel transient and steady state heat transfer. The test problems supplied with the program have been run to check out the as-received status of the program. Work is in progress to incorporate extensions made to the THTD program for liquid metal applications, and to complete the coding of routines where necessary to achieve full implementation of the program. Currently the binary tape usage of the program is being revised to allow more efficient computer memory utilization, and the isothermal phase change routines from the THTD program are being revised for use in THTE.

5.5 PLANNING AND DESIGN

Work has been initiated on a comparative study of the loss-of-flow testing capabilities of the ANL Mark II loop and the General Electric Forced Convection Capsule.

A thermal-hydraulic study of the ANL Mark II loop is in progress. This study is intended to determine the feasibility of using the loop to perform flow blockage and flow coast down experiments in the TREAT reactor, the specific points of interest are:

1. Whether a coolant and structure temperature distribution can be established which is representative of actual operating conditions.
2. Assuming that a satisfactory temperature distribution can be obtained, is there sufficient reactor time to properly conduct a flow coast down experiment.

Figure 5 1 Mockup 1A, Schematic

Figure 5-2. Measured versus Calculated Temperatures for the Mockup 1A Loss-of-Flow Test

Figure 5-3. Calculated Mockup 1A Bulk Coolant Temperature at Boiling Inception (2.7 sec after Pump Scram)

1

Figure 5-5. C10X Capsule

TASK E – FUEL MATERIALS PROPERTIES AND PERFORMANCE EVALUATION

6.1 GENERAL

The purpose of this task is to establish physical, mechanical and thermal properties of ceramic fuels and fuel elements. The properties are used to explain or predict the behavior of fuel elements under both steady-state and casualty conditions. Both ex-reactor and in-reactor experiments are conducted. Detailed examinations are made using an electron probe microanalyzer, x-ray diffraction and other analytical tools to identify reaction or impurity phases that may be present in irradiated specimens.

6.2 MATERIAL PROPERTY STUDIES

6.2.1 Mechanical Properties

The steady state compressive creep testing of mixed plutonium-uranium oxide samples continued. Preliminary data reported previously⁽¹⁾ have been revised by inclusion of additional data points and the availability of O/M analyses performed on samples after testing. The data obtained thus far on 93% dense samples of (U-20 w/o Pu)O_{1.95} can be described by an Arrhenius equation of the form:

$$\dot{\epsilon} = A \sigma \exp(-Q_1/RT) + B \sigma^{4.5} \exp(-Q_2/RT)$$

where:

$\dot{\epsilon}$ = steady-state creep rate (in./in./hour)

σ = initial compressive stress (psi)

$A = 5.32 \times 10^4$

$B = 1.83 \times 10^{-3}$

$Q_1 = 100,000$ calories/mole $\pm 10,000$

$Q_2 = 140,000$ calories/mole $\pm 10,000$

$R = 1.986$ calories/mole/ $^{\circ}$ K

T = temperature ($^{\circ}$ K)

These data were obtained on polycrystalline samples of approximately 22 to 25 micron grain size. Test temperatures varied from 1625 to 1475 $^{\circ}$ C and stress from $\sim 1,000$ to 8,000 psi. Post test O/M analysis indicated that the fuel O/M was ~ 1.95 rather than 1.98 nominal as reported previously⁽¹⁾. As additional creep data are obtained on the effects of stoichiometry grain size, density and plutonium content, further modifications will be made to incorporate these material variables into the empirical expression given above.

The measured activation energies (Q_1 and Q_2) for creep of the mixed-oxide samples agree within experimental error with the reported values for UO₂ creep, about 90k cal/mole at low stresses and 132k cal/mole at high stresses⁽²⁾. This indicates that similar mechanisms control the deformation process in both materials.

A single mixed-oxide sample has been creep tested at two stoichiometries (1.95 and 2.00) for a number of different stress and temperature conditions. This was

accomplished by altering the moisture content of the gas environment within the creep test furnace. Results are shown in Figure 6-1. Changing the stoichiometry from 1.95 to 2.00 increased the creep rate by about a factor of five. Additional creep data on the stoichiometric sample at 1625 and 1550 $^{\circ}$ C yielded activation energy values of 80-95 k cal/mole at 1,000 psi and an estimated 135-150 k cal/mole at 4,000 psi. These values also agree quite well with the activation energies for stoichiometric UO₂ and with the data obtained on mixed-oxides with an O/M of 1.95.

The effect of density on the deformation rate is shown in Figure 6-2. The creep rate gradually decreases with samples of higher density. The trend previously observed in stoichiometric UO₂⁽²⁾ is shown for comparative purposes. Both sets of data are for identical conditions of temperature, stress, pellet grain size and density.

Work is continuing on samples of various plutonium contents, grain sizes and densities.

6.2.2 Thermal Conductivity Studies

The thermal conductivity of mixed oxide samples at high temperature is being measured using a radial heat flow technique. Relative trends are becoming evident in the preliminary data. Above 1300 $^{\circ}$ C the stoichiometry of 20 w/o plutonium samples has no detectable effect on the heat transfer properties of the material. In a similar manner, no appreciable difference was found for samples prepared from physically mixed or coprecipitated powders. Work is continuing on samples of various plutonium contents, densities and stoichiometries.

6.3 CENTER TEMPERATURE LIMITS AND FUEL BEHAVIOR

6.3.1 Capsule Series E1J (Effects of Stoichiometry on Effective Thermal Conductivity of Restructured Fuel)

Eight of the nine capsules of this series, embodying triplication of tests at three fuel stoichiometries have been irradiated under comparable fuel pin power conditions resulting in fuel restructuring, and subsequent melting. The final capsule, E1J-8, will be irradiated during the next reporting period. Fabrication and operating parameters of the capsules are listed in Table 6-1.

An irradiated pin of each stoichiometry has been examined at this time, with results indicating that variations of O/M ratio from 1.936 to 2.00 cause no significant differences in

$$\int_{T_s}^{T_m} K d\theta.$$

A second trio of samples is presently being examined.

Figure 6-1. Comparison of Creep Rates for 2.00 and 1.95 Nominal Stoichiometries

Figure 6-2. Comparison of the Creep Rate versus Density Relationship for Mixed Plutonium - Uranium Oxide and UO_2

6.3.2 Central Hole Formation

Capsule E2R (Rate of Central Hole Formation at Startup)

The E2R capsule irradiation test is intended to experimentally determine the rate of center void formation in a pin containing 88 and 94% TD (U-25 w/o Pu) $\text{O}_{1.97}$ pellets at fuel temperatures approximating those predicted for startup of an LMFBR. The test will be conducted in the GETR trail cable facility to allow brief, carefully positioned capsule insertions and to provide accurately controlled capsule operating conditions. The capsule will be irradiated at a peak fuel center temperature of 2500°C for a series of exposures lasting: 10 minutes, 30 minutes, 1 hour, 5 hours, 24 hours, and one week. After each irradiation period the capsule will be removed from the trail cable facility and neutrographed to determine center hole existence or size.

Because of the significant role of neutron radiography in determining central hole size, a study was instituted to evaluate and optimize several neutrography parameters, using the Nuclear Test Reactor (NTR). The parameters included different transfer foils, neutron energies, film types, and exposure levels.

Table 6-2 is a tabulation of current findings. Thus far, the minimum detectable hole size is 11 mils, while the minimum measurable hole size is 16 mils. Future tests in

the program will involve other film types and the use of epithermal neutrons as opposed to thermal plus epithermal neutrons used to this point. The use of photographic image enhancement techniques will be evaluated as well as the use of a scanning micro densitometer.

The E2R capsule is currently being fabricated with the first GETR irradiation scheduled for June or July, 1970.

6.3.3 Fuel Swelling Behavior

Capsules E2E, E2F, E2G, E2H (Swelling of Radially Restrained Fuel at High Burnup)

Test capsules E2E, E2F, E2G and E2H were irradiated to high burnup to study swelling behavior and fission gas release from fuel operating at central temperatures of about 1400 , 1800 and 2200°C . The pins were irradiated in MTR to burnups of 320,000 MWd/Te. The pre-irradiation characteristics of these pins are summarized in Table 6-3. The most notable features are the high density of the pellets, the minimal diametral gaps, and the heavy 28 mil wall thickness of the cladding, combining to restrict radial fuel expansion and to confine expansion to axial movement.

These capsules were designed to be removed from the MTR for interim inspections. High resolution gamma activity scanning was used to determine the length of

Table 6-1

FABRICATION AND OPERATING PARAMETERS OF THE E1J CAPSULE SERIES

Capsule No.	1	2	3	4(3)	5	6	7	8	9
O/M (U-25 w/o Pu) _{0.2-X}	1.936	1.936	1.936	1.969	1.969	1.969	2.000	2.000	2.000
Average Pellet Density (% TD)	92.6	92.7	92.7	91.7	91.7	91.7	91.9	91.9	91.9
Initial Diametral Gap (Mils) ⁽¹⁾	3.0	2.8	2.9	3.0	2.9	2.9	3.0	3.0	3.0
Power (kW/ft) ⁽²⁾	23.1 ± 1.8	22.3 ± 1.3	21.7 ± 1.7	23.0 ± 0.6	24.0 ± 0.6	21.4 ± 1.9	23.6 ± 1.0	(6)	22.7 ± 1.1
Gap Conductance ⁽⁴⁾ Btu/hr-ft-°F	1500	1500	1500	1500	1500	1500	1500	1500	1500
Fuel Surface Temp. T _s (°C) ⁽⁵⁾	1150 ± 80	1120 ± 60	1090 ± 80	1150 ± 30	1190 ± 30	1080 ± 60	1170 ± 40	(6)	1130 ± 40

(1) ± 0.1 mils

(2) Uncertainty in power due to circumferential temperature gradient as indicated by three thermocouples equidistant from the fuel pin

(3) One of three control thermocouples failed to operate during irradiation.

(4) Assumed

(5) Calculated from assumed gap conductance

(6) Not yet determined

Table 6-2

SUMMARY OF NEUTRON RADIOGRAPHY PARAMETERS BEING STUDIED IN E2R PROGRAM

Transfer Foil	Method ⁽¹⁾	Film ⁽²⁾ Type	Relative ⁽³⁾ Exposure Period	Measuring ⁽⁴⁾ Bias (mils)	Comment
Gd	Direct	SCR	1.00	-2.5	For unirradiated capsules only
Thick Dy	Indirect	T	0.74	-3.4	} Best for low enrichment fuels
Thin Dy	Indirect	T	0.47	-2.0	
Thick Dy	Indirect	SCR	0.15	-2.4	
Indium	Indirect	T	0.26	-2.3	Best for high enrichment fuels

(1) In direct neutrography, the film is exposed at the same time as the transfer foil. In indirect neutrography, the film is exposed by the induced β and γ from the transfer foil after NTR exposure.

(2) SCR is Single Coated type R film;

(3) A relative exposure period of 1.00 is briefest, with other values requiring longer exposures in relative proportions.

(4) Measuring bias is the average amount that the measurement on the film varies from the actual dimension as measured during the pellet fabrication.

Table 6-3
FUEL SWELLING CAPSULE IRRADIATION DESIGN DATA

<u>Capsule Identification</u> <u>Fuel</u>	<u>E-2E</u>	<u>E-2F</u>	<u>E-2G</u>	<u>E-2H</u>
Composition, w/o UO ₂ /PuO ₂	75.7/24.3	75.7/24.3	75.24.3	77.9/22.1
Isotopic Distribution (%)				
U-235	25.3	25.3	25.3	47.1
Pu-239	91.0	91.0	91.0	90.96
Pu-240	8.27	8.27	8.27	8.30
Pu-241	0.69	0.69	0.69	0.701
Pu-242	0.04	0.04	0.04	0.032
Stoichiometry O/M (Avg)	1.999	1.998	2.000	2.000
Pellet Density (% TD)*	98.7	98.6	97.7	95.9
Smeared Density (% TD)* (including Pellet-clad Gap)	96.6	95.4	96.5	93.5
Cladding ID (Avg) inches	0.1020	0.1027	0.1016	0.1023
Cladding Wall, inches	0.028	0.028	0.028	0.028
Pellet Diameter (Avg) inches	0.1005	0.1011	0.1011	0.1011
Diametral Gap (Avg) inches	0.0015	0.0016	0.0005	0.0012
Fuel Length, inches	3.000	2.894	2.971	2.992
Fuel Weight, grams	4.235	4.131	4.199	4.152
100% TD Volume, cm ³	0.384	0.375	0.381	0.377
Initial Porosity, cm ³	0.0051	0.0053	0.0090	0.0161
<u>Target Irradiation Conditions</u>				
Fuel Central Temp., °C	2200	1800	1400	2200
Volume Average Temp., °C	1500	1200	1000	1500
Linear Power, kW/ft	11.6	9.4	7.9	11.6
Burnup, Fissions/cc X 10 ²⁰	28	28	28	56

*Theoretical Density - 11.02 gm/cc

the fuel column and the compressed length of calibrated bellows in the gas plenum that provided a record of fission gas pressure within the fuel pin.

During outages, the accumulated total burnups were determined from analysis of flux wires removed during interim examinations. The capsules were then relocated to positions of higher flux to compensate for reduced power generation due to fissile atom depletion and thereby maintain a nearly equivalent fission rate or center temperature throughout the test. A progressive description of the irradiation history and results may be found in Reference 3.

Initially, the experimental concept worked satisfactorily and systematic increases in fuel length and fission gas pressure were observed at the three nominal center temperatures as burnup was accumulated in the fuel. However, during the later phases of the test, the projected flux requirements were not met in the MTR reactor positions and the long term operation of the capsules at distinct fuel center temperatures could not be maintained. Consequently, the swelling rates and fission gas release values for the four capsules were obtained as a function of burnup but for a range of fuel temperatures. The final fuel swelling results and the range of fuel center temperatures during operation in the reactor are summarized in Table 6-4. In all cases the highest fuel temperatures occurred early in life.

Final burnups and gas release values are presented in Table 6-5.

The following conclusions may be drawn from this test:

1. Swelling rates were measured for mixed oxide fuels with burnups to 79×10^{20} fissions/cc. These data are plotted in Figure 6-3. For the minimum fuel swelling, assuming no utilization of the initial pellet porosity, the instantaneous swelling rate ranged from

$$0.10\% \frac{\Delta V}{V} / 10^{20} \text{ fissions/cc}$$

at low burnups to

$$0.14\% \frac{\Delta V}{V} / 10^{20} \text{ fissions/cc}$$

at 79×10^{20} fissions/cc. If the initial pellet void volume were completely utilized to accommodate swelling early in the test, the instantaneous rates for maximum fuel swelling would be $0.12\% \Delta V/V / 10^{20}$ fissions/cc at low burnups increased to $0.23\% \Delta V/V / 10^{20}$ fissions/cc at 79×10^{20} fissions/cc.

Table 6-4
FUEL SWELLING CAPSULES
POST-IRRADIATION CONDITIONS

<u>Capsule No.</u>	E2E	E2F	E2G	E2H
Fuel Diameter, in.	0.1020	0.1027	0.1016	0.1023
Total Fuel Length, in. ⁽¹⁾	3.09	3.18	3.18	3.44
Depth of End Void, in. ⁽¹⁾	0.12	0.20	0.20	1.55
End Void Volume, in ³	0.0005	0.00155	0.00073	0.00176
cc	0.0082	0.0254	0.0120	0.0288
Final Fuel Volume, in ³	0.0250	0.0248	0.0249	0.0263
cc	0.4096	0.4063	0.4079	0.4319
Final $\Delta L/L_o$, %	3.00	9.88	7.03	14.97
Final $\Delta V/V_o$, % ⁽²⁾	5.30	6.92	4.59	9.89
Burnup Fissions/cc X 10 ²⁰	35.85	61.35	52.07	79.06
$\frac{\% \Delta V}{V} / 10^{20}$ Fissions/cc	0.148	0.113	0.088	0.125
$\Delta V_{100}/V_{100}$, % ⁽³⁾	6.67	8.35	7.06	14.56
$\frac{\% \Delta V_{100}}{10^{20} \text{ Fissions/cc}}$	0.186	0.136	0.136	0.184
Fuel Center Temperature Range, °C (Start of Life/End of Life)	2420/1060	2450/450	1690/500	Molten/540

(1) Neutron Radiograph Measurement

(2) V_{100} = Fuel volume equivalent for 100% TD mixed oxide

$$V_{100} = V_{\text{original}} - V_{\text{porosity}} \cdot \frac{\Delta V_{100} - V_1 - V_{100}}{V_1 - V_{100}}$$

(3) Final $\Delta V/V_o$ = Fuel Envelope less central void; the latter volume accounts for the difference between $\Delta V/V_o$ and $\Delta V/V$.

Table 6-5
SUMMARY OF FUEL BURNUP AND FISSION GAS RELEASE FROM FUEL SWELLING CAPSULES

Capsule No.	Nd/Heavy Element	Mass Spectrographic Analysis Fission Burnup in Atom Percent $\pm 3\%$		Average ⁽¹⁾ MWD/Te X10 ³	Fission Gas ⁽²⁾ % Release
		Isotopic	Average		
E2E	14.75	14.73	14.74	141.7	(3)
E2F	25.04	25.37	25.20	242.3	79.0
E2G	21.54	21.75	21.64	208.1	70.8
E2H	33.93	33.14	33.53	322.4	89.5

(1) Based on 204 Mev/Fission (Representative of MTR Reactor Driver Fuel)

(2) Based on 0.32 Fission Gas Atoms Produced/Fission.

(3) Gas Sample Lost During Puncturing Operation.

Figure 6-3. Effect of Burnup on Swelling of Mixed Oxide

- The fission gas release increased with burnup. The percentage released ranged from 70.8% at 52×10^{20} fissions/cc to 89.5% at a burnup of 79×10^{20} fissions/cc. These data are listed in Table 6-5.
- A saturation level of 3×10^{20} to 6.5×10^{20} atoms of Xe + Kr retained in the fuel per cc of fuel is reached at burnups of 25×10^{20} to 35×10^{20} fissions/cc. Initially most of the Xe and Kr produced is retained within the fuel structure; however, at burnups in excess of $35-40 \times 10^{20}$ fissions/cc, the gases are released as rapidly as they are produced. These data are summarized in Figure 6-4.
- Fission gas release is enhanced when a period of operation with the fuel at high temperature is followed by a cool down, and subsequent operation at lower fuel temperatures. The cracks formed in the fuel due to thermal contractions during cool down apparently do not completely close during the next irradiation interval because of the lower operating temperature of the fuel. This increase in available free surface area in the fuel reduces the diffusion paths for the fission gases, resulting in gas releases exceeding the amount otherwise produced during this period.

Figure 6-4. Effect of Burnup on the Xe + Kr Inventory in the Fuel

5. Step increases in fuel swelling, detected by incremental increases in the fuel column length, accompany fuel temperature increases on the order of 300°C or greater.

6.4 CESIUM-FUEL-CLADDING REACTIONS

6.4.1 Electron Probe Microanalysis of Irradiated Sample F2H

A group of three specimens from sample F2H irradiated in EBR-II to about 64,000 MWd/Te was examined using a shielded electron microprobe. The sample containing the three specimens was prepared in non-aqueous media, and exposure to the atmosphere was minimized to preserve water soluble fission products.

Figure 6-5 shows 10X montages of each sample. The relative axial position of each sample and the starting $\text{Pu}/(\text{U}+\text{Pu})$ ratios are also shown. Burnup, power levels and calculated temperatures are presented in Table 6-6. The post-irradiation average plutonium content is compared to the radial plutonium distribution in Figure 6-6. The plutonium concentration at the center was slightly higher than the average analyzed across the radius. This increase amounted to about 1% in sections 6A1 and 8B, and a significant (2%) increase in section 10A. The operating temperature of the center section of 6A1 was calculated to be above 2500°C at the start of life but decreased to $<2500^{\circ}\text{C}$ at the end of life. The increase in plutonium concentration in the center of the fuel sections located toward the colder regions of the fuel, section 10A, infers axial migration of plutonium. The absence of a plutonium

10X

Figure 6-5. Section Diagram Showing the Location of the Three Samples of F2H Along with Pre-Irradiation $\text{Pu}/(\text{Pu} + \text{U})$ Ratios

Table 6-6
FUEL PIN F2H-DATA ON THREE SECTIONS TAKEN FOR MICROPROBE ANALYSIS

	Section 6A1		Section 8B		Section 10A	
	Start of Life	End of Life	Start of Life	End of Life	Start of Life	End of Life
Distance from Bottom of Fuel (inches)	5.61		10.71		13.86	
Linear Power* kW/ft	16.65	15.12	14.70	13.42	12.54	11.39
Center Temperature* °C	2760	2537	2537	2355	2204	2010
Clad I.D. Temperature* °F	1036	1006	1068	1035	1070	1038
Calculated * Pu/Pu + U	19.9	18.5	19.7	18.5	20.5	19.3
Burnup:						
Calculated a/o		7.3		6.4		5.5
Measured a/o (Nd/Heavy Element)		6.7		5.9		5.1
10 ³ MWd/Tc		64.5		56.7		49.1

*Based on calculated Burnup

gradient in sample 6A1 which operated at the highest temperatures suggests that as the fuel temperature is lowered due to burnup, the plutonium, concentrated at the center early in life, homogenizes due to thermal diffusion.

