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FAST FLUX TEST FACILITY
PERIODIC TECHNICAL REPORT
MARCH, APRIL, MAY, JUNE 1970

AEC RESEARCH &
DEVELOPMENT REPORT

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FAST FLUX TEST FACILITY
PERIODIC TECHNICAL REPORT
MARCH, APRIL, MAY, JUNE 1970

B. Wolfe^(a)
Acting Project Manager

Compiled by C. P. Cabell^(a)

August 1970

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MARCH, APRIL, MAY, JUNE 1970

ABSTRACT

This report was prepared by Battelle-Northwest under Contract No. AT(45-1)-1830 for the Atomic Energy Commission, Division of Reactor Development and Technology, to summarize technical progress made in the Fast Flux Test Facility Program during March, April, May and June 1970.

PRIOR REPORTS IN THIS SERIES

BNWL-325	FFTF Summary Technical Report	1-1-66 to 12-31-66
BNWL-501	FFTF Quarterly Technical Report	1-1-67 to 3-31-67
BNWL-541	FFTF Quarterly Technical Report	4-1-67 to 6-30-67
BNWL-567	FFTF Periodic Technical Report	7-1-67 to 12-31-67
BNWL-660	FFTF Quarterly Technical Report	1-1-68 to 3-31-68
BNWL-880	FFTF Quarterly Technical Report	4-1-68 to 6-30-68
BNWL-917	FFTF Quarterly Technical Report	7-1-68 to 9-30-68
BNWL-941	FFTF Periodic Technical Report	10-1-68 to 2-28-69
BNWL-1090	FFTF Quarterly Technical Report	3-1-69 to 5-31-69
BNWL-1174	FFTF Quarterly Technical Report	6-1-69 to 8-31-69
BNWL-1275	FFTF Quarterly Technical Report	9-1-69 to 11-30-69
BNWL-1328	FFTF Quarterly Technical Report	12-1-69 to 2-28-70

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CHAPTER I: INTRODUCTION

This report describes progress in the discipline-oriented technological program being conducted to ensure technical adequacy of the FFTF design.

Progress in discipline-oriented technological work is presented in Chapters II through X. These chapters generally correspond to the LMFB Program Plan Elements. Subdivisions of the chapters, however, are based upon specific FFTF components, systems, facilities, and programs. A list of reports issued during the March - June 1970 period is given in Appendix A.

CHAPTER II. PLANT DESIGNA. CURRENT VALUES OF FFTF DESIGN PARAMETERS

Values of FFTF design parameters of general interest are given in Table 2.1. The Core Map is given in Figure 2.1.

TABLE 2.1. Current Values of FFTF Design Parameters (June 5, 1970)

Parameter	Units	Value
1. <u>Electrical System</u>		
115 kV Supply Feeder	Number	1
13.8 kV Supply Feeder	Number	1
13.8 kV Transformer	Number	1
13.8 kV Transformer Voltage	kV	115/13.8
13.8 kV Transformer Rating	MVA	22.5/30
480 Volt Emergency Generators (Diesel)	Number	2
Power, Emergency Generators	kW each	1200
2. <u>Containment</u>		
Containment Bldg.		
Diameter	ft	135
Overall Height	ft	194
Depth Below Grade	ft	70
Design Pressure - internal	psig	10
- external	psig	0.2
Design High Temp.	°F	250
Design Low Temp.	°F	(-)10
3. <u>Reactor Core</u>		
Power Excluding Closed Loops	MW _t	400
Peak Flux @ 400 MW _t	N/cm ² -sec	7 x 10 ¹⁵
Doppler Coefficient	T dk/dt	-0.005
Drivers	Number	75
Reflectors	Number	111

TABLE 2.1. (contd)

Parameter	Units	Value
<u>Reactor Core</u> (contd)		
Driver Fuel Duct		
Outside Width Across Flats	in.	4.615
20% CW Material		Type 316 SS
Wall Thickness in Core	in.	0.120
In-Core Primary (Safety) Rods	Number	3
In-Core Secondary (Control) Rods	Number	4
Peripheral Control Rods	Number	15
Maximum Velocity, Core Coolant	ft/sec	30
4. <u>Irradiation Test Facilities</u>		
Closed Loop Capability	Number	6
Power Closed Loop - Each of 2 initial	MW _t	2
Temperature Closed Loop Coolant - Maximum	°F	1200
Closed Test Loops		
Test Train Length	ft	12
Available Test Diameter	in.	2.5
Open Test Loops with Specimen Instrumentation	Number	2
Open Test Positions with Proximity Instrumentation	Number	1
5. <u>Closed Loop Ex-Vessel Machine</u>		
"CLEM" Machine - Major Diameter	ft	8
- Overall Length	ft	55
- Weight	tons	234
"CLEM" Heat Removal Capability	kW	30
6. <u>Reactor Vessel</u>		
Material		Type 304 SS
Overall Height	ft-in.	43-2
Inlet Nozzles - Size	in.	16

TABLE 2.1. (contd)

Parameter	Units	Value
<u>Reactor Vessel</u> (contd)		
Outlet Nozzles - Size	in.	28
Maximum Operating Temperature		
Outlet Plenum @ 15 psig	°F	1050
Inlet Plenum @ 225 psig	°F	830
7. <u>Reactor Vessel Head</u>		
Diameter - OD	ft	25
Thickness	in.	46
8. <u>Heat Transport System - Design Ratings</u>		
<u>Total System</u>		
Number Cooling Circuits	Number	3
Heat Load Per Cooling Circuit	MW _t	133
Material		Type 304 SS
<u>Primary System</u>		
Hot Leg Piping to Isolation Valve		
- Size OD	in.	28
- Temp. @ 15 psig	°F	1050
Hot Leg Valves - Isolation	Number	3
- Size	in.	28
Cold Leg Piping IHX to Vessel Inlet		
- Size OD	in.	16
- Temp. @ 225 psig	°F	830
Cold Leg Valves - Isolation	Number	3
- Check	Number	3
- Size	in.	16
Primary Pumps	Number	3
Flow Each @ 500 ft Na	gal/min	14,500
Temperature	°F	1050
IHX (Intermediate Heat Exch.)	Number	3
IHX Heat Load, Each	MW _t	133
IHX Temp. Shell Side @ 225 psig	°F	1050

TABLE 2.1. (contd)

Parameter	Units	Value
<u>Heat Transport System - Design Ratings (contd)</u>		
<u>Primary System (cont)</u>		
IHX Temp. Tube Side @ 250 psig	°F	1050
IHX Log Mean Temp. Diff. for Ultimate Conditions	°F	100
<u>Secondary System</u>		
Main Piping Size OD	in.	16
Branch Piping Size OD	in.	12
Pipe Headers Size OD	in.	8
Piping IHX to DHX Temp. @ 250 psig	°F	1000
Piping DHX to IHX Temp. @ 250 psig	°F	830
Valves - Hot and Cold Leg Temp. @ 250 psig	°F	1000
Secondary Pumps	Number	3
Flow Each @ 260 ft Na Head	gal/min	14,500
Temp. @ 250 psig	°F	830
DHX Units	Number	3
DHX Modules Per Unit	Number	4
DHX Thermal Rating per module	MW _t	33
DHX Tube Side, Temp. @ 250 psig	°F	1000

FIGURE 2.1. Core Map January 1970

CHAPTER III. COMPONENTSA. REACTOR COMPONENTS1. Fuel Duct Procurement

R. N. Johnson and R. H. Todd

A contract was placed on March 11, 1970 with Atomics International for the fabrication of prototype fuel ducts.

2. Duct Gaging System

R. N. Johnson and R. H. Todd

A contract was placed with Shelton Metrology Laboratory on May 29, 1970, for the design and construction of a gaging system capable of measuring and characterizing the duct components and the completed duct assemblies. Delivery of the gaging system is expected in January 1971.

3. Duct Wear Pad Attachment Studies

W. F. Brown and R. N. Johnson

Tube 62 which was reported in the last quarterly report was thermal cycled 20 times between 400 and 1200 °F in a salt bath furnace. Visual and liquid penetrant inspection revealed no defects due to thermal cycling. Ultrasonic inspection showed that all spot welds were still bonded; however, there was an indication that two of the spot welds may have less bonded area than the original inspection revealed.

4. Automatic Duct Welding System

W. F. Brown and R. N. Johnson

During the past quarter, BNW completed fabrication of the fixture for the automatic duct welding system (Figure 3.1 and 3.2). Assembly and checkout was completed and the fixture was shipped to Sciaky Brothers, Inc. on the 15th of May for coupling to the welding power supply and automatic control equipment.

Neg 52163-1

FIGURE 3.1. Automatic Duct Welding Fixture Showing Sample Fuel Duct in Position for Welding

Neg 52163-4

FIGURE 3.2. Automatic Fuel Duct Welding Fixture

The purchase request for the computer, operator pendant control, and programming software and interfacing was approved by the AEC and the purchase order has been placed with the Sciaky Brothers Company. Sciaky plans to ship the completed system about September 3, 1970.

5. Reactor Vessel

F. J. Kempf and O. W. Priebe

Agreement was reached on the use of SA-508 Class 2 low alloy steel for the reactor vessel head. This choice was made on the basis of system performance, mechanical properties, cost, schedule, and fabricability.

B. HEAT TRANSPORT PROCESS TECHNOLOGY

1. Detailed Simulation of Piping Stored Heat

A. L. Gunby

Five model approaches were studied for simulating the effect of stored heat in steel pipe walls on thermal transients. Each model used ten axial segments with separate regions for metal and sodium. Material properties and steel-to-sodium heat transfer varied with temperature and flow. Each model was subjected to two example temperature ramps, one with constant flow, the other with ramped flow. The five models were: nodal heat balance, central difference, 2-point backward difference, 3-point backward difference, and mixed difference. (a)

Figure 3.3 shows the responses of the models to an inlet temperature ramp with constant flow. Clearly, the mixed difference case (alternating central and 3-point backward differences) produced the best approximation to the expected outlet transient. The mixed difference model was selected as the reference for evaluating simplified pipe heating models needed for the overall FFTF system simulations. Model simplifications will be investigated during the next report period.

a. *For differencing descriptions, see BNWL-2367, "Intermediate Heat Exchanger Modeling for FFTF Simulation," A. L. Gunby, May, 1970. The pipe model is Zike an IHX with zero flow on one side.*

FIGURE 3.3. Piping Stored Heat Study Na Response to Inlet Temperature Ramp, Detailed Models

2. Heat Transport System Control Study

R. A. Harvey and S. A. Hunt

The first draft of a report on the control of the FFTF Heat Transport System is essentially completed and will be distributed to the AEC and Westinghouse. The results of the study support the following major conclusions:

- a. The control configuration that appears best for reasons of transient suppression, ease of operation, and safety has controllers that:
 - Control secondary cold-leg temperature by manipulating air flow.
 - Control primary cold-leg temperature by manipulating the set point of the controller (above).
 - Control of the primary hot-leg temperature by manipulating the primary flow.

Other configurations that are almost as good control the reactor temperature rise and/or the reactor average temperature.

- b. For low power operation, the best transient suppression is obtained when the secondary flow is maintained at the full flow value. Although the primary flow has little effect on transient suppression, full primary flow is desirable in order to maintain a desirable mean temperature difference between primary and secondary loops. Thus, we recommend that an operating mode be adopted that requires sodium flows to be brought up to their full values as soon as possible during startup.
- c. The combination of desirable control configuration and the desirability of maintaining full secondary flow suggests that startup could be accomplished by maintaining full flows and allowing the reactor temperature rise to

increase with power. The primary cold leg would be most easily handled by keeping it at a constant temperature during the startup.

3. Heat Transport System Control Cycles Study

D. P. Schively, H. C. Martin

A set of proposed design cycles were developed, consistent with the vessel procurement specifications and ASME Code, including the rationale for categorizing the cycles. The recommended cycles are shown in Table 3.1. Only the steepest portion of the cycles are tabulated. Return to the start condition is assumed at the normal rate.

TABLE 3.1. Design Cycles for Heat Transport System

Type of Transient	Affected Section	Coolant Temp., °F		Maximum Rate, °F/t	Minimum Number
		Start	End		
<u>Normal Conditions</u>					
Startup	Hot Leg	500	1050	+50/hr	1100
	Cold Leg	500	750	+50/hr	
Shutdown	Hot Leg	1050	500	-50/hr	100
	Cold Leg	750	500	-50/hr	
<u>Upset Conditions</u>					
Scram	Hot Leg	1050	900	-150/min	1000
	Cold Leg	750	700	-16/min	
OBE Plus Upset Loads	All				100
<u>Emergency Conditions</u>					
Scram (with HTS in at-power cooling mode)	Hot Leg	1050	800	-17/sec	10
	Cold Leg	750	700	-50/min	
Scram (with secondary pump seizure)	Hot Leg	1050	1100	+2/sec	10
	Cold Leg	600	800	+10/sec	
<u>Faulted Conditions</u>					
DBE Plus Emergency Loads	All				

CHAPTER IV. INSTRUMENTATION AND CONTROLSA. FFTF INSTRUMENTATION AND CONTROL SYSTEMS1. New System' Titles and Numbers

J. W. Mitchell

A new set of system titles and numbers have been adopted to conform with the project breakdown structure as it is presently defined. Table 4.1 presents the old and new titles and numbers.

TABLE 4.1. Instrumentation and Control
Systems Titles and Numbers

<u>Old System</u>		<u>New System</u>	
<u>No.</u>	<u>Title</u>	<u>No.</u>	<u>Title</u>
91	Central Control and Data Handling	5300	Reactor Plant Control
		5500	Digital Data Handling and Display
92	Reactor and Vessel Instrumentation	5100	Reactor Monitoring and Control
94	Failed Fuel Monitoring		
95	Flux Monitoring and Control		
93	Plant Instrumentation	5200	Process Monitoring and Control
96	Radiation Monitoring		
99	Plant Protection	5400	Plant Protection

The Reactor Plant Control System (No. 5300) comprises all aspects of reactor plant control design and operation including control modes, control configuration (manual/automatic, analog/digital, etc.), control consoles and control room layout. The Digital Data Handling and Display System (No. 5500) encompasses the digital hardware (computer) and software and the computer directed displays,

2. In-Vessel Sodium Level Sensor

W. Dalos

An inductive-type level probe has been tentatively selected for in-vessel sodium level measurement. Probe development and testing will be performed at the LMEC. The probe tested must satisfy the following specifications:

- Reference coil is to be located below sensing coil.
- Thimble diameter is to be 1-1/2 in. ID. Level probes with 2-ft and 5-ft ranges will be built and tested.

B. INSTRUMENTATION AND CONTROL DEVELOPMENT

1. Mixed-Mean Coolant Temperature Measurement at Subassembly Outlet

W. Dalos, D. R. Dickinson, and M. R. Wood

An array of three thermocouples are used to measure the temperature in the outlet coolant stream in each subassembly channel. To obtain accurate measurements, mixers are required to reduce the radial temperature gradient across the channel.

Test results on fuel subassembly instrumentation testing in water indicate that a 2.45-in. ID square-edge orifice mixer in a 3.75-in. ID duct will produce satisfactory mixing if the thermocouples are located at least 18 in. downstream from the orifice. The pressure drop across the orifice at rated flow (450 gal/min) is about 8 psi. Further design-test iterations are planned to optimize these measurements.

The following mixing devices were tested in a 3.75-in. ID duct with cold water at flow rates of 100 and 450 gal/min.

- A single 2.45-in. square-edge orifice in a plate 1/4-in. thick.
- Nineteen 9/16-in. holes equally spaced over a plate 1/4-in. thick.

- A plate with twelve 5/8-in. slanting holes at an angle of 30° to the vertical. Six holes divert liquid from near the wall to the interior; these alternate with six holes to divert flow from interior to near the wall. The device also has a straight 1-1/4 in. hole on the axis. See Figure 4.1.

Mixing was measured by injecting LiOH upstream of the mixer and taking samples at points along a diameter at positions of 6 and 18 in. downstream.

Although there was not a great difference between the three devices tested, the best mixing appeared to be produced by the one-hole orifice device. However, the pressure drop in this device was the greatest of all designs tested. In addition, mixing was considerably better with the 18-in. mixing distances than with the 6-in. distances. No significant differences in mixing was noted between 100 and 450 gal/min.

2. Fuel Failure Monitoring

a. Development Plans and Status

R. R. De Russeau

A program plan for the design and development of systems to detect and locate failed fuel in the FIR was completed and transmitted to RDT for approval. The plan was also sent to organizations involved in the LMFBR program, ANL, LASL, GE, W, and AI for their review. In the interim, the related development programs were directed to conduct limited investigations of a number of proposed concepts for locating failed fuel. These concepts are xenon and krypton gas tagging, conductivity probe monitoring, in-vessel gas separation, fission product deposition, flowmeter monitoring, and bubble source triangulation. Whenever possible, facilities and data were borrowed from existing development programs.

FIGURE 4.1. Mixer for FFTF Instrument Package

b. Xenon and Krypton Gas Tagging

(1) Summary (T. J. Kabele). A number of studies were completed on the use of xenon and krypton for tagging the pins of a FTR fuel subassembly. The studies were aimed at the optimized use of available ratios of $^{129}\text{Xe}/^{124}\text{Xe}$ and $^{80}\text{Kr}/^{78}\text{Kr}$ and the reservation of a unique tag for each fuel subassembly in the core. The results of the studies indicated the 80 tags could be mixed, the tag material could be available in time for core fabrication, the cost of material was not prohibitive, and the tags should stay unique throughout the fuel lifetime. One test was conducted at EBR-II in support of both FFTF and EBR-II tagging development programs in which a volume of tag was released in the primary sodium and satisfactorily identified in the cover gas by means of a mass spectrometer.

(2) Xenon and Krypton Tag Ratios (C. A. Mansius).

Eighty unique tags have been specified by using mixtures of natural xenon and increasing volume percentages of xenon enriched in the ^{124}Xe isotope. These tags are based on the isotopic ratios of $^{129}\text{Xe}/^{124}\text{Xe}$ and vary from 220/1 to 7/1. They are spaced by assuming a 3% differential neutron burnout of ^{129}Xe and ^{124}Xe . Table 4.2 lists the 80 distinct tag ratios in order of increasing ^{124}Xe enrichments. Figure 4.2 illustrates the volume ratios of natural xenon and enriched xenon mixtures* that are necessary to provide the various tag ratios. The ^{124}Xe percent abundance and $^{129}\text{Xe}/^{124}\text{Xe}$ ratio in natural xenon are approximately 0.1% and 276/1, respectively. Therefore, Mound 1, 5, and 10% concentrations represent mixtures with the ^{124}Xe percent composition increased from 0.1% to 1, 5, and 10%. Mixing specific amounts of these commercially available enriched mixtures provides the 80 ratios. The major uncertainty in specifying these ratios is the neutron burnout factor.

* As prepared by the AEC's Mound Laboratory

TABLE 4.2. $^{129}\text{Xe}/^{124}\text{Xe}$ Isotopic Ratios for 80 Distinct Tags

<u>Tag No.</u>	<u>129/124 Ratio</u>	<u>Tag No.</u>	<u>Ratio</u>	<u>Tag No.</u>	<u>Ratio</u>
1	220	28	96	55	33
2	214	29	93	56	31
3	208	30	90	57	29
4	202	31	87	58	28
5	196	32	84	59	27
6	190	33	81	60	26
7	184	34	78	61	25
8	179	35	75	62	24
9	174	36	72	63	23
10	169	37	69	64	22
11	164	38	67	65	21
12	159	39	65	66	20
13	154	40	63	67	19
14	149	41	61	68	18
15	144	42	59	69	17
16	140	43	57	70	16
17	136	44	55	71	15
18	132	45	53	72	14
19	128	46	51	73	13
20	124	47	49	74	12
21	120	48	47	75	11
22	116	49	45	76	10
23	112	50	43	77	9
24	108	51	41	78	8
25	105	52	39	79	7
26	102	53	37	80	6
27	99	54	35

FIGURE 4.2. Mixing of Xenon Isotopes

(3) Tag Ratio Differential Neutron Burnout (R. E. Schenter).

Calculational analysis of the neutron-capture cross sections for ^{78}Kr , ^{80}Kr , ^{124}Xe , ^{126}Xe , ^{128}Xe , and ^{129}Xe was performed to define the tag ratio differential neutron burnout and the tag spacing. Because of the lack of experimental cross section data in many neutron energy ranges, the calculational results were divided into most probable (top value) and maximum (bottom value) in Table 4.3 for three gas plenum regions in a fuel pin. Each region represents 12 in. of the 36-in. plenum. Averaging the burnout effect over the entire gas plenum and evaluating the effect on the $^{129}\text{Xe}/^{124}\text{Xe}$ tag ratios, it was concluded that the most probable value would cause a 1% differential neutron burnout (or change) in the ratio. The worst case, using the most probable ^{124}Xe burnout and the ^{129}Xe maximum burnout, gives a 3% differential change in the ratio.

TABLE 4.3. Calculated Tag Isotope Cross Sections

<u>Isotope</u>	<u>Bottom Plenum Region, a barns</u>	<u>Middle Plenum Region, a barns</u>	<u>Top Plenum Region, a barns</u>
^{78}Kr	0.36	0.398	0.44
	1.29	1.61	1.89
^{80}Kr	0.40	0.468	0.542
	1.21	1.53	1.82
^{124}Xe	1.62	2.20	2.82
	2.34	3.16	3.99
^{126}Xe	0.397	0.43	0.46
	0.952	1.15	1.32
^{128}Xe	0.281	0.30	0.324
	0.640	0.77	0.88
^{129}Xe	2.26	2.79	3.27
	3.69	4.91	6.08

^{126}Xe was eliminated from consideration because of the limited number of tags that could be mixed and the large difference in burnout fractions between ^{124}Xe and ^{126}Xe . The production of ^{128}Xe in the FTR from fission is too high to permit its use as a tagging isotope.

