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NOVEMBER 1972

SODIUM-COOLED REACTORS CORE ENGINEERING PROGRAM

FORTY-FOURTH QUARTERLY REPORT
AUGUST-OCTOBER 1972

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

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**SODIUM-COOLED REACTORS
CORE ENGINEERING PROGRAM**

Forty-Fourth Quarterly Report

August—October 1972

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Prepared for the
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TABLE OF CONTENTS

	Page
ABSTRACT	xi
SUMMARY	xiii
1. ENGINEERING COORDINATION AND LIAISON	1-1
2. FUEL ROD ENGINEERING	2-1
2.1 Fuel Rod Design Engineering	2-1
2.1.1 Fuel Performance Modeling (LIFE)	2-1
2.1.2 Steady-State Thermal Performance	2-3
2.1.3 Defected Fuel Performance	2-3
2.2 Fuel Rod Irradiations	2-5
2.2.1 High Burnup Irradiations	2-5
2.2.2 High Cladding Temperature Irradiations	2-11
2.2.3 Operating Limits Irradiations	2-14
2.2.4 Fuel Rod Design Correlations	2-19
2.3 Material Properties	2-22
2.3.1 Design Correlations	2-22
2.3.2 L-15 Experiment	2-28
2.3.3 L-16 Experiment	2-31
2.3.4 L-17 Experiment	2-32
3. FUEL ASSEMBLY ENGINEERING	3-1
3.1 Grid-Spaced Assembly	3-1
3.1.1 Diagnostic Grid Spacer Experiment	3-1
3.1.2 Grid Spacer Assembly Experiment	3-2
3.2 Wire Wrap Assembly	3-5
3.2.1 F11B Diagnostic Wire-Wrapped Fuel Assembly	3-7
3.3 Fuel Assembly Hardware Testing	3-7
3.4 Fuel Assembly Design and Operating Limits	3-7
3.4.1 Structural Design Criteria for Core Components	3-7
3.4.2 Structural Design Codes	3-14
3.4.3 Probabilistic Core Design Tools	3-20
4. CORE RESTRAINT ENGINEERING	4-1
4.1 Core Restraint Analysis	4-1
4.1.1 Core-Wide Channel Wall Temperature Distribution	4-1
4.1.2 Hexagon Load Distribution	4-6
4.1.3 Passive Core Restraint	4-6
4.2 Core Restraint Mechanism	4-6
4.2.1 Design Basis for 3D Test Components	4-6
4.2.2 Low Friction Coating Material Study	4-8
4.3 Core Restraint Testing	4-8
4.3.1 Phase 1 Testing	4-9
4.3.2 Phase 2 Testing	4-10
4.3.3 Phase 3 Testing	4-12
5. CONTROL AND BLANKET ASSEMBLY ENGINEERING	5-1
5.1 Control Assembly Engineering	5-1
5.1.1 Control Assembly Nuclear Engineering	5-1
5.1.2 B ₄ C Rod Design	5-2

TABLE OF CONTENTS (Continued)

5.2	Blanket Assembly Engineering	5-7
5.2.1	Blanket Assembly Engineering	5-7
6.	DEFECTED FUEL TESTING	6-1
6.1	Defected Fuel Testing	6-1
6.1.1	B9B Capsule	6-1
6.1.2	B9D Capsule	6-1
6.1.3	Sodium Fuel Reaction Study	6-11
6.1.4	Run Beyond Failure Tests	6-16
7.	QUALITY ASSURANCE	7-1
7.1	Quality Assurance Program Progress and Accomplishments	7-1
7.2	Nonconformance Activities	7-1
7.3	Program Audit Activities	7-2
	DISTRIBUTION	1