The fuel-cladding interface was examined in all three specimens. The area shown in the photomicrograph in Figure 6-7 was examined in detail. A series of 2θ scans were taken at several places in this area to determine the elements present, then X-ray pulse images were taken for each of the elements detected. Of these, only Pd, Cs, Te, and Mo showed definite concentrations.

Specimen current images of the region are shown in Figure 6-8 along with X-ray pulse images of Pd and Cs, which show the locations of these elements. The concentration of Pd is greater than that of Cs. The positive current image enhances cladding materials, the lighter elements, while the negative current image emphasizes the heavier elements, U and Pu in the fuel. The concentrations of Mo and Te at this region are shown in Figure 6-9, while the uniform distribution of I and Ru is also shown. The elements Sr, Rh, Ce and Xe were also present in the manner of I and Ru with no concentrations observed.

The specimen current images of Figures 6-10 and 6-11 show radial and circumferential cracks in the fuel of specimen 6A1 near the cladding interface. X-ray pulse images indicate concentrations of Pd and Mo in some radial

but not circumferential cracks. Possibly, the cracks containing fission products existed during a number of irradiation cycles with vapor transport accounting for fission product migration, while cracks with no fission products developed during shutdown or existed for only a brief period of irradiation.

A bright area near the cladding of section 8B was observed and examined. The 250X specimen current image along with the X-ray pulse images of Te, I and Mo, are shown in Figure 6-12. This illustrates the concentration of fission products next to the cladding. Figure 6-13 shows this region at 500X. This bright area seems to be made up only of Cs as shown by the X-ray pulse images for U (Pu is similar) and Fe (the other clad constituents appear the same). The other prevalent fission products, I, Te, Mo and Pd, are concentrated next to the cladding but were not observed in the bright area.

A typical fuel-cladding reaction zone in section 10A was examined and Figure 6-14 shows the specimen current image along with the X-ray images for Cs, Fe and Te. No iodine was found in this section but Mo was observed near the cladding. The cladding constituents were noted moving into the fuel as observed in other samples.

The trio of samples from F2H yielded consistent results in examinations of fuel-cladding reaction zones. Iron migrated farthest into the fuel, nickel migrated to a lesser extent, while chromium was essentially unaltered. The

Figure 6-6. Plot of $P_u/(P_u + U)$ Ratios Measured for the Three Sections Taken Along the Length of the Fuel Pin F2H Including Calculated End-of-Life Average Values

Figure 6-7. Montage of Fuel-Cladding Reaction Zone along with Total Crack Examined in Section 6A1

500X

Figure 6-8. Comparison of Specimen Current Images with Pd and Cs X-ray images in Radial Crack of Section 6A1
(See Figure 6-7 for Metallographic View of Area)

Figure 6-9. X-ray Images for Mo, I, Te and Ru in Radial Crack of Section 6A1.
This is at the same location as Figure 6-8. See Figure 6-7 for Metallographic View of Area.

Figure 6-10. Specimen Current Image and X-ray Images of Pd and Mo Showing Fission Products in Radial but not in Circumferential Cracks in Section 6A1. (near the cladding). A Metallographic View is shown in Figure 6-7.

RADIAL CRACK

CIRCUMFERENTIAL
CRACK

NEGATIVE SPECIMEN CURRENT IMAGE

Pd-K α X-RAY PULSE IMAGE

Mo-K α X-RAY PULSE IMAGE

250X

Figure 6-11. Specimen Current Image and X-ray Images of Pd and Mo Showing Fission Products in Horizontal but not in Circumferential Cracks in Section 6A1. A Metallographic View is shown in Figure 6-7.

Figure 6-12. Specimen Current Image and X-ray Images for Te, I and Mo near Bright Area in Section 8B

CLADDING

FUEL

POSITIVE SPECIMEN CURRENT IMAGE

REACTION ZONE

Cs-K α X-RAY PULSE IMAGE

U-M α X-RAY PULSE IMAGE

Fe-K α X-RAY PULSE IMAGE

500X

Figure 6-13. Specimen Current Image and X-ray Images for Cs, U and Fe at Bright Area in Section 8B

FUEL
CLADDING

NEGATIVE SPECIMEN CURRENT IMAGE

Cs-K α X-RAY PULSE IMAGE

Fe-K α X-RAY PULSE IMAGE

Te-L α X RAY PULSE IMAGE

Figure 6 14. Typical Fuel-Cladding Interaction Zone in Section 10A Showing a Specimen Current Image along with the X-ray Pulse Images for Cs, Fe and Te

major volatile fission products (Cs and Te) along with Mo and Pd concentrated at the interface.

The amount of reaction was slight, however. No intergranular attack was observed in any of the specimens and iodine was observed only in one section.

6.4.2 Out of Pile Studies in a Temperature Gradient

To understand the nature of clad attack by cesium and oxygen, a series of fuel pins were designed to be heated in a temperature gradient and cesium added so that the initial O/M of the fuel controlled the oxygen activity level. In this way, the effect of O/M on the reaction with the cladding could be ascertained under realistic conditions. The status of thermal gradient experiments is summarized in Table 6-7. An initial set of experiments was run to check out the behavior of the system and to determine general migration features using fuel with an O/M of 1.97.

The first two series, see Table 6-7, used sealed Mo capsules to contain the fuel, Cs and cladding in order to be able to heat one end of the six-inch container to temperatures around 1500°C. Previous work has shown that this temperature was required for oxygen migration to occur in a temperature gradient. A one-inch long type 316 stainless steel tube was inserted inside the Mo tube and over the last four fuel pellets at the cold end of the capsule. It was found in the first series, and confirmed in the second series, that the oxygen migration rate was much higher in the temperature gradient capsule with cesium vapor than in experiments of oxygen migration with no cesium available. Within 100 hours, the O/M ratio of fuel in these tests varied along the gradient to an extent that corresponded to a near steady state heat of oxygen transport of about -10k cal/mole. Previous work without cesium indicated that even after 1,000 hours, no oxygen composition gradient could be established.

The cesium compound was always loaded at the high temperature end of the capsule so that its vapor would have maximum opportunity to interact with the fuel during its passage down the gradient. The amount of cesium corresponded to an equivalence of about 2 a/o burnup of all the fuel in the container. The oxygen associated with the cesium was sufficient to cause a measurable change in overall O/M of the fuel. Initially, the cesium oxide was produced by reaction of Cs_2MoO_4 and U in situ to form, in stoichiometric proportions, UO_2 , Mo and Cs_2O . This reaction does not take place until Cs_2MoO_4 begins to decompose at around 900°C, thus avoiding premature volatilization. The molybdenum and UO_2 remained at the high temperature end. In all cases, cesium has been found at the low temperature end after the heat treatment. Most of the cesium appears to have been absorbed by the fuel at temperatures below about 900°C. Cesium has been detected as a thin layer on stainless steel surface at temperatures below about 750°C. Some pellets at the cold

end appeared to absorb moisture on exposure to ambient atmosphere, possibly due to the reaction of condensed cesium vapor with moisture.

Metallographic examination of the annealed Type 316 cladding alloy, although not complete, has revealed some interesting features: (fuel at an initial O/M of 1.967).

1. Intergranular attack of the stainless steel appears to occur over the temperature range of 650 to 750°C. In the same temperature range, a moderate to dense pearlitic structure develops within the grains near the fuel-clad interface. The reason for this last observation is unknown.
2. At temperatures above about 900°C, the structure is completely annealed yielding large grains, but no visual tarnishing of the surface due to cesium attack is evident as it is at lower temperatures.
3. Molybdenum metal is transported in the presence of the condensed cesium compound and is deposited at the low temperature end. This observation could account for extensive concentrations of fission product molybdenum at the fuel-cladding interface as reported in the previous section.

6.4.3 Out of Pile Creep-Rupture Testing

To obtain further information on the nature of fission product attack of stainless steel cladding, and to define the attacking species, a series of out-of-pile capsules, loaded with volatile fission product elements cesium, iodine, or their compounds, were creep-rupture tested at 1200°F. The specific compounds loaded into the 0.250 in. OD Type 316 SS capsules are enumerated in Table 6-8. Internal pressures of the sealed capsules containing the various compounds ranged from 14.7 to 600 psi at 1200°F, yielding effective hoop stresses of from 300 to 11,000 psi. Rupture at 11,000 psi hoop stress would not be expected for 10,000 hours. Examination after 236 hours showed that the two capsules loaded with Cs OH had failed. One had operated at an effective stress of 300 psi while the second capsule operated at 11,000 psi. The rupture of the latter capsule was more drastic as shown in Figure 6-15, while the amount of cladding attack was greater in the lower stressed sample because of longer confinement of the cesium compound. Metallographic examination revealed extensive intergranular attack, extending almost completely through the cladding wall, in the lower stressed capsule.

The testing of additional capsules for 212 hours indicated that two capsules containing Cs_2O had failed. Intergranular attack of the cladding was also observed in these samples. During this test (up to 450 hours) no failure or local strain was noted in capsules loaded with CsI, CsCl, CsOH, Cs_2I , HIO_3 , HI, or Cs_2CO_3 . This indicates that cesium is the species which reacts the most with sensitized stainless steel grain boundaries but that it must be associated with oxygen or moisture to react.

100X

400X

Figure 6-15. Intergranular Attack of CsOH on Type 316 Tubing in Creep Rupture Testing at 1200°F

Table 6-7
CESIUM – FUEL – CLADDING PROGRAM

**Status and Summary of Fission Product Reaction Capsules
Heated in a Temperature Gradient for 100 Hours.**

	O/M	Temperature Range	Container	Additive	Status
Series I	1.967	660°C - 1550°C	Mo	Cs ₂ MoO ₄ /U	Completed
Series II-A	1.967	700°C - 1550°C	Mo	Cs ₂ MoO ₄ /U	Completed
B	1.967	550°C - 1550°C	Mo	Cs ₂ MoO ₄ /U	Completed
C	1.967	660°C - 1550°C	Mo	Cs ₂ MoO ₄ /U	Completed
Series III-A	1.967	550°C - 1150°C	316SS	Cs ₂ MoO ₄ /U	Completed
B	1.967	550°C - 1150°C	316SS	Cs ₂ O	Completed
C	1.967	550°C - 1150°C	316SS	None	Completed
Series IV-A	1.94	550°C - 1150°C	316SS	Cs ₂ O	Heat Treatment
B	1.967	550°C - 1150°C	316SS	Cs ₂ O	Completed--
C	2.00	550°C - 1150°C	316SS	Cs ₂ O	Under Examination
D	2.01	550°C - 1150°C	316SS	Cs ₂ O	
E	1.967	550°C - 1150°C	316SS	Cs ₂ O Excess	
Series V	To be selected from Series IV results		316SS	Cs ₂ O	To be run
			316SS	Cs ₂ O + Te	
			316SS	Cs ₂ O + I	

6.5 FUEL-SODIUM REACTIONS AND PLUTONIUM AND FISSION PRODUCT REDISTRIBUTION

6.5.1 Electron Probe Microanalysis

Sections from two experimental pins were examined using a shielded microprobe for sodium-fuel reaction products, plutonium, and uranium gradients. In each of these pins the cladding had failed during operation so that sodium entered the pin and was in contact with the fuel. Encapsulated pin F20 operated with (U-20 w/o Pu)O_{2,03} fuel in EBR-II and reached a burnup of about 63,000 MWd/Te. The peak linear power was ~16 kW/ft. Encapsulated pin B9A contained (U-25 w/o Pu)O_{1,99} fuel and operated in GETR at a maximum power of 23.4 kW/ft; it reached a burnup of about 8,000 MWd/Te. The uranium portions of the fuel of both pins were fully enriched in U-235. The sections examined are shown in Figure 6-16. The off-center central void of F20 apparently occurred as a result of the ingress of the sodium coolant.

Figure 6-17 illustrates plutonium, uranium, and Pu/(Pu + U) radial gradients for F20. The non-symmetric plutonium and uranium distribution could, in part, be accounted for if a significant central void existed prior to failure and the failure caused a mass transfer of uranium

and plutonium. A skewed thermal profile could also account for this pattern. A considerable amount of material was lost from the sample of B9A, making measurement of the U and Pu concentrations impractical. The relative intensity of the Pu-M β line was determined and calibrated against a pure PuO₂ standard. The relative intensity of the U-M α line was determined and calibrated against a pure UO₂ standard. In addition, a (U-20 w/o Pu)O₂ standard was used to check the mixed oxide system. The Pu/(Pu + U) ratio was determined from the expression:

$$\frac{\frac{I_{\text{Pu}}^{\text{Sample}}}{I_{\text{Pu}}^{\text{Standard}}}}{\frac{I_{\text{Pu}}^{\text{Sample}}}{I_{\text{Pu}}^{\text{Standard}}} + \frac{I_{\text{U}}^{\text{Sample}}}{I_{\text{U}}^{\text{Standard}}}} \times 100$$

where: I = intensity.

A slight error may exist in the determination of the ratio Pu/(Pu + U) because it does not account for the minor decrease in plutonium concentrations in the sample due to unequal fissioning of the two elements.

In analyzing for Na in a matrix of very heavy atoms, the following difficulties were encountered:

Table 6-8
ISOTHERMAL CREEP RUPTURE TESTING WITH FISSION PRODUCT
ELEMENTS OR COMPOUNDS ADDED TO CAPSULES

Compounds Tested	Stress, psi	Results
Cs ₂ O	11,000	Rupture, Intergranular Attack
Cs ₂ O	300	No Measurable Change
CsOH	300	Rupture, Intergranular Attack
CsOH	11,000	Rupture, Intergranular Attack
I	11,000	No Measurable Change
HIO ₃	11,000	No Measurable Change
Cs	11,000	No Measurable Change
CsCl-H ₂ O	11,000	No Measurable Change
CsI	11,000	No Measurable Change
Cs-Cs ₂ CO ₃	11,000	No Measurable Change
HI	11,000	No Measurable Change
CaF ₂ -H ₂ O	11,000	No Measurable Change
He	11,000	No Measurable Change

CONDITIONS: Type 316 Stainless Steel Maintained at 1200°F to 450 Hours

1. The mass absorption of the Na-K α X-ray is very high, and the absorption coefficient in heavy elements is not known with certainty.*
2. High current densities had to be used to achieve a statistically significant X-ray intensity.
3. Surface contamination effects were severe.
4. Well characterized standards are difficult to obtain.

In item (3) the surface contamination was aggravated by high beam current; this caused local heating and possible vaporization as well as deposition of hydrocarbons at a high rate. The absorption of Na-K α by carbon may also have occurred.

Despite these difficulties, the early results clearly showed that the sodium-fuel reaction product at the cladding interfaces in the microstructures of Figure 6-16 are of variable Na content. However, at this time it is not certain whether the observed reaction products are compounds or are simply an admixture of oxidized Na in varied proportions in the fuel.

This variation in sodium content is illustrated by the specimen current image of a particle, along with a sodium X-ray trace across it as shown in Figure 6-18. The lighter area is richer in sodium. These lighter regions were seen in other areas of the reaction product. They varied from entire particles to streaks and variations of lighter-sodium rich regions.

Experiments to characterize the reaction phases in the fuel by X-ray diffraction are in progress.

As Figure 6-17 illustrates, the Pu/(Pu + U) ratio in the reaction product was found to remain relatively constant, although both species were found in lesser quantity in the interaction product than the matrix material. It is significant that no partitioning of the uranium and plutonium occurred.

A specimen current image overlaid by an X-ray pulse image is used to compare the distribution of an

element with the physical characteristics of the sample. Figure 6-19 consists of two such composite photographs of a particle in the reaction zone. One is for sodium and one is for uranium, and the presence of both within the sample is illustrated. The plutonium to uranium ratio was found to be the same as the unaltered fuel.

Very little information is available in the literature on sodium-plutonium and sodium-mixed oxide systems.

Metallic appearing particles on the outside diameter of the sodium-fuel reaction product were shown to be cladding material. Figure 6-20 shows the K α X-ray pulse images of iron, chromium, and nickel as well as an iron K α X-ray image over the specimen current image. Iron, chromium, nickel, have been previously identified in similar locations⁽⁸⁾.

6.5.2 Fuel-Sodium Reaction Phase Property Determination

To help characterize and understand the nature of the sodium-fuel compounds found in irradiated fuel, a direct synthesis of sodium-fuel compounds has been undertaken. Blended powders in the ratio of 1.1 moles of Na₂O to 1 mole of (U-20 w/o Pu) O_{2.25} were hot pressed for a few hours in a dry-oxygen gettered argon atmosphere glove box. Whole pellets were obtained by this process, however, at reaction temperatures below 900°C they were generally weak and easily broken. At 550°C and below, there was indication of some unreacted material since the pellets would disintegrate upon immersion in water. At 815°C and at 900°C, disintegration in water was evident; however, exposure of a polished surface of a pellet pressed at 815°C

* for example, the mass absorption coefficient of Na-K α X-rays passing through uranium is shown by Birks⁽⁶⁾ to be 7,000. Other references (e.g. Heinrich⁽⁷⁾) do not even quote a coefficient for absorption of Na-K α by U or Pu.

SAMPLE FROM F20 IRRADIATED IN EBR-II
TO $\sim 63,000$ MWd/Te

SAMPLE FROM B9A IRRADIATED IN
GETR TO $\sim 8,000$ MWd/Te

Figure 6-16. Cross Sections of Irradiated Fuel Examined by Electron Microprobe

Figure 6-17. Variation of the Intensity Ratios for U, Pu and Pu/U + Pu as a Function of the Radial Position for Sample 1A

COMPARISON OF THE SPECIMEN CURRENT IMAGE OF
A Na-FUEL PARTICLE TO THE Na X-RAY
INTENSITY IN SAMPLE F20

← POSITION OF
X-RAY TRACE

← Na-FUEL REACTION PRODUCT

SPECIMEN CURRENT IMAGE

Na-K α X-RAY INTENSITY TRACE

Figure 6-18. Comparison of the Specimen Current Image of a Na-Fuel Particle to the Na X-ray Intensity in Sample F20

COMPARISON OF THE SPECIMEN CURRENT IMAGE OF A
Na-FUEL PARTICLE WITH THE X-RAY PULSE IMAGE IN
SAMPLE F20

Na-K α X-RAY PULSE IMAGE OVER THE
SPECIMEN CURRENT IMAGE

U-M α X-RAY PULSE IMAGE OVER THE
SPECIMEN CURRENT IMAGE

Figure 6-19. Comparison of the Specimen Current Image of Na-Fuel Particle with the X-ray Pulse Image in Sample F20

Fe-K α X-RAY PULSE IMAGE OVER SPECIMEN CURRENT IMAGE

Fe-K α X-RAY PULSE IMAGE

Cr-K α X-RAY PULSE IMAGE

Ni-K α X-RAY PULSE IMAGE

Figure 6-20. Examination of Metal Phase Slightly Removed from Cladding in Sample F-20

to air caused white whiskers to grow from the surface overnight. No deterioration was observed upon storage in kerosene or in pure argon.

Several pellets have been hot pressed and prepared for X-ray diffraction examination. In the first series, increasing amounts of sodium metal were added with the powder blend to produce a sodium-saturated compound and to aid in promoting reaction.

The hot pressed compound showed no weight change after a four hour treatment in argon at 900°C. In a hydrogen atmosphere, the sodium vaporized and the compound was not stable. Metallographic examination showed an absence of microstructural features such as grain boundaries. Water did not etch the surface, but oxalic acid produced a uniform etching action. X-ray examination is in progress.

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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). More recently, model analysis (Task P) has been applied quantitatively to interpretation of the results, and to aid in the selection of preferred ranges of parameters for further tests. 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 B on defected fuel testing,

Task C, which focuses on the preferred range of parameters for safety considerations of LMFBR designs and Task G, which is concerned with subassembly design and performance. 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.

A summary of the fast flux irradiation tests in the EBR-II is given in Table 2-4. The format of the summary table has been revised. Calculated values for total fluence,

fluence greater than 0.1 MeV, and fuel burnup values in MWd/Te and atom percent (taking into account fuel depletion) are now incorporated into the summary table. The fluence, power, and burnup values given in Table 2-4 were calculated using the fission rates provided in the latest revision of the "Experimentor's Guide for Irradiations in EBR-II" as a basis*.

Capsule irradiation tests performed in thermal spectrum test reactors which are being performed under Task F and capsule tests which were transferred from Task E to Task F, are listed in Table 2-5.

7.2 MEDIUM POWER, LONG TERM IRRADIATIONS

7.2.1 Group F2 (Medium Power Scoping Irradiations)

Approval to extend the irradiation time for the four Group F2 capsules being irradiated in EBR-II subassembly X050 was granted by the EBR-II Project. The result will be an increase in fuel burnup, 11.3% greater than initially authorized by the EBR-II Project. The increased irradiation time was requested as a result of comparison of calculated and experimentally determined burnup results from the Group F2 pins examined previously. The capsules will reach their extended burnup goal at the end of EBR-II run 42. The average burnup and total fluence received by the highest burnup capsule in the F2 group at the end of the reporting period** was calculated*** to be 100,017 MWd/Te (11.19 a/o) and 0.98×10^{23} nvt (0.90×10^{23} nvt for $E > 0.1$ MeV). These capsules have obtained the highest fuel burnup and fast neutron fluence of any mixed oxide irradiations in the EBR-II under the FCR Program.