The krypton isotopes, ^{78}Kr and ^{80}Kr , have calculated cross sections that are low and similar. There appears to be no burnout problems associated with a limited use of $^{80}\text{Kr}/^{78}\text{Kr}$ tags in the FTR.

(4) Gas Volumes and Costs (T. J. Kabele). The volumes of natural and enriched krypton and xenon gases needed for tagging the first two FTR cores were calculated with four different cases considered. Case 1 used only natural and enriched xenon to mix 80 distinct tags with an isotopic ratio ($^{129}\text{Xe}/^{124}\text{Xe}$) difference of 3% to cover the change in the isotopic concentration from neutron burnout over the fuel lifetime. Case 2 used the 40 less-enriched ^{124}Xe tag ratios (with and without natural krypton) to produce 80 distinct tags. Case 3 increased the ratio spacing to 6% and used cost considerations to determine the choice between the xenon tags and enriched krypton mixtures. Case 4 was similar to Case 3 except that the xenon tag spacing was increased to 9% to cover the uncertainty in the ^{124}Xe and ^{129}Xe neutron capture cross sections. Furthermore, it was assumed that each pin would be loaded with 1 cm^3 of xenon tag and in Cases 2, 3, and 4, an additional 1 cm^3 of krypton tag. Volume calculations assumed 100% wastage from loading. The volumes of noble gases needed for one core are given in Table 4.4, while the tag material costs per core are found in Table 4.5.

TABLE 4.4. Gas Requirements Per Core

Enrichments	Volume, liters			
	Case 1	Case 2	Case 3	Case 4
Natural Xenon	30.0	30.0	31.0	32.0
1% ^{124}Xe Enriched	2.3	4.6	3.4	2.6
5% ^{124}Xe Enriched	0.077	0.15	0.11	0.075
10% ^{124}Xe Enriched	2.5	0.025	0.53	0.95
20% ^{124}Xe Enriched	0	0	0	0
Natural Krypton	0	18.0	14.0	10.5
1% ^{78}Kr Enriched	0	0	7.9	9.6
8% ^{78}Kr Enriched	0	0	0	4.4

TABLE 4.5. Cost for 80 Unique Tags

Case 1.	80 unique xenon tags (no krypton)
	Total Cost $\approx$ \$48,000
Case 2.	First 40 xenon tags, with and without natural krypton
	Total Cost $\approx$ \$9,000
Case 3.	Using every second xenon tag ^(a) and cost minimizing by using natural and enriched krypton
	Total Cost $\approx$ \$28,000
Case 4.	Using every third xenon tag ^(a) and cost minimizing by using natural and enriched krypton
	Total Cost $\approx$ \$52,000

a. Increasing tag ratio spacing to allow for unknowns in the neutron absorption cross sections for xenon.

c. Deleted

d. In-Vessel Gas Separation

R. J. Cash

Fabrication and assembly of a plexiglass model of the in-vessel gas separation concept has been completed, but test results are not yet available. This concept would use the upper portion of the reactor instrumentation guide tube to separate and collect a small portion of the total fission gas and helium released from a fuel pin failure. Gas samples would be withdrawn after a fuel failure and reactor shutdown.

e. Fission Product Deposition

W. F. Brehm

Tests are being run to study the feasibility of using gettering materials as indicators of failure locations in combination with other concepts. Candidate gettering materials are ZrO_2 on zirconium, TiO_2 on titanium, "passivated" stainless steel, and electropolished stainless steel. These materials were loaded in capsules made from stainless steel pipe, partially filled with sodium containing 3 to 4 wppm ^{134}Cs , and sealed. Tests are being run at 900 and 1050 °F.

f. Flowmeter Monitoring

The use of an instrument package flowmeter for monitoring abnormal amounts of gas coming through the fuel duct is being evaluated at ANL (Chicago). Preliminary tests indicate that a release equivalent to the amount of gas available in a pin near goal burnup causes approximately a 10% increase in the probe flowmeter signal. The total significance of this result is unknown due to other results not fully understood. Testing will continue.

g. Triangulation

R. J. Cash

The use of ultrasonic detectors on the Hydraulic Core Mockup (HCM) outlet pipes to monitor the passage of entrained

gas has led to the investigation of triangulating the time per amplitude signals obtained from these detectors to indicate the source of the gas. Preliminary tests were promising, and further tests with an updated HCM core model are planned.

3. Low-Level Neutron Flux Instrumentation

C. N. Jackson, N. C. Hoitink, R. C. Weddle, B. E. Dozer,
and D. C. Thompson

a. Objectives

In the FTR, the in-vessel reactor start-up and subcriticality instrumentation will experience severe environmental conditions of high temperatures and greater than 10^6 R/hr gamma dose rates. This task encompasses the developmental test and evaluation of sensors, cables and other components for high-sensitivity, low-level neutron counting systems capable of operation in this environment. The first objective of this task is the full establishment of a "300 °F System" with the detector and cable contained in the cooled thimble.

b. Long-Term Tests

Long term (1000 hr) gamma flux tests at 350 °F on cable-detector "**test assemblies**" have started in the gamma facility. Figure 4.3 shows a typical detector and cable assembly wrapped with heater tape, ready to be placed in the gamma facility. Tests on one commercial fission counter revealed stable operation with no apparent loss in sensitivity at the 175th hr of the planned 1000-hr test.

1. GUARDED FISSION COUNTER (ABOUT 3-in. DIAMETER BY 14-in. LONG)
2. TWO HIGH TEMPERATURE COAXIAL CABLES (0.170-in. DIAMETER BY 25-ft LENGTH)
- 3-7. CHROMEL-ALUMEL THERMOCOUPLES
8. CERAMIC BASE PLATE (3.75-in. DIAMETER BY 0.5-in. HEIGHT)
9. DETECTOR HEAT TAPE (FIBER-INSULATED, RATED TO 1600 °F)
10. CABLE HEAT MANDREL (2.0-in. DIAMETER BY 12-in. LONG, RATED TO AT LEAST 1600 °F)
11. CONNECTOR ■ NTERFACE (HN STYLE, ALUM ■ NA D ■ ELECTR ■ C)

Neg 700708-1

FIGURE 4.3. Typical Guarded Fission Counter and Two Cable "Test Assembly"
Prepared for Combined Gamma and Temperature Testing

BNWL-1394

CHAPTER V. SODIUM TECHNOLOGY

See BNWL-1200-3 and BNWL-1200-4 for reports on sodium technology progress for the period ending June 30, 1970.

CHAPTER VI. CORE DESIGNA. CORE MECHANICS1. Core Model

L. R. Besel, W. C. Kinsel, and G. R. Waymire

Fabrication for the basic structural core model (SCM) is complete. Figure 6.1 shows an overall top view of the completed SCM. Note the location of hydraulic actuators and yokes as well as the hydraulic module. Figure 6.2 shows a detailed view of the top of the ducts which more clearly shows attachment of the cast iron pads to the stiffness model (tubing) of the remainder of the duct as well as the detailed design of the yoke.

At the close of this report period, testing in the SCM had been confined to checking out the facility and establishing the initial operating procedures. Clamping forces to approximately 10,000 lb have been applied with no detrimental effects to the facility or alignment of the equipment.

2. Cladding and Spacer Wire Stresses

I. L. Metcalf and R. J. Jackson

An analysis has been conducted to determine the effect of irradiation creep, fast neutron metal swelling, and thermal expansion on the fuel cladding and wire wrap stresses. Because the clad is at higher temperatures than the wire, it experiences greater thermal expansion and swelling which is predicted to cause high wire tensile stresses.

Analysis indicates that when irradiation creep is considered, the wire stress is greatly reduced (Figure 6.3). Thus, the severity of the wire-endcap weld problem is greatly reduced by irradiation creep.

Neg 702639-2

FIGURE 6.1 View of Top of Structural Core Model

Neg 702639-10

FIGURE 6.2. Overall View of Structural Core Model

BNWL-1394

FIGURE 6.3. Wire Stress as a Function of Time, **with** and Without In-Reactor Creep

B. CORE PROCESS TECHNOLOGY

1. Core Map

R. J. Hennig

BNW and WARD have agreed on a core map for the initial FTR loading. The core and reflector consist of the following:

Core (91 Channels)

- 3 safety rods - "primary rods" - B_4C
- a 4 shim/scram rods - "secondary rods" - B_4C
- a 27 inner zone drivers
- a 48 outer zone drivers
- 2 fully-instrumented closed loops (6 future)
- 6 fully-instrumented open loops (2 future)
- 1 proximity instrumented open loop

Core Periphery

- 8 peripheral rods - B_4C
- 7 fixed peripheral shim rods - tantalum

Reflector

- 111 reflector channels

2. Control Rod Worth Requirements

R. J. Hennig

A re-evaluation of control rod worth requirements produced agreements between RDT, BNW, and WARD as shown in Tables 6.1 and 6.2.

TABLE 6.1. Primary System Worth Requirements ^(a)

	Nominal Requirement, <u>% $\Delta k/k$</u>	Estimated Uncertainty, (b) <u>% $\Delta k/k$</u>
Temperature and power (400 °F to Operating)	1.0	± 0.25
Reactivity Fault and Shutdown Margin	1.2	---
Control Rod Burnup	0.05	-10.02
Uncertainty in Requirements	<u>0.25</u>	
Total Requirement (Exclusive of Stuck Rod)	2.5	
Uncertainty in Control Worth Calculation	0.25	
Total Calculational Require- ment (Exclusive of Stuck Rod)	2.75	

-
- a. *These requirements may change as the iteration in design proceeds.*
- b. *The uncertainties listed are those appropriate for final design.*

TABLE 6.2. Secondary System Worth Requirements(a)

	Moveable Rod Requirement, % $\Delta k/k$		Fixed Shim Requirement, % $\Delta k/k$
	Nominal	Uncertainty ^(b)	Nominal
Temp. and Power (600 °F to operating)	0.8	± 0.2	---
Reactivity Fault and Margin	1.2	---	---
Control Rod Burnup	(d)	---	---
Fuel Burnup (15 MWd/kg)	2.6 ^(e)	± 0.26	---
Irradiation Induced Core Growth	0.3	± 0.15	---
Enrichment Increase for Uncertainty	0.35	---	0.5
Operating Flexibility	0.2	---	---
Test Allowance	---	---	1.0
Provision for Initial k Higher than Expected	---	---	0.5
Total Requirement	5.45	---	2.0
Uncertainty in Control Worth Calculation	0.55 ^(e)	---	0.2
Total Calculational Requirement	6.0	---	2.2

- a. These requirements may change as the iteration in design proceeds.
- b. The uncertainties listed are those appropriate for final design.
- c. The requirement can be met by shimming the reactivity in a variety of ways, including the use of fixed poison shims (no movement throughout an operating cycle required). The requirements are based on the assumption that the fuel enrichment will vary in the first few cycles until an equilibrium condition exists; if a decision is made that the initial core will use equilibrium enrichments, the requirements could more than double. Also, if the variation on the total fissile loading exceeds $\pm 0.5\%$, additional fixed shimming will be required.
- d. The control rod burnup requirement is zero in the first cycle and a few tenths of a percent in later cycles. However, after the first cycle, the irradiation-induced core growth allowance given above decreases to approximately balance the increased control rod burnup allowance.
- e. Current critical experiment analyses indicate that the ratio of plutonium-to-boron worth is less than calculated. Resolution of this discrepancy could either reduce the fuel burnup requirement or increase the calculated rod worths. In the latter case the heat generation rates for the control rod would be greater than now calculated.

3. CCTL Mk II Hydraulic Life Test*

E. G. Stevens

The Mk II full scale fuel assembly, which is to be hydraulic life tested under prototypic hot flowing sodium conditions, was received at Argonne National Laboratory in early March. Alignment fit-up problems between the centering rings of the flow duct and the support webbing of the vessel were corrected by removing metal from the centering rings. In early May, the vessel was filled with sodium, and calibration flow sweeps up to 450 gal/min at ~400 °F were successfully conducted. This was followed by heat-up to ~800 °F and additional flow sweeps up to the maximum test flow of 525 gal/min. Final test conditions are 525 gal/min and 1100 to 1150 °F. The 9,000 hr life test will run continuously until the latter part of CY-71. Experimented data to be obtained include:

- Sodium coolant-stainless steel clad corrosion and erosion at 1100 to 1150 °F; 525 gal/min (~25 ft/sec through the pin bundle) with typical FIR purity limits of oxygen <10 ppm and total carbon <20 ppm.
- Gross vibrational behavior of the 217 pin bundle, full-scale prototypic FIR fuel assembly.
- Pressure drop data for the overall bundle and partial increments such as the lower support grid.
- Wire-wrap mechanical behavior with a direct wire-to-end cap fillet weld attachment.

Figure 6.4 shows the as-received assembly. Figure 6.5 shows the overall 14-ft long, Mark II, test assembly and a magnified view of a typical pressure transducer (total of 4 required) which were attached to the duct by ANL just prior to insertion into the vessel. The high-temperature service pressure sensors provide the pin bundle coolant pressure drop data. Figure 6.6 shows the final machined surface of the lower support ring which was reworked to alleviate the

* Also see Item VIII.D.1 this report

Neg 113-3298

FIGURE 6.4. CCTL MK II as Received at Argonne National Laboratory, March 2, 1970

Neg ANL 113-3295

Overall Test Assembly

Neg ANL 113-3340

Pressure Transducer

FIGURE: 6.5. Mark II Test Assembly and Magnified View of Pressure Transducer

Neg ANL 113-3344

FIGURE 6.6. Magnified View of Final Machined Surface of Lower Support Ring

assembly-to-vessel fitup alignment problem. A crescent-shaped area, approximately 60 mils wide at the thickest part, was removed from one side of the ring. Metal was also removed from the upper support ring, and the four bolt holes in the top holddown flange were enlarged to effect a good ball-seat seal at the inlet to the test assembly.

Subassembly instrument probe testing will be conducted in conjunction with the Mark II hydraulic life test. Eddy-current or permanent magnet flowmeters along with thermocouples will supply data on subassembly operating coolant conditions. A high temperature accelerometer mounted on the duct exterior was intended to provide gross bundle vibration data. However, this instrument shorted out in early June before full flow and temperature conditions were achieved.

Initial pressure drop data at 450 °F and 800 °F at flows up to ~520 gal/min are in good agreement with those predicted.

4. Subassembly Hydraulic Testing

R. A. Hildner and T. C. Reihman

The object of the experimental hydraulics program is to evaluate the performance of FTR driver fuel subassemblies in terms of pressure drop-flow behavior, local flow distribution, and coolant mixing. The planned experimental program is divided into three subtasks:

- Pressure drop, local flow distribution, and fluid mixing studies in water for normal and deformed seven-pin hexagonal fuel assemblies. These studies will provide the hydraulic input required for an analytical model of the heated seven-pin fuel assemblies studied under Task TH-A. The effects of bundle-to-flow duct clearance, fuel bundle dispersion, and bowed pins will be investigated.
- Pressure drop, local flow distribution, and fluid mixing studies in water on normal and deformed 37-pin hexagonal

fuel assemblies. These studies will provide the hydraulic input for the analytical model to extend the results of the heated seven-pin studies to full size 217-pin FFTF fuel subassemblies.

- Pressure drop study in water on mechanically prototypic 217-pin fuel subassembly. This study will provide the experimental verification of the hydraulic behavior of full size fuel subassemblies over the entire range of operating conditions.

Assembly of the first seven-pin test section is in progress. All components are on hand and the wire wrapping of the instrumented pins has been completed.

C. IRRADIATION TESTING

1. Closed Loop Top Closure Development Program

The objectives of this task are to (a) demonstrate the feasibility of employing remote controlled, mechanically-driven, tungsten arc welding as a means of making a final seal closure between the closed loop nozzle plug and reactor top face spool piece, and (b) demonstrate reliable remote-controlled, mechanical cutting methods of the separation of the welded joint to facilitate repeated removal and replacement of the closed-loop test assemblies.

A remote-controlled, mechanically driven, tungsten-arc welder has been constructed to demonstrate remote welding capabilities.

Remote welding tests are being performed in an environmental test chamber by using 316 SS work samples to gather statistical data for design optimizations and to evaluate weld repeatability.

Two simulated, closed-loop test specimens were welded during this report period. Both welds were performed by

using identical test parameters of weld current/time and test temperature. Weld bead appearance is very good. Base material and the weld-filler ring of one test specimen are Type 304 SS, while the spool and nozzle plug of the other are Type 316 SS, with a Type 304 SS weld-insert-filler ring. Both test specimens survived a gas-pressure test at 6000 psig and a temperature of 1000 °F.

A cutter support ring incorporating hard chrome bearings has undergone limited testing in a cutter test stand. Remote cutting was performed on both cold heated stainless steel work samples. The only apparent difference between hot- (450 °F) and cold-cutting is the dimensional size of the groove as a result of thermal expansion.

2. Closed Loop System Design

Atomics International (AI) under the technical direction of Westinghouse, completed the conceptual design of the reference 2MW piped system using electromagnetic pumps. This concept has been recommended as a result of their earlier studies. The development effort recommended by AI includes work on sodium level detectors and on NaK-filled differential pressure cells. In addition to these, a verification test was recommended for the entire closed loop system under anticipated heat loads and at operating temperatures and pressures.

AI compared electromagnetic and centrifugal pumps using many different criteria such as operating history, coolant boundary integrity, safety provisions for the system, moving parts, maintainability and others.

The recommendation was made to use EM pumps for the closed loop application primarily because:

- There are no moving parts.
- They provide a hermetically sealed coolant boundary.

- Their gas-cooled throats provide an emergency cooling mechanism.
- Their integral location in-cell does not cause any physical disruption above the cell floor for shaft and drive motor.

AI also evaluated the possibility of providing the IHX and the cold trap with removable internals to enhance maintenance. The studies revealed that although a removable tubesheet IHX was entirely feasible, it would complicate the design and increase cost without adding substantially to maintainability. For the cold trap, it was determined that removable internals pose serious feasibility questions and would require a separate shielded handling cask for removal.

3. FFTF Closed Loop Test Characteristics

R. E. Keyes

Test characteristics were explored over a range of test assembly sizes from 1.8 to 3.0 in. maximum diameter.

Figure 6.7 illustrates the relations between test dimensions and power for 19-pin and 37-pin bundles of various pin diameters.

Further details are given in BNWL-1251, "FFTF Closed Loop Test Characteristics," R. E. Keyes, June 1970.

4. Open Test Assembly

G. R. **Waymire** and A. C. Richards

In support of a first issue of an experimenters manual for the FTR, an open test conceptual design was completed using latest design data available. The lance for contact instrumentation incorporates a shear-type remote disconnect which allows initial contact instrumentation in combination with an assembly which can be handled using the IVHM.

Figure 6.8 shows the layout drawing.

FIGURE 6.7. Closed Loop Test Bundle Capability

CONCEPTUAL

FIGURE 6.8 Open Test Assembly in Reactor Shortened Concept

SK-3-14108

D. OTHER CORE DESIGN

1. Examination Specimen Decay Heat Removal Development

D. R. Dickenson, T. C. Reihman and C. L. Boyd

The purpose of this task is to provide experimental data to define the requirements for gas cooling of discharged FFTF fuel assemblies during examination and disassembly. The object of the current tests is to determine what axial and transverse flow rates are needed at each state of the transition from axial to transverse flow during removal of the flow duct. In the current tests, both the axial and transverse flow is in suction. These tests are made in air using an electrically heated 169-pin bundle. The pins have a 37-in. heated length, 0.250-in. diameter, and are wire wrapped to give a 0.030-in. spacing. The bundle is enclosed by a tight-fitting hexagonal flow duct.

Tests were completed with axial flow only, and the results were expressed as a dimensionless cooling efficiency,

$$E = \frac{P}{\rho C_p F (T_{\max} - T_{\text{in}})}$$

where P = power input

ρ = density of gas

C_p = heat capacity of gas

F = volumetric flow rate of gas

$T_{\max}$ = maximum fuel pin temperature

T_{in} = inlet gas temperature.

Values of the cooling efficiency, E , were measured to be 0.75, 0.78, and 0.85 for powers of 8, 16, and 24 kW, respectively; $T_{\max}$ was in the neighborhood of 600 °F. Expressed in the above form, the data should apply to both air and argon. The power inputs in air of 8, 16, and 24 kW correspond (in terms of equal Reynolds numbers) to 5, 10, and 15 kW in argon.

The maximum pin temperatures were found about 1/2 in. from the downstream end of the heated length and near the center of the bundle. The outer rows of pins had a much lower temperature because of greater air flow around the edges.

The test facility is currently being modified to run tests with transverse flow only. The data with axial and transverse flow alone are required for comparison of data with mixed flow which will be obtained next.

CHAPTER VII. FUELS AND MATERIALSA. FUELS AND MATERIALS EVALUATION1. EBR-II Structural Materials Irradiations

The objective of the EBR-II Irradiations Program is to provide coordination and direction for the timely accomplishment of fast reactor irradiations on structural materials.