LIST OF ILLUSTRATIONS

Figure	Title	Page
2-1	Simulated LMFBR Startup Showing Sodium Temperature Rise to Rated Level with Reactor at Low Power, Followed by Rise to Full Power	2-2
2-2	LIFE Code Prediction of Fuel-Cladding Interface Pressure for Typical High Power LMFBR Fuel Rod	2-4
2-3	LIFE Code Prediction of Cladding Diameter Increase for Typical High Power LMFBR Fuel Rod.	2-4
2-4	Maximum Cladding Attack Observed Near Peak Flux Region of Fuel Rod F4E. Maximum Cladding Inside Surface Temperature (at Start-of-Life) was 1080°F. It Occurred at Start-of-Life in This Region	2-9
2-5	Typical Cladding Appearance in Peak Temperature Region of F4E. Maximum Cladding Inside Surface Temperature in this Region (at Start-of-Life) was 1092°F	2-10
2-6	Maximum Cladding Attack Observed in Peak Temperature Region of Rod F4A. Maximum Cladding Inside Surface Temperature (at Start-of-Life) was 1065°F	2-10
2-7	Coolant Temperature Distribution Across Subassembly in Experiment F10A	2-13
2-8	Calculated Coolant Temperature Distribution Across Subassembly in Experiment F10B	2-13
2-9	Calculated Coolant Temperature Distribution Across Subassembly in Experiment F11A	2-13
2-10	Transverse Metallographic Section Near Peak Flux Region of Rod F9C-13	2-16
2-11	Longitudinal Metallographic Section Taken at Top of Fuel Column on Rod F9C-13	2-17
2-12	Electron Microprobe Analysis for Radial Plutonium Distribution at Peak Flux Region on Rod F9C-13	2-18
2-13	Photomicrograph Showing Metallic Particles in Fuel Near Cladding. Square Indicates Area Covered by Scans to Produce X-ray Images Shown in Figure 2-14	2-19
2-14	X-ray Images of Iron, Nickel, Chromium, Molybdenum, and Cesium Distribution in Region Indicated in Figure 2-13. Elements Analyzed for, but not Detected by this Technique: Mn, Nd, Pr, Ce, Ba, I, Xe, Te, Rh, Ru, Tc, Nb, Zr, Sr, and Si	2-20
2-15	Electron Microprobe Analysis of Fuel Near Central Void at Peak Flux Region on Rod F9C-13	2-21
2-16	Distribution of Zirconium X-ray Intensities Across Portion of Fuel Including Dark Ring. (Rod F9C-13)	2-22
2-17	Uniform Strain of 20%CW Type-316 Stainless Steel as Function of Irradiation and Thermal Annealing at Different Temperatures. (2.5)	2-24
2-18	Elastic Strain of Unirradiated 20%CW Type-316 Stainless Steel as Function of Stress and Temperature	2-25
2-19	Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 700°F	2-26

LIST OF ILLUSTRATIONS (Continued)

2-20	Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 800°F	2-26
2-21	Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 900°F	2-26
2-22	Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1000°F	2-26
2-23	Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1100°F	2-27
2-24	Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1200°F	2-27
2-25	Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1300°F	2-27
2-26	Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1400°F	2-27
2-27	Effect of Strain Rate on Uniform Plastic Strain of Irradiated 20%CW Type-316 Stainless Steel at Different Temperatures	2-28
3-1	EBR-II Fuel ASM—(F9F)	3-3
3-2	Sodium Flow Test Bundle	3-6
3-3	Sample Problem Used in Analyses	3-9
3-4	Damage Accumulation and Inelastic Strain Buildup with Time for a 20%CW Type-316 Stainless Steel Fuel Rod	3-12
3-5	Variation with Time of Maximum Bending Stress in a Channel Located Near the Edge of LMFBR Core and Subjected to Nominal Swelling—Nominal Radiation Creep Behavior	3-14
3-6	Axial Profile of Temperature and Maximum Creep Strain Occurring in a Channel Bowing Due to Nominal Swelling and Nominal Creep	3-15
3-7	Temperature Gradients and Cladding Model Used in Analyses	3-17
3-8	Maximum Midwall Circumferential Thermal Creep Strain Occurring in Cladding with Time (see Table 3-2)	3-19
3-9	Maximum Equivalent Thermal Creep Strain Occurring in Cladding as Function of Time	3-19
3-10	Biased Sampling Scheme for PECS	3-21
4-1	Reference Orificing Scheme	4-3
4-2	Channel Mid-Wall Temperatures at 136 in. Elevation	4-4
4-3	Channel Wall Temperature Gradients	4-5
4-4	One-Sixth Sector of Core Array for Use in Hexagon Load Distribution Problems	4-7
4-5	Core Restraint Test Fixture	4-11