7.3 LOW POWER FUEL SWELLING IRRADIATIONS

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

The remaining Group FO capsules in EBR-II subassemblies XG03 and XG04 have reached average burnups of 54,490 MWd/Te (6.10 a/o) and 60,610 MWd/Te (6.78 a/o) respectively. Target burnup for the fuel in XG03 and XG04 is 80,000 MWd/Te (8.95 a/o) and 100,000 MWd/Te (11.18 a/o) respectively.

The operating power history of fuel pin FOE is shown in Figure 7-1. This fuel pin was irradiated in EBR-II

subassembly XG02. The details of the post-irradiation examination of this fuel pin were reported previously(1). An operating power of ~6 kW/ft was reported for this pin. The pin initially operated at ~6 kW/ft in a peripheral core position (Row 7), however, about midway through the total scheduled irradiation time the reflector in the EBR-II was changed from UO₂ to stainless steel. Based on the fission rates published by the EBR-II Project for Row 7 positions the power of the FOE pin increased from 5.7 kW/ft to 10 kW/ft as a result of the reflector change.

The peak fuel burnup calculated using the fission rates published by the EBR-II Project and correcting for fuel depletion is 4.3 a/o. Results from experimental burnup analysis of the fuel indicate a peak fuel burnup of 3.6 a/o. The dotted curve in Figure 7-1 represents the operating power of FOE calculated from the EBR-II fission rates reduced by the ratio of the analytical to the calculated burnup (3.6/4.3).

The higher operating power which the FOE fuel pin experienced during the last half of its irradiation explains the anomalous fuel microstructures noted during the post-irradiation examination. It was noted that the fuel microstructure was typical of fuel which had operated at temperatures higher than could be attained at a peak power of 6 kW/ft. The fuel temperatures are being recalculated using the higher power level.

7.3.2 Group F6 (Fuel Cladding Interactions Under Accelerated Swelling)

The details of the post irradiation examination of three of the four Group F6 capsules, FOJ, -K, and -L were reported previously(2) with the exception of fuel burnup and fission gas analyses results. The fuel burnup and fission gas analyses are complete. The examination of the failed Group F6 fuel pin, FOM, was initiated.

Burnup samples were taken from the peak flux region of each Group F6 pin. The fuel burnup obtained from the analysis of each sample for neodymium isotopes, the calculated burnup based on the fission rates in the EBR-II Experimentors Guide corrected for fuel depletion, and the burnup value reported by the EBR-II Project for the lead Group F6 fuel pin, FOM, are tabulated in Table 7-1. The analytical value for pin FOL was higher than anticipated and a second analysis is being run to confirm the burnup value. Comparison of the analytically determined values and the calculated values which account for fuel depletion indicate that a 9 to 10% discrepancy exists between the calculated and measured burnup values. This finding is in agreement with previously reported burnup analyses for the Group F2 fuel pins.

Fission gas release fractions based on the analytically determined fuel burnup values and gas chromatograph analysis of fission gas samples from each pin are summarized in Table 7-2.

* Where applicable fission rates supplied for earlier EBR-II core configurations by the EBR-II Project were used.

** Burnup and fluence values for the report period are based on EBR-II exposures through the end of run 41

*** All burnup and fluence values reported were calculated based upon fission rates in the EBR-II Experimentors Guide unless noted otherwise. The burnup values reported include corrections for fuel depletion as a function of burnup.

Figure 7-1. Power History of Group O Fuel Pin FOE

Table 7-1
BURNUP VALUES FOR GROUP F6 FUEL PINS

Fuel Pin	Analytically Measured Burnup⁽¹⁾ (a/o)	Burnup Calculated from EBR-II Fission Rates and Corrected for Depletion (a/o)	Reported by EBR-II Project (a/o)
FOJ	5.74	6.29	-----
FOK	5.57	6.07	-----
FOL	6.36	6.15	-----
FOM	(2)	6.39	6.42

(1) Based on neodymium isotope analysis.

(2) Pin failed, no burnup sample analyzed.

Table 7-2
FISSION GAS RELEASE FRACTIONS
FOR GROUP F6 FUEL PINS

Fuel Pin	Fission Gas Release (%)	Xe/Kr Ratios	
		Gas Chromatograph	Mass Spectrometer
FOJ	62.9	7.1	6.1
FOK	56.0	7.4	6.0
FOL	48.1	7.4	6.2
FOM	*		

* Analysis of gas sample not available on failed fuel pin.

Examination of the failed Group F6 pin, FOM, was initiated. Several cladding cracks were observed over the fueled region of the pin. The cracks were not connected and did not follow the spacer wire which spiralled around the fuel pin. High magnification photographs of three segments cut from the fueled region of the pin are shown in Figure 7-2. Although a quantitative measurement of the amount of fuel released from the pin was not obtained, examination of the neutron radiographs of the capsule and the deencapsulated pin indicate that the amount of fuel lost from the pin was small.

The total volume of helium and fission gas collected from the FOM capsule was 35.1 cc (STP). The capsule and fuel pin contained 10 to 12 cc of helium initially. The gas released from the other three Group F6 fuel pins ranged from 66-72 cc (STP). The lower fission gas release from the FOM pin (approximately half that from the unfailed pins) is thought to indicate cladding failure early in the life of the pin. Such a failure would have caused low lifetime average fuel temperatures as a result of sodium entering the pin. The lower fuel temperatures would cause a decrease in the

amount of fission gas released. Reaction between the sodium and fuel, resulting in a reaction product of lower density (higher volume) than the fuel, is hypothesized to have strained the fuel pin cladding, causing the multiple fractures observed in the cladding. The location of the initial clad failure has not been determined.

Maximum diametral measurements taken over the fueled region of the FOM pin are plotted in Figure 7-3. The maximum diameter change, 50 mils, occurred in the region of peak fuel temperature. No anomolous diameter change was measured on the FOM capsule tube⁽²⁾. Since there was only 13 mils clearance between the capsule tube and spacer wire (wrapped around the fuel pin with a 6" pitch) the capsule tube and spacer wire apparently restrained the fuel pin swelling and caused the diameter change of the fuel pin to occur in areas not covered by the wire. Metallography is continuing on samples from this pin.

7.3.3 Group F8A (Effect of Fuel Density on Fuel Swelling Rate)

The examination of the Group F8A fuel pins removed from EBR-II subassembly X019 was continued.

ARROW DESIGNATES TERMINATION
OF ONE CRACK AND INITIATION
OF SECOND CRACK

CRACK WHICH HAS
NOT OPENED

AS BUILT
TOP OF FUEL

TERMINATION OF CRACK
AT TOP OF FUEL

Figure 7-2. Sections from the Fueled Region of Failed Group 6 Fuel Pin FOM

Figure 7-3. Variation in Maximum Diameter over Fueled Length of Pin FOM

Results of the examination of the capsules containing the Group F8A pins were reported previously⁽²⁾. Pins F8A, -C, -E, and -G are being examined. Capsules F8B, -D, and -F are being stored for reirradiation at a later date.

Pins F8A, -C, -E, and -G were cleaned with alcohol, the spacer wires removed, and profile traces obtained for each fuel pin at 0°, 45°, 90°, and 135° orientations. Profile traces of these fuel pins are shown in Figures 7-4 through 7-7. The data points for the preirradiation fuel pin diameters are average values obtained from the preirradiation profile traces. Differences between the equipment used to obtain the pre- and post-irradiation diameter measurements and bowing of the pins during irradiation limit the accuracy of the traces to about 0.2 mils, therefore, differences of up to 0.2 mils cannot be interpreted as real diameter changes. Also shown in these Figures are the initial diameters, lengths and densities of individual pellets in the fuel lineups. Fuel pellet diameters are not shown for pin F8G as it contained powder fuel. The position of individual pellets for pin F8A are not shown since the individual pellet identities were lost during loading of the fuel into the clad. The position of the pellets in this fuel column is not of major significance in the analysis of the

profilometer traces for this one rod since there was little variation in the densities and diameters of the pellets with the exception of a single pellet which was ~1 mil larger in diameter than the other pellets. The location of this pellet is not clearly indicated on the post-irradiation profile trace as a characteristic diameter change or by ridges in the clad.

The profile traces of pin F8G, Figure 7-7, indicate that within the accuracy of the measurements no increase in diameter occurred over the length of the fuel pin.

The profile traces of the three Group F8A fuel pins containing pellet fuel indicate that no diameter increases greater than 1 mil occurred. In addition, no distinctive ridges were observed at pellet interfaces on any of the fuel pins. These observations are markedly different than the data for the three Group F6 fuel pins which operated at similar powers to equivalent burnups. Profile traces of the Group F6 pins were reported previously⁽²⁾. The significant difference between the fuel in the Group F6 and Group F8A pins and their operating parameters were: fuel densities, clad temperature, and in some regions of the fuel columns, fuel-clad gap. The Group F6 and F8A pins had smeared fuel densities in the ranges of 93.9–95.4 and 84.2–87.3%, respectively. The calculated peak clad temper-

Figure 7-4. Profilometer Traces of Group 8A Fuel Pin F8A Compared to Pre-Irradiation Fuel Pellet Lengths and Diameters (Note: Fuel Lineup was Random)

*One Fuel pellet diameter was 0.2180. Position in Fuel column is not known.

Figure 7-5. Profilometer Traces of Group 8A Fuel Pin F8C Compared to Pre-Irradiation Fuel Pellet Lengths and Diameters

Figure 7-6. Profilometer Traces of Group 8A Fuel Pin F8E Compared to Pre-Irradiation Fuel Pellet Lengths and Diameters

Figure 7-7. Profilometer Traces of Group 8A Fuel Pin F8G
(Note: Pin F8G Contained Vibratory Compacted Powder)

ature of the Group F6 pins was $\sim 1200^{\circ}\text{F}$, about 150°F higher than for the Group F8A pins. The Group F6 pins had regions with diametral fuel-clad gaps as small as 1.6 mils while the Group F8A pins had minimum fuel-clad diametral gaps of 2.2 mils. Distinctive clad ridging and diameter increases of 2 to 3 mils were observed on the Group F6 pins. In regions of the Group F6 pins which had fuel-clad gaps comparable to the Group F8A pins, diameter changes were greater than observed on the Group F8A pins. The differences in measured diameter changes for fuel pins from the two groups is attributed more to the differences in smeared fuel density than to the clad temperature differences since diameter differences in the Group F6 pins were

also significantly different in regions of the fuel pins which operated at comparable temperatures.

Metallographic examination of the Group F8A fuel pins was completed. Typical fuel structures are shown in Figure 7-8. The fuel sections shown in this figure were cut from the peak flux region of each fuel pin. The fuel microstructures indicate that the maximum fuel temperature was near that required for columnar grain growth. A small central void was observed to have formed in fuel pin F8G which contained powder fuel. No significant fuel-clad reaction was observed in any of the fuel pins.

The measured volume of gas collected from each of the Group F8A pins is listed in Table 7-3.

F8A
347 SS O/M 1.98
PELLET FUEL

F8C
304 SS O/M 1.98
PELLET FUEL

F8E
316 SS O/M 1.97
PELLET FUEL

F8G
316 SS O/M 1.97
POWDER FUEL

Figure 7-8. Metallographic Sections Taken from Peak Flux Regions of Group 8A Fuel Pins.
Fuel Burnup is $\sim 50,000$ MWd/Te.

Table 7-3
VOLUME OF GAS RELEASED FROM GROUP F8A FUEL PINS

Fuel Pin	Volume of Fission Gas and Helium Collected (cc @ STP)
F8A	38.0
F8C	36.6
F8E	38.4
F8G	41.2

Table 7-4
**BURNUP ANALYSES RESULTS FOR FUEL
PINS F12C, -D, and -E**

Fuel Pin	Peak Burnup a/o
F12C	14.0
F12D	14.4
F12E	13.3

The calculated power difference between the four pins was 1.4%. Pin F8G, which contained powder fuel, released 14% more gas than the average gas release from the other pins which contained pellet fuel. Detailed analysis of the fission gas data will be performed after the fuel burnup analyses and isotopic analyses of the gas samples are complete.

The nine Group F8A capsules removed from EBR-II subassembly X020 were given an interim examination at the EBR-II site. Neutron radiographs taken of the capsules showed no indications that any of the fuel pins failed. The average burnup of the highest burnup capsule in this group was 54,600 MWd/Te (6.15 a/o) when they were removed from the EBR-II core. Reinsertion of the capsules and irradiation toward a goal burnup of 100,000 MWd/Te (11.26 a/o) is planned.

7.3.4 Group F8B (Effect of Void Deployment on Fuel Cladding Interactions)

Irradiation of the eighteen Group F8B capsules in EBR-II subassembly X036 continued. The average burnup on the highest burnup capsule in the subassembly was 32,210 MWd/Te (3.62 a/o). Target burnup for these capsules is 50,000 MWd/Te (5.6 a/o).

7.4 HIGH POWER INTERMEDIATE BURNUP IRRADIATIONS

7.4.1 Group F3B (Density and Stoichiometry Effects)

The average burnup on the highest burnup in the Group F3B was 73,530 MWd/Te (8.27 a/o). Irradiation of

the eighteen Group F3B capsules in EBR-II subassembly X027 is continuing. Target burnup for these capsules is 80,000 MWd/Te (9.0 a/o).

7.4.2 Group F4 (Low Density Fuel Behavior at High Heat Ratings)

The burnup calculated for the highest burnup pin in the Group F4 (seven encapsulated pins) test was 56,155 MWd/Te (6.34 a/o). These capsules are being irradiated in EBR-II subassembly X070 toward a target burnup of 80,000 MWd/Te (9.0 a/o).

7.5 ELEVATED CLADDING TEMPERATURE TESTS

7.5.1 Group F5 (Fuel Performance at Peak FCR Cladding Temperatures)

The nineteen Group F5 capsules in EBR-II subassembly X064 continued their irradiation toward a target burnup of 100,000 MWd/Te (11.2 a/o). The average burnup on the highest burnup capsule was 34,110 MWd/Te (3.8 a/o).

7.5.2 Group F10A (Cladding Performance at Peak FCR Cladding Temperatures)

Fabrication and inspection of eighteen of the nineteen Group F10A fuel pins was completed. Twelve of the fuel pins were encapsulated and the capsules filled with sodium. These capsules are undergoing final inspection. Six of the remaining seven fuel pins were encapsulated and are being prepared for filling with sodium. The nineteenth fuel pin is being reworked as a result of the detection of a large

plutonium bearing particle in one of the UO_2 blanket pellets during final inspection using neutron radiography.

7.5.3 Group F10B (Cladding Performance at Peak FCR Cladding Temperatures)

Inspection of the six spare encapsulated Group F10B fuel pins was completed. Preparation of the data package for irradiation of Group F10B in the EBR-II was continued.

Several concepts for inserting coolant flow equalizers in the Mark J-19 subassembly, which is to be used for the Group F10A, F10B, and F10C tests, were prepared and submitted to the EBR-II Project for review. One concept was tentatively agreed upon between General Electric and the EBR-II Project and preparation of detailed design drawings for the parts required was initiated. The purpose of the modification is to reduce coolant flow differences between capsules in the peripheral row and those in the inner rows of the Mark J-19 subassembly. An analysis of the temperature difference resulting from the difference in coolant flow in the Mark J-19 subassembly with and without flow equalizers was reported previously⁽²⁾.

7.6 UNENCAPSULATED FUEL PIN IRRADIATIONS

7.6.1 Group F3A (Unencapsulated Fuel Pin Irradiation to Evaluate Fuel-Cladding and Cladding-Coolant Interactions)

The Group F3A fuel pins are scheduled to be removed from the EBR-II core at the end of run 42 (May, 1970). The fuel pins will be examined at the EBR-II site by the EBR-II Project and selected fuel pins will then be returned to the GE-Vallecitos Laboratory for destructive examination. When removed from the reactor, the fuel pins will have reached a burnup near 50,000 MWd/Te (5.6 a/o). The average burnup for the highest burnup pin in the Group F3A was calculated to be 41,320 MWd/Te (4.6 a/o).

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

The thirty-seven unencapsulated Group F9A fuel pins continued their irradiation in EBR-II subassembly XO43. The pins are scheduled to be removed from the EBR-II core and placed in the storage basket, under sodium, at the end of Run 42. Reinsertion of the subassembly into the EBR-II core is dependent on the EBR-II Project's analysis of the data obtained from the interim examination of the Group F3A. If the EBR-II Project's analysis of the Group F3A performance indicates that the risk associated with continued irradiation of the Group F9A pins is low, the pins are scheduled to be reinserted for irradiation to a burnup of 85,000 MWd/Te (9.57 a/o) prior to performing an interim examination. Target burnup for the fuel in the Group F9A test is 100,000 MWd/Te (11.2 a/o). The calculated average burnup on the highest exposure pin in Group F9A was 43,990 MWd/Te (4.95 a/o).

7.6.3 Group F9B (Medium Power, High Burnup, Unencapsulated Fuel Pin Irradiations)

The Group F9B pins continued their irradiation in EBR-II subassembly XO62. The calculated average burnup of the highest burnup pin in Group F9B was 24,930 MWd/Te (2.8 a/o). Target burnup for these pins is 100,000 MWd/Te (11.2 a/o).

7.6.4 Group F9C (High Power, High Burnup, Unencapsulated Fuel Pin Irradiation)

Calculated average burnup of the highest burnup pin in the thirty-seven pin Group F9C was 37,220 MWd/Te (4.18 a/o). These pins are being irradiated in EBR-II subassembly XO56 toward a target burnup of 100,000 MWd/Te (11.2 a/o).

7.6.5 Group F9D (Medium Power, High Burnup, Unencapsulated Fuel Pin Irradiations)

The calculated average burnup of the highest burnup pin in the thirty-seven pin Group F9D was 26,870 MWd/Te (3.0 a/o). These pins are being irradiated in EBR-II subassembly XO58 toward a target burnup of 100,000 MWd/Te (11.2 a/o).

7.6.6 Group F9E (High Power, High Burnup, Grid Spacer Subassembly)

An approval-in-principle for the Group F9E test was prepared and submitted to AEC-RDT for approval. The proposed test will consist of a thirty-seven pin bundle to be irradiated in a grid spacer type subassembly designed by G. E.

7.6.7 Group F11A (Cladding Performance at Peak FCR Cladding Temperatures)

Thirty-five of the thirty-seven Group F11A fuel pins were loaded with fuel and prepared for xenon tagging prior to welding of the top end plug. The two fuel pins which have not been loaded with fuel are to contain vibratory compacted fuel. The powder and clad for these two fuel pins were prepared for loading. Vibratory compaction of the powder into the clad will be completed during the next report period.

7.7 PRE-IRRADIATION TRANSIENT TESTS

7.7.1 Group F7 (Effect of Irradiation, Density and Axial Restraint)

Calculated average burnup on the fuel in the four Group F7 capsules in EBR-II subassembly XO50 was 47,403 MWd/Te (5.16 a/o). Target burnup for the fuel in these capsules is 50,000 MWd/Te (5.5 a/o). These capsules will be removed from the EBR-II along with the Group F2 capsules in subassembly XO50 at the end of run 42 (May 1970).

7.8 CAPSULES F12C, F12D, AND F12E (E5C, E5D, AND E5E) COMPARISON OF LONG COLUMNS OF MIXED OXIDE FUEL IN THE FORM OF SOLID PELLETS, ANNULAR PELLETS, AND POWDER

Metallographic examination of fuel sections from the F12C, -D, and -E fuel pins was completed. The observations indicated that the fuel in the central region of each fuel pin operated molten. The post-irradiation fuel microstructures of the solid pellet, annular pellet, and powder fuel were similar.

Extensive fuel-clad reaction was observed around the i.d. of the pins. In regions of the pins where the clad i.d. was $>1300^{\circ}\text{F}$ reaction of up to 3 mils of the cladding was observed. Typical photomicrographs showing fuel sections taken from the peak flux region of each fuel pin are shown in Figure 7-9. A section of fuel pin F12C in which part of the fuel was molten is shown in Figure 7-10. The high magnification photographs in Figure 7-10 show a region at the fuel-clad interface where significant fuel-clad reaction has occurred. The photograph taken after etching the clad shows that one mode of reaction is by reaction with the cladding grains. In other regions of the clad evidence of grain boundary reactions proceeding ahead of the general reaction with the grains was observed. Typical photographs of a region where grain boundary attack occurred is shown in Figure 7-11.

Examination of the reaction zones is being carried out using electron microprobe analysis. The results of this investigation will be reported under Task E.

Analysis of samples for fuel burnup from the F12C, -D, and -E fuel pins was completed and are listed in Table 7-4. The burnup values listed are based on neodymium to heavy element ratios at the peak burnup position along the length of the fuel pins.