Structural materials irradiation experiments are in various stages of design, construction, and irradiation in EBR-II. Manufacture of specimens and subcapsules from 45 different AISI 316 SS compositions for the metallurgical variables subassembly is nearing completion.

On March 26, 1970 BNW Pin No. B-39 was placed in Subassembly X077 and charged in position 8C4 of the reactor grid. The goal fluence for the subassembly is 5×10^{21} n/cm² (total).

Subassembly X021B was discharged from EBR-II on March 1, 1970 for a scheduled pin interchange. Pin BNW-24 was discharged with the intent of replacing it with BNW-25. However, the original pins had swollen so much that it was impossible to reconstitute the subassembly.

Discussions with EBR-II and RDT personnel resulted in a decision to supply EBR-II with four undersized diameter pins (0.790 in. in diameter) in order to reconstitute the subassembly. The four pins discharged were 7-2, 7-3, 7-6, and 7-24. These were replaced by 7-25, 7-54, 7-55, and 7-56. The subassembly was reassembled and placed in the reactor for the beginning of Run 42. The purpose and objective of the replacement pins is to determine the effects of fast neutron irradiation on the mechanical properties of both biaxial and uniaxial Type 316 SS specimens having various amounts of cold work. The projected temperature and fluences

to be obtained during this irradiation are of considerable interest to FFTF and cover the range of variables where no other irradiation in progress or completed will provide adequate information. The goal exposure for the replacement pins will be the remainder of resident time in the reactor for Subassembly X021C (approximately 1×10^{22} n/cm²).

Arrangements have been made with Idaho Nuclear to disassemble the discharged experiments in the PNL Facility. During disassembly, a careful examination of the pins and subcapsules will be made to determine the diameter change due to the swelling. This will provide information needed to calculate the operating temperature of the subcapsules.

The purpose and objective of the original experiment (X021) was to determine the effects of fast reactor irradiation on uniaxial tensile, creep, creep-rupture and biaxial stress rupture properties of fast reactor cladding and structural materials.

Materials included in subassembly X021 were mainly 304 and 316 SS. The discharged pins contained primarily 304 SS. An exposure of 1×10^{23} n/cm² was originally planned for all pins. The discharged pins received an exposure of 7.3×10^{22} n/cm².

An updated summary of current irradiation in EBR-II and experiments discharged from EBR-II is given in Table 7.1.

2. EBR-II Subassembly Irradiations

L. A. Pember and E. O. Ballard

a. Subassembly PNL-3, PNL-4, and PNL-5

These 37-pin experimental subassemblies which contain Type 304 SS cladding, 0.250-in. diameter, mixed-oxide fuel pins continued without incident toward their goal exposures.

TABLE 7.1. Summary of Experiments Discharged from EBR-II

Subassembly Designation	Reactor Position	Date Charged	Date Discharged	Type of Specimen	Material	Accumulative ^(d) Exposure, n/cm ²	Goal ^(d) Exposure, n/cm ²
X-009 ^(a)	4A2	3-24-66	11-15-66	18 Tensile 10 Uniaxial Creep	304 SS	2.0×10^{22}	
X-010 ^(a)	7F3	3-24-66	4-24-69	18 Tensile 10 Uniaxial Creep	304 SS	3.9×10^{22}	
X-014 ^(a)	2D1	7-17-66	12-31-66	50 Tensile	304 SS	1.7×10^{22}	
X-018	2B1	12-06-66	2-25-69	198 Tensile or Uniaxial Creep	304 SS, 316 SS 348 SS, 321 SS	4.4×10^{22}	
X-019	6D2	1-13-67	8-05-69	64 Tensile 24 Uniaxial Creep	304 SS 316 SS	4.0×10^{22}	
X-020	6B5	1-13-67	1-29-70	28 Tensile 8 Uniaxial Creep	304 SS 316 SS	3.3×10^{22}	
X-021 ^(b)	2D1	2-27-67	9-16-68	127 Tensile or Uniaxial Creep	304 SS, 316 SS ^(c) Ni Base Alloys	3.2×10^{22}	
X-021A ^(b)	2D1	9-27-68	11-16-68	86 Density 16 Biaxial Creep 14 Tensile 10 Uniaxial Creep	316 SS ^(c) Ni Base Alloys	5.0×10^{21}	
X-021B	2D1	2-24-69	3-01-70	58 Density	316 SS and Ni Base Alloys	3.1×10^{22}	
X-022	7C4	2-27-67	6-15-68	595 Tensile or Uniaxial Creep 65 Biaxial Creep	304 SS, 316 SS, 348 SS, 321 SS	1.1×10^{22}	
X-030 ^(a)	6E1	10-10-67	3-06-68	5 Tensile 3 Uniaxial Creep	304 SS	5.2×10^{21}	
X-067	4F2	1-05-69	Sept. 69	204 Tensile or Uniaxial Creep 20 Fatigue	304 SS, 316 SS Weldment	0.9×10^{22}	
X-018B	4E2	10-02-69	Oct. 70	Density	304 SS, 316 SS	1.37×10^{22}	3.0×10^{22}
X-021B ^(e)	2D1	2-27-70	Dec. 70	501 Tensile or Uniaxial Creep	304 SS, 316 SS 348 SS, 321 SS	6.15×10^{22}	1×10^{23}
(Pin Interchanges) Pin 25		4-16-70	July 70	65 Biaxial Creep Swelling	Ni Base Alloys ^(c) Ni Base Alloys, 316 and 304 SS	2.5×10^{21}	1×10^{22}
Pins 54-56		4-16-70	July 70	(84) Uniaxial (42) Biaxial	Different cold work levels 316 SS	2.5×10^{21}	1×10^{22}
X-027	4B3	11-21-67	July 70	5 Tensile 3 Uniaxial Creep	304 SS	4.7×10^{22}	5×10^{22}
X-041	7C4	7-24-68	June 70	713 Tensile or Uniaxial Creep	304 SS, 316 SS, 321 SS, 348 SS	2.6×10^{22}	3×10^{22}
X-057	2B1	2-24-69	Mar. 71	74 Biaxial Creep 577 Tensile or Uniaxial Creep	304 SS, 316 SS ^(c) 321 SS, 348 SS	3.7×10^{22}	6×10^{22}
				164 Biaxial Creep 58 Density	Ni Base Alloys		
X-077 (Pin No. 39)	Row 8	3-01-70		Uniaxial Specimen	Zirconium Alloys	1.6×10^{20}	5×10^{21}

a. Mark A pins irradiated on space available basis, all other pins are B-7 type.

b. Single pin interchange.

c. Variety of other alloys also included.

d. Fluences greater than 0.1 MeV.

e. Original X-021 subassembly with three pin interchanges. Pins 25, 54, 55 and 56 are in now.

Estimated peak burnups at the end of Run 42 (May 18, 1970) are:

<u>Subassembly</u>	<u>Burnup, MWd/tonneM</u>
PNL-3 (X051)	21,200
PNL-4 (X059)	31,100
PNL-5 (X054)	50,300

Subassembly PNL-5 was discharged to the EBR-II in-vessel basket at the end of Run 42. This subassembly will be held in the basket pending availability at the EBR-II Mark E-19 subassembly hardware. The fuel pins will then be subjected to an interim exam, and selected pins will be reconstituted into a lead exposure test in the Mark E-19 subassembly.

b. Subassembly PNL-6, PNL-7, and PNL-8

These 37-pin experimental subassemblies which contain annealed Type 316 SS cladding, 0.250-in. diameter, mixed-oxide fuel pins continue toward their goal exposures. Estimated peak burnup at the end of Run 42 (May 18, 1970) are:

<u>Subassembly</u>	<u>Burnup, MWd/tonneM</u>
PNL-6 (X073)	10,400
PNL-7 (X069)	17,200
PNL-8 (X074)	17,900

c. Subassembly PNL-9, PNL-10, and PNL-11

Subassembly PNL-9 (X087) was charged into EBR-II position 5B4 on May 24, 1970. This subassembly contains 61 mixed oxide fuel pins clad in Type 316 SS, 20% cold worked. The cladding is 0.230-in. OD by 0.015-in. thick. Fuel-to-cladding gap is nominally 0.006-in. and the resulting nominal smeared density is 85.5% TD. The power generation of the peak pin will be 5.8 kW/ft.

Revision of the PNL-9, PNL-10, and PNL-11 data package is proceeding for EBR-II reactor power levels 50 and 62.5 MW with a depleted uranium blanket.

3. Irradiation of Hypostoichiometric $\text{UO}_2\text{-PuO}_2$

J. W. Weber

This task includes irradiation testing of mixed oxide fuel specimens of different stoichiometries to high burnups in a thermal flux. Substoichiometric mixed oxides are expected to exhibit better fuel-cladding-sodium compatibility at high **burnups** than nearly stoichiometric oxides currently specified for the FTR. System compatibility, swelling, and gas release characteristics of $\text{UO}_2\text{-PuO}_2$ fuel as a function of stoichiometry and fuel form must be determined at high **burnup** before a maximum performance fuel can be designed.

a. Postirradiation Examination of Fuel Pins

The status of the BNW-1 series of capsules is summarized in Table 7.2.

Sample D from fuel pin BNW 1-2 showed a two-phase melted region in the central void. One of the phases extends from the central void along several radial macrocracks into the columnar grains. Preliminary electron microprobe examinations on the two-phase region shows that the phase extending into the radial cracks consists primarily of Mo and Ba. The other phase is mixed oxide fuel. Examinations of the melt region in the central void and the fuel-cladding interface region are continuing.

Chemical **burnup** analysis on samples from fuel pins BNW 1-3 and 1-5 have been completed. Results show irradiation exposure for BNW 1-3 and 1-5 of 113,500 and 137,200 MWd/MTM respectively. These values are lower than the original exposure estimates for these fuel pins.

TABLE 7.2. Summary Status of Irradiation Experiments

Issue Date: 6-11-70

Test Designation	Description ^(a) Fuel Form	O/M	Facility	Goal Exposure MWd/MTM	Actual Exposure MWd/MTM	Discharge Date	Status
BW 1-1	Solid	1.93	MIR	100,000	122,000	2-69	Being examined at BW
BW 1-2	Solid	1.93	ETR	50,000	82,800	2-69	Being examined at BW
BW 1-3	Solid	1.96	MIR	100,000	113,500	2-69	Being examined at BW
BW 1-4	Solid	1.96	MIR	50,000	71,000	6-68	Examined at BW
BW 1-5	Solid	2.00	MIR	100,000	137,200	2-69	Being examined at BW
BW 1-6	Solid	2.00	MIR	50,000	75,200	10-68	Examined at BW
BW 1-7	Annular	1.93	MIR	50,000	<1,000		Lead tube failed. Examined at BW
BW 1-8	Annular	1.93	MIR	100,000	29,000	1-68	Failed. Examined at BW
BW 1-9	Annular	1.96	MIR	50,000	81,800	6-68	Examination completed at LASL
BW 1-10	Annular	1.96	ETR	100,000	(Est) 92,200	11-69	(b)
BW 1-11	Annular	2.00	MIR	50,000	87,400	8-68	Examination completed at LASL
BW 1-12	Annular	2.00	ETR	100,000	(Est) 96,500	11-69	(b)

a. Fuel25% PuO₂-75% UO₂

Pellets 0.212 in. OD 10.052 in. ID for annular pellets)

Smeared density - 87.5% TD

Pellet density - solid pellet 93% TD, annular pellet 95% TD

Fuel column length 13.5 in.

Cladding

OD - 0.250 in.

Wall thickness - 0.016 in.

304 SS

- b. Discharged 2-69 for interim examination. Recharged 5-69.
Discharged 11-9-69 for final examination.

4. Transient Irradiations

G. E. Culley and D. O. Sheppard

A fuel pin (PNL-1-2) that was transient irradiated at the TREAT facility on May 7, 1970 was returned to BNW for examination. The fuel pin became bowed and the fuel column length increased by 1/8 in.

Profilometry data exhibited a significant increase in fuel pin diameter as shown in Figure 7.1. The post-transient data reveal the fuel pin diameter has increased by about 1 mil over the length of the fuel column. This increase is not smooth but suggests fuel-cladding mechanical interaction at the pellet interfaces.

Shown in Figure 7.1 are pre- and post-transient photographs of the fuel pin showing the bowed condition. The magnitude of the bow is about 1/8 in. as measured from the photographs. (The apparent decrease in fuel pin length is caused by photographic interpretations and is not real.)

The most striking feature found in examination of a longitudinal section is the disappearance of the pellet-to-pellet interfaces. Figure 7.2 illustrates the phenomenon which has never been found during the examination of steady-state irradiated PNL-1 pins.

The transient test conditions and T/C response for the PNL-1-2 capsule are shown in Figure 7.3. The received transient was very close to **that requested**.

5. Effect of Cold Work on Irradiation-Induced Swelling of Type 316 SS

J. L. Straalsund, H. R. Brager, and J. F. Bates

Density measurements have been completed on a series of 32 stainless steel alloy specimens, with varying degrees of cold work, which were irradiated in BNW Pin No. 24 in EBR-II.

FIGURE 7.1. Transient Irradiation Changes in PNL-1-2

FIGURE 7.2. Fuel Pellet Interface Disappearance in PNL-1-2 Fuel Pin

FIGURE 7.3. PNL-1-2 Transient Test Condition

For irradiation temperatures between 700 and 1400 °F and fluences between 2 and 3×10^{22} n/cm² ($E > 0.1$ MeV), swelling in Type 316 SS cold-worked specimens was negligible.

Test conditions are listed in Table 7.3. The irradiation temperatures were estimated by assuming radial heat flow only and will be revised when more detailed heat transfer calculations are completed. The fluence values were calculated on the basis of the irradiation history given below and will be updated when dosimetry results become available.

TABLE 7.3. Irradiation Data on Pin No. 24

EBR-II Runs	33 to 40 inclusive
Position	2D-1 except for Run 38-B (62 MW) in which the pin occupied position 5D-2 (exposure in this position was 770 MW days)
Total Exposure	10,014 MWd
Calculated Peak Fluence	3.05×10^{22} n/cm ² ($E > 0.1$ MeV)

During the next report period cold-worked Type 316 SS specimens will be thermally aged to determine what magnitude of density changes may result from precipitation processes.

6. Damage Analysis for Solution-Treated 304 SS

J. L. Straalsund, H. R. Brager, and J. F. Bates

The objectives of this effort are to establish the irradiation-induced swelling characteristics of FFTF alloys in order to relate fast-reactor induced substructural changes in microstructure to corresponding changes in mechanical properties and to develop means for data extrapolation and interpolation by using both microscopic and empirical approaches.

Development of a self-consistent set of empirical equations describing void size, void concentration, and total void volume (swelling) is continuing. The following set of equations appear to account for all of the trends in the data for solution-treated 304 SS.

$$\frac{\Delta V}{V} = (\phi t)^{2.05-27/\theta + 78/\theta^4} \cdot [(T - 40) \times 10^{-10}] \exp[-0.015T - \frac{5100}{T} + 32.61] \quad (1)$$

$$d = (\phi t)^{0.15-24/\theta^2} \exp(7.45 - 1700/T) \quad (2)$$

$$\rho = (\phi t)^{1.60-27/\theta + 150/\theta^4} \exp(-0.015T + 10.253) \quad (3)$$

$$\frac{\Delta V}{V} = k\rho d^3 \quad (4)$$

where

$\frac{\Delta V}{V}$ = % volume change

ϕt = neutron fluence ($\text{n/cm}^2 \times 10^{-22}$, $E > 0.1 \text{ MeV}$)

T = temperature, $^{\circ}\text{K}$

$\theta = T - 623$

d = average void diameter, Angstrom units

ρ = void number density, $\text{number/cm}^3 \times 10^{-15}$

A geometric conversion factor, k , takes into account the ratio between the average of the cubed void diameters to the cube of the average void diameter. This ratio was determined from the void size distributions.

The principal difference between Equation (1) and the engineering design equation for swelling is the temperature dependent fluence exponent. The current engineering equation uses a constant fluence exponent of 1.71; whereas, Equation (1) predicts a fluence exponent varying from 0.94 at 700 $^{\circ}\text{F}$ to 1.94 at 1200 $^{\circ}\text{F}$. Figures 7.4, 7.5, and 7.6 compare the predictions of the two equations with experimental data at 375, 410, and 590 $^{\circ}\text{C}$. As may be seen from the figures, both equations appear to fit the data equally well.

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FIGURE 7.4. Swelling in AISI Type 304 SS at 370 to 380 °C (698 to 716 °F)

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FIGURE 7.5. Swelling in AISI 304 SS at 400 to 420 °C
(752 to 788 °F)

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FIGURE 7.6. Swelling in AISI Type 304 SS at 580 to 600 °C (1076 to 1112 °F)

7. Analysis of Irradiation-Induced Voids in Stainless Steels

J. L. Straalsund, H. R. Brager, and J. F. Bates

a. Stress-Induced Void Growth

Classical thermodynamic and growth theory indicates that the growth rate for an isolated void in the absence of radiation but in a stress field is given by:

$$\frac{dr}{dt} = \frac{D_s \Omega}{rkt} \left(\sigma - \frac{6\gamma}{r} \right) \quad (1)$$

where r is the void radius, D_s is the self-diffusion coefficient, k is the Boltzman constant, t is the temperature, σ is the uniaxial tension stress and γ is the surface energy. Thus, according to Equation (1), during annealing in the absence of stress, voids should shrink, but as the stress is increased, the voids should become more stable, until they begin to grow if:

$$\sigma > \frac{6\gamma}{r}. \quad (2)$$

Because this phenomenon could have an important bearing on the effects of stress on swelling, a series of annealing experiments were performed to determine if the effect could be detected. For an average void size of 1500 Å, no significant effects of stress on annealing at 1200 °F was found for stresses up to 12,000 psi (Figure 7.7).

b. Defect Structure Mapping

Transmission electron microscopy is being used to develop a quantitative map, similar to that for voids, describing interstitial cluster formation in irradiated stainless steels. Qualitatively, the structure of the interstitial clusters is strongly temperature dependent. At low irradiation temperatures (370 to 380 °C) and low fluences [4×10^{21} n/cm² ($E > 0.1$ MeV)], the microstructure consists of small (200 to 500 Å diameter) faulted loops. The shape of the loops vary

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FIGURE 7.7. Effect of Stress on Annealing of Irradiation-Induced Swelling

from nearly circular to extremely complex shapes. These complex shapes suggest that loops are interacting with other interstitial and/or vacancy clusters. In addition, faulted interstitial clusters exist in the form of curved streamers 2000 to 6000 Å long.

c. Effect of Irradiation Conditions

Transmission Electron Microscopy studies of void production in stainless steel have revealed that the shape of the void size distribution changes with irradiation conditions. Figure 7.8 illustrates how the size distributions vary with temperature and fluence for Type 304 SS. As may be seen from the figure, the shape changes from approximately Gaussian at low temperatures to double peaked at higher irradiation temperatures. The reason for the double peaked distribution is not known but it may be associated with either the changing loop structure with increasing temperature or preferential nucleation of voids (or vice versa) on large high temperature precipitates.

d. Use of Heavy Ion Bombardment to Simulate Fast Neutron Irradiation Damage

The use of heavy ion bombardment as a means of simulating fast neutron irradiation damage is being investigated. It has been recently reported^(1,2) that high temperature bombardment of austenitic stainless steels produces voids, interstitial clusters, and precipitates.

In tentative results that we have obtained to date from a foil that had been bombarded with 3×10^{16} nickel ions/cm² at 500 °C, the voids, loops, and precipitation are similar to that produced from neutron irradiation.

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1. R. S. Nelson and D. J. Mazey. "Radiation Damage in Reactor Materials," vol. II, IAEA Vienna (1967) 517.
 2. D. J. Mazey. "The Nature of Faults Formed in Association with Voids in 316 Stainless Steel During 22 MeV Carbon Ion Bombardment," Submitted to J. of Nuc. Materials, December 1969.

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FIGURE 7.8. Typical Distributions of Voids in Fast Reactor Irradiated Type 304 SS

8. Cladding Thickness Loss

J. W. Weber

Fuel-cladding reaction layer thicknesses on samples from BNW 1-2, 1-6, 1-8, 1-9, and 1-11 were measured on micro-photographs. These data, along with similar data published by GE and NUMEC, were collected and reviewed, and an empirical expression for loss-of-cladding thickness from fuel-cladding, fission-product reaction was determined. The expression for cladding loss

$$t \text{ (inch)} = 7.175 \times 10^{-11} [T-700] [\text{BU}]$$

where T = temperature of the cladding inside surface, °F

BU = fuel burnup in MWd/MTM

defines an upper limit for reaction layer thickness. It assumes that the reaction of fuel and cladding is proportional to burnup.

9. Mechanical Testing of Fuel Pin Cladding

R. L. Fish

The purpose of this program is to provide a basis for evaluating the combined effects of fuel cladding interaction, fluence, and operating temperature on the postirradiation burst and stress rupture properties of fuel pin cladding.

Previously reported burst test results on fueled specimens from the PNL-1 series fuel pins indicated cladding weakening after long periods of internal air exposure (>8 months). The exposure time required for this weakening was longer for cladding that had operated at lower temperatures (below the fuel midplane). Every specimen tested from above the midplane that had been exposed for a sufficient length of time, experienced a low strength, low strain failure.

Recent burst tests performed on specimens from above the midplane in fuel pins PNL 1-17 and PNL 1-19 have revealed a variation from this established pattern. These two burst specimens had been internally exposed to hot cell air for 10 to 12 months but failed with good rupture strength (65,150 and 57,120 psi respectively). Strain measurements are not yet available. This behavior is not consistent with previously obtained data on fueled specimens of other pins taken from the same general region and internally exposed to air for comparable times.

A detailed investigation of the handling and storage of these particular pins as well as a study of the operating conditions of the pins is in progress. Peculiarities in these aspects of the fuel pin history may have been significant in affecting the burst test results.

10. Controlled Clad Failure Techniques

M. K. Millhollen

Tests are continuing in an endeavor to establish a reliable fuel pin defecting mechanism for in-reactor defect testing in the EBR-II. Two artificial defects, consisting of nickel-plated, Silfos-filled, 0.005-in. diameter holes through the cladding wall, have been flow tested for 100 hr in 950 °F sodium. In both samples, the Silfos was smeared on the outer surface of the pin around the plugged, defect location to form a base for the nickel which was plated on. The size of this smeared area was $\sim 1/8$ -in. diameter.

The sample with the defect hole located approximately in the center of the nickel plate area survived the 100-hr test without difficulty. A sample with the defect hole located at one edge of the nickel-plated area failed.

11. Detection of Irradiation-Induced Fuel Pin Swelling by
Vibration Measurement

D. O. Hunter

It has been suggested that irradiation-induced swelling of fuel pins with a hex fuel duct can be detected through measurement of changes in the subassembly vibration properties. Since irradiation will cause increased stiffness of the assembly, it appears feasible that a correlation between vibrational properties and stiffness can be extended to include correlation between vibrational properties and irradiation damage. It is further expected that the tighter mechanical coupling between fuel pins and hex can at the fuel zone region, due to swelling in the pins, will in itself affect vibrational parameters--perhaps to a greater extent than will increased stiffness. An investigation is being conducted.

An experiment has compared the vibrational behavior of a 90-in. long $\times$ 2-in. diameter tube, tightly filled with wire-wrapped 1/4 in. diameter thick-wall tubing, against the vibrational behavior of the same assembly, except with several sheet metal screws forced through the 2-in. diameter tube, at the midsection, into the wire-wrapped tubing inside--thus simulating fuel swelling. The vibration changes caused by the screws were unmistakable. Frequencies of vibrational peaks were shifted and generally reduced in amplitude. One new vibrational peak appeared at a very low frequency as a result of the simulated swelling.

12. In-Cell Profilometry on Irradiated Fuel Pins

J. W. Weber

All major modifications to the 324 Bldg. East Cell profilometer have been completed. The control console and measuring frame (Figure 7.9) have been installed in the 324 Bldg. and checked out. The profilometer was calibrated, and the first diameter measurements on an irradiated fuel

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FIGURE 7.9. Measuring Frame and Electrical Control Console for
324 Building East Cell Remote Profilometer

pin PNL 1-2 have been completed. Results from these first calibrations and measurements indicate that the precision of measurements should be better than ± 0.0001 . Additional tests and analyses are necessary to establish the accuracy of the measurements.

B. MATERIALS TECHNOLOGY

1. High Strain Rate Effects

J. M. Steichen

The objective of the high strain rate studies is to evaluate the effects of high strain rates on the mechanical properties of ~~LMFBR~~ materials which are expected to be used in the FTR vessel and core components. The results of this program will provide information on the behavior of the vessel during impact from within and on the behavior of core components during thermal transients and other conditions of rapid loading.

A Kraft-Hahn dynamic loader was installed in the hot cell and three irradiated specimens tested. All three specimens were made from ~~IDRM~~ program 304 SS in the solution-annealed condition and were irradiated in EBR-II in PNL Subassembly X-022. Two specimens, 15 F 24-1 and 15 F 24-3, were irradiated to a total fluence of about 2×10^{21} ($\sim 0.09 \times 10^{21}$ E > 0.1 MeV) 700 °F, and a third specimen, 9 K 24-1, was irradiated to the same fluence at a temperature slightly above 800 °F.

In comparing the strength data obtained on irradiated 304 SS to that obtained on unirradiated material, as shown in Figure 7.10, two differences arise. The first difference is that the ultimate strength for unirradiated 304 SS appears to remain fairly constant to a strain rate of about 1.0 sec^{-1} . At a rate of 10.0 sec^{-1} , the strength falls off and then increases with strain rate to a rate of 100.0 sec^{-1} .

FIGURE 7.10. The Effect of Strain Rate on the Strength of Unirradiated and Irradiated Type 304 SS at 1000°F

The two tests at 1000 °F on irradiated material indicate that ultimate strength slightly increases with strain rate, and no drop in ultimate strength is observed at a rate of $\sim 10.0 \text{ sec}^{-1}$.

The yield strength for the unirradiated and irradiated material appears to slightly increase with strain rate. The slope of the two lines appears to be about parallel. The difference in the yield strength values between the unirradiated and irradiated material probably results from the control specimens being slightly prestrained during machining. Total and uniform elongation appear to be more greatly affected by strain rate in the irradiated material than the unirradiated material as shown in Figure 7.11. In the unirradiated 304 SS, the total and uniform elongation appears to remain essentially constant with strain rate, but the ductility of the irradiated material appears to slightly decrease with strain rate.

The third irradiated specimen, 9 K 24-1, was tested at 600 °F at a rate of $\sim 10.0 \text{ sec}^{-1}$. Only minor variations were observed in the strength values between control and irradiated material, but the ductility for the irradiated material was slightly higher than that of the unirradiated material.

2. PSAR "Leak-Before-Break" Analysis for Vessel and Primary Piping

L. A. James

In order to answer questions pertaining to the need for complete NDT inspection of the FTR vessel and primary piping at intervals during inspection, an analysis is being made of cracking behavior which results from the application of cyclic stresses. The concepts of linear elastic fracture mechanics are being used to estimate whether or not it is possible for a fatigue crack to propagate during reactor operations from an initial size associated with the limits of NDT detection to a

FIGURE 7.11. The Effect of Strain Rate on the Ductility of Unirradiated and Irradiated Type 304 SS at 1000 °F

"failure" size and whether the crack would allow a detectable amount of liquid sodium to leak before the "failure" size is reached.

The crack growth test results obtained to-date are summarized in Figure 7.12. Fatigue-crack propagation is a thermally-activated process. Crack growth increases markedly with increasing temperature (for example, at the same value of AK , the crack growth rate is approximately an order of magnitude higher at 1200 °F than at 75 °F). The test results shown in Figure 7.12 were obtained using single-edge, notched specimens cycled in uniaxial tension at a frequency of 4 cycles/min (except for some of the 75 °F specimens which were tested at higher frequencies). All specimens were tested in air, and none have been exposed to irradiation.

Future program plans call for fatigue testing of specimens in liquid sodium in addition to testing irradiated specimens. Some conceptual planning has been accomplished for the necessary apparatus and fixtures for the sodium testing.

3. Weldment Studies

A. L. Ward, A. J. Lovell, and L. D. Blackburn

The objective of this program is to provide mechanical property and metallurgical stability data for candidate LMFBR vessel and core structural weldment materials in order to ensure adequate lifetime performance under anticipated FTR service conditions.

Review of the tensile test results suggests that the previously reported⁽¹⁾ data must be reinterpreted. Selected specimens were designated "all-weld" and included in the data

1. FFTF Quarterly Technical Report, December 1969, January, February 1970, BNWL-1328. Battelle-Northwest, Richland, Washington, April 1970.

FIGURE 7.12. Summary of Fatigue-Crack propagation Behavior of Type 304 SS

plots of Figures 7.13 and 7.14. The selections were made on the basis of weld metal distribution and fracture location.

Detailed analyses of these data are not complete; however, the following summary observations, which support earlier conclusions, may be made:

- In all cases, the yield strength of weld metal exceeds that of the base plate, primarily because of the presence of residual welding stresses (Figure 7.13).
- The ultimate strengths tend to be somewhat insensitive to weld process and filler metal variation, with the possible exception of the shielded metal-arc (16-8-2 filler) case, where uniformly higher values presumably result from the solid solution strengthening of ~2% molybdenum content (Figure 7.13).
- Ductility parameters are generally consistent with the strength values. Reduction in area and uniform and total elongation values for weld metal consistently fall below those for the base plate material (Figure 7.14).

The creep-rupture results from weldment specimens are currently being reviewed in the preparation of a detailed report covering this phase of the program. As an interim summary, the major features of this study are presented here.

The creep and creep-rupture properties of unirradiated weldments can show a significant variation from the properties of annealed Type 304 SS; for example, the submerged-arc weld and the gas metal-arc weld exhibit shorter rupture times than annealed Type 304 material, while the gas tungsten-arc weld and the shielded metal-arc weld exhibit longer rupture times than the Type 304 alloy. Minimum creep rates of the submerged-arc weld and the gas metal-arc weld are higher than those of Type 304 material, at least at high stresses.

FIGURE 7.13.

Strength of Weld-Deposited Materials as a Function of Test Temperature and Weld Process

FIGURE 7.14-
Ductility of Weld Deposited Materials as a
Function of Test Temperature and Weld Process

Conversely, the minimum creep rates of the gas tungsten-arc weld and the shielded metal-arc weld are lower than creep rates of Type 304 material, especially at low stresses. The creep ductility, as measured by total elongation, is significantly lower in all the weld materials.

4. Uniaxial Creep-Rupture

A. J. Lovell and L. D. Blackburn

Results of postirradiation creep-rupture tests are currently being analyzed in some detail to provide a more complete description of the response of austenitic stainless steels to fast reactor irradiation. This report presents a general summary of the important changes in the creep and creep-rupture behavior produced by irradiation.

The creep-rupture behavior of austenitic stainless steels after irradiation in EBR-II is controlled by the response of the minimum creep rate and of the ductility to the irradiation conditions. The minimum creep rate is given by:

$$\dot{\epsilon} = \frac{\epsilon_2 - \epsilon_0}{t_2} \quad (1)$$

where $\dot{\epsilon}$ = the minimum creep rate

ϵ_2 = the strain at the end of second stage creep

ϵ_0 = a strain value obtained by extrapolating a steady-state portion of the creep curve to zero time

t_2 = the time at the end of second stage creep.

An empirical relationship is found to exist between the rupture life, t_r , and the end of second stage, t_2 , and is of the form:

$$t_r = B t_2 \quad (2)$$

where B is a constant, independent of stress, at constant temperature.

Substitution of Equation (1) into Equation (2) results in the relation

$$t_r = \frac{B (\epsilon_2 - \epsilon_0)}{\dot{\epsilon}} \quad (3)$$

This equation relates the rupture life to the minimum creep rate, to the linear creep strain, $\epsilon_2 - \epsilon_0$, and to the tertiary creep stage through the value B. The effects of irradiation on the creep and creep-rupture results determined in uniaxial, constant load creep tests are being analyzed in terms of the quantities in Equation (3).

Annealed Type 316 Stainless Steel

The creep-rupture results determined in uniaxial tests for a range of irradiation conditions [fluences from 0.3 to 3.5×10^{22} n/cm² (total) and irradiation temperatures from 700 to 1100 °F] show that $\dot{\epsilon}$, B, and $\epsilon_2 - \epsilon_0$ are all affected by irradiation.

Minimum Creep Rate. At low test temperatures (for example, 1000 and 1100 °F), creep rates are increased after irradiation to the higher fluences [that is, $>0.7 \times 10^{22}$ n/cm² (total)]. Reduced creep rates were not observed even though many of the irradiations result in substantial irradiation hardening in elevated temperature tensile tests. The mechanism responsible for the increased creep rates has not been clearly identified, but the effect may be related to second phase precipitation which occurs during most of the irradiations.

At a test temperature of 1200 °F, creep rates are reduced at the higher fluences. Again, a detailed mechanistic interpretation is not yet available, but it is possible that the reduced rates are related to the irradiation defect structure (for example, voids, loops, or dislocation networks).

The observations of increased creep rates at low test temperatures may be important in understanding in-reactor creep results. First, the postirradiation results may be relevant in establishing whether accelerated in-reactor creep rates are truly a dynamic effect of the neutron bombardment. Second, it is possible that the postirradiation results may establish new mechanisms of accelerated in-reactor creep at high fluences.

Linear Creep Strain. The values of $\epsilon_2 - \epsilon_0$ decrease with increasing fluence, but apparently not in a smooth or continuous fashion. Irradiation also causes $\epsilon_2 - \epsilon_0$ to become stress-dependent, $\epsilon_2 - \epsilon_0$ generally decreasing with decreasing stress for a given irradiation condition. These results indicate that irradiation may cause earlier onset of cracking or a greater amount of cracking, and thus limit steady-state creep strain.

The values of $\epsilon_2 - \epsilon_0$ are quite low at the high fluences; total creep strain values as low as 0.5% are observed in some cases. These results indicate that strain limitation problems may be encountered at still higher fluences.

Tertiary Creep. The value of B is a measure of tertiary creep; when $B = 1$, there is no tertiary creep stage. The results show that $B \approx 1$ for most of the irradiation conditions throughout the test temperature range. This elimination of tertiary creep suggests a rapid link-up of cracks or rapid crack propagation in the irradiated material.

Rupture Life. The combined effects of irradiation on $\dot{\epsilon}$, B , and $\epsilon_2 - \epsilon_0$ are reflected in the rupture life through Equation (3). Rupture life is reduced by factors of up to 1/100 at the low test temperatures and high fluences. This loss in rupture life translates into approximately a 40% reduction in the stress to produce rupture in 100,000 hr.

Irradiation can therefore have a significant effect on the conventional design allowable stresses. This effect is most significant at low creep temperatures and tends to lower the temperature at which creep strength becomes the controlling factor in determining allowable stresses.

Annealed Type 304 Stainless Steel

The creep-rupture results determined in uniaxial tests show that $\dot{\epsilon}$, B , and $\epsilon_2 - \epsilon_0$ are affected by irradiation.

Minimum Creep Rate. In this alloy, minimum creep rates at 1000 and 1100 °F are reduced at the higher fluences [that is, $>1 \times 10^{22}$ n/cm² (total)]. This behavior contrasts to that of the Type 316 SS alloy which showed increased creep rates at these test temperatures. Available information on the general features of irradiation defect structure (for example, voids, loops, dislocation networks) gives no indication of significantly different defect structures in the two alloys which would explain the difference in response to irradiation. It may be that second phase precipitation is different in the two materials, so that this factor controls the behavior of the Type 316 SS alloy, while defect structure controls the behavior of the Type 304 SS alloy.

Linear Creep Strain. As in the case of Type 316 SS, $\epsilon_2 - \epsilon_0$ decreases with increasing fluence in the Type 304 SS alloy. Very low values of $\epsilon_2 - \epsilon_0$ and total creep strain are observed in the Type 304 SS material at the high fluences. Again, this points to possible strain limit conditions at higher fluences.

Tertiary Creep. Values of B for irradiated material are approximately unity at a test temperature of 1000 °F. However, B values tend to be higher at a test temperature of 1100 °F. The presence of the tertiary creep state suggests that the irradiated Type 304 SS material may be somewhat more tolerant to cracking or flaws at this temperature.

Rupture Life. Reductions in rupture life of the Type 304 SS alloy are generally not as severe as those of the Type 316 SS alloy, particularly at 1100 °F. The reason for this difference stems primarily from the creep rate behavior, the increased rates in Type 316 SS material contributing to larger reductions at 1000 and 1100 °F, and the reduced rates in Type 304 SS material prolonging life at 1100 °F.

5. Tensile Studies

A. L. Ward

The objective of this program is to provide mechanical property and metallurgical stability data for LMFBR vessel and core structural materials in direct support of FFTF in order to provide assurance of adequate lifetime performance under anticipated FTR service conditions.

Tensile test results determined at elevated temperatures for annealed and 20% cold-worked Type 316 SS provide a direct comparison of the response of these two materials to fast reactor irradiation. After irradiation to 8×10^{21} n/cm² (total) at 1100 °F, annealed material exhibits substantial irradiation hardening and a loss in ductility. The 20% cold-worked material, however, shows a significant decrease in yield strength at low test temperatures (<1100 °F) after irradiation. Apparently, significant thermal recovery occurred during irradiation of the cold-worked material, but the relationship between thermal recovery and irradiation hardening has not yet been investigated in detail. The ductility of 20% cold-worked material remains low after irradiation.

Tensile tests have been completed on a series of Type 304 SS specimens following irradiation in the SURV-2 EBR-II surveillance subassembly (Row 12) to a peak total fluence of 3.2×10^{21} n/cm² at 700 °F. Three types of materials

are represented:

- Type 304 SS wrought specimens (Code K) which were fabricated prior to the availability of the original EBR-II stock.
- Type 304 SS from the original EBR-II cover-plate stock. (Code P).

Type 304 SS containing a section of weld-deposited Type 308 SS (Code L).

Preliminary examination of the test results indicates that the materials have undergone only minor, if any, irradiation hardening. Slight increases in strength and losses in total elongation are suggested in the Codes K and P specimens, while the Code L specimens exhibit minor ductility losses with no effect on the strength.

Tensile and hardness testing is in progress on Fermi surveillance specimens of Types 304 and 347 SS and Inconel X750 following accumulation of $\sim 1.5 \times 10^{21}$ n/cm² (total) at temperatures between 540 and 700 °F.

6. Fluence Limits

R. A. Moen

Fluence limits based on retaining a fixed percentage of total elongation capability at end-of-life are being used in the design of the FTR vessel, core barrel, and core support structure. These limits depend solely on the output of development programs which produce irradiation effects data. As these programs produce the type of data desired, the fluence limits are subject to revision.

Revised fluence limits for Type 304 SS and weldments of the same material compared to previous limits are shown in Table 7.4.

Fluence limits for shielded metal arc, gas-tungsten arc, and gas-metal arc were also prepared. In this re-evaluation

of data, all thermal reactor data were omitted. The omission of thermal reactor data primarily affects the lower temperature regime, (500 to 700 °F) where until now there were only thermal reactor data. Now, data from the EBR-II irradiation of base metal and weldments have shown that fast reactor exposures at these lower temperatures are less damaging than thermal reactor exposures. Therefore, with the increasing availability of fast reactor data, it will now become standard practice to use only fast reactor coupon data in the establishment of fluence limits for FTR structural materials.

TABLE 7.4. Fluence Limits for FTR Vessel, Core Barrel and Core Support Structure

Temp, °F	Revised Fluence Limit		Previous Fluence Limit	
	Base Metal, n/cm ² (total)	Weldments, (a) n/cm ² (total)	Base Metal, n/cm ² (total)	Weldments, n/cm ² (total)
600	9.5×10^{21}	7.3×10^{21}	6.0×10^{21}	2.5×10^{21}
700	1.15×10^{22}	9.4×10^{21}	1.4×10^{22}	7.0×10^{21}
800	1.19×10^{22}	7.9×10^{21}	2.1×10^{22}	1.0×10^{22}
900	2.1×10^{22}	4.1×10^{21}	2.4×10^{22}	1.5×10^{22}
1000	1.55×10^{22}	---	1.8×10^{22}	9.6×10^{21}
1100	1.12×10^{22}	---	2.7×10^{21}	1.0×10^{21}
1200	4.3×10^{21}	---	---	---

a. *For Type 304 SS weldments, using Type 308 SS filler, the submerged arc process and no post-weld treatment.*

7. Carbon Transport

W. F. Brehm

Carbon transport is of primary interest to designers concerned with long-term stability of materials used in the FTR primary heat transport system. Without some assurance that carbon transport will not occur, the designer is forced to use allowable stresses for low carbon stainless steels,

which in turn result in heavier wall thicknesses. A study recently completed concluded that for all sections of the primary heat transport system from the reactor outlet nozzle downstream to the reactor inlet nozzle, decarburization or carbon transport is not expected to occur. This conclusion is supported by theoretical and experimental evidence. (1)

8. Construction Codes for Nonradioactive Sodium Loops at PNL

M. T. Jakub

Four small nonradioactive sodium loop facilities (PAL, HTSF, FRTEF, and CRCTA) are being constructed in support of the FFTF. After consultants evaluated various alternatives, RDT and PNL representatives have agreed to use the following construction codes: B31.7, Class II, for piping; Section III, Class C, for vessels; Draft Pump and Valve Code, Class II, for pump casings and valve bodies. Appendix IX (ASME) will be used for all Quality Assurance requirements, but the code stamps will not be required for all components. The quality control provisions (including third-party inspection) are recognized as being of primary importance for quality fabrication.

9. Irradiated B_4C Pellet Fragmentation

L. R. Bunnell

Irradiated B_4C from the second Hanford discharge was examined externally to characterize the fragmentation of hot-pressed pellets as a function of original density and to provide a background for metallography. The following trends were observed for these 9 to 13% B_{10} burnup pellets:

- 65% TD pellets fractured into small, irregularly-shaped pieces, generally left an irregularly-shaped core. Much fine material was generated.

1. J. C. Tobin and W. F. Brehm, *Unpublished Data, Battelle-Northwest. (Carbon Transport in the FFTF-MMR-36, A-0199).*

- 90% TD pellets also fractured, left less fine material. The fractures also left core material, but broken pieces tended to take the form of "shells," that is, annular rings of B_4C .
- 99% TD pellets, the most difficult to remove from capsules, contained even less fine material; the fracture habit was extremely shell-like.

The irradiated material was red when viewed in transmission, apparently because of color centers introduced by gamma exposure in-reactor.