7.9 CAPSULE F12B, IN-PILE AXIAL EXPANSION AND GAS RELEASE OF MIXED OXIDE FUEL

Fabrication of capsule and fuel pin parts for Capsule F12B was initiated. Orders were placed for the displacement (LVDT) and pressure transducers, and their respective recorders. The recorders were received and are being checked out.

The F12B capsule is to be irradiated in the GETR (thermal flux test reactor) and will provide information on the axial thermal expansion and gas release characteristics of mixed oxide fuel during irradiation to burnups up to 100,000 MWd/Te.

7.10 CAPSULES F12P AND F12Q (E2P AND E2Q) EFFECT OF RADIAL RESTRAINT ON FUEL SWELLING

The F12P and F12Q capsules were removed from the GETR on March 29, 1970 for their fourth interim examination. The burnup on the capsules at the time of removal was calculated to be $\sim 40,000$ MWd/Te. Neutron radiographs of the capsules indicate that the fuel pins did not fail. The fuel pins will be removed from their capsules, profilometer traces obtained, and gamma scanned. The fuel pins will then be reencapsulated, in new capsules, for further irradiation toward a target burnup of 100,000 MWd/Te.

REFERENCES

1. Sodium Cooled Reactors Fast Ceramic Reactor Development Program, Thirty-First Quarterly Report, May-July, 1969, GEAP-10028-31.
2. Sodium Cooled Reactors Fast Ceramic Reactor Development Program, Thirty-Third Quarterly Report, November, 1969-January, 1970, GEAP-10028-33.

8. TASK G-6—CORE DESIGN ANALYSIS

8.1 GENERAL

The purpose of this task 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. The results of these 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 must focus.

8.2 FUEL SUPPORT SYSTEM

Modifications of the COBRA-II flow mixing code have been completed and test problems run for both water and sodium coolants. Dynamic programming techniques and core overlays have been used to reduce the computer

FUEL CENTER

F12C, PELLET FUEL, 316 CLADDING

FUEL-CLADDING
INTERFACE

FUEL CENTER

F12D, POWDER FUEL, 316 CLADDING

FUEL-CLADDING
INTERFACE

FUEL CENTER

F12E, ANNULAR PELLET FUEL, 316 CLADDING

FUEL-CLADDING
INTERFACE

Figure 7-9. Radial Montages of Fuel Sections Cut from Peak Flux Region of Fuel Pins F12C, -D, and -E

(a) RADIAL SECTION OF F12C FUEL

REACTION PHASE

(b) FUEL CLADDING REACTION

REACTION PHASE WITH ETCHED CLADDING

(c) FUEL-CLADDING REACTION ETCHED

Figure 7 10 Radial Montage of Section from Pin F12E Showing (a) Region of Fuel which Operated in Molten State, (b) High Magnification (500X) Photograph of Region of Fuel Clad Reaction Area Marked by Rectangle in (a), (c) same as (b) except Cladding has been Etched (500X)

GEAP-10028-34

AS-POLISHED

(250X)

FUEL ETCHED

(250X)

CLADDING ETCHED

(250X)

Figure 7-11. Photomicrograph of Typical Cladding Grain Boundary Attach Observed in Fuel Pin F12D

Figure 8-1. Grid Spacer Bundle

core requirements by 40 to 75%, depending on the size of the rod bundle investigated. Sodium properties and single-phase heat transfer correlations have been incorporated.

An auxiliary subroutine which will automatically calculate the geometry inputs required by COBRA-II is 80% complete. The calculated inputs include identification of flow channels (areas, perimeters and hydraulic diameters) and specification of inter-channel and fuel rod-channel connections. This will greatly simplify data preparation. For example, a 127-rod bundle has approximately 250 flow channels and 750 inter-channel connections.

The sample water flow case presented in Appendix E of Ref. 1 was rerun to verify the basic code, and a seven-rod sodium case was run to check the property modifications. Input data decks for 37 and 169-rod bundles, corresponding to the EBR-II and FFTF in pile bundles, are now being prepared.

A study was performed to determine how well the F9E grid spacer experiment in EBR-II would simulate environmental conditions, loads and resultant stresses that would occur in a 350 MWe LMFBR. The fuel assemblies have 0.25 inch outside diameter fuel clad 0.015 inches thick and a 1.28 pitch to diameter ratio with 37 and 127 pins in F9E and the LMFBR respectively. Comparative conditions are shown in Table 8-1. The two LMFBR core positions are on the inner and outer edge of the outside fuel zone.

The major loads in the fuel elements result from pin bowing which is caused by cladding material thermal expansion and irradiation induced swelling. An index which can be used for comparison is the temperature gradient and the equivalent temperature gradient caused by swelling. This is indicated in Table 8-2. These thermal gradients result in comparable stresses in both pins and grids. Other stresses caused by vibrations and pressure drops are very small in comparison. The results of this study led to the conclusions that the F9E experiment proposed for irradiation in EBR-II does provide a reasonable simulation of conditions expected in an early LMFBR.

Drawings of 169, 127, 61 and 37 rod bundle cross-sections were made to help ascertain needed channel sizes and wall thicknesses for six pitch-to-diameter ratios from 1.20 through 1.30 in increments of 0.02 inch. This covers the range of interest for the comparison between fuel environments in EBR-II, FFTF, 350 MWe and 1000 MWe LMFBR. This resulted in a limit for the EBR-II test bundle of 37 pins to enable maximum utilization of existing parts and tools and meet all major technical simulation requirements. Exploratory layouts were completed covering several approaches to fuel support, grid spacer mounting, spacer to channel assembly, by-pass flow passage insulating means, fuel removeability in the event of need for interim examination, and the investigation of the bundle assembly and disassembly sequence. A conceptual layout of a

tentative reference bundle was completed. See Figure 8-1. This reference approach utilizes a maximum number of components interchangeable with existing EBR-II bundle hardware.

Design effort has been concentrated more recently on evolving alternative methods of spacer to channel assembly. Design emphasis was also placed on approaches to interim and post-irradiation examination of grids, examination of rods in contact with grids and measurements of wear, damage and/or dimension changes and characteristic changes in material.

A conceptual design was completed of a pressure drop experiment version of the EBR-II grid spacer bundle. The design includes non-prototypical re-usable fasteners for disassembling major components and accessories for adapting the test bundle to the flow loop test sections. Capability to change internal orificing at the nose piece is a feature of the design so the internal-to-external flow splits can be tested. Connection adapters are incorporated in the removable channel and liner to permit measurement of pressure drop and detection of vibration.

8.3 STEADY-STATE IRRADIATION EVALUATION

This subtask is aimed at assessing the 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 are being assessed on the basis of their impact on core design, safety and fuel cycle economics.

A draft copy of a topical report has been completed and is now undergoing extensive internal review, editing and rewriting. Particular emphasis during this review will be placed on focusing recommendations on those tests which are critical to the understanding of fuel and bundle performance. The test recommendations, their costs and schedule will be consistent with the need for design information in support of FFTF and the demonstration plant.

8.4 FUEL FAILURE EFFECTS

The objective of this subtask is to produce a comprehensive assessment of the information on the effects of fuel element failure which is needed to design LMFBR fuel assemblies.

8.4.1 Fault Trees

Given that a fuel pin failure occurs in one bundle somewhere in the reactor, three questions are relevant:

1. Does the initial failure cause failure in adjacent pins, failure in adjacent fuel assemblies, or failure in many assemblies?
2. Do the released fission products and fuel from both the initial failure and the induced failures add

Table 8-1
TYPICAL EBR-II AND LMFBR
ENVIRONMENTAL CONDITIONS

Conditions	LMFBR 1st Core		
	EBR-II 4N1	Zone II Inner Edge	Zone II Outer Edge
Maximum Fluence > 0.1 MeV (10^{22} n/cm ²)	6.1	8.3	5.5
Fluence Gradient (10^{22} n/cm ² /in.)	0.33	0.14	0.49
Burnup (MWd/kg)	100	50	31
Linear Power (kW/ft)	15.3	11.6	7.9
Coolant Velocity (ft/sec)	10.0	19.0	11.8

Table 8-2
EBR-II AND LMFBR COMPARATIVE TEMPERATURE GRADIENTS

		LMFBR 1st Core		
		EBR-II 4N1	Zone II Inner Edge	Zone II Outer Edge
<u>Thermal</u>				
Lateral Temperature Gradient (°F/in.)	Pin	220	200	200
	Channel	12	10	24
Expansion Coefficient (10^{-6} in./in. °F)		10	10	10
<u>Swelling</u>				
Equivalent Temperature Gradient (°F/in.) caused by				
a) Fluence Gradient		216	121	327
b) Temperature Gradient		<u>460</u>	<u>745</u>	<u>450</u>
Total a) and b)		676	866	777
Equivalent Expansion Coefficient (10^{-6} in./in. °F)		21	37	22

significantly to the radioactive contamination of the reactor systems?

3. Must the reactor be shut down to remove the failed fuel?

If the answer to all three of these questions is negative then the fact that the fuel pin failure occurred is insignificant.

However, the answers to these questions are interdependent. If the failure propagates to adjacent pins, there is probably a point beyond which the added radioactive contamination is significant. Even if the contamination is insignificant, there is probably a point beyond which the assembly is structurally unsound and the difficulty of

removing it is significantly increased. If the reactor is shut down immediately to remove the failed fuel, then the probabilities of failure propagation and of excessive contamination are reduced.

It is apparent, then, that trade-offs exist in the fuel design features, the plant design features, and the plant operating philosophy to minimize the net effect of fuel element failure. For instance, an operating philosophy which requires the immediate detection, location, and removal of each failed fuel pin will probably require complex instrumentation and will probably incur operating cost penalties due to frequent reactor shutdowns and to reduced average fuel burnup. However, this operating

philosophy will allow cost savings in the provisions for handling highly contaminated cover gas, contaminated primary sodium, and contaminated equipment.

In addition, the design of the fuel assembly will affect both the frequency of initial pin failures and the probabilities of failure propagation. However, the incentive to make compromises on the fuel assembly design will depend upon the cost savings and safety benefits to be realized in the remainder of the reactor system.

An orderly framework is required in which to compare logically the many effects involved (fuel design and performance characteristics, system design and performance, and reactor operating philosophy). Such a framework can be provided through the use of a "fault tree".

A rigorous introductory description to the concept of a fault tree is presented in Reference 2. Basically, the fault tree is a graphic representation of all of the events which lead to a given undesirable event. Each undesirable event constitutes the tree trunk and the events leading to the undesirable event constitute the tree roots.

Extensive and detailed fault trees were completed for twelve arbitrarily defined states of the reactor. It was intended that the twelve states would effectively characterize the full spectrum of final states which result from fuel failure effects. Included are reactor shutdowns to remove fuel bundles which contain damaged fuel and either damaged or undamaged channel walls. Also included is continued reactor operation with different levels of fuel damage.

Similarly, twelve initiating events were arbitrarily defined, which apply to all twelve of the fault trees. The twelve events are intended to characterize the full spectrum of initiating events. The initiating events considered were "normal" fuel pin failures, channel inlet flow blockages, and large reactivity transients. Fuel pin failures were divided into three categories: (1) Crack Failure — fission gas is released through the clad but significant amounts of sodium have not yet entered; (2) Large Area Failure — a significant amount of sodium contacts the fuel; (3) Burst Failure — fuel is rapidly expelled from the pin into the coolant. Preliminary values of frequency of occurrence were assigned to each of these initiating events.

The fault trees show nearly all of the intermediate steps required for the reactor to progress from one of the initiating events to one of the final states. Preliminary values were assigned to each of the probabilities of progressing from one state to the next successive state, based on the state-of-the-art as determined through an extensive literature survey.

All of the preliminary estimates were based on the reactor design and operating philosophy presented by GE in the Task II report of the 1000 MWe LMFBR Follow-On Study, Reference 3. These estimates were combined, using

the fault trees, to establish the frequency and estimate the mean-annual-cost of each of the final states of the reactor.

The results were used to identify the most important sequences of events through which fuel failures affect the cost of the reactor and of reactor operation. Effort will be devoted to examining, more carefully, each of these important events, and identifying the development effort necessary to minimize its probability.

8.4.2 Structural Analysis

8.4.2.1 Transmission, Reflection and Absorption of Shock Impulse

There appear to be at least three regimes in the sonic transmission of plane pressure pulses that require differing techniques of analysis for assessing the reflection, transmission and absorption of energy by an obstacle immersed in the fluid traversed by the pulse. The three regimes for normal incidence are indicated as:

- a. For a thin solid (where the thickness in the direction of travel of the pulse is much less than a wave length of the pulse), the wall appears to be nearly transparent to the pulse. In this case the pulse seems to carry the solid along with the particle motion of the fluid, with energy consistent with the particle displacement absorbed by the wall and the remainder transmitted. An approximation of the energy deposited can be obtained by consideration of the amplitude of the particle motion of the fluid forcing a deflection of the flexible solid, with the energy associated with plastic deformation of the solid absorbed. It would appear necessary that the intensity of such a pulse be great enough to deform the solid plastically and that relatively rigid portions of the solid might not fit into this regime. It would also appear necessary that the dimensions of the wall perpendicular to the direction of travel be equal to, or greater than, a wave length for this response.

This regime has not been investigated sufficiently by either analytical or experimental methods to instill confidence in such speculation and only limited evidence is available to support the indicated procedure.

The test of Reference 7 would appear to be in this regime and the limited information available from this test would support the inferences drawn above, from References 4, 5 & 6. This test is discussed later.

- b. For a thick solid (where the thickness in the direction of travel of the pulse is very much greater than the wave length of the pulse) the acoustic laws of transmission, reflection and absorption apply at each interface in the elastic range.
- c. For intermediate thickness of solid (where the thickness in the direction of the travel of the pulse is a

wave length or more), the phenomena are even less well known. It would seem prudent for energy absorption in the solid to consider both acoustic absorption and impulsive loading as additive as in the thick solid. The present state of the art does not seem to permit a correction for the relative velocities of the wall and the pressure pulse.

It would appear that in a prediction of damage from a pressure pulse originating from contact of liquid sodium and over-heated fuel rods that a worst case assumption would be necessary for either deposition or transmission of energy. A combination of acoustic energy absorption and impulsive energy absorbed from the reflected wave (as illustrated in Reference 8) without accounting for thickness of obstacle, is probably a worst case for energy absorbed. For reflected energy, an acoustic reflection is probably the maximum. For transmitted energy, it would appear possible to transmit the wave nearly undiminished. The prospect for properly partitioning the energy of the pulse between absorbed, transmitted and reflected appears dim unless the wave length or period of the pulse can be estimated with fair accuracy and confidence.

An effort has been made to correlate the test reported in Reference 7, where an explosive charge detonated in a fuel pin of an EBR-II sub-assembly in water. An approximate pressure vs time is reported, as is the deformation of the duct. The presence of a cluster of fuel pins surrounding the central one containing the charge is reported to have negligible effect on the deformation of the duct when compared with the single pin exploding. The explosive charge of 125 mg of Bullseye Pistol Powder is reported to release less than 0.15 Btu. The pressure-time pulse at the duct wall was measured for 20 explosions with a maximum peak pressure of 3150 psi and an average peak pressure of 2600 psi. The rise time was very short, less than $20 \mu\text{sec}$, but the pulse endured for about $200 \mu\text{sec}$. The decay pressure is reported to be defined by the equation

$$P(t) = P_o e^{-t/\theta}$$

where

- $P(t)$ = Pressure at time "t".
- t = Time from peak pressure
- θ = Time constant of $58(10)^{-6}$
- $P(o)$ = Peak pressure

The maximum value of deformation of each flat is reported as 0.0206 inches. From other sources it was determined that the duct was a hexagonal cylinder of 1.3025 inches flat dimension, with 0.040 inch thickness of 304 stainless steel. The fuel cladding tubes of the same material were 0.174 inch O.D. and 0.009 inch wall thickness. Using a linear rise to a peak pressure of 3000 psi and the indicated decay curve, the impulse (momentum) of

the pulse was computed as approximately 0.15 lb sec/in^2 . On the same basis, the energy of the pulse is calculated to be about 31 in lb/in^2 . The energy absorbed by the duct when the deformation of the flat is 0.0206 inches is computed as about 0.5 in lb/in^2 . The wave length of the pulse in water is $\lambda = TC = 0.0004 (65,400) = 26$ inches and in steel is $0.0004 (220,000) = 88$ inches where T is the period of the wave. The wave length, when compared with the wall thickness of the duct of 0.04 inches, indicates that the duct wall is very thin for this pulse and should transmit the pulse undiminished, by acoustic theory. The natural period of vibration for the duct is about 0.00014 sec as compared to 0.0002 sec for the duration of the pulse.

The best correlation obtained was the particle travel of the fluid during the pulse compared to the observed deformation of the wall. By integrating

$$\delta = \int \frac{P}{k} dx$$

over the length of the pulse one obtains a travel of 0.024 inches for a particle of the water. In this integral P is a function of position in the pulse and k is the bulk modulus of elasticity of the fluid. This compares very well with the observed plastic deformation of 0.0206 inches at the center of the duct wall. This evidence would indicate that the flexible span of the thin wall was simply forced to follow the motion of the water as the pulse passed.

The clad tubes used in the experiment were also assessed. The empty clad tubes had a bending frequency of about 93 Hz with a period of 0.011 sec, while water filling changed the frequency to 82 Hz period to 0.012 sec. Sand fill resulted in frequency of 74 cps and period of 0.0135 sec. The O.D. of the tubes, equal to 0.17 inch when compared to the wave length of the pulse in water of 26 inch would indicate little, if any, interruption of the pulse by the tubes. For a circular shell, the frequency is about 37,000 cps with a period of $0.000027 < 0.0002$ (duration of pulse) so that a quasi-static pressure is indicated for collapse of the tubes. The elastic buckling strength of the tubes for external pressure is about 10,000 psi (> 3000 psi peak pressure). Assuming the yield strength of the material to be 36,000 psi, yielding of the tube (plastic instability) might occur at about 3700 psi (> 3000 psi peak pressure). The Reference 7 reports little energy absorbed by the tubes and this report is consistent with the pulse duration and dimension of tube.

8.4.2.2 Resistance of Duct to Shock Impulse:

In order to assess the ability of the hexagonal ducts to absorb energy in the large deflection, plastic distortion range, from shock impulses originating from re-entrant coolant, a solution has been formulated relating distortion and strain to energy, that can be programmed for computer solution. With this tool available, it should be possible to

run parameter studies of variations in geometry, materials and condition of the material for the ducts.

The solution for an impulse which originates inside the duct is based on an axisymmetric loading that can be treated as a plane wave impinging normal to the duct wall and of sufficient axial extent to make a plane strain solution adequate for the loaded length. The shock wave is assumed to impart kinetic energy to the wall of the duct which is then degraded by the elastic and plastic absorption of energy by the duct. It is assumed that this absorption is independent of the manner of loading and dependent upon the distortion of the duct. With this assumption, an equivalent loading producing a similar distortion will result in an approximately equal deposition of energy in the duct. The problem is then reduced to finding a tractable loading that will produce an essentially proportional distortion of the section. It is reasoned that a shock impulse will reach the flats of the hexagonal ducts, deposit energy in the kinetic form, and that the momentum will be retarded by the resistance of the duct. The flexibility of the duct would lead to the assumption that the resistance, and therefore the loadings, would not be uniform but be greater at the corners than at the center of the flat. Such a loading is indeterminate, the solution difficult, if not intractable, and the accuracy of the input energy probably does not justify the effort required to secure an exact solution. As an approximate solution, it is proposed that a uniform loading with equivalent energy input would result in larger deflections at the center of the flats and greater strain at the corners than the precise loading and that the uniform loading would be conservative in the sense that it would yield larger ratios of strain to energy or deflection to energy than the exact loading.

The solution programmed is a limit-load-type with idealized elastic-plastic material. The solution is in steps for bending resistance at: the elastic limit, the double plastic hinge limit, the triple plastic hinge limit and continues with the membrane resistance, if applicable, for progressive distortion of the section. For internal loading of the duct, the adjacent sides of the hexagonal duct will provide the reactions required for membrane loading of the flats, but for external loading the solution terminates when three plastic hinges develop.

A solution for internal loading has been programmed with an automatic plotting routine for energy versus maximum deflection and strain versus maximum deflection. There is a slight discontinuity evident at the juncture of the three hinge and membrane solution in some trial cases, but it is believed that it can be readily smoothed by a combined solution in this region. The solution agrees rather well with a more complex solution by finite elements of a beam with material in the plastic range.

A solution for loading external to the duct (unsymmetrical) has been formulated and programming will be

initiated immediately. This solution will enable analysis of ducts loaded from one side only from a transmitted energy pulse.

An extension of this type of solution is planned for fuel pins supported at intervals to study the effect of varying parameters on the absorption of energy in these elements from a shock pulse.

8.4.3 Fuel, Clad, Sodium Interactions

Phenomena studied under "Fuel-Clad-Sodium Interactions" include the rate of heat transfer to sodium and mechanisms for the propagation of energy to adjacent fuel pins and channel walls. The studies are aimed toward better understanding of basic phenomena, which in turn should lead to less conservative, but acceptable analytical predictions of accident consequences.

8.4.3.1 Fuel-Sodium Interaction Analytical Models

8.4.3.1.1 Process of Fuel-Sodium Interaction

All of the analytical models presently in use attempt to characterize the process of energy transfer when fuel (and possibly cladding), heated in the absence of coolant, come into contact with liquid sodium. The fuel and clad may be in the solid or liquid state. Contact may occur following a pin failure which expels fuel into the coolant stream, or when condensed coolant re-enters a voided channel. While the area of contact between fuel and sodium may differ greatly between these two situations, the basic process is the same in either case. The sequence of events leading to propagation of the energy to channel walls is as follows:

1. Fuel is heated to a high temperature in the absence of coolant, then the hot fuel and sodium are abruptly brought into intimate contact.
2. Heat flows from the fuel to the sodium at a very high rate, thus creating a high temperature gradient in a thin layer of coolant near the fuel.
3. The rate of heat addition is initially much higher than the rate at which the thin coolant layer can dissipate the energy by expansion against adjacent coolant. Therefore, a high pressure buildup occurs.
4. Energy contained in the highly compressed coolant layer is transferred to surrounding coolant in the form of a steep-fronted pressure wave, or "shock" pulse. The potential of this acoustic wave for doing mechanical work on adjacent pins or channel walls is referred to as the "acoustic work". The term "violent boiling" is also used in reference to this process.
5. Behind the acoustic wave, some pressure is sustained by the inertial resistance of fluid adjacent to the fuel and ultimately by the rigidity of the surrounding structure. The mechanical work potential of this relatively long-acting pressure loading is referred to by some investigators as the "inertial work".

6. Continued heat transfer from the fuel provides energy which tends to sustain the pressure level. Some analytical models utilize the "thermal equilibrium" assumption as a basis for the termination of heat transfer. Under this assumption, heat transfer continues until the fuel and adjacent sodium reach the same temperature. However, it is likely that heat transfer will actually stop earlier than this due to coolant vaporization at the fuel-coolant interface. This film blanketing condition follows the local pressure decrease which occurs as the fluid volume expands.

8.4.3.1.2 Hicks-Menzies Model

The thermodynamic analysis of Hicks-Menzies⁽⁹⁾ gives an upper limit of the work corresponding to adiabatic expansion of the fuel-sodium mixture. No attempt is made to predict the partitioning of work into the forms of shock and fluid expansion. The model is conservative with regard to the transfer of energy from fuel particles to sodium, in that it assumes the fuel-sodium mixture to remain in "thermal equilibrium" throughout the transient. That is, the fuel particles and sodium are both at the same temperature throughout the transient.

The initial interaction is assumed to take place at constant volume. Heat is transferred from very hot fuel particles to sodium until the fuel and sodium temperatures are equal, but no external work is done in this process. From the initial equilibrium point, the mixture is assumed to expand adiabatically to a lower pressure. This is certainly conservative since in reality heat transfer to structural materials will reduce the potential for damage.

The amount of work is dependent on the pressure to which the expansion is carried. Expansion to 1 atm gives a very conservative result because the damage threshold is well above this level (perhaps in the 60-70 atm range). The amount of work done in expansion from this threshold to 1 atm is a considerable fraction of the total (e.g., on the order of 50% according to Hicks-Menzies published results.⁽⁹⁾

The work done is also dependent on the fuel-sodium mass ratio. Parametric studies by Hicks and Menzies⁽⁹⁾ show that as the ratio increases, the work done first increases, then decreases.

8.4.3.1.3 Improvement of the Thermodynamic Analysis of the GE Analytical Model

Reference 10 gives a detailed derivation and explanation of the FFTF model of fuel-cladding-sodium thermal interaction developed by G.E. This model gave a very conservative estimate of the "acoustic" work potential of the initial shock, and an estimate of the vapor expansion work potential.

Before it is possible to study the thermodynamics of a system, it is necessary to establish its boundaries. Having

done this, thermodynamic principles may be applied to predict the amount and form of energy which crosses the system boundaries. Define the thermodynamic system to be that mass of coolant which is originally within a distance, X_S , of the fuel-coolant interface, where X_S describes the position of the shock front at the time that heat transfer from the fuel terminates.

The degree of conservatism in the calculated acoustic work potential of the model in Reference 10 can be reduced substantially. Consider the generally applicable statement:⁽⁸⁾,

$$ds \geq \frac{d'Q}{T}, \quad (1)$$

which refers in this instance to the entropy of, heat addition to, and temperature of the thermodynamic system as defined. This, together with the first law of thermodynamics leads to the inequality:

$$d'w \leq Tds - du. \quad (2)$$

Since $d'Q$, the heat added to the system, is positive, ds is also positive. The temperature of the coolant in the thermodynamic system, T , is a monotonically increasing function during the heat transfer process. It follows that

$$Tds \leq T_{AVG}(t_3) ds, \quad (3)$$

where $T_{AVG}(t_3)$, it will be recalled, is the system temperature at the end of the process (defined as the time that coolant boiling begins at the heat reservoir interface).

$$d'W \leq T_{AVG}(t_3) ds - du. \quad (4)$$

The heat reservoir interface temperature, T_0 , is greater than $T_{AVG}(t_3)$. Use of a nominal value for $T_{AVG}(t_3) = 1408^\circ\text{F}$ in place of the heat source interface temperature used in Reference 2 (2818°F), reduces the calculated acoustical work potential from 16,316 in.-lb/lbm to 63 in.-lb/lbm.

8.4.3.1.4 A More Detailed Model of Acoustic Wave Propagation

This section describes the results of an analysis of the acoustic wave associated with violent boiling, utilizing the heat conduction equations of the previous model⁽¹⁰⁾ and detailed hydrodynamics equations. The analysis is programmed in a computer code. The "acoustic work" is energy associated with the acoustic wave and includes the energy of compression (work done in an isentropic expansion to one atmosphere), and kinetic energy of fluid behind the shock front. Comparison of these results with those given by the earlier model⁽¹⁰⁾ affirms the conclusion that the latter highly overestimates the acoustic work potential.

A solution for the pressure wave's shape at a given time after the initiation of the interaction, and the internal (local) velocities in the sodium behind the pressure wave front were obtained. The very high peak pressure—the shock wave—predicted in Reference 2 appears as a very narrow (<0.1 mil) wave, followed by a relatively low pressure wave extending back to the interface.

Figure 8-2 shows the pressure distribution 1 microsecond after the meeting of 6800°F UO_2 and 1000°F sodium at a slab interface. No damping of the peak pressure occurs in the model; at $t = 10 \mu$ seconds, the peak value is the same, but the location is 0.90431 inches into the sodium.

Table 8-3 shows the energy distribution associated with the pressure wave shown in Figure 8-2. It will be noted that the kinetic and potential energy terms, which are independently calculated, are equal as predicted by hydrodynamic theory. The combined thickness of the outer two zones is only 0.015 mils, but contains about one-quarter of the wave's "acoustic" energy. The total energy transferred, up to this point, into the sodium, is about 48.4 in-lb/in.^2 (of interface), assuming that vapor formation has not yet interrupted the heat transfer process. Thus, only 0.55% of the transferred energy is acoustic.

The change of the acoustic energy within the wave is quite slow. The total energy (heat) transferred varies as the square root of the time. Table 8-4 shows the relationship of the acoustic to total energy transferred as a function of time.

The time at which vapor formation occurs and cuts off the heat transfer process depends on the amount of superheat assumed. The amount of acoustic energy predicted is much lower than the upper limit described by the model of Reference 10, in agreement with the results given in the previous section.

8.4.3.2 Experimental Programs

Experimental studies of water-liquid metal interactions are being reviewed. Several TRW reports⁽¹¹⁾ indicate that as much as 10% of the energy in molten metal can be converted to mechanical energy in 1 millisecond after being hit by a column of water. Pressures as high as 5800 psi were measured. The amount of energy transferred from solid surfaces (metal and UO_2) to water was relatively small, and appeared to be insensitive to the solid material's temperature. The pressure peak in a test with UO_2 at 1200°C was less than 500 psi; the 5800 psi was reached with liquid aluminum at 1000°C . The efficiency of the conversion of the thermal energy to mechanical energy is apparently about 100 times higher in the liquid to liquid case than in the solid to liquid case. Thus, the TRW experimental results suggest that in order to get high rates of heat transfer and energy conversion, the heat source must be molten.

Review of the similar and more recent work at ANL is continuing. The relatively abundant metal-water data may be useful in confirming models of coolant-fuel and clad interaction.

8.5 COOLANT BOILING AND OPERATING SAFETY LIMITS

The object of this task is to make a comprehensive assessment of the information on coolant boiling and operating safety limits needed to design LMFBR fuel assemblies.

8.5.1 Coolant Boiling

The key areas defined for the study of sodium boiling are summarized as follows:

1. Threshold conditions causing sodium boiling
2. Sodium boiling stability
3. Threshold conditions for fuel and clad melting
4. Threshold conditions and consequences of violent boiling
5. Rate and coherence of core voiding
6. Rate and coherence of fuel meltdown
7. Propagation potential due to sodium vapor bubble collapse
8. Propagation potential due to sodium hammer or reentry after voiding
9. Damage potential due to hydraulic forces of reentry after boiling or fission gas release
10. Propagation potential by debris or gas swept into the inlet plenum

The last four areas, the mechanical consequences of sodium boiling, were identified this quarter in a study of mechanistic fault trees in conjunction with the fuel failure effects task. Figure 8-3 is typical of the fault trees showing possible paths leading from flow blockage initiation to damage sustained by either fuel pins or channel walls that have been completed. The initial review of the coolant boiling, United States and European programs, based on available literature, has been completed.

8.5.2 Safety Limits

For review of the operating safety limit, defined as that condition which causes immediate fuel pin failure, consideration is given to three general reactor perturbations,

1. overpower transients
2. flow reductions
3. fuel failure effects (propagation).

The state-of-the-art review is essentially complete for the power transient category and is progressing for flow reduction perturbations. The study of propagation mechanisms which exceed the safety limit will be coordinated with the effort of the Fuel Failure Effects subtask.

Figure 8-2. Pressure in 1000°F Sodium 1 Microsecond after Contact with 6800°F Slab of UO_2

Table 8-3
ACOUSTIC ENERGY IN WAVE 1 μ SECOND AFTER START
OF PROPAGATION FROM SLAB SOURCE
(UO₂ SOURCE @ 6800°F, SODIUM @ 1000°F)

Zone	Width Inches	Maximum Distance From Interface Inches	$\bar{P}$ Atmosphere	K.E. in.-lb/in. ²	P.E. in.-lb/in. ²
1	0.08	0.08	45.77	0.0303	0.0315
2	0.01	0.09	139	0.038	0.0385
3	3x10 ⁻⁴	0.0903	461	0.0111	0.0112
4	1x10 ⁻⁴	0.0904	818	0.0119	0.0119
5	2x10 ⁻⁵	0.09042	1497	0.0078	0.0078
6	1x10 ⁻⁵	0.09043	2801	0.0144	0.0144
7	0.5x10 ⁻⁵	0.090435	3824	0.0205	0.0205

Total — "acoustic energy"

0.27 in.-lb/in.²

Table 8-4
ACOUSTIC TOTAL ENERGY — AS FUNCTION OF TIME
UO₂ @ 6800°F, SODIUM @ 1000°F, — SLAB SOURCE

Time-Seconds	Total E Transferred in.-lb/in. ²	Acoustic Energy in.-lb/in. ²	Acoustic/Total
1x10 ⁻⁹	1.53	0.10	6.5%
1x10 ⁻⁸	4.84	0.17	3.3%
1x10 ⁻⁷	15.3	0.23	1.5%
1x10 ⁻⁶	48.4	0.27	0.55%
1x10 ⁻⁵	153	0.35	0.23%
1x10 ⁻⁴	484	0.41	0.08%

The object of the state-of-the-art review for safety limits is to identify the parameters directly affecting the primary failure mode and to determine the present knowledge of the transient magnitude of these parameters. As an example, for a transient overpower accident extending into fuel melting, the present uncertainty in the effective transient thermal diffusivity of the molten fuel will directly affect the predicted time of cladding breach, as well as breach size, magnitude of Doppler feedback, extent of post-failure movement of fuel and the rate of ability to dump energy to other reactor components. This uncertainty in accident consequences might lead to uneconomical design choices, for example, lower fuel density, to gain assurances of adequate transient overpower margin.

8.5.3 Sensitivity Studies, Sodium Boiling

The state-of-the-art review forms the basis for accomplishing the sensitivity studies of the important design parameters, analytical assumptions and experimental accuracy. The analytical tools being investigated for use in

this study include the VOIDM, FREADM and COBRA II codes, the first two for whole core or bundle transients and the latter for the steady state temperatures following local blockages.

8.5.4 Sensitivity Studies, Sodium Superheat

The significance of sodium superheat in loss of flow situations was reviewed. The result was a proposed criterion for defining the conditions under which the sodium superheat is important. "Low" superheat can be considered the regime where the value of superheat to initiate boiling does *not* have a commanding influence on the subsequent rate of reactivity insertion due to voiding. For significant superheats, perhaps greater than 40 to 50°F, inertia limited slug flow will occur on boiling initiation. The rate of voiding and hence rate of reactivity change will be a function of the integrated or average pressure acting on the slug. This average pressure is a function of both the initiating superheat and the temperature transient experienced by the vapor bubble.

Figure 8-3. Voiding One Channel Causes Many Cracks, Large Area or Burst Failures in a Few (Many) Sound Bundles

"Low" superheat could be considered the regime when the temperature increase of the sodium remaining in the core (driving the vapor bubble temperature and pressure up) during the initial voiding process is larger than the initial superheat. A simple example is the FFTF flow blockage case(10). Figure 8-4 which illustrates this criterion. The voiding time shown was calculated for a constant driving pressure equal to saturation at the initiating superheat, i.e., the slowest time to void. The temperature increase causes a higher average pressure and resultant shorter voiding time.

If the superheat to initiate boiling were 65°F (point A), the time to void at the corresponding saturation pressure would be about 0.2 second (point B). However, during this time interval, the temperature of the sodium (either trapped in the core or remaining as a film on pins and spacers), and consequently the vapor bubble temperature, would increase about 200°F (point C). Assuming inertia limited voiding, the peak temperature and corre-

sponding pressure in the core then corresponds to 265°F superheat at the time the vapor bubble passes the core boundary. The average driving pressure then corresponds to some saturation temperature between 65 and 265°F .

This indicates that a minimum effective voiding pressure exists because of the transient temperature increase for low superheats, or that the voiding rate for "low" superheat is not strongly dependent on the value of superheat but rather on the temperature transient. In contrast, the voiding rate with "high" superheat (point D) is clearly a strong function of the value of superheat and resultant vapor pressure, and the temperature transient would have minor influence on the voiding rate.

For the complete loss of flow in FFTF(10), the value of superheat dividing the arbitrary "high" and "low" regimes is about 130°F . For comparison, superheat tests which closely simulate LMFBR conditions(12) show the superheat to be generally less than 100°F , with the majority of runs less than 60°F , i.e., in the "low" superheat category.

Figure 8-4. Initial Superheat or Transient Temperature Increase

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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. 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 must focus.

9.2 MODEL DEVELOPMENT

9.2.1 FREADM Code

Coding to permit simulation of full or partial flow blockage in selected bundle types was added to the FREADM code. Flow blockages may be triggered (in cases where velocity tables are used) based on computed conditions reaching pre-set trigger levels within the blocked bundle type or within another specified bundle type. A summary of the flow blockage logic is shown in Table 9-1.

A more accurate steady-state temperature calculation was incorporated using a heat balance to compute correct

steady-state coolant and clad temperatures before computing fuel temperatures. The previous method did not insure the correct coolant nodal output temperature with variable fuel properties. The resultant errors accumulated as the calculation progressed up the channel.

The voiding model used in FREADM was compared with static 2-D flux synthesis (BISYN) calculations for the reactivity effect of space dependent sodium voiding. Figure 9-1 shows the integrated reactivity effect of voiding one axial segment of a 350 MW core from the centerline out. A parabolic distribution is assumed in FREADM with the location of the peak specified by the user. An axial voiding distribution representative of a pump coastdown was used to compute a table of reactivity versus time using BISYN voiding reactivity calculations for a 350 MW plant. Comparison with FREADM calculations (assuming identical voiding patterns) is shown in Figure 9-2. The initial reactivity effect is negative, since voiding is initiated at the core outlet.

9.2.2 Space Dependent Kinetics

Efforts to develop the GAKIN⁽¹⁾ code as a calculational standard for fast reactor kinetics analysis continued. Problems in the algorithm used to integrate the fluxes were

Table 9-1
SUMMARY OF FLOW BLOCKAGE TRIGGERS

1. Elapsed time from start of transient reaches input value.
2. Average fuel temperature of an axial section reaches or exceeds input value.
3. Clad temperature reaches or exceeds input value.
4. Temperature at a specified radial node reaches or exceeds input value.
5. Elapsed time from flow blockage in another specified bundle type reaches input value (propagation).
6. Elapsed time from average fuel temperature exceeding trigger level in another specified bundle type reaches input value.
7. Elapsed time from clad temperature exceeding trigger levels in another specified bundle type reaches input value.
8. Elapsed time from temperature of a specified radial node exceeding trigger level in another specified bundle type exceeds input value.

found which severely limit the utility of GAKIN for fast reactor calculations.

1. The assumption is made that all energy groups have the same temporal behavior as the lowest energy group. This assumption leads to errors in the sign of the computed values for ω where the flux at a space-energy point is assumed to obey

$$\phi(t) = \phi_0 e^{\omega t}.$$

2. For ramp reactivity insertions in which the value of ω is constantly changing, the growth rate of the power is limited to $\sim 0.1\%$ per time step.

These results suggest that a change in the basic algorithm for integrating the flux is required. A possible solution may be to compute separate values of ω for each space-energy point.

The QX-1 code is being acquired and its use as a calculational standard will be investigated.

9.2.3 Kinetics Models

A study has been completed on the problems associated with using the unperturbed spectrum to obtain constants for few-group reactor kinetics. It has been found that by preserving the delayed neutron effectiveness, the errors of up to 30% previously found⁽²⁾ in the calculation of the dominant reactor period have been reduced to less than 1% (in a bare homogeneous reactor model). The

procedure is to use a β_i -effective as in the multigroup model:

$$\beta'_1 = \frac{(\beta'_1) \frac{16 \text{ gr}}{4 \text{ gr}}}{(\beta'_1)} \beta_1$$

where: β_1 is the effective β_1 .

This procedure is similar to that of Putnam⁽³⁾ who has developed a scheme using flux-adjoint weighted prompt and delayed neutron spectra, plus a new effective delayed neutron fraction. In this way he is able to create few group constants which do not excessively increase or decrease the relative importance of delayed to prompt neutrons.

9.2.4 Single Channel Voiding Model

The improved single channel voiding model being developed describes the formation of an initial bubble in the hot region of a reactor channel, and follows its growth and recondensation in cooler regions. The main difficulty encountered thus far was an instability in the calculation when the bubble moves from the first node to the second, in which recondensation starts. This problem has been solved by the development of a suitable time-step control, producing a reduction of the time-step to the necessary low level at which the calculation converges.

In addition, the model has been completely reviewed and several improvements introduced. Work has been initiated to allow for the formation and condensation of several consecutive bubbles, but this formulation has not yet been complete. In parallel to this effort, a literature review was performed as the calculations are based on assumptions of the superheat, initial film thickness, evaporation and condensation coefficients, etc. It was found that the experimental results presented in different reports differ significantly. Especially in connection with the measurement of superheat, some doubt is justified, since in general, the inertial terms in the pressure and temperature determinations are neglected. This introduces particularly high superheats, when system pressures are low. For this reason, a sensitivity study of the different parameters in the voiding model is necessary.

In conjunction with Task C-2, effort has begun to adapt the input values of the code to the geometry and test conditions of capsule C10X. In addition, the model has been applied to the case of Mockup 1A, and a pressure-history calculation has been completed. The boiling behavior of this Mockup cannot be completely simulated by the present model, since the horizontal temperature gradients in the actual three-pin geometry lead to lateral condensation effects which are not included in the existing version.

Figure 9-1. FREADM Approximation Compared with Radial Void Worth Calculation

Figure 9-2. FREAMM Void Worth Calculation

9.3 SENSITIVITY STUDIES

As a first step in a continuing study of fuel melting, slumping, and freezing during fast reactor transients, a scoping study was performed to determine the significant parameters associated with transient failure of a fuel rod and post-failure molten fuel ejection. This kind of failure will depend on factors such as fuel burnup, fission gas release, cladding strength, rate of energy input to a fuel rod during the transient, and individual pin or subassembly operating history prior to the start of the transient.

9.3.1 Results

Linear relationships have been obtained which express the amount of molten fuel required to burst the fuel rod cladding (failure threshold) as a function of fuel rod initial smeared density. Figures 9-3 through 9-6 illustrate these relationships.