Selected B_4C pellets from the second Hanford discharge were polished and examined metallographically. Figures 7.15, 7.16, and 7.17 show traverse views of the pellets; they follow trends noted previously at lower burnup with radial cracks observed in low-density pellets and circumferential cracks present in high-density pellets. Micrographs of the same pellets showed the presence of microcracks in the 99% TD pellet with few, if any, present in the lower density pellets. The microcracks observed extend to the center of the high density pellet, and the presence of these cracks, which is beyond the normal range of thermal neutrons, is attributed to radiation damage due to the fast neutron component of the flux.

10. Sodium- B_4C Compatibility

L. R. Bunnell

In order to extend previous data to higher temperatures, hot-pressed B_4C pellets were exposed to sodium at 600 °C for 100 hr in compatibility-transport capsules. Metallographic results followed the same trends noted at lower temperatures, with sodium attack more severe in the 75% and 85% TD pellets than in the 96% TD pellet. Attack was noticeably more severe in the lower density pellets than in the previous 500 °C 100 hr experiments. Areas of low density B_4C were apparently removed

Neg 702409-1

9X

FIGURE 7.15. Macrograph of 65% TD Pellet (4-5) Irradiated to 11% Burnup at 350 °C with Pronounced Splitting and Radial Cracks. (Pellet segment on lower right was drilled for thermocouple.)

9X

FIGURE 7.16. Macrograph of 90% TD Pellet (4-4) Irradiated to 9% Burnup at 370 °C and Containing Both Radial and Circumferential Cracks. Central Hole Is a Thermocouple Well and Black Areas Are Bubbles Within a Mount. (Pellet at lower right has lost its outer circumferential shell.)

9X

FIGURE 7.17. Macrograph of 99% TD Pellet (4-14) Irradiated to $\sim 7\%$ Burnup at 300°C . (Note absence of radial cracking.)

by the sodium. Metallography of the Type 316 SS capsule revealed no interaction or evidence of carburization; however, chemical analysis for boron and carbon is proceeding.

11. Lithium- B_4C Effects

L. R. Bunnell

The production of lithium by $^{10}\text{B}(\text{n},\alpha)$ reaction prompted an investigation of the effects of lithium on B_4C . At temperatures of $>500^\circ\text{C}$ for periods of ~ 100 hr, a gross expansion occurs; it amounts to a 50% increase in length (factor-of-three volume increase). This length increase was noted in capsule tests and was observed in experiments performed in the inert atmosphere facility. X-ray diffraction analysis showed lithium carbide (Li_2C_2) initially, with lithium boride ($\sim \text{LiB}_{11}$) forming after exposure to a moist atmosphere. Similar reaction products have been detected by Secrist. (1)

1. D. R. Secrist. "Compound Formation in the Systems Lithium-Carbon and Lithium-Boron," J. Am. Cer. Soc., vol. 50 (10) pp. 520-523. 1967.

12. B₄C Gas Release in Irradiation

A. L. Pitner

The gas release behavior of the different forms of boron carbide (60 and 80% TD powders and 65, 90, and 99% TD pellets) as a function of irradiation temperature is shown in Figure 7.18. Each data point is accompanied by a number indicating which subassembly the sample resided in. (The results from Subassembly No. 1 are probably the least reliable.)

The percent of gas released from boron carbide appears to increase rapidly when the irradiation temperature exceeds 1000 °F. The 99% TD pellets release the lowest fraction of gas. The 65 and 80% TD pellets do not appear to vary significantly in their gas release behavior, but both release a higher gas fraction than the 99% TD pellets.

13. Radiation Effects on Tantalum Absorbing Material for Control Rods

A. L. Pitner

Density and length changes of the samples in the tantalum Capsule TA-5 in the EBR-II are given in Table 7.5 along with the estimated exposures and irradiation temperatures. The results from the previously disassembled Capsule TA-4 are also included for comparison, and they agree reasonably well. As noted previously, the irradiation-induced swelling is highest for the lower-temperature samples, even though they received the least exposure.

The tantalum replacement capsules, BTA-6 and BTA-7, have been loaded into the X042 subassembly and are now under irradiation in the EBR-II. Based on the temperature measurement from the SiC pellet in TA-5, tantalum samples in BTA-6 and BTA-7 are expected to reach maximum temperatures of 2300 °F.

FIGURE 7.18. Helium Gas Release of Boron Carbide Irradiated in a Thermal Flux ,

TABLE 7.5. Swelling of Tantalum Irradiated in EBR-II

Sample	Est. Exposure, E > 0.1 MeV	Temp, °F	TA-4		TA-5	
			$\Delta L, \%$	$\Delta \rho, \%$	$\Delta L, \%$	$\Delta \rho, \%$
1	3.0×10^{21}	1175	+0.13	-0.69	+0.18	-0.88
2	4.6×10^{21}	1550	+0.13	-0.68	+0.13	-0.72
3	6.7×10^{21}	1875	+0.11	-0.85 ^(a)	+0.07 ^(a)	-0.47
4	8.2×10^{21}	2050	--	-0.58	+0.06	-0.53
5	8.6×10^{21}	2100	+0.01	-0.42	+0.02	-0.29
6	8.2×10^{21}	2050	+0.05	-0.45	+0.03	-0.25 ^(a)
7	6.7×10^{21}	1875	+0.06	-0.62	+0.10 ^(a)	-0.61
8	4.6×10^{21}	1550	+0.09	-0.63	+0.11	-0.82 ^(a)
9	3.0×10^{21}	1175	+0.13	-0.72	+0.09 ^(a)	-0.58

a. Plating from nearby melt wire apparently caused measurement to be in error.

CHAPTER VIII. FUELS RECYCLEA. FUELS TECHNOLOGY1. Laboratory Size Moisture Measuring Instrument

M. J. Barr

A compact moisture measuring instrument was built to provide moisture content data on development size powder batches. The instrument is based on a prototype designed by J. E. Sammis and W. F. Bonner (FFTF Fuel Department). The instrument shown in Figure 8.1 is air tight and consists of a 2-1/2 in. OD $\times$ 5 in. long aluminum tube with a Panametric moisture sensor and a thermometer mounted in the threaded cap. The instrument will be used to correlate powder moisture content with the pressing behavior of mixed oxide batches.

Neg 700969-2

FIGURE 8.1. Laboratory Size Moisture Measuring Instrument

2. X-ray Fluorescence as a Quality Assurance Tool

H. G. Powers

Recent trial applications of X-ray fluorescence for rapid material analysis and survey ~~have~~ shown this technique to be useful on a limited basis. Advantages of X-ray analysis in the characterization of materials are:

- Nondestructiveness
- Speed
- Simple emission spectra. This minimizes interference effects since only 2 to 10 X-ray lines are detected.
- With proper equipment, many and diverse sample shapes can be accommodated.
- It is specific for elements. For example, it makes no difference whether iron is present as a metal or as any of the diverse iron compounds.

X-ray fluorescence analysis also has disadvantages:

- Low sensitivity. It requires several hundred ppm of an element for positive identification.
- Matrix effects on the sample. These effects can be complex and severe (depending upon the matrix composition), unless an internal standard can be introduced.
- It does not provide the ultimate in precision and accuracy. All such precision and accuracy are desired, the sample will probably require destructive analysis by other techniques."

As a quality assurance device, this method would work well if the material discrepancies were of a large order (approximately

* *Such as:*

- *Spectrographic Analysis*
- *Atomic Absorption*
- *Wet Chemistry*

0.1 to 0.2%). For small differences (100 to 1000 ppm), a rapid survey would probably not be sufficiently accurate. In such cases, the sample would have to be quantitatively analyzed, probably in a destructive manner; therefore, other techniques might be more applicable.

X-ray fluorescence techniques are currently being applied as a means to provide interim overchecks of vendor chemical analysis on as-received hardware. During this reporting period, such an overcheck was made on 302 springs, with lead showing up in an alarming amount. First evaluations of the situation were based upon the assumption that the parent metal contained lead as an impurity; however, further analysis showed a corresponding apparent reduction in indications of iron. Experience has shown that when surfaces are contaminated by heavier metals such as lead, iron emissions are attenuated and cannot be recorded. This accounts for the apparent low concentration of iron. Processing test springs through the 308 cleaning process and re-examining them by X-ray fluorescence resulted in a normal analysis of the 302 springs when they were compared with an NBS standard of similar materials. Further investigations showed that use of lead in lubricants is common in the spring fabrication industry.

The significance of this evaluation is that X-ray fluorescence is not only capable of detecting elemental inconsistencies in alloys, but can also detect undesirable surface conditions. Therefore, X-ray fluorescence may be a valid test for evaluation of process cleaning cycles. The test is fast and relatively cheap, and permanent records are produced for verification and documentation.

3. Fuel Pellet Homogeneity Inspection and Testing

H. G. Powers

Standard pellets have been selected to be used by industry to determine the relative homogeneity of fuel pellets manufactured in the qualification round. Eight fuel pellets of varying homogeneity have been analyzed with an electron microprobe. The concentrations of PuO_2 and UO_2 at $2\ \mu$ intervals across a transverse section of each of the pellets were recorded by the microprobe on paper tape. These data have been transferred to magnetic tape for computer calculation of a figure of merit for each pellet. Five figures of merit have been calculated to date. Three pellets for which figures of merit have been calculated via computer code have also had figure of merit values calculated for them by hand via previously-obtained microprobe continuous chart data. The hand-calculated values are up to 10% higher than those obtained from the computer code. This low-side bias appears in all the pellets analyzed thus far. Although nothing has been ascertained, problems with the data recorded on the magnetic tape may be the prime cause of the bias.

Seven standard pellets to be used by industry in the qualification round to determine the relationship between allowable PuO_2 -enriched area size and PuO_2 concentration have been fabricated. The pellets have also been mounted, polished, *a* autoradiographed, and photographed at 50X. Each of these standard pellets was fabricated with both $300\ \mu$, 30 wt% PuO_2 and $120\ \mu$, 100 wt% PuO_2 spheroids in a standard matrix of 25 wt% PuO_2 -75 wt% UO_2 . These spheroids represent the maximum allowable PuO_2 enriched area size at the maximum concentration level respectively. The relationship between allowable PuO_2 enriched area size and allowable PuO_2 concentration in the area is a linear function between these two extremes.

4. Fabrication of Mixed Oxide Fuel for EBR-II Subassemblies W. E. Warden

Fuel fabrication for PNL-10 and 11 is complete. This fuel consists of 65% enriched (^{235}U) mixed oxide in the EBR-II test composition of 75% UO_2 -25% PuO_2 . Sixteen kilograms of fuel, in 4 kg lots, were sintered for the two subassemblies. Three of the lots were centerless ground and one of the lots (FE-93) was sintered to meet the required diametral tolerance (0.194 in. $\pm$ 0.0015 in.). The sintered-to-size fuel represents a major accomplishment in fuel fabrication for irradiation test assemblies. Diametral data from lot FE-93 (see Table 8.1) showed the tight tolerances maintained within the lot. A second lot of fuel (FE-94) was fabricated to establish the process repeatability and for use in PNL-12A. Diametral data from lot FE-94 show the same tight diametral tolerance exhibited by lot FE-93.

The tight control-to-diametral tolerance is representative of fuel fabricated by the high pressure preslug-granulate, low-pressure compact fabrication sequence. Experience has shown that fuel fabricated by this sequence has a smaller pressure gradient in the compacted pellet and less shrinkage in sintering.

TABLE 8.1. Diametral Control Data for **Preslug-Granulate-Compact** Fabricated Fuel

Lot	Sample Size	First End, in.		Middle, in.		Second End, in.		Maximum Δ in. diam. of One Pellet, in.
		Mean	σ	Mean	σ	Mean	σ	
FE-93.0	30	0.19354	0.0018	0.19352	0.00019	0.19340	0.00013	0.0005
FE-94.0	30	0.19376	0.00023	0.19369	0.00023	0.19354	0.00026	0.0006
FE-73.4	15	0.19656	0.00025	0.19642	0.00021	0.19631	0.00023	0.0004
FE-74.0	10	0.19699	0.00017	0.19681	0.00026	0.19674	0.00026	0.0006

5. FIR Fuel Specifications

C. A. Strand

Specifications for FIR fuel were written as RDT standards for use in the RFP package for the Vendor Qualification program. These standards and associated drawings were prepared by BNW and WARD. Preparations are being made for finalizing the standards. A listing of the RDT standards and the status of each is shown in Table 8.2.

**TABLE 8.2. Status of RDT Standards Preparation
for FTR Driver Fuel**

RDT Std. No.	Title	Status Code ^(a)
E 13-1	FFTF Ceramic Grade Plutonium Dioxide	1
E 13-2	FFTF Ceramic Grade Uranium Dioxide	1
E 13-3	FFTF Uranyl Nitrate Solution	2
E 13-4	FFTF Plutonium Nitrate Solution	2
E 13-5	FFTF Driver Fuel Pin	1
E 13-6	FFTF Driver Fuel Pin Fuel Pellet	1
E 13-7	FFTF Driver Fuel Pin Insulator Pellet	1
E 13-8	FFTF Driver Fuel Pin Seamless Cladding Tube	1
E 13-9	FFTF Driver Fuel Pin End Caps	1
E 13-10	FFTF Driver Fuel Pin Reflector	1
E 13-11	FFTF Driver Fuel Pin Plenum Spacer	1
E 13-12	FFTF Driver Fuel Pin Plenum Spring	1
E 13-13	FFTF Driver Fuel Pin Wire Wrap	1

a. Status Code:

1. *Draft reviewed and comments submitted to WARD*
2. *Being prepared by BNW*

6. Void Size Distribution Measurements

M. C. J. Carlson

A comparison of the average void frequencies predicted by three methods for pellet MEE-63-4-B is shown in Table 8.3 and Figure 8.2. No significant ($>2\sigma$) variation exists between the void frequencies obtained by the three methods.

TABLE 8.3. Comparison of Frequency of 8- μ Diameter Voids in an MEE-63-4 Fuel Pellet as Found by Three Methods

Method	Void Size, μ	Void Concentra- tion, Voids/cm ³ / μ	Unfolding Uncertainties, 1 σ level
Lineal Micro- probe Trace Analysis (Specimen current traces)	8	2.6×10^7	$\pm 0.46 \times 10^7$
Zeiss Particle Analyzer (Photographs along traces)	8.15	3.4×10^7	$\pm 0.46 \times 10^7$
Lineal Analysis of Photographs Along Trace	8	3.2×10^7	$\pm 0.46 \times 10^7$

7. Pellet Sintered Density Control

W. A. Ross and W. E. Warden

Development is continuing on a process to achieve densities of 85 and 87% TD for a 25 wt% PuO₂-75 wt% normal UO₂ mixture.

Methyl cellulose was evaluated as a means of sintered density control. One, two, and three wt% methyl cellulose from a 3 wt% solution was added to standard process powder which was dried, then pressed into pellets at pressures between

FIGURE 8.2. Comparison of Void Size Distributions Found by Examination of Microprobe Traces and Photographs

20,000 and 40,000 psi. The pellets were then debound in normal cycle (600 °C) and sintered in an all-metal furnace at 1690 °C. As can be seen in Figure 8.3, the effect of the small addition showed a large decrease in sintered density, and the decrease appears to be nearly linear.

8. Pre-irradiation Characterization for Test Fuels

M. C. J. Carlson

Analytical modelling of test fuel performance requires quantitative input data on the pre-irradiation condition of the fuel. Such data is needed in order to provide a good understanding of the fuel fabrication - fuel structure - fuel performance relationships. To provide characterization data for both fuel performance modelling efforts and evaluation of the fuel characteristics from standard fuel fabrication methods, a program has been developed for detailed characterization of large sintering lots of fuel material prepared for fuel test subassemblies. An outline of the program is shown in Table 8.4. Figure 8.4 shows a schematic flow sheet for sample progression through characterization steps.

TABLE 8.4. Advanced Characterization Outline

I. Density Determinations	IV. Composition Distributions
A. Physical Measurements Density	A. Macrodistributions
B. Lattice Parameter Measurements	1. Uranium
C. Immersion Density Measurements	2. Plutonium
II. Porosity Characterization	3. Oxygen/Metal Ratio
A. Void Size Distributions	4. Impurities
B. Void Spatial Distributions	B. Microdistributions
C. Surface Connected Porosity Determinations	1. Uranium
III. Grain Characterization	2. Plutonium
A. Average Grain Size Determinations	3. Impurities
B. Grain Geometry Characterization	V. Inclusion Characteristics
C. Grain Size Heterogeneity	A. Chemical Content
	B. Size Distribution
	C. Inclusion Frequency
	VI. Examination for Second Phase

FIGURE 8.3. Effect of Methyl Cellulose in Decreasing Density

Neg 701096-2

FIGURE 8.4. Proposed Advanced Characterization Schematic

9. Fuel Performance Prediction and Analysis

R. D. Leggett, R. B. Baker, L. E. Addison,
and D. C. Bullington

Work is in progress directed toward the development of empirically-based and theoretically-based formulations for predicting mixed oxide fuel behavior during irradiation. Detailed analyses of irradiation data are employed to "calibrate" the various mathematical models that best describe fuel performance. The ultimate goal of this task is to develop a comprehensive analytical procedure that can be used to predict fuel performance in LMFBR systems from considerations of fabrication parameters, irradiation conditions, and basic material properties of the fuel and cladding. Both steady-state and transient conditions are being considered.

Programming and upgrading of capabilities have continued with the Hybrid Computer.

A preliminary study was made of the sensitivity of physical properties and heat transfer variables upon the estimated fuel swelling and maximum fuel temperature of a reference FFTF fuel pin operating at 12.2 kW/ft.

Table 8.5 shows the variables used for the reference calculation.

**TABLE 8.5. Reference Data for FFTF Peak Power
Fuel Pin ($Q/L = 12.2$ kW/ft)**

Fuel Pin Length	3.00	ft
Extrapolation Distance	0.625	ft
Maximum Heat Rating	12.2	kW/ft
Clad Thickness	0.015	in.
Coolant Mass Flow Rate	830	lb/hr
Coolant Specific Heat	0.305	Btu/lb-°F
Coolant Film Coefficient	37,500	Btu/ft ² -hr-°F
Fuel-Cladding Heat Trans. Coef.	1,000	Btu/ft ² -hr-°F
Cladding Thermal Conductivity	11.4	Btu/ft-hr-°F

TABLE 8.5. (Contd)

Inlet Coolant Temperature	600	°F
Temperature Columnar Grain Growth	1,700	°C
Equiaxed Grain Growth in Fuel	1,450	°C
Initial Pellet Theoretical Density	0.90	
Final Density Columnar Grains	0.98	
Theoretical Fuel Density	10.71	g/cm ³
Pellet Diameter	0.194	in.
Peak Burnup	120,000	MWd/MT
Gas Release Inner Zone	1.00	
Gas Release Center Zone	0.67	
Gas Release Outer Zone	0.33	
Adsorbed Gas	0.044	cm ³ /g
Effective Plenum Length	36.8	in.
Defects on Inner Clad Surface	0.003	in.
Defect Depth Outer Clad Surface	0.002	in.
Coefficient 1 in a	9.1×10^{-6}	in./in.°F
Coefficient 2 in a	1.25×10^{-9}	
Peak Flux	4.55×10^{15}	n/cm ² sec
External Pressure	32.0	psig
Activation Energy Gas Diffusion	43.2	kcal
Diffusion Coefficient	6.6×10^{-6}	cm ² /sec
Activation Energy for Creep of Matrix	60	kcal
Hydrostatic Pressure in Fuel	500	psi

Swelling is sensitive to the heat transfer between the fuel and cladding for the case examined when the heat transfer coefficient is less than 5000 Btu/ft²-hr-°F.

The sensitivity to the activation energy for gaseous diffusion is a strong function of **burnup**.

Table 8.6 presents a summary of the results of this preliminary sensitivity analysis.

TABLE 8.6. Sensitivity of Unrestrained Pellet Expansion to Variables

Variable	Range	Expected Value	% Change	Sensitivity
• <u>Heat Transfer Variables</u>				
Sodium Heat Trans. Coef.	25,000 to 50,000	32,000	-22 to +56	Verylow
Fuel-Cladding Heat Trans. Coef.	750 to 7,000	1,200	-38 to +480	Sensitive below Hg = 5000
• <u>Fission Gas Diffusion</u>				
Coefficient	6×10^{-6} to 6×10^{-2}	6×10^{-6}	-0 to 4 orders of magnitude	Moderate for $D_0 < 10^{-4}$
Activation Energy	20-60 kcal	42 kcal	-55 to +43	Very sensitive $Q > 40$ kcal at high burnup
• <u>Fuel Plasticity</u>				
Creep Activation Energy	60-110 kcal	(In Pile) 60 kcal	-0 to +84	Moderate - High
• <u>Hydrostatic Pressure</u>				
	500-10,000 psi	2000 psi	-75 to +400	Insensitive
• <u>Fission Rate</u>				
Linear Power	6-16 kW/ft	Peak - 12.2	-50 to +30	Sensitive at low linear power
Pellet Diameter	0.18 to 0.30 in.	0.230	-22 to +30	Moderately sensitive

10. Prequalification of LMFBR Fuel Suppliers

J. F. Williams

Contractors participating in this program have essentially finished their effort except for completion of final reports.

A quality assurance evaluation of the Fuel Supplier Pre-qualification Program is being performed to provide a comparison of:

- Quality Assurance systems and operations versus quality assurance requirements.
- Manufacturing capabilities versus product requirements.

B. CLADDING TECHNOLOGY

1. Vendor Qualification Criteria

J. V. Tverberg, R. J. Lobsinger,
R. C. Aungst and R. R. Studer

Criteria were established for qualification of the FFTF fuel cladding vendors. These criteria are being published in document BNWL-1417. Briefly, this program outlines a four-step program for qualification.

- Step I - Vendor Prerequisites (go-no-go items).
- Step II - Inspection of Facilities (Audit).
- Step III - Evaluate Fabrication Process.
- Step IV - Evaluation of Vendor's Tubes.

Each vendor must meet the criteria in each step to be qualified. If after qualification the vendor makes a significant change in the process, then the vendor must be requalified before he can produce additional tubing for the program. Both the tubing manufacturer and the steel supplier must be qualified before the vendor can receive a bid package for FFTF tubing.

2. LMFBR Fuel and Cladding Information Center

F. R. Shoher and B. R. Hayward

The LMFBR Fuel and Cladding Information Center is a computerized system designed to contain detailed data for LMFBR type experimental fuel pins and selected LMFBR core materials. Similarity in data file structures and the use of the Fortran File Process (FFP) generator provides the flexibility required in the formative stage of the system. This flexibility permits any changes made in one file to be made in another, if appropriate.

The design and operation of the data center system was reviewed and evaluated by a consultant on information systems. His evaluation describes the information center as a system having realistic objectives and goals. He agrees that the modular design and nested file structure also provides for simple operation and projected growth of the files. Some problems related to retrieval and organization are anticipated as the files become larger.

The experimental fuel pin file is operational and data on pin fabrication pre-irradiation measurements are being placed in storage for PNL-3, -4, and -5 subassemblies.

Two additional files will be added to increase the effectiveness of the data center:

- a special pin file which contains all pertinent data on a small group of highly characterized fuel pins being tested by each contractor. These data will be valid, up to date, and certified by management.
- A fuel pin failure file that contains complete details including observations of all failed fuel pin results.

We expect that these files will be used in conjunction with the fuel performance modelling programs. Preliminary outlines of these files have been made.

The austenitic stainless steel tensile file was increased by 116 data sets bringing its total to 2563, the creep file was increased by 74 data sets bringing its total to 321 and data for three lots of stainless steel cladding was entered in storage. About 110 computer plots were prepared from retrievals made from the mechanical properties file.

3. Acceptance Testing and Clad Characterization

M. M. Paxton

The cladding characterization program is being conducted to ensure that high-quality stainless steel cladding is obtained and used throughout the FFTF fuels development and associated irradiation testing programs. Under this program, selected mechanical properties are being determined and analyzed to evaluate fabrication variables and to assist in vendor qualification.

Initial data indicate that planetary ball swaged tubing is weaker than the plug drawn tubing when tested biaxially at 1200 °F for times less than 100 hr. The diametrical strain is significantly less for the planetary swaged tubing. Tests are continuing.

4. Interaction of Cladding Defects

R. C. Aungst, R. J. Lobsinger, R. R. Studer
and J. C. Tverberg

One of the major problems in establishing the wastage allowance on the fuel cladding is in the amount to allow for defects. One major area of concern is the coincidence of both an OD and ID defect. Analysis was made of both situations in which an OD and an ID defect would have an influence on reducing the cladding wall. This analysis indicated that two defects, both at the maximum size of 0.001 in. deep $\times$ 0.030 in. long, one located on the outside and one on the inside of the tubes, can approach within 0.005 in. of each other or 50° and still not influence each other. However, if the two defects approach closer than this, then there will be an interaction between the two defects, and we will have an effective reduction of the cladding wall. Figure 8.5 illustrates this proximity interaction. From this information, and by analyzing these data as a probability function, we have developed a probability equation:

$$p = (n_o) (n_i) (0.00000545)$$

FIGURE 8.5. Schematic Showing Proximity of an OD and an ID Tubing Defect Whereby Combined Defects Will Result in Decreased Effective Wall Thickness

where

n_o = the number of defects on the outer surface

n_i = number of defects on inner surface

0.00000545 = a constant based on geometric consideration.

Data from this equation were plotted on Figure 8.6 where the curve shows that if we have 50 defects on the outside of the tube and 50 defects on the inside of the tube, there will be one chance in 100 of the two defects coinciding. However, with our present defect level (as obtained in the prime lot of CarTech tubing), there is a probability of three chances in 1,000,000 of obtaining coincidence defects or a possibility of one tube in 16 FIR core loadings having this situation develop. Thus, it is safe to assume that as long as the present high standards are maintained, there is no point in increasing wall thickness by another thousandths of an inch to compensate for the possibility of coincident defects.

FIGURE 8.6. Curve Illustrating Probability of Coincidence of OD and ID Defects Based on Schematic Shown in Figure 8.1

C. FUEL PIN TECHNOLOGY

1. Fuel-Pin End-Closure Welding Anomalies

R. M. Crawford, R. D. Aungst, R. J. Lobsinger
and J. C. Tverberg

The FFTF welding standard,⁽¹⁾ requires forty consecutive welds made at three different welding conditions to qualify a welding procedure.

Attempts to qualify a welding procedure (Figure 8.7) for irradiation test pins for PNL-9, 10, and 11 encountered extreme variation in weld penetration. Figure 8.8 shows different levels of penetration in different end-closure welds made by using identical welding parameters and end caps made from the same heat (PNL-Lot-L-15) bar stock. Note that the weld penetration in sample SQ-10 is 0.033 in. while the penetration in SQ-9, made using the same equipment and welding procedure, is ~0.054 in. Similar variations were observed among other samples. To determine if the variation in weld penetration was caused by the end-cap material or weld equipment variation, a fusion pass was run around several of the end caps that had exhibited high and low penetration. In every case, end caps exhibiting low penetration when welded to a tube also exhibited low penetration on the fusion pass. End caps exhibiting high penetration when welded to a tube also exhibited high penetration on the fusion pass.

1. W. F. Brown and R. M. Crawford. Welding Standards for the Driver Fuel Assembly, BNWL-1271, Battelle-Northwest, Richland, Washington, April, 1970.

BATTELLE-NORTHWEST		MELDING PROCEDURE	NUMBER
Project	PNL-9, 10, 11	Spec. No.	BNWL-1271
Process	GIAW	Date	3/9/70
		Drawing No.	H-3-29176
Alloy	316 Vacuum Melt	Prepared by	D. L.
		Approved by	R. M.
Power Supply		Weld Settings	Qualified by
Mfg.	Merrick	Prepurge Time	1 sec
Model	ESR-300	Start Current	16 A
Serial No.	HW22979	Upslope Time	0
Torch		Fixture Delay	0
Mfg.	Weldina Head	Weld Current	16 A
Model	HW-20 Torch	Voltage	14-16
Serial No.	#1	Type/Polarity	DCSP
Cup Size	#4 Gas Screen	Weld Time	14 sec
Shielding Gas		Downslope Time	5 sec
Type	75 He-25A	Finish Current	5 A
Volume	8 CFH	Finish Time	7 sec
Backup Gas		1 Rev Time	12 sec
Type	He Grade "A" Comm.	Weld Speed IPM	~4
Volume	20 CFH	Fixture Speed Dial	45
Electrode		Postpurge Time	11 sec
Type	2% Th	Weld Position	Horizontal
Dia.	1/16 in.	Arc Start Method	High Freq.
Ext.	0.250 in.	Arc Start Intensity	20
Shape	30°		

Sketch

FIGURE 8.7. Welding Procedure for Vacuum-Melted End-Cap Material

BNWL-1394

Sample Number SQ-9

15X

Sample Number SQ-10

15X

Neg PM 701864-17

FIGURE 8.8. Welding Procedure #19, 15A, 12 sec/revolution

An attempt to duplicate the high-penetration weld on end-cap material exhibiting low penetration was made by increasing the welding current. This increased the width of the fusion zone without significantly changing the penetration.

During welding, other differences in the behavior of the material were noted. The material exhibiting low penetration welded "quietly" with a smooth finished bead. The material exhibiting high penetration showed a more active puddle during welding, and the finished weld showed more ripple than welds from low-penetrating material. All the vacuum-melted material tested required more current to weld, had a "quieter" puddle action during welding, and gave a smoother finished weld than the air-melted end-cap material. Samples of material exhibiting high and low penetration were submitted for extensive chemical analysis. Weldability tests run on end-cap material from PNL lots L-14 and L-16 indicated both lots are weldable and acceptable for use. The erratic weld penetration encountered in welding end caps fabricated from Lot L-15 was not found in end cap welds made from Lots L-14 and L-16.

At this time the cause of the variation in welding penetration has not yet been identified.

The vacuum-melted stock in question was supplied as part of a large order of vacuum-melted Type 316 SS. After induction melting, the steel was consumable-electrode, vacuum-arc remelted (CEVAR) into 16 and 18-in. ingots for subsequent reduction. The 4.5 in. diameter bars produced from the 16-in. ingots were used to fabricate 1/4 in. diameter bar stock (Lot L-14), 0.230 in. diameter bar stock (Lot L-15), and 1/4 in. diameter coiled bar (Lot L-16). The L-14 and L-15 lots were received by BNW in as-drawn condition. Subsequently, the material was bright hydrogen annealed; L-14 and L-15 came from different heats and were annealed at different times.

Chemical analyses and qualitative spectrographic scanning indicate it is unlikely that chemical differences can account for the variation in weld penetration.

Slight but real differences in electrical resistivity were observed between these high- and low-penetration samples from Lot L-15.

Metallography to characterize grain size and inclusion distribution is being performed on the samples for L-15 which exhibit welding variations.

2. Fuel-Pin End-Closure Weld Development

R. M. Crawford

New end-cap dimensional tolerances are being considered. Preliminary tests indicate that acceptable welds can be produced with these tolerances, and machining costs will be reduced. The established tolerances (Figure 8.9) are: Dimension T, weld lip thickness 0.017 ± 0.003 in.; Dimension D, weld lip diameter 0.216 ± 0.002 in.; and Dimension C, tube-to-end cap clearance 0.0005 in. to 0.003 in. The following 2^5 factorial experiment is currently in progress to determine the effect of welding parameter variations and machining tolerances on the finished weld:

Variables	Range
Current, A	A-1 = 16 A-2 = 18
Welding Speed	S-1 = 1 revolution in 11 sec S-2 = 1 revolution in 13 sec
Weld Lip Thickness, in.	T-1 = 0.020 T-2 = 0.014
Weld Lip Diameter, in.	D-1 = 0.214 D-2 = 0.218
Clearance Tube-To-End Cap, in.	C-1 = 0.0005 C-2 = 0.003

FIGURE 8. 9. Reference FFTF Fuel Pin End Closure Weld Penetration

			D-1		D-2	
			S-1	S-2	S-1	S-2
A-1	T-1	C-1	1 33	2 34	3 35	4 36
		C-2	5 37	6 38	7 39	8 40
	T-2	C-1	9 41	10 42	11 43	12 44
		C-2	13 45	14 46	15 47	16 48
A-2	T-1	C-1	17 49	18 50	19 51	20 52
		C-2	21 53	22 54	23 55	24 56
	T-2	C-1	25 57	26 58	27 59	28 60
		C-2	29 61	30 62	31 63	32 64

There will be two replications of each combination of levels. The samples will be welded in the following order to minimize the effect of any individual equipment set up:

54, 47, 20, 10, 41, 35, 22, 23, 03, 05, 58, 11, 08, 29, 01, 25, 06, 45, 64, 34, 42, 21, 12, 09, 36, 48, 30, 56, 15, 07, 24, 44, 19, 04, 32, 55, 50, 16, 18, 43, 17, 57, 40, 13, 61, 37, 31, 33, 59, 52, 26, 28, 53, 39, 46, 63, 60, 02, 38, 14, 62, 27, 49, 51.

All samples will be measured for weld buildup, radiographically examined in six positions, and metallographically examined for weld penetration, throat thickness, and cracking.

3. Prototypic FTR Fuel Pin Fabrication Development H. T. Blair

FFTF Engineering Test Plan for Fabrication of FTR Prototypic Fuel Pins, was approved on March 25, 1970. This plan specifies the general requirements for the development

fabrication of 300 prototypic FIR fuel pins (Figure 8.10) in the fuel fabrication demonstration facility. These fuel pins will be evaluated to preliminary FIR specifications.

D. FUEL SUBASSEMBLY TECHNOLOGY

1. CCTL Mark II Test Assembly Fabrication Development*

R. L. Myers

Discussion

A full-scale model of an FFTF Mark II fuel subassembly was built and shipped to the Argonne National Laboratory for flow testing in the Core Components Test Loop. The design for this subassembly is prototypic of the FFTF driver fuel subassembly, except for some deviations which have been allowed to either simulate reactor operating conditions, allow easier fabrication, or shorten the fabrication schedule. Standard fabrication techniques were used except where new designs made it necessary to develop new procedures or alter established ones. Significant development work included setting new parameters for bottom end cap welding, establishing a vertical loading procedure for a small clad-pellet gap, developing a welding technique for the lower grid plates, and developing duct-nosepiece components welding procedures. A quality assurance program was used to verify conformance of each component and the completed subassembly to design requirements and document all deviations.

Fuel Pin

The fuel pellets were made using natural UO_2 in place of $\text{UO}_2\text{-PuO}_2$ since the CCTL flow test is an out-of-reactor test. The insulator pellet lengths were made identical to those of the fuel pellets, resulting in a column length of 37-in. and simulating the combined fuel column and insulator pellets. Considering allowable tolerances, the pellet-clad gap is expected to range from 0.0021 to 0.0032 in. The pellet lengths and diameters were controlled using a sampling inspection plan.

* Also see Item VI.B.3 this report

Neg 702158-1

FIGURE 8.10. Prototypic FTR Fuel Pin for Test Demonstration Fabrication

The close pellet-clad gap made it necessary to load the pins in the vertical position. The new loading procedure was developed. The fuel column was weighed and loaded in 6-in. increments. Each 6-in. increment was seated to eliminate any gaps between pellets (Figures 8.11 and 8.12).

Each pin was wrapped with a 0.056-in. diameter Type 316 SS 20% cold-work wire (Figures 8.13 and 8.14). The wire was attached at each end cap with a 3/32-in. fillet weld.

A summary of fuel pin inspections and acceptance rates is listed in Table 8.7.

TABLE 8.7. Fuel Pin Inspection Acceptance Rates

<u>Type of Inspection</u>	<u>Acceptance Rates, %</u>
Radiography	
Bottom End Cap Weld	90.5
Top End Cap Weld	95.0
Full Length	100.0
Wire Wrap Weld	97.4
Helium Leak Check	100.0
Loading	97.0
Overall	81.4

Pin Bundle

The lower grid assembly consists of the lower grid frame, 17 grid plates, and 3 support bars. The fabrication of the plates required a development program to select a method for welding the round rails to the plates. An electron beam welding technique was selected, the weld parameters were determined and sample welds (Figure 8.15) were tested for strength and quality.

Neg 700273-9

FIGURE 8.11 Fuel Pellet Loading Station

Neg 700273-11

FIGURE 8.12. Fuel Pellet Seating

Neg 700611-7

FIGURE 8.13. Wire Wrap Machine

Neg 700611-8

FIGURE 8.14. Removal of Fuel Pin from Wire Wrap Machine

Neg 069-5165-1

FIGURE 8.15. Lower Grid Plate Electron Beam Weld

The fuel pins were assembled into strip layers by pinning the bottom end caps to the lower grid plates (Figure 8.16') and these strip layers were then assembled into a fuel bundle (Figure 8.17). Thirty of the characterized pins were assembled in predetermined locations in the bundle. Seven extra characterized pins were randomly located throughout the bundle.

The top grid assembly (Figure 8.18) was welded together and put in place on the hexagonal array of pins, a process which proved to be long and tedious due to the problems of lining up all 217 pins.

Subassembly

The hexagonal flow duct was expanded 0.054 in. beyond the actual tight bundle dimension. This provides for duct-bundle gap that is anticipated during reactor operation.

A development program using sample pieces to determine welding techniques and parameters was completed prior to welding the actual test assembly nosepiece (Figure 8.19).

Argonne National Laboratory furnished four components of the subassembly--the spherical seal, two guide rings, and the top flange.

The completed test assembly is shown in Figure 8.20.

Quality Assurance

A quality assurance program was used to verify conformance of each component to design requirements and document any deviations. All materials used were certified and traceability was maintained through the fabrication, inspection, and assembly processes. All components fabricated or assembled outside of the FFTF Fuels Department were inspected for conformance to design specifications. All nonconforming items were either refabricated, reworked, or approved in the as-built condition by waiver at Technical Review Board meetings. All design

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FIGURE 8.16 Strip Layer Assembly

Neg 700611-6

FIGURE 8.17. Pin Bundle Assembly

Neg 700611-3

FIGURE 8.18. Top Grid Assembly

Neg 700611-1

FIGURE 8.19. Nose Piece Assembly

Neg 700760-5

FIGURE 8.20 Completed Subassembly

deviations and inspection data are recorded and will be kept for comparison with post-test inspection data. An archive sample of each component part, where possible, and two complete fuel pins are being kept for similar comparisons.

Fuel pin follower cards were generated for each pin at the beginning of assembly recording all of the assembly processes and required inspection data. Rejected pins were separated from the acceptable pins which continued through the assembly process.

2. CCTL Mark II Subassembly Compression Test

D. E. Blahnik and R. B. Baker

The purpose of compression testing the CCTL Mark II subassembly was to determine the springiness* that existed within the subassembly when it was compressed symmetrically from a nominal internal duct size down to beyond a tight subassembly size.**

* *Subassembly Springiness - When a fuel pin subassembly, such as Mark II, is in the vertical position, the fuel pins cannot bow significantly towards the center. Therefore, most of the outer pins are randomly forced outward by a combination of their own natural bow and the forces transmitted by the natural bow of internal pins. When a bundle is compressed to a tight subassembly size,** all of the natural spring is absorbed. If a subassembly is compressed beyond tight subassembly size, the fuel pins react as a combination of leaf and torsion springs.*

** *Tight Subassembly Size - This is the point at which all fuel pins and wires are compressed tightly together without any deformation of the subassembly components. There is no outward displacement due to natural bow of the fuel pins and no gaps between wires and fuel pins. When a subassembly is compressed beyond this point, the load-compression deflection curve becomes linear. This term was previously referred to as "bundle theoretical tightness."*

Test apparatus pressure plates (simulating duct walls) were advanced inward against the subassembly in equal increments, maintaining a regular hexagon geometry over a 4-ft span of the subassembly. The spring load on the simulated duct was measured as a function of various across-the-flat dimensions. Figure 8.21 shows a typical curve which can be plotted from subassembly compression test data.

The data is used to:

- Characterize the subassembly's tight size and springiness.
- Analyze the in-reactor cases both of the fuel pins swelling larger than the duct dimensions and vice versa.
- Assist in establishing dimensions and tolerances on the fuel pin OD, wire OD, and the duct inner dimensions to assure proper assembly fit for reactor requirements.

Figure 8.22 shows the compression test apparatus which was used to compress both the CCIL Mark I and Mark II subassemblies. Subassemblies are compressed in the vertical position to minimize the effect of gravity. One side of the test apparatus has a 2-ft load measurement section so that during subassembly compression or decompression the side load can be measured as a function of hexagonal size.

Figure 8.23 illustrates a summary of the CCIL Mark II compression tests results.

Figure 8.24 compares the summary of test results for Mark II to those for Mark I.

FIGURE 8.21. Typical Subassembly Compression Test Data Curve

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FIGURE 8.22. Subassembly Compression Test Apparatus and Instrumentation

FIGURE: 8.2 3. Summary of Mark II Compressibility Tests Run Numbers: One, Two and Three Average Range

FIGURE 8.24. Summary Comparison of Mark II and Mark I Sub-assembly Compressibility Test Run Curves

CHAPTER IX. PHYSICSA. CORE PHYSICS1. Dependence of the Diffusion-Transport Correction on the Reactor Composition and the Number of Energy Groups

R. W. Hardie

A series of one-dimensional (spherical geometry) runs were made to determine if the difference in eigenvalue between diffusion and transport S_n calculations is strongly dependent on either the reactor composition or the number of energy groups. A description of the four reactors studied is given in Table 9.1.

TABLE 9.1. Reactor Compositions and Dimensions

Item	Reactor 1		Reactor 2		Reactor 3		Reactor 4	
	Core	Refl.	Core	Refl.	Core	Refl.	Core	Refl.
^{239}Pu	0.00394	0.0	0.00368	0.0	0.00151	0.0	0.00164	0.0
^{238}U	0.005	0.0	0.032	0.041	0.005	0.0	0.0075	0.040
O	0.010	0.0	0.0	0.0	0.010	0.0	0.021	0.0
Na	0.010	0.005	0.0	0.0	0.010	0.005	0.006	0.0
Fe	0.020	0.050	0.011	0.005	0.020	0.050	0.014	0.006
Outer Radius (cm)	25.0	55.0	25.0	55.0	50.0	80.0	50.0	80.0

Using a two-and a five-energy group cross-section set, the diffusion and transport theory eigenvalues were calculated by using 1DX and DTF-IV, respectively. The ratio of the transport theory eigenvalue to the diffusion theory eigenvalue is given in Table 9.2.

TABLE 9.2. Ratio of Transport Theory Eigenvalue to Diffusion Theory Eigenvalue

Number of Groups in Cross-Section Set	Reactor 1	Reactor 2	Reactor 3	Reactor 4
2	1.0339	1.0069	1.0093	1.0034
5	1.0401	1.0117	1.0100	1.0051

Two observations can be made from Table 9.2. First, the diffusion-transport difference can vary widely for two reactors of the same size with different compositions. Second, a very small number of groups is not sufficient for accurately calculating the difference between diffusion and transport theory.