Primary factors which affect the failure threshold are:

1. Fission gas production and release prior to and during the transient.
2. Cladding temperature during the transient.
3. Thermal volume of the fuel, including the volume change associated with the fuel phase changes.
4. Fuel rod cladding strength.
5. Fuel and cladding swelling.
6. Rate of energy input during the transient.

9.3.2 Failure Mechanism

The effect of the above parameters on the cladding failure threshold was evaluated via a computer model based on the assumptions illustrated in Figure 9-7. This model assumes that if sufficient energy is generated in the fuel rod during a short period of time, the central region of the fuel begins to melt and expand due to the phase change volume expansion of approximately 9.6%⁽⁴⁾. The outer, cooler parts of the fuel also thermally expand and are cracked. As the molten portion of the fuel increases in temperature and spreads radially towards the cladding, fission gas bubbles, initially entrapped in the solid fuel matrix, are released within the molten fuel. This enables the bubbles to act as a freely compressible medium that hydrostatically pressurizes the molten core of the fuel rod. The gas occupies a diminishing volume owing to the significant increase in the fuel volume during melting, plus additional thermal expansion above the melting temperature. The decrease in gas volume causes a rise in pressure which is transmitted through the outer solid fuel to the cladding. Inherent in this model is the assumption that the fission gas released during melting does not have time to reach the plenum during the transient.

9.3.3 Steady-State Parameters

The following representative values were used to determine initial steady-state conditions before the start of a transient:

Steady-state operating power (at rod mid plane)	= $\sim 14 \text{ kW/rod}$
Coolant inlet temperature	= 735°F
Coolant outlet temperature	= variable
Coolant heat transfer coefficient	= $20,000 \text{ Btu/ft}^2 \text{ hr } ^\circ\text{F}$
Fuel to cladding gap heat transfer coefficient	= $1500 \text{ Btu/ft}^2 \text{ hr } ^\circ\text{F}$
Coolant velocity	Variable
Active fuel length	= 30 inches
Fuel to cladding gap (diametral)	= 0.005 inches
Fuel rod plenum length	= 30 inches
Cladding material	316 SS

9.3.4 Details of Failure Models

- a. Cladding Stress Analysis. In this analysis fuel rod cladding failure is defined as a cladding breach caused by pressure from the fission gas released in the molten fuel region and strain from the expanding fuel. The stress analysis of the cladding was performed using a simplified quasi-static model, implying no rapid loading of the cladding relative to the time scale of the transient. However, the loading is of short enough duration that effects of creep are neglected. A cladding swelling rate proportional to the fuel burnup was included prior to the start of the overpower transient; at 100,000 MWd/T the cladding is assumed to have undergone 6% swelling by volume.

A rigorous solution of the cladding stress for every time step involved in the computational procedure increases the amount of computation far in excess of that which is warranted, considering the large number of assumptions made for other variables of the model. Thus, to avoid complicating the mathematical model, an idealized stress strain curve (see Figure 9-8) was used, with an arbitrarily specified strain at which cladding burst will occur. Since the strain at which a burst will occur will presumably vary with burnup, a burst strain of 1% was assumed at high fluences while at zero burnup, the ultimate value was chosen to be 4%. The failure strain at intermediate burnups were assumed to vary linearly with burnup. The elastic limit point in the idealized stress-strain curve is determined by Von Mises octahedral stress criteria.

- b. Thermal Expansion of Cladding and Fuel. The cladding thermal expansion was calculated from density change with temperature for 316 stainless steel, and is assumed to be isotropic. Since the fuel and cladding are in contact for most of the transient, it was assumed that the fuel expands axially at the same rate as the cladding.
- c. Fission Gas Release and Inventory. The significance of fission gas release during power transients has been previously identified as a result of TREAT experiments⁽⁵⁾. For the purpose of this analysis, fission gas release was subdivided into two parts; that which is

Figure 9-3. Fuel Failure Model Results

Figure 9-4. Fuel Failure Model Results

Figure 9-5. Fuel Failure Model Results

Figure 9-6. Fuel Failure Model Results

Figure 9-7. Assumed Failure Model

Figure 9-8. Idealized Stress-Strain Assumption

released during steady state operation and that which is released during the transient.

The fission gas that is released during a transient is assumed to be the quantity of gas that is contained within the melted fuel region. It is this quantity of gas, together with the fission gas accumulated in the central void region during steady state release, which supplies the pressure to cause the fuel rod cladding to fail. Thus, 100% release usually assumed in plenum design calculations, is no longer conservative, but in fact optimistic with respect to this analysis. A different fission gas release model was therefore adopted.

In Reference (6) it was concluded that fission gas release from a mixed oxide fuel pellet being irradiated in a fast flux could best be described by a three regional model. Different release rates were estimated for three regions of the fuel, corresponding to the columnar grain growth region, equiaxed grain growth region, and as fabricated region. The release fraction for the columnar grain growth region was estimated to be from 95% to 100%. In the two outer regions, the fission gas release fraction is harder to determine, since it is a function of the reactor operating history. For this study, however, the release rate from the "as fabricated" region of the fuel was of no importance, since failure of the transient cladding occurs before

the outer boundary of the melted fuel region reaches the outer boundary of the equiaxed grain growth region.

Experimental evidence shows that the transition from columnar grains to equiaxed grains is gradual, and a function of temperature. Thus, in order to make an estimate of the fission gas release in the equiaxed grain growth region of the fuel, and at the same time keep the mathematical computation simple, a linear decrease in this rate is assumed.

The central void, plus columnar and equiaxed grain boundary radii, were determined using the following correlation:

$$R_E = 0.05 G^{0.0654} 10^{0.025P}$$

$$R_C = 1.643 R_E - 0.5 \left[0.152 + \frac{1}{(2 R_E)^9 \cdot 10^{9.65}} \right]$$

$$R_V = R_C \sqrt{(1 - D) / (1 + \frac{1}{2} D)}$$

where D is the fuel pellet relative density; fraction of theoretical

G is the as manufactured fuel to clad diametral gap, inches

P is the fuel pellet linear heat generation rate, kW/ft

R_E is the equiaxed grain growth boundary radius, inches

R_C is the columnar grain boundary radius, inches

R_V is the central void boundary radius, inches

In order to account for all the available volume that the fission gas occupies, a porosity distribution in the fuel is assumed. The model is deduced by similar reasoning to the fission gas release model. A porosity of 2% is assumed for the columnar grain growth region of the fuel, and a linear increase through the equiaxed grain growth region to the "as fabricated" region. In the "as fabricated" region the porosity is simply (1-D) where D is fraction of fuel pellet theoretical density.

- d. Axial Position of Cladding Failure Point. The model assumes that failure of the cladding will occur at the mid-plane of the active fuel length. This assumption is made primarily to simplify the computational mathematics of the program, but is probably conservative due to the potential reactivity effects of flowing molten fuel.

In an actual case, the lowest yield point of the cladding, for a given mean temperature, may be at the mid-plane, since this is the point of maximum neutron dosage and the point of maximum fuel and cladding interaction. However, the cladding temperature will be greatest at the top of the fuel column, so that the point of lowest yield strength could vary with the coolant temperature difference. Following this line of reasoning, the failure point would approach the fuel rod mid-plane as the core ΔT decreases. In consideration of these factors, it would appear most likely that the fuel rod will fail near its mid-plane. It should be recognized, however, that due to variations in pellet and cladding dimensions, and the possibility of small flaws beyond the quality control detection standards, the actual weakest point in the cladding could be randomly located anywhere along the length of the fuel rod.

- e. Heat Transfer Calculations. Heat transfer calculations were performed using the THTD heat transfer program. With this program the heat transfer characteristics of a single pin were simulated by nodalizing a unit flow cell of a typical fast reactor fuel rod bundle. Five axial nodes were chosen and at each axial location there were twenty fuel nodes of equal radial spacing, plus five cladding and two coolant nodes. It was recognized that different types of overpower transients result in different transient energy insertion

rates, and various fuel rod lattice configurations could lead to variations in energy retention characteristics for the same type of accident. Thus, to keep the analysis as general as possible, and to simplify the heat transfer calculations, simple triangular pulses initiated from steady state power levels were assumed. Pulse half widths of 1/8, 1, 2-1/2 seconds were used. Figure 9-9 illustrates the type of power pulse used in the analysis.

9.3.5 Conclusions

The single most significant parameter which affects the amount of melted fuel volume required to burst the cladding was found to be fuel burnup. Burnup affects four parameters required in the analysis: fission gas production, fuel swelling, cladding swelling, and cladding failure strain. Of these four, the two with the greatest effect on failure are fission gas production and cladding failure strain.

As burnup increases, the total fission gas inventory increases, and therefore, the fission gas remaining in the fuel after "steady-state" release increases. Consequently, the volume of fission gas released in the melted fuel during an overpower transient also increases. Combined with decreasing cladding ductility due to irradiation, the increase in burnup causes a sharp decrease in the amount of fuel melting required to fail the cladding. If the steady-state fission gas release model were changed to some other less conservative model (lower fraction of retained fission gas), the dependence of the fuel melt volume at failure on burnup would decrease. However, a decrease in the fission gas remaining in the fuel implies an increase in the amount of gas in the central void. Therefore, the total fission gas available for causing failure would not decrease in complete proportion to the increase in steady-state release rate.

Inspection of Figures 9-3 through 9-6 indicates that lower fuel density requires a greater fuel volume and hence a greater energy input to the fuel in order to fail the cladding. At 100,000 MWd/Te, the fission gas inventory is large so that increasing the fuel porosity only increases the volume the fission gas can occupy by a small amount. Conversely, increasing the fuel porosity at low burnup or low fission gas inventory has the effect of increasing the required fuel melting volume to cause cladding failure.

Rate of energy input to the fuel, or rapidity of the overpower transient affects the distribution of the melted fuel volume required to fail the cladding. At rapid rates of energy input, the coolant does not have sufficient time to remove energy from the fuel. This results in a large volume of melted fuel at low average temperature. A low rate of energy input results in a significant quantity of the transient energy being removed from the fuel, which results in a smaller volume of molten fuel at a higher central

Figure 9-9. Assumed Transient Power Input

temperature. Thus, at rapid rates of energy input the amount of melted fuel volume required to fail the fuel rod is greater than at slow rates because the higher average temperature of the smaller melt volume causes a fission gas pressure equal to the pressure in the larger melt volume at a lower temperature.

Increasing the cladding temperature has two effects, the major one being a reduction in cladding strength of the order of 50% between the temperatures of 1100°F to 1500 F. A minor effect is the slight increase in fuel rod volume with increase in cladding temperature, which allows for a small increase in the fuel melt volume at failure.

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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

The objective of this subtask is to provide and follow capsules used as irradiation vehicles for irradiating clad alloy specimens in EBR-II, and then to characterize the irradiation history of the capsules and prepare specimens for evaluation of mechanical and physical properties. A summary of this effort is shown in Table 2-6.

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

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 X050, which is continuing irradiation to mid-1970. The L-2 (four capsules) have accumulated 8.5×10^{22} n/cm² total fluence.*

The L-6 capsules (Mark B-7 type) are presently being irradiated in S/A X018B. These capsules have accumulated a total fluence of 6.7×10^{22} n/cm² and will continue a scheduled irradiation to 1×10^{23} n/cm² in X018B.

The L-15 materials capsules (Mark A-19 Type), previously irradiated in S/A X039 to 0.94×10^{22} n/cm² total fluence, are waiting reinsertion into another Mark A-19 S/A for further irradiation. Target total fluence for the L-15 is 3.0×10^{22} n/cm².

10.2.2 Series L-14 EBR-II Materials Capsules

A description of the nineteen L-14 capsules has also been presented earlier.⁽¹⁾ These capsules were irradiated in the S/A X025, 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 X025 was 187 EFPD (~4500 hours). Work on the L-14 capsules examination was completed. A topical report describing the irradiation history and post-irradiation examination is in preparation. No further reporting will be made under the above heading.

10.2.3 MT-3 Materials Capsules

The MT-3 was irradiated in S/A XA08 (pin position 13) to 19,278 MWd in row 4 of EBR-II, which is equivalent to 6.9×10^{22} n/cm² total fluence. The exposure time was ~428 effective full power days at 45 MW (~10,280 hours). Evaluation of the thermal exposure for the MT-3 capsule has been completed. The melting alloy temperature sentinels have also been examined, and they confirmed the temperature profile calculations. Temperatures for the 304 stainless steel strip and the tensile specimens are shown in Figure 10-1 for the 45 MW power level. A peak power of 50 MW was maintained for 27% of the irradiation, and the corresponding temperatures are 25 to 35°F above the curve in Figure 10-1.

10.2.3.1 Method of Solution

The MT-3 capsule consisted of (from bottom to top) a bottom end plug, six sets of tensile specimens, a solid positioner, two additional sets of tensile specimens, and an upper end cap. A thin stainless steel strip (0.010 x 0.200 in.) ran the entire length of the assembly,

and the tensile specimens were wired to the strip. A narrow argon gas gap separated the control assembly from the stainless steel capsule tube.

Temperature profiles were calculated using the TIGER-V computer code which employs a two-dimensional nodal model. The gamma heating rate axial profile (at 50 MW) for the EBR-II, Row 4 location, was taken from Reference 3. In all regions, the capsule tubing adjacent to the sodium coolant was assumed to be at 700°F - the core inlet temperature. The maximum coolant temperature rise across the core was estimated to be 20 to 30°F and was neglected. Both conduction and radiation occurred across the gas gap between the central assembly and the capsule tube. For the radiative transfer, all surfaces were assumed to have an emissivity of 0.5, and an effective emissivity at each axial plane was calculated from the standard formula,

$$\epsilon_{\text{eff}} = \frac{1}{\frac{1}{\epsilon_1} + \frac{A_1}{A_0} \left(\frac{1}{\epsilon_0} - 1 \right)}$$

10.2.3.2 Axial Temperature Distribution Along Tensile Specimen

Each tensile specimen has the shape of a capital I, with wide sections (0.200 in.) at each end and a narrow section (0.100 in.) in the middle. The 10-mil thick connecting strip is uniformly 0.200 inch wide and, therefore, extends as a fin on both sides of the central section of the specimen. The resulting difference in the surface area-to-volume ratio between the ends and the center produces a significant thermal gradient along each specimen. For example, at 50 MWt, the maximum temperature difference along a specimen is 110°F (1324 - 1214).

The axial temperature gradient along each tensile as noted in Figure 10-1 occurred as a result of the small cross sectional areas. The small cross-sectional areas for axial heat transfer along the specimen caused almost all (>90%) of the heat flow to be radial. Because of the connector strip the surface area for radial heat loss along the specimen is essentially constant while a significant difference in volume and internal heat generation rate exists between the ends and the center. With only a relatively small amount of axial heat transfer possible, the ends run considerably hotter than the center.

10.2.3.3 Estimate of Thermal Uncertainty

The largest uncertainty in the thermal calculations is that due to the local heating rate. This includes overall reactor power level, local rod variations due to adjacent assemblies, and the axial profile shape. It is estimated to be ±20%. The heating rate uncertainty dominates the thermal calculations to such an extent that all other uncertainties can be neglected. Since going from 45 to 50 MW is a 10% increase in power, thermal uncertainties can be estimated

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

Figure 10-1. Temperature Profile for MT-3 Tensiles and Holding Strip

by doubling the temperature difference in each node between these power levels. Therefore, the maximum temperature uncertainty for the tensile specimens is approximately $\pm 90^\circ\text{F}$ (at the core midplane). This temperature distribution is being used as a parameter to choose samples for examination by transmission electron microscopy. The TEM exam is progressing with several samples from the 304 stainless steel strip presently under examination.

10.2.4 Series L-16 Materials Capsules

The new group of materials capsules designated L-16 series 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.

Three types of capsules are required. The different designs allow:

1. Verification that all capsules will operate in EBR-II at the design temperature, Design L-16A.
2. Exposing constant fluence specimens to constant temperature of irradiation, Design L-16B.
3. Exposing constant fluence specimens to varying axial temperature of irradiation, Design L-16C.

The design of the first 19 constant temperature capsules L-16B has been completed and fabrication of these capsules is in progress. A request for approval-in-principle has been approved by RDT. The design of the L-16A has been initiated.

10.3 CLAD ALLOY SWELLING

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

10.3.1 Capsule L-21

Immersion density measurements, Table 10-1, have been completed on test sections 2 and 3 of the L-21 materials capsule. These specimens were made of 0.250 inch diameter Type 321 stainless steel tubing cut to one-inch lengths of the tubular sections. L-21 was irradiated in Rows 3 (S/A X039) and 4 (S/A XG05) of EBR-II to a peak total fluence of 5.7×10^{22} n/cm². Figures 10-2 and 10-3 summarize the irradiation conditions and performance of the two sections. As can be seen, both test sections were subject to high temperature gradients over their length and high degrees of strain due to internal pressure. In both sections, the strain is apparently temperature dependent because it is almost zero in the region of the temperature sentinel which ran cooler than the rest of the section. The degree of swelling determined by density measurement is expressed as per cent volume change.

In Section 3, the swelling appears to follow the fluence profile. This is not the case in Section 2 because the bottom specimen from the section showed less volume increase than would be expected. This anomaly suggests that there was some error during the density determination. To resolve this question, the sample will be reweighed.

Comparison was made between the measured change in volume and that which would be predicted by an empirical equation for swelling of Type 304 and Type 316,⁽⁴⁾ (Figure 10-4). As can be seen, the amount of swelling predicted by the equation is considerably higher than what is shown by the density measurement. It should be noted, however, that the general shapes of the curves are similar with the exception of the afore-mentioned anomalous datum point in Section 2. One possible explanation for the difference between the calculated and measured values may lie in the fact that the empirical equation was derived using only data from Types 316 and 304 stainless steel, suggesting that Type 321 experiences less swelling in fast flux than Types 304 and 316. This could occur because the

Table 10-1
RADIATION-INDUCED DENSITY CHANGE IN TYPE 321 AS DETERMINED
BY IMMERSION DENSITY MEASUREMENT

Sample	Temperature °C	Fluence $\times 10^{-22}$ ($E > 0.1$ MeV)	$\frac{\Delta\rho}{\rho}$	% $\frac{\Delta V}{V_{exp}}$
L-213A	543	3.44	-0.60	0.60
L-213C	630	3.84	-1.33	1.35
L-213E	564	4.04	-1.67	1.70
L-212A	609	3.98	-1.14	1.15
L-212C	638	3.67	-1.56	1.58
L-212E	538	3.14	-0.69	0.69

Figure 10-2. L-2I Section 2, 321 Stainless Steel

Figure 10-3. L-2I Section 3, 321 Stainless Steel

Figure 10-4. L-2I Volume Change

smaller grain size (ASTM-10) and large number of precipitates in Type 321 provide more void sinks than Types 304 (ASTM-7) and 316 (ASTM-6). This is, of course, a preliminary observation and further evaluation and data will be necessary to draw any definite conclusions.

Further comparison is provided in Figure 10-3 by including predicted swelling from an empirical equation derived by another experimenter.⁽⁵⁾ Although the magnitude of swelling calculated using this equation is closer to the measured values for L-21, the axial distribution of swelling does not conform to the shape exhibited by the experimental data. This occurs because the swelling predicted for the higher temperature specimens (in the center of the test section) is less than for the lower temperature specimens. The data indicate that either the reverse situation is true or that, in this range, temperature is not a significant variable.

10.3.2 Alpha Cleaning Fuel Clad

A process for removing fuel from irradiated fuel pins has been successfully developed. The ability to clean alpha-contaminated fuel cladding has provided the opportunity to test Task F material that was previously unavailable. Alpha cleaning of a first group of irradiated fuel pins (F2 series fuel pins (75,000 MWd/T burnup) is progressing. The bulk fuel removal from 40 sections of clad has been completed. Reaming, electropolishing, and dimensional analysis of fuel pin specimens are scheduled in the near future.

10.4 CLAD ALLOY MECHANICAL PROPERTIES

The objective of this subtask is to evaluate the post-irradiation mechanical properties of available clad alloy specimens, including pre-cut tensile specimens and tubular specimens from Task L and Task F fuel clad and capsule tubing. Various uniaxial and biaxial techniques are being used to perform the required mechanical evaluation.

10.4.1 Biaxial Creep Program

The biaxial creep facility described earlier⁽⁶⁾ was designed and fabricated to provide facilities to creep test irradiated tubing materials at elevated temperatures to gain creep rate data on highly-irradiated tubular specimens. The initial control specimens have accumulated 1516 hours under test to demonstrate handling procedures and gain additional control data for comparison with the first lot of irradiated specimens. Tests have also begun on the first lot of irradiated specimens. These specimens have accumulated 500 hours and are continuing.

10.4.2 Uniaxial Creep Program

The uniaxial creep facility was designed and fabricated at GE's expense to provide RHO with facilities to creep test irradiated flat sheet-type specimens to provide

creep rate data on highly-irradiated tensile specimens. Check out of this equipment has been completed. The initial program utilizing this equipment will investigate creep of Type 316 stainless steel to see if a cumulative strain damage theory⁽⁶⁾ applies to austenitic stainless steels during high temperature creep.

10.5 MATERIALS MANAGEMENT

10.5.1 The Influence of Cold Work on The Stress-Rupture Properties of Austenitic Stainless Steel Tubing

A comprehensive experimental program is currently underway to establish the as-received stress-rupture behavior of cold-worked austenitic stainless steel tubing. Specimens of ten lots of material, representing annealed and 10, 20, 30, and 50 per cent cold-worked tubing of both 304H and 316H, are being tested at 1300°F with pressures that yield hoop stresses ranging from 5000 to 15,000 psi. All tests are being performed in assemblies that provide a constant stress by dynamic gas pressurization. The tubular specimens are 0.250-inch in outer diameter with a 0.015-inch wall. Certified chemical compositions of the two material heats are shown in Table 10-2. The degree of cold work represents the per cent reduction in cross-sectional area occurring on the final cold draw.

Table 10-2
VENDOR-CERTIFIED CHEMICAL COMPOSITION
OF THE 5H090G MATERIAL
(LADLE ANALYSIS)

	304H Ht. No. 04200	316H Ht. No. M2783
C	0.05 (1)	0.05
Mn	1.70	1.73
P	0.024	0.018
S	0.026	0.017
Si	0.39	0.51
Ni	9.61	13.28
Cr	18.47	16.75
Mo	— (2)	2.50
Cu	0.20	0.09
Co	0.14	0.073
B	0.001	0.0005
N	— (3)	0.043
Fe	Bal.	Bal.

(1) All compositions expressed in weight per cent.

(2) Analysis not reported by vendor. Subsequent analytical work showed a molybdenum content of 0.24 per cent.

(3) Analysis not reported by vendor. Subsequent analytical work yielded an average nitrogen content of 0.041 per cent.

Results to date on the four lots of cold-worked Type 304H show a reduction in time-to-rupture with increasing levels of cold work. Preliminary information on annealed tubing of the same heat of material indicates a rupture strength somewhat less than that of the 20 per cent cold worked tubing and greater than the 30 per cent cold-worked material. The experimental data collected on the Type 304H material to date is depicted graphically in Figure 10-5. Similar information on Type 316H tubing is too sketchy at this time to predict meaningful trends in rupture life. The paucity of data on the Type 316H material, the testing of which was concurrent with that of the Type 304H tubing, does illustrate the expectedly higher stress-rupture strength exhibited by this alloy. Data collected on the two materials at identical stresses show as much as a five-fold increase in the rupture life of 316H.

Examination of the specimens after testing revealed that the failure region in the majority of the specimens was not discernable with the unaided eye. Under low power magnification, the failure zones were observed to be

characterized in most cases by a family of parallel fractures propagating longitudinally. This is shown in Figure 10-6.

A comparison of the as-received dimensional measurements with those performed after failure revealed an initial decrease in diametral strain to fracture with increasing cold work (up to 20 per cent cold work) followed by an increase in fracture strain for the 30 per cent and 50 per cent cold worked material. These trends are illustrated in Figure 10-7. Why the more severely cold worked tubing should exhibit ductilities comparable to that of annealed material has not been clearly established. However, this behavior suggests that the structural modifications of the two highest cold-worked materials are highly unstable under the conditions of this test and that partial to complete recovery has occurred. Structural examination of selected specimens is being performed to clarify this behavior.

This test program is continuing, with long-range plans to establish stress-rupture properties at a range of temperatures, and also to assess the effect of surface defects on the long-term integrity of cold-worked tubing.

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Figure 10-5. Stress-Rupture Behavior of Annealed and Cold Worked Tubing of Type 304H Stainless Steel Test Temperature 1300° F

Figure 10-6. Typical Stress-Rupture Failure at 1300°F. Type 316H Annealed

X10

Figure 10-7. Influence of Cold Working on the Ductility of Austenitic Stainless Steel Tubing

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. 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 COMPILATION AND DISTRIBUTION OF EVALUATED DATA

Preliminary tapes 991, 992 and 997 for Version II of the ENDF/B libraries were received from Brookhaven National Laboratory. The data on these tapes consist of updated nuclear files which resulted from a cooperative evaluation by participants of the Cross Section Evaluation Working Group. For phase I testing of ENDF/B, Version II, General Electric was assigned two separate data files for iron provided by Oak Ridge National Laboratory and Westinghouse. The phase I evaluation was carried out by calculating coarse group cross sections using the ENDRUN code and comparing the two cross section sets with each other and with other available information. The conclusions of this evaluation are summarized as follows:

1. Resonance self-shielding f-factors for both sets of iron are larger (less self-shielded) than the Bondarenko values above 100 keV, indicating that it may be necessary to use statistical resonance parameters (in the place of smooth cross section data) to adequately represent this material above this energy.
2. A comparison of the Oak Ridge and Westinghouse data indicates few major differences in the basic data used. The capture cross sections for iron as given by Oak Ridge in the energy range from 40 keV to 100 keV are about 2.5-3.0 times larger than the Westinghouse values. The reason for this difference could not be ascertained, although there is a large variation in measured data. However the capture cross sections in this energy range are small and this difference should have only a small effect on reactor design calculations.
3. The number of data points which are used to represent the Oak Ridge capture cross section should be reduced where the capture cross section is only a slowly varying function of energy.

4. The Oak Ridge data file does not contain a listing of the average logarithmic energy loss per collision, ξ or the average scattering angle, $\bar{\mu}$. In addition, small errors in the Oak Ridge ENDF/B format have been detected. It was therefore recommended to the CSEWG that the Westinghouse data file for iron be used for phase II testing.

The version II ENDF/B data are presently being used in the ENDRUN-TDOWN system to generate cross sections for computations of selected and assigned benchmark problems, as part of the activities of the Data Testing Subcommittee of the Cross Section Evaluation Working Group.⁽¹⁾ The benchmarks include the following.

1. Vera 11-A - A critical assembly fueled with plutonium and diluted with graphite
2. ZPR-3 Assembly 48 - A Pu-U-C-Na system simulating the spectrum of large power reactors
3. ZEBRA Core 3 - A 9:1 uranium/plutonium metal assembly
4. ZPR-3 Assembly 11 - A 7:1 Uranium-238/Uranium-235 plus graphite system
5. ZPR-3 Assembly 12 - A 4:1 Uranium-238/Uranium-235 plus graphite system

Twenty-nine group cross sections have been generated from this data for all the non-fuel materials. Generation of 29-group values for the fuel materials will require much more computer time than for the non fuel materials, so these problems have been deferred until modifications due to phase I testing have been made. This cooperative evaluation of benchmark problems will provide an excellent test of the effectiveness of the ENDRUN-TDOWN system for generating cross section data.

A re-evaluation of the Pu-239 neutron cross section data in the unresolved resonance region was completed and a revised set of resonance parameters sent to the National Neutron Cross Section Center for use in the ENDF/B nuclear data file, Version II. The new set of unresolved resonance parameters was adjusted to account for more recent data and a revised method of calculating the penetration factor, as recommended by the Cross Section Evaluation Working Group in September, 1969. With this new method, the penetration factor is dependent upon AWRI, the ratio of the mass of the isotope to neutron mass, rather than the input value of the scattering radius. The effect of this change is to decrease the Pu-239 p-wave contributions slightly. In the revised evaluation the s-wave neutron widths and the 1^+ fission widths were adjusted such that the sums of the fission and capture cross sections from all the reaction sequences give the same values as used previously above 1 keV. Below 1 keV modifications were made to the evaluated alpha values at 525, 580 and 870 eV

to account for small changes in the measured data of Gwin(2) and Czirr(3).

The parameters that were adjusted are the energy dependent values of

$$\left\langle \Gamma_f \right\rangle_{J=0}^{\ell=0}, \left\langle \Gamma_n^o \right\rangle_{J=0}^{\ell=0}, \text{ and } \left\langle \Gamma_n^o \right\rangle_{J=0}^{\ell=0}$$

The revised values for these parameters are listed in Table 11-1. Resulting values for alpha and the fission cross sections are shown in Figures 11-1 and 11-2, along with the most recent experimental data. It is seen from these figures

that the revised analysis still results in good predictions of both the fission cross sections and the alpha values over the unresolved resonance range. The agreement is acceptable in both the magnitude and the shape of the curves, without the use of any smooth background. Since many of the fission and alpha measurements are independent of each other it is noteworthy that both can be adequately described with one set of parameters.

A topical report describing this analysis of the Pu-239 unresolved resonance parameters has been written and is in reproduction(8).

Table 11-1
ENERGY DEPENDENT UNRESOLVED RESONANCE PARAMETERS USED IN ANALYSIS
OF Pu-239 FOR ENDF/B, VERSION II

$\frac{E}{\text{eV}}$	$\left\langle \Gamma_f \right\rangle_{J=1}^{\ell=0} \text{ (eV)}$	$\left\langle \Gamma_n^o \right\rangle_{J=0}^{\ell=0} \text{ (eV)}$	$\left\langle \Gamma_n^o \right\rangle_{J=1}^{\ell=0} \text{ (eV)}$
300	0.0191	1.137×10^{-3}	0.404×10^{-3}
310	0.0081	1.096×10^{-3}	0.389×10^{-3}
340	0.0084	0.936×10^{-3}	0.333×10^{-3}
365	0.0667	0.845×10^{-3}	0.300×10^{-3}
475	0.3253	0.581×10^{-3}	0.207×10^{-3}
525	0.0938	1.652×10^{-3}	0.587×10^{-3}
580	0.0805	1.651×10^{-3}	0.587×10^{-3}
610	0.0063	0.716×10^{-3}	0.255×10^{-3}
665	0.0063	0.711×10^{-3}	0.253×10^{-3}
725	0.0290	0.730×10^{-3}	0.259×10^{-3}
825	0.0440	0.734×10^{-3}	0.261×10^{-3}
870	0.0398	0.726×10^{-3}	0.258×10^{-3}
930	0.0610	1.223×10^{-3}	0.435×10^{-3}
975	0.0660	1.172×10^{-3}	0.417×10^{-3}
1050	0.0805	0.968×10^{-3}	0.344×10^{-3}
1075	0.0473	0.802×10^{-3}	0.285×10^{-3}
1225	0.0444	0.833×10^{-3}	0.296×10^{-3}
1325	0.2131	0.851×10^{-3}	0.302×10^{-3}
1550	0.0160	0.885×10^{-3}	0.314×10^{-3}
2000	0.0136	0.943×10^{-3}	0.335×10^{-3}
2100	0.0001	0.947×10^{-3}	0.337×10^{-3}
2200	0.0011	0.949×10^{-3}	0.337×10^{-3}
2400	0.0236	0.927×10^{-3}	0.329×10^{-3}
2800	0.0525	0.929×10^{-3}	0.330×10^{-3}
3500	0.0223	0.944×10^{-3}	0.335×10^{-3}
6500	0.0190	0.980×10^{-3}	0.348×10^{-3}
8500	0.0581	0.961×10^{-3}	0.342×10^{-3}
10000	0.0398	0.890×10^{-3}	0.316×10^{-3}
17000	0.0766	0.869×10^{-3}	0.309×10^{-3}
25000	0.0924	0.866×10^{-3}	0.308×10^{-3}

Figure 11-1. Comparison of Pu-239 α in ENDF/B, Version II with Experimental Data

Figure 11-2. Comparison of Pu-239 σ_f in ENDF/B Version II with Experimental Data

11.3 IMPROVEMENTS OF FUEL CYCLE AND MANAGEMENT SCHEMES

A preliminary draft of the topical report describing the startup fuel management code FUMBLE has been completed. The topical report describing the two-dimensional fuel cycle code BICYCL(9) has been distributed.

Since the BICYCL code is directly linked to the two-dimensional flux-synthesis code BISYN, the combined code system is being prepared for shipment to the Argonne Code Center under the name SYN. The necessary subroutines for SYN are being compiled and the I/O routines will include both GE635 machine language versions and FORTRAN-IV versions. A report describing programming features of SYN has been written and will be sent along with the code system.

11.4 IMPROVEMENTS OF EXISTING METHODS FOR GENERATING GROUP CONSTANTS

The first version of the ENDRUN code, ENDRUN-1, has been thoroughly checked out and is being prepared for transmittal to the Argonne Code Center. Status of the checkout for the more important options and limits is as follows, where a ✓ indicates that a problem has been successfully run:

1. General	✓ - 100 coarse groups ✓ - 30 fine groups/coarse group ✓ - starting lethargy other than 0.0 - use of ENDF/B total cross section rather than sum total for self-shielding - use of ENDF/B total cross section as output, adjusting σ_{es} for consistency ✓ - running two or more independent cases
2. Data Source	✓ - ENDF/B data on punched cards ✓ - BCD tape for a single material ✓ - binary tape created by DAMMET containing several materials ✓ - NPOST compressed binary tape
3. Flux	✓ - entirely 1/E flux spectrum ✓ - 1/E spectrum combined with a fission spectrum at high energies ✓ - input flux spectrum from punched cards ✓ - intermediate output of fine group energies and fluxes
4. Smooth	- a single reaction only (without total cross section) ✓ - a single reaction, also included as total ✓ - all smooth cross section contributions

5. Unresolved

- ✓ - 2 ENDF/B reaction MT values included in the same ICT
- ✓ - self-shielded smooth contribution
- ✓ - infinitely dilute calculation only
- ✓ - self-shielding (RAPTURE) with 4 σ_0 s, 3 T's, 5 points
- ✓ - 30 energy points
- ✓ - punched card output of point-wise unresolved contribution
- ✓ - re-run from previously punched cards
- ✓ - input of energy-dependent $\left\langle \Gamma_n^0 \right\rangle$ values
 - maximum of 15 sequences (combinations of isotope, energy range and (l, J) state)
- ✓ - maximum of 6 isotopes
- ✓ - several energy ranges
- ✓ - overlap correction above energy ELAPC
- ✓ - intermediate output of coarse and fine group unresolved averages
 - unresolved only, without smooth

6. Resolved

- ✓ - 1 resonance, infinitely dilute only
- ✓ - many resonances, infinitely dilute
- ✓ - self-shielding with 4 σ_0 s, 3 T's
 - maximum of 7 resolved resonance energy ranges
 - maximum of 6 isotopes
- ✓ - intermediate output of coarse and fine group resolved resonance averages
- ✓ - maximum of 20 ultra-fine groups/fine group
- ✓ - maximum of 30 mini-groups/ultra-fine group
 - resolved only, without smooth

7. Overlap

- ✓ - resolved and unresolved resonance ranges ending in the same coarse group, but no energy overlap
- energy overlap between resolved and unresolved contributions but ending in the same coarse group
- energy overlap extending more than one coarse group

8. Non-Cross Section

- ✓ - overlap of resolved resonance contribution with smooth
- ✓ - overlap of unresolved contribution with smooth
- ✓ - Averaging of $\bar{\mu}$ and ξ directly from ENDF/B data

- ✓ - LDXI = 1, average of ξ based on theoretical value and anisotropic correction
- ✓ - calculation of $\bar{\nu}$ from polynomial representation
 - calculation of $\bar{\nu}$ by interpolation
- ✓ - secondary fission spectrum based on given ENDF/B θ temperature (fission, Maxwellian, or Watt spectra)
- ✓ - secondary fission spectrum when θ is energy-dependent (LECHI = 0)
- ✓ - secondary fission spectrum when more than one type of spectrum is used (NK > 1)
- 9. Matrices
 - ✓ - calculation of $\sigma_{d,r}$ (elastic removal)
 - ✓ - elastic matrix
 - ✓ - inelastic matrix
 - LF=3 level data
 - LF=8 Maxwellian spectrum
 - LF=9 energy-dependent Maxwellian spectrum
 - ✓ - (n, 2n) matrix
 - LF=1 Arbitrary tabulated function
 - LF=8 Maxwellian spectrum
 - LF=9 energy-dependent Maxwellian spectrum
 - LF=10 Watt spectrum
 - ✓ - combined (n, 2n) and (n, 3n) matrices
 - ✓ - all matrices combined into inelastic matrix
 - ✓ - maximum matrix size of 70x50 (49 downscatter groups)
- 10. Output
 - ✓ - standard printout
 - ✓ - FCC-IV punched cards
 - ✓ - TROUT punched cards
 - ✓ - GMUG compressed binary file tape
 - ✓ - plotting of cross sections
 - ✓ - plotting of f-factors

ENDRUN-2, the second version, is still undergoing modifications. The latest modifications were required to

- 1 Read inelastic level data for $MT \geq 5$ in File 3, and
2. Read the newly specified order of energy dependent unresolved resonance parameters.

These two changes were necessary in order to use data for some of the ENDF/B, Version II materials, and both changes have been checked out.

The problem of high costs when using the Rhombberg integration scheme for the resolved resonance integration has not been totally solved. The computer time used for the Rhombberg computation has been substantially reduced, but it still remains from 2 to 3 times more time consuming than the resonance integration scheme used in Version I. The trapezoidal approach in Version I is both fast and accurate if the input is carefully chosen, but it can give poor answers with careless input. It may be necessary to include both schemes in ENDRUN-2 if this difference in efficiency cannot be resolved.

The TDOWN-1 code, for computing effective cross sections from the generalized data files generated by ENDRUN, has been successfully run for 60 energy groups. For this group structure, elastic scattering can cause group skipping and an elastic matrix must be used. ENDRUN-1 computes such a matrix, which is corrected for flux spectrum in TDOWN-1 in the same way as σ_{er} . An additional problem with this approach is that when σ_{er} is set equal to $\xi \sigma_{es}/\Delta\mu$ it can be greater than σ_{es} for narrow groups. In such cases ENDRUN sets σ_{ers} equal to σ_{es} . However, TDOWN-1 does not recognize that this has been done and consequently applies an erroneous flux correction. This is expected to cause only a small error in fast reactor calculations, but is being corrected in TDOWN-1.

A topical report describing ENDRUN-1 has been finished and is in reproduction. The topical report for TDOWN-1 is being written.

11.5 SENSITIVITY STUDIES

A study of the effects of nuclear data uncertainties on fast reactor design has been completed and will be reported at the "Second International Conference on Nuclear Data for Reactors", Helsinki, Finland, June 15-19 (1970). This study was based upon a similar study reported in 1968⁽¹⁰⁾ using the nuclear data uncertainty ranges from that work and modifying them according to the indications of recent measurements. The greatest change in an uncertainty range was for the Pu-239 alpha, where there has been much improvement in the past few years. The range for U-238 $\sigma(n,\gamma)$ was also affected.

A typical LMFBR demonstration plant design was used for this study. Uncertainties in various reactor parameters were computed for the data uncertainties of 1968, for the present uncertainties and for uncertainty goals for 1971. These are shown in Table 11-2. Uncertainties for additional core and shielding parameters are also shown for the present situation. The improvement since 1968 is seen, while the goals for 1971 indicate that much work must be done to reduce the data uncertainties. The associated uncertainty in the fuel cost is 0.13 mil/kWh at present.

Table 11-2.
UNCERTAINTY RANGES IN PREDICTED PHYSICS PARAMETERS OF FAST BREEDER
POWER REACTORS DUE TO NUCLEAR DATA UNCERTAINTIES

Parameters	Present	Uncertainty Range	
		1968	1971 Goals
$^{239} + ^{241}\text{Pu}$ Inventory in Reactor ($\pm\%$)	8	10	3
Breeding Ratio ($\pm$)	0.10	0.13	0.05
Doppler Coefficient ($\pm\%$)	20	30	10
Total Sodium Void Reactivity ($\pm\%$)	2.0	2.3	1.3
Control Rod Reactivity Worth ($\pm\%$)	15		
Peak-to-Avg. Core Power Density ($\pm$)	5		
Total Neutron Flux at Reactor Vessel	1/3 to 3		
^{24}Na Radiation Level in Secondary Coolant Circuit	1/10 to 10		
Neutron Source Level of Recycled Pu	1/5 to 5		

11.6 IMPROVEMENT OF EXISTING TRANSPORT THEORY CODES

General distribution of the user's manual for the two-dimensional, transport/diffusion code DOT2DB⁽¹¹⁾ has been made. The code has been recompiled and checked and is ready to send to the Argonne Code Center.

The DOT2DB code has been modified to conform to common data interfaces specified by the *de facto* Com-

mittee on Computer Code Coordination. Checkout of the modified code has proceeded through the reading of common interfaces and the generation of common interfaces from the quantities computed by DOT2DB. Small difficulties in completion of the problems must be cleared up before the modified DOT2DB is ready for distribution to the other organizations represented on the *de facto* Committee.