2. FFTF Critical Experiments

a. Program

R. A. Bennett

Measurements of radial and axial reaction rate distributions were completed in this reporting period. The FTR-3 assembly is now being prepared for measurements of the absolute fission ratios at the core center.

A series of tantalum peripheral control rod experiments is being planned for addition to the FTR-3 program. Tentatively, the experiments will yield experimental reactivity worths of

- 8 B₄C rods in the presence of 8 B₄C rods.
- 8 Ta rods in the presence of 8 B₄C rods.
- 8 Ta rods alone.
- 8 B₄C rods closed.

It is estimated that these experiments will require approximately 2 weeks of ZPR-9 reactor time.

Additional experiments are to be done using the ORNL noise and source multiplication techniques for subcritical reactivity determinations. These experiments should require less than 2 additional weeks of ZPR-9 reactor time. Hence, the FTR-3 program is now expected to be completed by mid-October 1970.

b. The Effect of Heterogeneity on the Worth Calculation of the FTR-2 B₄C Control Ring

R. P. Omberg, J. V. Nelson and R. A. Bennett

A number of calculations were made to determine the effect of a heterogeneous-versus-homogeneous representation of the FTR-2 control ring on its calculated worth. The ring is actually composed of alternating layers of B₄C and sodium (four B₄C plates and five sodium plates) previously represented in FTR-2 calculations by a homogeneous mixture. The calculations to determine heterogeneity effect were performed in one-dimensional cylindrical geometry with both diffusion theory and S-8 transport theory. The control ring was simulated by five annular sodium rings interspaced with four annular B₄C rings. This annular arrangement is not exact since some of the B₄C plates in the physical core were not perpendicular to a radius vector. However, the approximation greatly simplifies the calculation since it then becomes one-dimensional. The diffusion and transport multiplication constants are listed in Table 9.3.

TABLE 9.3. Effective Multiplication Constant

	<u>Heterogeneous</u>	<u>Homogeneous</u>
Transport	0.99879	0.99874
Diffusion	0.99285	0.99319

The differences in k_{eff} are evident in Table 9.4.

TABLE 9.4. Changes in the Multiplication Constant

	<u>Δk (het minus hom)</u>
Transport	+0.00005
Diffusion	-0.00034
	<u>Δk (trans minus diff)</u>
Heterogeneous	+0.00594
Homogeneous	+0.00555

The differences in k_{eff} between the homogeneous and heterogeneous calculations are small.

A second set of calculations was made to determine the effect of removing individual B_4C rings and thus to indicate the interaction between rings and also their relative worth. The second and third rings* were removed individually and then together. When a ring was removed, the space was filled with a sodium-stainless steel mixture. The transport and diffusion multiplication constants are listed below.

<u>Control Ring</u>	<u>k_{tr}</u>	<u>k_{diff}</u>
4 het B_4C rings in	0.99879	0.99285
2nd B_4C ring removed	1.00312	0.99730
3rd B_4C ring removed	1.00176	0.99592
2nd and 3rd B_4C ring removed	1.00768	1.00198

The following worths can be calculated from the above numbers.

<u>Problem</u>	<u>Δk_{tr}</u>	<u>Δk_{diff}</u>
2nd ring removed	0.00433	0.00445
3rd ring removed	0.00297	0.00307
sum of 2nd and 3rd ring	0.00730	0.00752
2nd and 3rd ring removed	0.00889	0.00913

Two points are illustrated here. First, the worth of B_4C decreases rapidly with increasing radius through the second and third rings, as indicated by the large differences between them. This suggests that the agreement between the homogeneous and the heterogeneous ring results could be fortuitous since homogenizing involves transporting B_4C . To resolve this, the worth of B_4C through the ring will have to be investigated in more detail. Second, the interaction between rings is

* The ring closest to the core is considered the first ring.

quite large as indicated by the difference between the sum of the worths of the second and third rings removed individually and by the second and third rings removed together.

c. Inferred Experimental Worth of the 'FTR-2 Control Ring
R. P. Omberg and R. A. Bennett

One of the principal objectives of Critical Assembly FTR-2 was to measure the reactivity worth of large segments of a peripheral control ring located at the core-reflector interface. A comparison of the calculated and measured worths of the individual segments has been made.⁽¹⁾ As a further check on the correlation between calculation and experiments it would be desirable to compare a calculated and experimental worth for the entire ring. Unfortunately, the experimental worth of the complete ring is not the sum of the worth of its individual segments since B₄C and fissile material had to be removed in alternating steps, and the interaction between them cannot be disregarded. Thus, a different method for inferring the worth of the entire ring had to be developed.

This method consisted of noting the difference between calculation and experiment in Assembly FTR-1, an assembly which is very similar to FTR-2 with its control ring deleted, and then using this difference to correct the calculated k_{eff} for FTR-2 with no control ring. The resulting k_{eff} was called an inferred experimental value. The inferred experimental worth for the ring is then obtained by subtracting the experimental k_{eff} of FTR-2 with the control ring. This is shown in Table 9.5.

1. Trans. American Nuclear Society, 1970 Summer Meeting, Los Angeles, to be presented.

TABLE 9.5 Inferred Experimental Worth of
the FTR-2 Control Ring

FTR-1:	$k_{\text{eff}} = 0.99711$ calculated (c)
	$k_{\text{eff}} = 1.00136$ experimental (e)
	c-e = -4.25 mk
FTR-2:	$k_{\text{eff}} = 1.05634$ calculated, without control ring
	$k_{\text{eff}} = 1.06059$ inferred experimental value with- out control ring
	$k_{\text{eff}} = 1.00098$ measured, with control ring
	Inferred experimental worth of the control ring
	= 59.6 mk or
	$\rho = 5.62\%$
	Calculated worth of the control ring
	= 48.7 mk or
	$\rho = 4.64\% \Delta k/k$
	c/e = 0.82

It can be seen that the inferred experimental worth of the ring is 59.6 mk or $\rho = 5.62\% \Delta k/k$. The ratio of calculation to experiment is 0.82. This c/e is somewhat smaller than average c/e of 0.9 obtained for the individual segments of the ring;⁽¹⁾ however, it does fall within 0.9 ± 0.1 which was found to be the range of the individual c/e's for the ring segments. It is interesting to note that ANL obtained an inferred experimental worth of the ring of approximately 5-1/2% $\Delta k/k$ by a completely independent method.⁽²⁾ If the interaction between the depleted zone and the B₄C ring is

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1. *Trans. American Nuclear Society, 1970 Summer Meeting, Los Angeles, to be presented.*
 2. A. Travelli, A. J. Ulrich, and J. C. Beitel. "Analysis of Reactivity Changes Associated with Large Perturbations in ZPPR/FTR-2," Trans. Am. Nucl. Soc., 1970 Summer Meeting, Los Angeles, to be presented.

disregarded and the individual B_4C exchanges simply added, the ring is found to be worth 5.8% $\Delta k/k$. Thus, the interaction effect is about 0.2% $\Delta k/k$.

d. Control Rod Heterogeneity

S. L. Engstrom and R. A. Bennett

Rectangular bars of boron carbide used in the FTR Critical Experiments to represent control rods of interest, are usually arranged in patterns that require extremely detailed two-dimensional transport calculations in order to represent them precisely. In fact, as much as is possible, diffusion theory homogeneous approximations of the rods are made for calculations. This is known to introduce errors in the calculations.⁽¹⁾ As a prelude to a detailed two-dimensional transport analyses of actual rod configurations, a series of comparisons of cylindrical calculations of rod worths have been made.

A single cylindrical control rod, of 3.12 cm diameter, was represented at the center of a cylindrical reactor closely approximating the early FTR Critical, ZPR-III, Assembly 51.

Four B_4C arrangements compared were:

- an homogeneous cylinder of B_4C -Na-SS. $R = 3.12$ cm.
- a small ring of B_4C , $IR = 1.43$ cm, $OR = 2.02$ cm, imbedded in an homogeneous cylinder of sodium and stainless steel, $OR = 3.12$ cm.
- a large ring of B_4C , $IR = 2.78$ cm, $OR = 3.12$ cm, surrounding an homogeneous cylinder of sodium and stainless steel.
- a solid B_4C cylinder $R = 1.43$ cm, centered in an homogeneous cylinder of sodium and stainless steel, $R = 3.12$ cm.

1. R. A. Bennett and S. L. DeMyer. Analysis of FTR Phase A Control Rod Experiments in ZPR-111, Assembly 48 and 48A, BNWL-967, Richland, Washington, April 1969.

For each physical configuration, the mass of B_4C , sodium, and stainless steel was identically the same. Three enrichments of ^{10}B in boron were used to comprise a set of worths. In Table 9.6 the % difference in worth given by the exact transport model, and alternative models are given for each rod configuration.

TABLE 9.6. Percent Difference, Approximations Versus Precise Transport Model

Enrichment	Approximate Model	Rod Configuration		
		Solid B_4C Cylinder	Small Ring	Large Ring
Natural (19.2% ^{10}B)	Transport Homog.	+6.22	+1.19	-3.03
	Diffusion Homog.	+6.02	+0.99	-3.22
	Diffusion Hetero.	+3.32	+1.58	0.00
Intermediate (56.1% ^{10}B)	Transport Homog.	+19.0	+5.26	-2.78
	Diffusion Homog.	+21.2	+7.27	-0.93
	Diffusion Hetero.	+11.6	+4.76	+2.31
Highly Enriched (90.7% ^{10}B)	Transport Homog.	+19.7	+7.69	-2.67
	Diffusion Homog.	+26.7	+11.2	+0.53
	Diffusion Hetero.	+10.5	+7.25	+3.74

To illustrate the contents of Table 9.6, the homogeneous diffusion approximation of the solid, highly-enriched B_4C cylinder overestimates the rod worth by 26.7%.

In general, these calculations illustrate that the sign of the disagreement between the worths given by the approximate models depends upon the extent of the moderating material internal to the rod and upon the degree of enrichment. One may make at least two conclusions:

- Calculated worths of FTR enriched control rod designs should be determined with transport theory using at least approximate physical models; that is, B_4C annuli to represent hexagonal arrays of rods.
- a Rod configurations used in critical experiments should be evaluated in detail with good transport approximations. The latter was illustrated in the analysis of ZPR-III 48 central rod experiments.(1)

1. R. A. Bennett and S. L. DeMyer. Analysis of FTR Phase A Control Rod Experiments in ZPR-III, Assembly 48 and 43A, BNWL-967, Richland, Washington, April 1969.

e. Effect of a Radial Shield on Peripheral Control Worth in FTR-3

3. V. Nelson

A series of 2DBS X-Y quarter core calculations in 13 groups was run to determine the change in peripheral control rod worth induced by the addition of a radial shield to FTR-3. The worth of four B_4C control clusters, each consisting of four columns, was calculated for two different shield thicknesses as well as for no shield present. One shield was approximately four drawers thick, containing 436 columns of shield material while the other was about nine drawers thick and contained 1060 columns. In both cases, the composition of the shield was 85 vol% Type 304 SS and 15 vol% sodium. The results of these calculations are shown in Table 9.7.

TABLE 9.7. **Pheripheral** Control Worth Versus Shield Thickness in FTR-3

<u>Shield Thickness (Drawers)</u>	<u>Four Control Clusters</u>		<u>Worth of Four Clusters (Δk)</u>	<u>% Change in Worth</u>
	<u>In (k_{eff})</u>	<u>Out (k_{eff})</u>		
0	0.98347	0.99125	0.00778	0
4	0.98465	0.99274	0.00809	3.98
9	0.98456	0.99287	0.00831	6.81

g. Effect on Reactivity of Changes in Atom Densities

J. V. Nelson

A series of calculations was performed to determine the reactivity change in the Engineering Mockup as the result of sequentially altering the atom densities of ^{239}Pu , ^{238}U , sodium, and ^{16}O in the inner and outer drawer zones. The material compositions of all zones of the model were initially the desired values. Then, the atom densities of each of the four isotopes listed above were changed one at a time from desired to anticipated values in both driver zones. All

calculations were made using 2DBS in the X-Y mode with a 13-group cross-section set. The reactivity changes at each step are listed in Table 9.8 with the corresponding mass changes used in these calculations from Reference 1.

TABLE 9.8. Reactivity Changes in Engineering Mockup Caused by Differences Between Desired and Anticipated Atom Densities

Run	k_{eff}	Δk	AM, kg	
			Dr I	Dr II
Base Case	0.99388	-	-	-
$^{239}\text{Pu}(\text{a})$	0.98421	-0.00967	-9.85	9.25
^{239}Pu and $^{238}\text{U}(\text{a})$ }	0.97934	0.00467	53.71	60.08
^{239}Pu , ^{238}U and sodium (a) }	0.97950	-0.00004	-10.60	3.62
^{239}Pu , ^{238}U , sodium and $^{16}\text{O}(\text{a})$ }	0.97829	-0.00121	0.69	-16.88

a. *Changed to Anticipated Densities*

3. Anomaly Detection in a Reactor at Power

James R. Sheff

Measurement of neutron and other variable fluctuations in a reactor at full power is useful in seeking reactor anomalies. Theories that describe the full-power fluctuations of the neutron population and reactor temperatures have been developed here⁽¹⁾ and in Japan.⁽²⁾ Both models provide a method for

1. James R. Sheff. "Neutron Fluctuations at Full Power," Japan-United States Seminar on Nuclear Reactor Noise Analysis, 2nd-7th September 1968, Tokyo and Kyoto.
2. K. Saito and Y. Tajiri. "Theoretical Study of At-Power Reactor Noise," Japan-United States Seminar on Nuclear Reactor Noise Analysis, 2nd-7th September 1968, Tokyo and Kyoto.

predicting the frequency dependence of the auto and cross-spectral density functions for these variables. The timely discovery of anomalies such as coolant flow blockage and vibrations of reactor components may be possible through measurements of neutron, temperature, sodium flow, and system pressure fluctuations.

Development of a means to apply these theories to FTR is underway. The models developed here have been programmed⁽¹⁾ for use on the Univac 1108. The digital computer program, NESAP2,⁽²⁾ has been obtained and converted for use on the Univac 1108 as a part of the FTR program for evaluation of these methods. A comparison of the two formulations using the two computer codes is planned as a partial check upon the correctness of both.

4. Reactivity Effects of Incoherent Sodium Voiding Within a Ring of Subassemblies

A. E. Waltar

Although the MELT-II neutronics-heat transfer computer program explicitly allows for core radial and axial incoherence reactivity feedback induced by coolant voiding, incoherencies within a radial channel could be quite significant for some cases. Such incoherency could be caused by differences in power density and pin burnup.

In a first attempt to establish the importance of this "third dimension" effect on reactivity feedback induced by voiding, a hypothetical pipe rupture accident which was

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1. James R. Sheff. User's Manual for NOISY1 - A Program for Calculation of Space Dependent Spectral Densities in Cubical Reactors, BNWL-1260, Battelle-Northwest, Richland, Washington, 1970.
 2. K. Saito. NESAP2, A Fortran IV Code for Calculation of Noise Spectra in At-Power Reactors, JAERI-2186, Atomic Energy Research Institute, Tokyo, Japan, May 1969.

previously investigated for the case of a high sodium void worth was re-evaluated. In the original case (Case A), the six rings of subassemblies were modelled by assuming that a single pin represented all pins within a given ring. Furthermore, goal exposure was assumed throughout the core, and voiding was assumed to result from the release of fission gas at 900 psi through the failed cladding at the top of the core. Rings 4 and 5 began to void almost simultaneously, and because of the large volume of sodium contained in these two rings, the reactivity ramp rate at the time of disassembly was very large.

In the present study (Case B), Rings 4 and 5 were each computatively divided into three equal volume channels to depict three possible power ratings and fission gas driving pressures with each subassembly ring. Since the subassemblies in the corner of the hexagonal core array operate at a relatively low flux level, it was assumed that these positions would be charged with fresh fuel to flatten the core power level whereas subassemblies in the "flats" would remain for higher burnups. Hence, the relative power ratings and fission gas plenum pressures were assumed as follows:

<u>Burnup</u>	<u>Fission Gas Pressure, psi</u>	<u>Relative Power Change, %</u>
High	800	+5
Medium	500	0
Low	200	-5

Figure 9.1 illustrates the resulting comparison of reactivity ramp rates caused by sodium expulsion over the time that spans the voiding of subassembly Rings 3 through 5. The numbers in the circle identify the times at which various channels begin to void; for example, 5-1 indicates that the hottest third of the subassemblies in Ring 5 began to void at

FIGURE 9.1. Reactivity Ramp Rate Variations as a Function of Voiding Incoherences Within a Radial Channel

1.762 sec after the start of the accident for Case B. As evident, the explicit treatment of incoherencies within a ring results in a smearing out of the reactivity consequences, and the ramp rate at the time of disassembly is much lower than for Case A. However, the assumptions required which determine the basis for incoherencies within a subassembly ring are numerous, and the particular assumptions made certainly will alter the resulting time sequence for voiding. Hence, one can probably generalize that taking account of such incoherence will smear out the reactivity effect and lead to the calculation of lower energy releases, but the degree of such reduction may be quite case dependent.

5. Critical Approach and Exponential Experiments with 25.2 wt% PuO₂-U (Nat.) O₂ Rods in Water

C. L. Brown, L. C. Davenport, and C. A. Rogers

The final data reduction and analysis of a series of critical approach and exponential experiments with 25.2 wt% PuO₂-U (Nat.) O₂ fuel rods in water was completed. The fuel column in each pin was 0.195 in. diameter × 27.25 in. long; the cladding was 0.015 in. thick Type 316 SS. The plutonium contained 11.88 wt% ²⁴⁰Pu and 1.55 wt% ²⁴¹Pu. The critical number of pins, material buckling, and extrapolation distance was measured for seven triangular pitch lattice spacings. Summary results are given in Table 9.9. These data will provide an essential part of the criticality safety basis for the handling, storage, and transportation of LMFBR fuels. A final report is in preparation.

Critical approach and exponential experiments are in progress with the above described 25.2 wt% PuO₂-U (Nat.) O₂ fuel pins in water containing boron. The objective of these experiments is to establish the boron concentrations necessary to reduce k_{∞} to unity for the 0.6 in. and 0.9 in. lattices. These are the lattices for maximum material buckling and minimum critical mass, respectively. These measurements are also

needed to provide check points for calculational methods. These data will be needed for future criticality safety evaluations involving FFTF fuel reprocessing plants and FFTF fuel transportation. Tentative results obtained from the critical approach experiments are given in Table 9.10. Exponential experiments with 0.6 in. lattice poisoned with 350 ppm ^{10}B are currently in progress.

TABLE 9.9. Results of Critical Approach Experiments with 25.2 wt% PuO_2 -U (Nat.) O_2 Fuel Rods for Water

(11.88 wt% ^{240}Pu)		
<u>Lattice Pitch, in.</u>	<u>Number of Pins for Criticality</u>	<u>Critical Mass, kg Pu</u>
0.450	41.0 $\pm$ 5	12.8
0.600	219.2 $\pm$ 0.6	6.74
0.779	156.2 $\pm$ 0.8	4.80
0.900	149.8 $\pm$ 0.6	4.61
1.039	165.0 $\pm$ 0.2	5.07
1.200	232.6 $\pm$ 0.1	7.15
1.350	410 $\pm$ 10	12.6

TABLE 9.10. Tentative Results of Critical Approach Experiments with 25.2 wt% PuO_2 - UO_2 Fuel Pins in Water Containing Boron

(11.88 wt% ^{240}Pu)			
<u>Lattice Pitch, in.</u>	<u>^{10}B Concentration, ppm</u>	<u>Estimated Critical Number of Fuel Pins</u>	<u>Measured Critical Number of Fuel Pins</u>
0.600	180	275	334
0.900	133	283	322

B. RADIATION AND SHIELDING

1. Fission Product Decay Heat

D. R. Marr

Emergency cooling requirements after the first few seconds are primarily determined by the fission product decay heat. An extensive literature search was made to determine the total sensible decay energy available from the fast fission of ^{239}Pu and ^{238}U , since these values establish the decay heat rate at shutdown. Based on the limited experimental evidence available for fast fission of these isotopes and on a consideration of the energies available from other fissile isotopes at other energies, values of 14.0 and 18.7 MeV per fission were determined for ^{239}Pu and ^{238}U , respectively. These values establish the shutdown decay power for FIR goal exposure fuel at 6.6% of equilibrium power after 1 sec, 4.8% after 10 sec, 35% after 100 sec, 1.1% after 1 hr, and 0.42% after 24 hr.

For verification of computations, the summation method of calculating decay heat, proved at longer decay times, was extended to the first few seconds of decay by including all the short-lived isotopes that are predicted to occur in the fast fission of either ^{239}Pu or ^{238}U . Energies and half lives for these isotopes were empirically determined. The energies give the correct total decay energy release per fission, while the half lives provide an adequate description of the time dependence of the gamma power measured by Fisher and Engle, (1) as seen in Figure 9.2. Although the experimental exposure was of a burst nature, the agreement provides confidence in calculations of the FIR decay power at short times after shutdown.

1. P. C. Fisher and L. B. Engle. "Delayed Gammas from Fast Neutron Fission of ^{232}Th , ^{233}U , ^{238}U , and ^{239}Pu ," Phys. Rev., 134 4B, 1964.