REFERENCES

1. Davey, W.G., "Fast Reactor Benchmarks for Phase II Testing", Argonne National Laboratory memo to Members of the CSEWG, June 21, 1968.
2. R. Gwin, et al, "Measurements of the Neutron Fission and Absorption Cross Sections of Pu-239 Over the Energy Region 0.02 eV to 30 keV", Nuclear Science and Engineering, 40, 306, May (1970).
3. J. B. Czirr, J. S. Lindsey, "A Measurement of the Capture-to-Fission Ratio for Pu-239", UCRL-72080, November 1969.
4. M. G. Sowerby, provisional results as reported in Reference 7.
5. A. Lottin, L. W. Weston, G. de Saussure, J. H. Todd, "Ratio of Capture-to-Fission in Pu-239 at keV Neutron Energies," Proceeding of the International Conference on Fast Critical Experiments and Their Analysis, ANL-7320, October 1966.
6. E. R. Shunk, et al, as reported in Reference 7.
7. G. D. James and B. H. Patrick, "Evaluation of the Pu-239 Fission Cross Section in the Energy Range 1 keV to 100 keV", AERE-M-2065 (Amended), 1968.
8. J. T. Hitchcock, B. A. Hutchins, "Pu-239 Unresolved Resonance Data for ENDF/B, Version II", GEAP-13591, General Electric Co. (to be issued).
9. Vaughan, P.C., Butz, H., Greebler, P., Hutchins, B.A., Kelley, M. D., and Reynolds, A. B., "BICYCL - A Computer Code for Two-Dimensional Analysis of Equilibrium Fuel Cycles," GE-BRDO, December, 1969 (GEAP-13556).
10. Greebler, P., Hutchins, B. A., Wolfe, B., "Significance of Neutron Data to Fast Reactor Power Plant Design," Conference on Neutron Cross-Sections and Technology, Washington, D. C., March 4-7, 1968.
11. Protsik, R., Leff, E. G., "Users Manual for DOT2DB: A Two-Dimensional Multigroup Discrete Ordinates Transport/Diffusion Code with Anisotropic Scattering", GE-BRDO, September, 1969 (GEAP-13537).

12. TASK P - FUEL ELEMENT DESIGN ANALYSIS

12.1 GENERAL

The objective of Task P is the development of an integral fuel performance and 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 is being developed for use in parametric design studies of the sodium cooled breeder reactor.

12.2 EXPERIMENTAL DATA ANALYSIS

12.2.1 Fast Flux Fuel Pin Profilometer Analysis

The interpretation and correlation of profilometer traces of experimental fuel pins has previously been reported under Task P. The purpose was to derive empirical relations to predict performance, particularly those related to diameter changes. Work of this nature is being continued and will henceforth be reported as part of Task F.

12.2.2 Microsurveying

The analysis of the experimental data necessary for the understanding of local clad loading mechanisms is continuing. During this reporting period, preliminary specifications were drawn up for the procurement of a device to accurately measure the distance between the fiducial marks placed on pins for the purpose of determining axial strain distributions. The first pins subjected to this method of "microsurveying", described in subsection 12.2.2 of the Thirty-second Quarterly Progress Report, have been used in assembly F10A which will be irradiated in EBR-II. The method will also be used on material in the L-16 series under Task L.

The measurement concept employs a stylus pickup for mark location and transducers for measuring distances between indicated mark positions. Two types of pickup heads have been specified. The first is a single stylus head on a carriage traveling relative to the pin, designed to measure distance between the marks of one inch spacing to an accuracy of $\pm 0.1\%$. The second type of head uses a dual

stylus having a predetermined gage length spacing. With the traveling carriage locked in place, the head translates relative to the carriage and draws the stylus across the fiducial marks on the pin. The dual stylus device allows high magnification of recorded graphical output without using excessive chart length. This and the fact that more accurate transducers can be used to measure the short relative distance traveled makes measurement accuracies of $\pm 0.2\%$ realistic for gage lengths down to 0.1 inch.

12.3 PERFORMANCE AND FAILURE MODE STUDIES

12.3.1 Model of Swelling Caused By Differential Expansion at Startup

An analytical model of fuel-cladding differential thermal expansion at first startup of a mixed oxide fuel pin has been formulated. The model, embodied in the computer program START, was used to calculate the amount of diametral increase attributable to plastic clad deformation at startup for stainless steel clad mixed oxide fuel pins irradiated in EBR-II. This model which treats in detail the cracking, crack healing, and hot pressing of ceramic fuel during startup, yields results which are in close agreement with the experimental data.

A typical START prediction plotted in Figure 12-1 shows the mean diameter increase of the fuel column that would occur if the outside of the fuel were continually restrained by the high radial stresses characteristic of fuel-cladding mechanical interaction. This model is inappropriate if a condition of radial restraint does not exist. The curve exhibits regions corresponding to four fuel expansion modes. At very low linear power, the brittle fuel will crack under the thermal stresses resulting from the radial temperature gradient. Mode 1, the concave upward portion, reflects the expansion of the fuel before it has reached a center temperature of 1300°C . That fuel which is below this temperature is sufficiently strong and therefore will not plastically deform to eliminate the presence of finite cracks. As linear power is increased, such that the center temperature is above 1300°C , (mode 2) the curve turns concave downward and a larger and larger fraction of the thermal expansion of the fuel is absorbed by hot pressing of the porosity in the plastic central core of fuel. The driving force for this porosity decrease is provided by the restraint of the

Figure 12-1. Mean Fuel Diametral Increase Predicted by Start versus Peak Linear Power

cladding. When the central core is porous, any load imposed on the outside of the fuel is borne by a thin ring of fuel at the root of the crack. The circumferential or hoop stress in the fuel is maximum at the root of the crack and reduces exponentially with decreasing radius due to the temperature gradient and resultant gradient in material strength. Because of the thinness of the load bearing area, stresses are very high and the fuel deforms at relatively low temperatures. Analysis utilizing thermally activated creep data indicates that the temperature at the root of the crack will be maintained at about 1300°C during startup. The crack-root temperature is weakly dependent on the rate of power increase during startup and on the fuel physical properties.

Mode 3 occurs when the fuel in the plastic core has densified to virtually 100% of theoretical density and the fuel's stress distribution changes markedly. From this point on, the central portion of fuel can expand only by displacing the peripheral fuel.

Referring to Figure 12-2, the restraint imposed by the cladding is carried by a relatively large diameter core of weak fuel under a constant hydrostatic pressure P_H . The characteristic temperature at the outside of the 100% dense core is the temperature below which the fuel can resist the hydrostatic pressure without collapse of its porosity. Since the stress on the fuel is much lower than in the mode 2 configuration, the temperature necessary for fuel deformation is higher. The fact that the fuel is deforming under hydrostatic compression rather than a uniaxial load will also tend to increase the characteristic temperature.

The net effect is to reduce the effectiveness of the remaining porosity in absorbing the thermal expansion. Thus within mode 3, the curve exhibits a sharp upward slope.

Finally, in Mode 4, the volume expansion on melting of that fuel which is above the melting point contributes to the volume expansion of the plastic core. At linear powers typical of the pins used for this study, the effect of melting on diameter change is insignificant.

When operating with the fuel in modes 3 or 4, the 100% dense core of fuel at a constant hydrostatic pressure extends up and down most of the length of the fuel pin. If the cladding were of uniform strength and thickness, the cladding would be expected to deform most at the point where the radius of the 100% dense core is a maximum since the cladding stress would also be maximized at that point. Thus, the peak in the fuel pin profilometer trace would occur close to the peak flux position. However, in reality variations in the cladding wall thickness and local temperature effects on the cladding yield or flow stress are sufficient to render the cladding deformation as a function of length unpredictable. It is for this reason that START has been used to predict mean diametral increases rather than local values.

12.3.2 Comparison of START Results With Experimental Data

It is a plausible hypothesis that when a new fuel pin is placed in a reactor the first expansion of the cladding is caused by differential thermal expansion of the fuel for an initial hot gap ≤ 0 , and that further swelling caused by the irradiation of the cladding starts with the cladding in the "pre-expanded" condition. On this hypothesis the expansion from the two separate causes are directly additive.

This was tested using START and data for 18 mixed oxide pins irradiated in EBR-II. The mean diameter change for the pins was obtained from profilometry and compared to the mean diameter change predicted by START plus the amount of diameter change attributed to cladding swelling using a stainless steel swelling data correlation described in the 30th FCR Quarterly Report.

For the purpose of comparison, a single number describing the condition of each pin is preferable to a profilometer trace; hence the mean diameter, or Δ mean diameter was used, where the word "mean" implies an average taken over the active length of the pin.

This model of fuel pin performance is illustrated in Figure 12-3 in which the mean diametral increase of the cladding of fuel pin F2S is plotted as a function of full power hours in EBR-II. This pin, which had an unusually high fuel density and small cold gap is predicted by START to have undergone the majority of its diametral increase during its first approach to full power. Thenceforth, the only additional increment in diameter increase is attributed to the effects of cladding swelling alone. The START model is seen to be highly sensitive to the value of fuel-cladding contact conductance chosen. The value of 2500 Btu/hr-ft²-°F is the particular value which gives the best results for the 18 pins studied. This value of conductance is used in the comparison between profilometer results and model predictions shown in Figure 12-4. Agreement between model and experimental data is excellent for 16 of the 18 pins representing burnups of 2000 to 70,000 MWd/Te and peak linear powers from 11 to 17 kW/ft. Of the other two pins, F2E is suspected of having an error in calculated density leading to an error in the model prediction. It has recently been found that pin F0E experienced a substantial step increase in linear power midway through its lifetime, probably a significant factor in its departure from the model predictions.

The model of startup has been found to be not applicable to vipak powder fuel pins which have the apparent ability to come up to power without deforming the cladding despite the fact that they have zero cold gap.

It is significant that all of the pin deformation can be attributed to initial start-up interference and clad swelling. If there is any deformation due to steady state fuel fission product swelling, the effect is negligible at least up to a burnup of 70,000 MWd/Te, in agreement with thermal reactor tests.

Figure 12-2. Tangential Stress Distribution in Fuel and Cladding

Figure 12-3. Model of Fuel Pin Diametral Increase

Figure 12-4. Mean Pin Diameter Increase from Profilometry versus Predicted Mean Diameter-Increase at Startup Plus Calculated Cladding Swelling

Previous calculations of the thermal expansion of fuel have generally presumed the fuel to expand according to its volumetric average temperature. When contrasted to the more accurate model in START, it is found that such a volumetric average temperature model will overpredict the expansion of low density pellets and will underpredict the expansion of high density pellets.

12.3.3 Model of Thermal Differential Expansion at Shutdown

Shrinking of the cladding over the fuel during shutdown is a possible source of permanent clad strain for pins irradiated in EBR-II. The primary tank temperature is dropped from its normal 700°F to a lower value for maintenance purposes. An even larger drop in pin temperature occurs when the pin is removed from the reactor. Since the thermal expansion coefficient of the fuel is less than that of the cladding, it is possible that the cladding would shrink down over the fuel and be plastically deformed. Whether this phenomenon actually occurs, however, depends on the fuel-cladding gap existing before the tank temperature is dropped. If the gap is large enough, the effect will not occur. What is needed then is a model of the net fuel-clad interference when the fuel pin is dropped from full to zero power and the tank temperature is subsequently dropped.

The model chosen to represent the behavior of the fuel contains the following assumptions:

1. The fuel at full power is stress free and uncracked.
2. The fuel has zero tensile strength but high compressive strength and cracks where the stress would otherwise go into tension.
3. The fuel is elastic.
4. There is no fuel-clad gap at full power.

Given these assumptions, it can be shown that a circumferential crack will form in the fuel during a power reduction at the radius where the radial compressive stress goes to zero. The position of the circumferential crack is important because the ring of fuel outside the crack will contract inward under a power reduction at a rate determined by its mean temperature. Thus a thick ring will contract more than a thin ring under equivalent power reductions.

A stress analysis was performed based on the foregoing assumptions. The amount of fuel-cladding interference was calculated as a function of linear power and inlet temperature. It was noted that low linear power pins should be most vulnerable to this effect so pin F0E was chosen for a simulation. At 6 kW/ft peak, F0E is the lowest linear power pin tested by GE in EBR-II. The solution indicated a tendency for the fuel to pull away from the cladding as power is reduced. Only when the tank temperature has been reduced substantially is fuel-cladding interaction predicted. The EBR-II tank temperature was never dropped

below 280°F. At this temperature, the maximum predicted strain was less than 0.1%; less than the plastic yield strain. In the case in which the pin was removed from the reactor, a maximum strain of 0.2% was predicted at the top of the pin, about half a mil of diameter change.

12.4 INTEGRAL PERFORMANCE AND FAILURE MODELING

The outline of a method of predicting clad deformation and failure was presented in the 30th Quarterly Progress Report. A report on this subject has been completed and is in review. The superposition of various individual clad loading mechanisms was divided into two parts for the purpose of computerization. One part now called BEHAVE is being constructed to predict *nominal* pin behavior. The other part, STATUS, will calculate cladding local strain levels and the remaining ductile capability of the clad.

12.4.1 BEHAVE: Fuel Element Performance Code

This is a new effort in response to the need for a comprehensive computer code to simulate the nominal performance of a fuel element throughout its operating history, including power changes and transients.

The stress-strain distribution throughout the fuel element during any period of operation is largely unknown since it generally cannot be sensed directly. Consequently, it has been necessary to infer the general stress pattern from post irradiation examination. In the process of analysing and modeling the fuel element behavior, it is customary to make a number of restrictive assumptions in order to yield a tractable analytical problem. For example, in even some of the more sophisticated fuel element models it has been assumed that an axisymmetric condition of plane strain exists. This is equivalent to neglecting the effects of fuel cracks and pellet-to-pellet interfaces, and also neglecting the possibility for axial fuel movement from the central hot plastic regions of the fuel toward the cooler and more rigid ends. For an equilibrated steady state condition of fuel-clad interaction, this assumption may be sufficiently accurate. However, for time varying stress-strain patterns such as during startup or power changes, considerable error may be introduced. For example, in the START model previously described, such unsymmetric behavior becomes all important.

The computer code BEHAVE is basically a flexible executive-type routine which relates subroutines describing fuel and clad swelling, thermal expansion, fuel restructuring, clad ductility, corrosion, etc. with reactor operating conditions.

The modeling approach is: (1) to assume as much separability of the effects as can be justified, and (2) to group the reactor power history into defined types, or regimes, of operation. Within each regime, rigor is focused

1. START UP WITH FRESH FUEL
2. INITIAL FUEL RESTRUCTURING
3. RESTART AFTER SHUTDOWN
4. STEADY STATE OPERATION
5. POWER INCREASE
6. OPERATION AFTER A POWER INCREASE
7. REGULAR POWER REDUCTION
8. OPERATION AFTER A POWER REDUCTION
9. FAST POWER REDUCTION (SCRAM)
10. REMOVAL FROM REACTOR
11. POWER CYCLING

Figure 12-5. Computer Code BEHAVE Fuel Element Performance Simulation

only on those effects that substantially affect performance. Eleven significant types of operation have been identified (Figure 12-5). As each regime of operation is analysed, subroutines will be written and added to BEHAVE.

12.4.2 STATUS: Local Deformation and Life Code

The purpose of STATUS is:

1. To calculate local anomalies in clad strain as a function of reactor operation.
2. To determine residual pin life as a function of operation by comparing local maximum strain levels with a failure criterion.

Present emphasis is on the first item. Using the nominal performance of the pin obtained from the BEHAVE code as a base, the perturbations caused by local clad loading mechanisms will be calculated and superimposed to produce detailed strain distributions as a function of time. Mechanisms presently considered are:

1. Anomalies in heat transfer by the coolant
 - a. coolant restrictions (hot spots)
 - b. circumferential variation because of adjacent pins
 - c. axial variation . . . (bowing)

2. Anomalies in heat transfer through the clad wall:
 - a. wall thickness variation
 - b. local pits and die scratches
 - c. effect of wire wrap
3. Anomalies in heat generation
 - a. pellet ridging
 - b. missing chips of fuel
 - c. spacer
 - d. discontinuity in adjacent pellet diameters
 - e. pellet cracks

The output from BEHAVE necessary as input to STATUS will be presented as nominal or average values of diameter, mechanical strain, temperature, linear power and fluence as a function of incremental axial positions at specific times. STATUS will modify the temperature and fluence nominal values to account for radial and circumferential variations. Values will be established for nodes around the circumference and at each of these positions on the inner and outer surfaces. These nodal positions are shown in the sketch of Figure 12-6.

The strains due to differential thermal expansion and clad swelling will be determined at each node and added to the nominal strain values supplied by BEHAVE. Then the

local strain distributions associated with the other loading mechanisms, such as those shown in Figure 12-7 for spacer contact and pellet cracks will be added. These distributions will not be considered in great detail for initial calculations. Simplifying assumptions will be made that produce symmetric and sinusoidal distributions that can be characterized by peak amplitudes as indicated by the small circles on the distribution examples shown in Figures 12-6 and 12-7. Some strain distributions require only one value to define the character of the strain under this simplified approach, e.g., that shown for cracks. The strain due to the spacer on the other hand needs values at two positions to characterize the distribution. As shown in Figure 12-6, tension occurs on the outside at position 1 and inside at position 2. It becomes obvious that superimposing a crack at position 1 produces a different state of strain than if superimposed at position 2. For those mechanisms requiring two positions for characterization, both values will be considered individually at each calculational node.

Thermal loading has been reduced to the usual three components, as shown in Figure 12-6. The circumferential temperature distribution caused by the adjacent six fuel pins has been assumed sinusoidal, as has the diametral distribution due to bowing.

Initially the treatment of the strain due to differential clad swelling will be similar to that developed for differen-

tial clad thermal expansion. That is, the strain produced by radial variation will be superimposed on that produced by circumferential variation, etc. The error introduced by this approximation will be reviewed.

The code is being constructed with the strain from each mechanism calculated in a separate subroutine. This will facilitate modification and upgrading as more accurate models and calculation procedures are employed. The subroutines for temperature distributions and thermal strain calculations have been completed.

Individually and in all possible combinations, increments of strain from these loading mechanisms will be considered superimposed on the previously established strain matrix. In cases such as the spacer, this superposition process will include using both extreme conditions, since the probability of either region occurring in a specific relation to another local load mechanism is about equal.

In the future the strains incurred at each node will be analyzed for the remaining lifetime, and the failure potential considered from a probabilistic point of view. A review of the cumulative strain damage criterion, as outlined in the FCR 30th Quarterly Report, has already been initiated to determine the range of validity. The result of such analyses is not only the ability to predict failure but to identify conditions or events that limit life, and hence to direct attention to the most important items.

Figure 12-6. Assumed Strain Distributions from Thermal Loading Mechanisms

a. SPACER CONTACT LOADING

NOTE: $\circ$ SIGNIFIES CHARACTERISTIC VALUES

B. FUEL CRACK

Figure 12-7. Examples of Assumed Strain Distribution from Local Loading Mechanisms

APPENDIX A 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
Fifth Quarterly Report
October—December 1962.

GEAP-4214
Sixth Quarterly Report
January—March 1963.

GEAP-4300
Seventh Quarterly Report
April—June 1963.

GEAP-4382
Eighth Quarterly Report
July—September 1963.

GEAP-4480
Ninth Quarterly Report
October—December 1963.

GEAP-4601
Tenth Quarterly Report
January—March 1964.

GEAP-4640
Eleventh Quarterly Report
April—June 1964.

GEAP-4723
Twelfth Quarterly Report
July—September 1964.

GEAP-4795
Thirteenth Quarterly Report
October 1964—January 1965.

GEAP-4860
Fourteenth Quarterly Report
February—April 1965.

GEAP-4931
Fifteenth Quarterly Report
May—July 1965.

GEAP-4982
Sixteenth Quarterly Report
August—October 1965.

GEAP-5098
Seventeenth Quarterly Report
November 1965—January 1966.

GEAP-5158
Eighteenth Quarterly Report
February—April 1966.

GEAP-5198
Nineteenth Quarterly Report
May—July 1966.

GEAP-5292
Twentieth Quarterly Report
August—October 1966.

GEAP-5433

Twenty-First Quarterly Report
November 1966—January 1967.

GEAP-5491

Twenty-Second Quarterly Report
February—April 1967.

GEAP-5522

Twenty-Third Quarterly Report
May—July 1967.

GEAP-5541

Twenty-Fourth Quarterly Report
August—October 1967.

GEAP-5584

Twenty-Fifth Quarterly Report
November 1967—January 1968.

GEAP-5631

Twenty-Sixth Quarterly Report
February—April 1968.

GEAP-5677

Twenty-Seventh Quarterly Report
May—July 1968.

GEAP-5700

Twenty-Eighth Quarterly Report
August—October 1968.

GEAP-5753

Twenty-Ninth Quarterly Report
November 1968—January 1969.

GEAP-10028

Thirtieth Quarterly Report
February—April 1969.

GEAP-10028-31

Thirty-First Quarterly Report
May—July 1969.

GEAP-10028-32

Thirty-Second Quarterly Report
August—October 1969.

GEAP-10028-33

Thirty-Third Quarterly Report
November 1969—January 1970.

Supplementary Program Letters, Nos. 1-11 from
October 1962 through April 1964.

Reports from G. D. Collins and W. J. Ozeroff on
Assignment to CEA—France.

In addition, the following topical reports have been
issued:

GEAP-3287

Fast Oxide Breeder - Reactor Physics,
Part I - Parametric Study of 3000 MWe Reactor Core;
P. Greebler, P. Aline, and J. Sueoka
(November 10, 1959).

GEAP-3347

Fast Oxide Breeder - Stress Considerations
in Fuel Rod Design;
K. M. Horst
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