FIGURE 9.2. Decay Gamma Power After a Fast Neutron Burst in ^{239}Pu

2. Na and Mn Disintegration Rate Distributions in the ZPPR
E. T. Boulette

Disintegration rate distributions of $\text{Na}(n,\gamma)$ and $\text{Mn}(n,\gamma)$ were measured in the ZPPR, Assembly FTR-2, to provide a partial evaluation basis for the FTR conceptual shield design. Two-dimensional diffusion theory calculations made with 2DBS were compared to the experimental results on an absolute basis. The nuclear data used in the calculations were extracted from the 31-group HOMSET transport-corrected, cross-section library.

The comparison of the $\text{Mn}(n,\gamma)$ results showed agreement comparable to those previously found for the $\text{B}(n,\gamma)$, $^{239}\text{Pu}(n,f)$ and $^{238}\text{U}(n,f)$ reaction rate distributions. The $\text{Na}(n,\gamma)$ comparison, however, indicated a significant difference in the shapes of the experimental and calculated distributions. The calculation was significantly biased in the core, where the spectrum is relatively hard. Because of this, the sodium capture cross-sections in the high-energy region were empirically adjusted, and new flux calculations were made. These adjusted cross-sections significantly improved the agreement for the $\text{Na}(n,\gamma)$ results without adversely affecting the agreement of the other reaction rate distributions.

3. ZPPR Stored Fuel Analysis
E. T. Boulette

The ZPPR shield experiments included fission rate measurements through the radial shield segment, both with and without stored fuel in the shield. Rigorous treatment of these experiments requires three-dimensional calculations, and these are not presently practical. Two-dimensional calculations were made with 2DBS in X-Y and R-Z geometry. The X-Y calculations rigorously include the finite transverse dimensions of the fuel but not the length; whereas, the R-Z calculations must

include consideration of a full ring of fuel even though accounting for the finite length. Calculated fission rate distributions for ^{238}U are compared in Figures 9.3 and 9.4 to the experimental results. Differences between the calculations are attributed to the geometrical limitations in incorporating the stored fuel. The X-Y calculation was assumed to be valid at the reactor midplane. At other elevations, spatially dependent correction factors were calculated by taking the ratio of the X-Y result to the R-Z midplane result and adjusting the R-Z values at all elevations by the corresponding ratio. The corrected values for $^{238}\text{U}(n,f)$ are in reasonably good agreement with the experiment; however, further analyses are needed to treat the other reaction rate distributions.

4. ZPPR Gamma Ray Shielding Experiments

E. T. Boulette and W. L. Bunch

Thermoluminescent dosimeters (TLD's) were irradiated in the ZPPR/FTR-2 configuration to obtain gamma flux distributions for use in evaluating the FTR conceptual shield design. Gamma transport calculations were made in one-dimensional cylindrical geometry by using the multigroup discrete-ordinates code ANISN in an 18-group, S_8P_3 approximation. Preliminary calculations were made with a 10-energy group approximation; however, this was found to underestimate the gamma flux in the core region. The present 18-group calculations include a much finer energy group structure below 1 MeV, resulting in better agreement in the experimental data.

The results of the calculations are depicted in Figures 9.5 and 9.6, together with the measured gamma dose values. The calculated curves are based on an integrated irradiation of 750 W/hr. Figure 9.5 represents the comparison at the ZPPR/FTR-2 core midplane, with stored fuel in the radial shield; whereas, Figure 9.6 is a comparison at the

FIGURE 9.3. $^{238}\text{U}(n,f)$ Reaction Rate Distribution--R-Z Calculation

FIGURE 9.4. $^{238}\text{U}(n,f)$ Reaction Rate Distribution--X-Y Calculation

FIGURE 9.5. Midplane Radial Gamma Distribution in ZPPR/FTR-2 with Stored Fuel Present

FIGURE 9.6 . Midplane Radial Gamma Distribution in ZPPR/FTR-2 Without Stored Fuel Present

midplane with no stored fuel in the shield. The indicated agreement is believed adequate to permit use of these techniques and data for preliminary design of the FTR.

5. Decay Heat Measurements

W. L. Bunch

Calorimetric measurements are being obtained of the fission product decay heat associated with the Phenix (plutonium) fuel irradiated in MTR. Two data points were obtained during the first week following shutdown of the reactor. Subsequent data points are being obtained about every 10 days. Calibration data are being obtained using electric heaters. It is estimated that results will be valid to within about 10%. The measurements and data reduction are being carried out by Idaho Nuclear Corporation.

6. Shield Development Program

W. L. Bunch

The experimental shield development program to be carried out by ORNL in support of the FFTF project during FY-1971 has been defined. The program is to investigate attenuation characteristics of the FIR cover, including effects of penetrations. In addition, measurements are to be made of lower axial shield mockups. The sodium, iron, steel, and nickel slabs employed in the STIR shielding experiments are being shipped to the Tower Shield Facility for use in this program.

CHAPTER X. SAFETYA. PLANT SAFETY ANALYSIS STUDIES1. Theoretical Work Energy for Core Disassembly Accidents

A. Padilla

A transient thermodynamic model, SOCOOL-II, has been developed to predict the work energy which may be generated in a hypothetical reactor core disassembly accident. After a core disassembly accident, part of the energy which has been deposited in core materials may be released in a form capable of doing mechanical work. The most efficient process for generating this mechanical work energy is to transfer the energy stored in the molten fuel at the end of disassembly to the sodium coolant and allow the sodium to expand against the system. The SOCOOL-II code computes the rate of energy transfer and pressure generation in a molten fuel-sodium mixture and evaluates the resulting acoustic and inertial work energy.

The geometry assumed for the analytical model is shown in Figure 10.1,. Spherical fuel particles are assumed to be uniformly dispersed in a sodium-fuel region located at a given distance from the nearest free surface of a sodium pool. The temperature and pressure within the sodium-fuel region are assumed to be uniform with no heat transfer between the heated and unheated regions.

The rate of heat transfer is calculated from a unit cell consisting of a single fuel particle concentrically surrounded by sodium liquid. Variable properties for the fuel and sodium, including the heat of fusion of the fuel, are considered. If the time for heat transfer is large compared to the acoustic period of the system (the time for a pressure wave to travel to the free surface and back), then the system

FIGURE 10.1. Model for Molten Fuel-Sodium Interaction

is under inertial restraint and boiling will occur whenever the sodium reaches the boiling point. If the time for heat transfer is small compared to the acoustic period, then the system is under acoustic restraint and the expansion of the sodium-fuel mixture is inhibited by the unheated liquid resulting in high pressures which suppress normal boiling. The one-dimensional acoustic equations are used to predict the transient pressures within the sodium-fuel mixture until the pressure decreases below the saturation pressure corresponding to the sodium temperature. At the onset of boiling, the fuel particles are assumed to be vapor-blanketed so that no further heat transfer takes place.

The acoustic work energy, generated while the system is under acoustic restraint, is calculated by integrating under the pressure-volume curve to the time that boiling conditions are attained. For analysis of structural damage from pressure waves, the impulse (pressure-time curve) may be more useful. The inertial work energy, generated while the system is under inertial restraint, is calculated from isentropic expansion of sodium to a specified terminal pressure. The entire pressure-volume curve for the process could be used in an analysis to determine the coolant slug acceleration toward the reactor cover.

Parametric analyses have been carried out using the SOCOOL-II computer program to assess the effect of various parameters on the magnitude of the theoretical work energy generated from a core disassembly accident. Among the parameters investigated were: fuel fragmentation, energy in the molten fuel, sodium temperature, time for heat transfer, final pressure, and sodium properties.

Figure 10.2 shows the effect of fuel particle size on the work energy, both acoustic (shock) and inertial (blast), when

FIGURE 10.2. Effect of Fuel Fragmentation on Work Energy

all of the fuel in the core is at the melting point and is assumed to mix with all of the sodium in the core. For fuel particles smaller than about 500 μ , the sodium is heated above the critical point, introducing greater uncertainties associated with the prediction of supercritical sodium properties.

Figure 10.3 shows the variation in total work energy as a function of the initial fuel temperature when all of the fuel in the core is assumed to be at uniform temperature. The work energy is much more sensitive to the initial fuel temperature when the calculated sodium temperatures are below the critical point.

Figure 10.4 shows the effect of sodium temperature on the acoustic and inertial work energy for constant sodium volume (decreasing sodium mass for increasing sodium temperature). Although the increased compressibility of sodium at the higher initial temperatures results in lower pressures generated within the molten fuel-sodium mixture, these lower pressures result in slower expansion of the system and substantially longer times are required to attain boiling conditions. Therefore, the acoustic work increases with increasing initial sodium temperature due to the longer times associated with the generation of acoustic energy, but lower pressures are involved. The decrease of inertial energy at the higher initial sodium temperatures is associated with the decreasing mass of sodium for the same volume. However, the total work energy continues to increase for higher initial sodium temperatures.

2. Containment Transients

R. D. Peak

The CACECO Code (cavity-cell-containment transient code) was used to investigate the early portions of containment

FIGURE 10.3. Effect of Initial Fuel Temperature on Work Energy

FIGURE 10.4. Effect of Initial Sodium Temperature on Work Energy

transients following various core disassembly accidents. Also, the FFTF containment model was modified to conform to the latest facility drawings. The present model is summarized below in Table 10.1.

TABLE 10.1. FFTF Containment Model for the CACECO Code

Reactor Cavity:	Volume of atmosphere	32,000 ft ³
	Number of heat structures	5
	Surface area of insulated structures	6,160 ft ²
	Surface area of steel-concrete structures	9,500 ft ²
Head Box:	Volume of atmosphere	10,200 ft ³
	Number of heat structures	6
	Surface area of steel structures	1,710 ft ²
	Surface area of steel-concrete structures	2,610 ft ²
Each HTS Cell:	Volume of atmosphere	20,000 ft ³
	Number of heat structures	4
	Surface area of insulated structures	5,725 ft ²
	Surface area of steel-concrete structures	7,300 ft ²

The insulated structures of the cavity are the reactor vessel, its guard vessel, the latter's supports, and the primary piping. The steel structures of the head box are the shielding for the closed loop piping and the reactor vessel cover. The insulated structures of the HTS cell are the primary pump and guard vessel, the intermediate heat exchanger and guard vessel, and the primary and secondary loop piping. The steel-concrete structures are the roof, walls, and floor of each space.

The following case was prepared for the PSAR: In this facility design, the atmospheres of the cavity, head box, and

each HTS cell are separate in order to prevent spread of gas-borne radioactive contamination during normal operation. However, in case of accidental overpressure, a rupture diaphragm-relief vent connects the cavity to the head box and the cavity to each HTS cell. In the following analysis, these vents are assumed to have a 1 ft^2 flow area.

The case is based upon energy released from a hypothetical disruptive accident. This energy would derive from the expansion of some 434 lb (197 kg) of core sodium heated by molten and vaporized fuel to the fiducial pressure of 1 atm. Inherent in this case is the assumption that most of this core sodium condenses quickly in the surrounding sodium inside the vessel except for core sodium vapor vented directly into the head access space through multiple ruptures of the closed loop piping. Another assumption is that upper plenum sodium blown through the peripheral gap of the raised reactor cover is effectively deflected out of the head box into the reactor cavity. The sodium releases into the head box and reactor cavity are assumed complete in about 1.1 sec because the driving pressure of core sodium vapor inside the reactor vessel decays in this time due to vapor condensation.

Results were submitted for use in the PSAR.

3. Space-Time Decay Heat Distribution from Fission Products in the Reactor Cavity

P. R. Shire

In the unlikely event of a hypothetical core disassembly accident, fission products may be released to the reactor cavity. Decay heat from fission products is significant for safety analyses treating long-term accident pressures and temperatures within the containment barrier.

As a limiting case, all of the fission products are assumed to be retained in the reactor cavity along with 6000 lb of reactor fuel and 25 tons (or more) of high temperature sodium. Initially the following species are estimated to be aerosols in the reactor cavity inert gas space: 100% of noble gases krypton and xenon; 100% of halogens iodine and boron; 100% of volatiles tellurium, selenium and cesium; and 10% of all remaining fission products (ARFP). The fuel mass containing all other fission products is assumed to be deposited uniformly on the 1000 ft² cavity floor, with the sodium pool above it.

Following this event the 10% ARFP are assumed to settle from the cavity gas space into the sodium pool with a fallout half life of ~ 10 min. The halogens and volatiles are assumed to settle into the sodium pool with a fallout half life of ~ 1 day. After ~ 8 days only, the noble gases remain in the cavity gas space.

Decay heat data was obtained from the RIBD⁽¹⁾ computer code for fuel at 300-day exposure. The time dependent decay heat generation for each isotopic group was multiplied by the distribution percentages estimated for the fission product release model and allocated to the three fission product carriers: inert gas, fuel and sodium. In order to estimate the radiation energy absorption in the carriers and in the reactor cavity structure, it was assumed that there was an equal division of energy between β and γ emitters and that both types of radiation could be represented by mono-energetic 1 MeV particles relative to energy absorption coefficients.

1. W. L. Bunch and L. D. O'Dell. Fission Product Inventory and Decay Heat Associated with FTR Fuel, BNWL-961, Battelle-Northwest, Richland, Washington, February 1969.

The resulting summary of decay heat generation versus time in system components was submitted for use in the PSAR.

4. Sodium Pool Fire Analyses for New FIR Equipment

Cell Volumes

P. R. Shire

. Following revisions in the sizes of primary heat transport system (HTS) cells, sodium pool fire analyses have been repeated using the SOFIRE-I code. Two sets of cases have been analyzed. In the first case, a 20,000 ft³ cell is conservatively assumed to remain gas-tight throughout the transient. In the second case, rupture disks are assumed to relieve gas pressures in excess of ~1 psi differential to the reactor cavity, 32,000 ft³, and then to the two other HTS cells for a total volume of 92,000 ft³. In each case, the initial conditions were as follows:

- Cell gas: 1 vol% oxygen (99% nitrogen) and 100 °F at atmospheric pressure.
 - Cell structure: 1/4 in. steel liner, 5 ft concrete walls, floor area (burning surface) 1250 ft², all at 100 °F.
- Sodium: 1100 °F and 5, 25, and 75 tons for Case 1. 1100 °F and 100 tons for Case 2.

The SOFIRE-I code was modified to allow the sodium volume to compress the cell gas adiabatically at time zero and to allow the steel floor liner to attain thermal equilibrium with the sodium at time zero. The results were submitted for use in the PSAR.

5. Containment Analyses - SOHOT Code

G. L. Fox

The development of the computer program (SOHOT), was described previously.⁽¹⁾ This code predicts pressures and temperatures in the reactor cavity, equipment cells, and containment sphere as the consequence of a boiling sodium pool heated by fission products on the cavity floor. Revisions have been made to this code to permit evaluation of reactor cavity thermal insulation schemes.

Previous SOHOT results have shown that the pressure in the reactor cavity and equipment cells could be suppressed by heat conduction into the structural concrete walls. However, after a sufficiently long time, the concrete temperature rose to a level considered structurally undesirable. Therefore, systems were evaluated which control the atmosphere temperature and protect the reactor cavity liner from the hot sodium.

The analyses showed that the cavity liner temperature may be limited to 250 °F by insulation and use of cooling coils from space coolers provided for normal operation.

In addition to the sodium boiling accident, two other sodium handling accidents were analyzed. The first analysis examined the effects of a fuel handling accident where one sub-assembly was dropped, releasing 500 lb of sodium and its fission product decay heat within the containment sphere.

The second analysis examined the terminal phase of a sodium pool fire in a heat transport cell which is open to outer containment. The SOFIRE code calculated the non-nuclear fire for 75 tons of 1050 °F sodium released on the floor with 100 ft² of vent area from the cell to outer containment.

1. FFTF Quarterly Technical Report, December 1969, January, February 1970, BNWL-1328. Battelle-Northwest, Richland, Washington. April 1970.

Both of these cases showed the resultant pressures were below the containment design pressure of 10 psig.

6. Site Radiological Analysis

C. L. Fies

Site radiological calculations were updated for bone, lung, and thyroid doses resulting from a hypothetical core disruptive accident. In addition to a base case analysis, several variations were made to evaluate the effect of changes in the basic assumptions. The dose was computed at the site boundary (4-1/2 miles) for both 2 hr and 30 day inhalation times.

Permissible limits are 150 rem to the bone, 75 rem to the lung, and 300 rem to the thyroid. All values computed for doses at the site boundary are well below these limits.

APPENDIX A

FFTF BNWL REPORTS AND DOCUMENTS ISSUEDMARCH 1 - JUNE 15*TOPICAL REPORTS (FORMAL)

BNWL Number	Authors	Title
1214	H. T. Blair	Fabrication Report FFTF Proto- typic Fuel Pellets for Analy- tical Chemistry Program
1261	M. K. Millhollen, T. J. Bennett, and R. L. Myers	Mark I Prototypic FTR Fuel Element Subassembly Sodium Flow Test
1264	R. W. Hardie and W. W. Little, Jr.	3DB , A Three-Dimensional Diffusion Theory Burnup Code
1271	W. F. Brown and R. M. Crawford	FFTF Welding Standards for the Driver Fuel Assembly
1286	R. J. Jackson, C. L. Wheeler, G. R. Waymire and W. W. Little, Jr.	Metal Swelling Effects on FTR Core Components
1303	R. F. Gilmore	Open and Closed Loop Top Closure Cutting and Welding Equipment Evaluation
1319	R. N. Madsen	Dynamic Digital Computer Simula- tion of the FFTF Dump Heat Exchanger
1321	B. R. Hayward	The Application of Image Enhancement to FFTF Fuel Technology'
1323	R. L. Fish, J. J. Holmes and R. D. Leggett	Interim Burst Test Results on EBR-II Irradiated Fuel Pin Sections
1324	R. E. Dahl, Jr.	Calculations of Burnup and Damage for Fast Reactor Fuel
1330	B. E. Dozer	FFTF Process Data Display Prototype
1331	R. L. Myers	Evaluation of Weld Joint Designs Between Wire Wrap and End Cap

* *Compiled by L. R. Stevens*

BNWL Number	Authors	Title
1332	R. L. Myers	Evaluation of 3/32 in. Fillet Wire-End Cap Weld
1333	W. L. Bunch	FFTF Shielding Program
1335	J. R. McBride	Modified Fermi Control Rod Latch for the FFTF--Report on the Development and Testing Program
1339	C. A. Mansius	Activation and Shielding of FTR Sodium
1347	J. E. Krajicek	Dynamic Simulation of Primary Loop Check Valves for the Fast Flux Test Facility
1350	J. P. Hale	Site Safety Analysis
1355	J. Ryden, Jr.	An Eddy Current Tube Testing Technique Using a Hybrid Coil Configuration with Sequential Sampling
1359	J. Ryden, Jr. and E. F. Perrizo	The Nondestructive Examination of G and H Lots FFTF Fuel Cladding
1367	A. L. Gunby	Intermediate Heat Exchanger Modeling for FFTF Simulation
1376	R. R. Studer	Mechanical Cleaning of Small Diameter Tubing

TOPICAL REPORTS (INFORMAL)

BNWL Number	Authors	Title
Ma-73	H. G. Powers	FFTF Fuels Dept. Quality Assurance Manual
1111-FF-1	A. R. Shade	Parametric Reactivity Transient Analysis for the FFTF Nuclear Proof Test Reactor
1294-FF-1	M. R. Kreiter, S. K. Myers and E. F. Antal	Closed Loop Seal and Creep Compensating Connector Evaluation
1374 RD	G. E. Culley and D. O. Sheppard	Hazards Analysis for the Battelle-Northwest GETR/TREAT Transient Irradiation Test Series
1390 RD	D. E. Simpson, A. E. Waltar and A. Padilla	Assessment of Magnitude and Uncertainties of Hypothetical Accidents for the FFTF

BNWL Number	Authors	Title
1398 RD	M. K. Millhollen	Engineering Test Plan for Mixed Oxide Unencapsulated Fuel Pin Irradiations in EBR-II (ANL Run-to-Failure Test B)
CC-2369	G. Janson	Fast Reactor Fuel Interaction with a Concrete Floor After a Hypothetical Core Meltdown
CC-2452	F. R. Standley	Sodium Service Pressure Measurement NaK Capillary System Test Report

SPEECH AND ARTICLES

BNWL Number	Authors	Title
SA-2758	W. W. Little, Jr., R. W. Hardie, L. D. O'dell and R. B. Kidman	Fuel Management Models and Analysis for the Fast Test Reactor (FTR)
SA-3093		Lecture Series, Fast Reactor Safety Technology and Practices
Vol. 1	P. L. Hofmann	Safety Practices and Background Information
Vol. 2	P. L. Hofmann	Accident Analyses
SA-3269	L. M. Polentz and E. R. Astley	Stainless Steel Swelling in the Breeder Reactor

PERIODICALS

BNWL Number	Authors	Title
325	E. R. Astley and C. P. Cabell	FFTF Summary Technical Report, January-December, 1966
1300-2	B. Wolfe	FFTF Monthly Informal Technical Progress Report February 1970
1300-3	B. Wolfe	FFTF Monthly Informal Technical Progress Report March 1970
1300-4	B. Wolfe	FFTF Monthly Informal Technical Progress Report April 1970
1300-5	B. Wolfe	FFTF Monthly Informal Technical Report May 1970

<u>BNWL Number</u>	<u>Authors</u>	<u>Title</u>
1300-6	B. Wolfe	FFTF Monthly Informal Technical Report June 1970
1328	B. Wolfe	FFTF Quarterly Technical Report December 1969, January, February 1970

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