LIST OF ILLUSTRATIONS (Continued)

4-6	Internal Tightener	4-13
5-1	Relative Distribution of B^{10} (in, α) Reaction Rate	5-2
5-2	Heat Generation by Control Rods	5-2
5-3	Minimal Ultimate Strength of 20% Cold Worked Type-316 Stainless Steel	5-4
5-4	Stress Rupture Life of 20% Cold Worked Type-316 Stainless Steel	5-4
5-5	1/3 UTS Criteria, 950°F, 0.483 in. i.d. Rod	5-5
5-6	1/3 UTS Criteria, 1100°F, 0.483 in. i.d. Rod	5-5
5-7	1/3 UTS Criteria, 950°F, 0.331 in. i.d. Rod	5-5
5-8	Sealed Plenum Wall Thickness—1/3 UTS Criteria, 1100°F, in. i.d. Rod	5-5
5-9	Scaled Plenum Wall Thickness—Creep Rupture, 950°F, 0.483 in. i.d. Rod	5-6
5-10	Sealed Plenum Wall Thickness—Creep Rupture, 1100°F, 0.983 in. i.d. Rod	5-6
5-11	Sealed Plenum Wall Thickness—Creep Rupture, 950°F, 0.331 in. i.d. Rod	5-6
5-12	Sealed Plenum Wall Thickness—Creep Rupture, 1100°F, 0.331 in. i.d. Rod	5-6
5-13	Plenum Temperature versus Plenum Length—0.475 in. o.d. Rod, 0.305 in. i.d., 9.44 Liters of Gas	5-8
5-14	Effect of Wire Pitch on Computed Temperature Distribution in a Blanket Assembly	5-10
5-15	Effect of Wire Pitch on Computed Pressure Drop in Radial Blanket	5-11
6-1	Calculated Average Friction Factor as a Function of Reynold's Number	6-2
6-2	Metallography Sectioning Diagram (B9D Fuel Rods)	6-7
6-3	Transverse Fuel Section 1/4 inch Below B9D-2 Defect	6-8
6-4	Transverse Fuel Section 1/16 inch Below B9D-2 Defect	6-9
6-5	Longitudinal Fuel Section of Bottom 1 inch of B9D-2	6-10
6-6	Variation in Thickness of Reaction Phase in B9D Rods (Transverse Sections)	6-12
6-6	Variation in Thickness of Reaction Phase in B9D Rods (Transverse Sections) (Continued)	6-13
6-7	Variation in Thickness of Reaction Phase along B9D Fuel Rods	6-14
	Capsule B11A, B, C Irradiation Plan	6-15

LIST OF TABLES

Table	Title	Page
2-1	Different Results for Same LIFE Code Case on Different Computers	2-2
2-2	Input Parameters for LIFE Code Prediction of Typical LMFBR Fuel Rod Performance	2-3
2-3	Summary of Task 2 Fast Flux Testing Program in EBR-II	2-7
2-4	Summary of Rod F4A and F4E Design and Irradiation Parameters	2-10
2-5	Cladding Immersion Density Measurements from Fuel Rods F4A and F4E	2-11
2-6	Comparison of Calculated Temperature Differences in the Coolant Around an Edge Rod for Intact and Sodium-Filled Insulating Gas Annulus in Experiments F10A, F10B, and F11A	2-12
2-7	Fabrication and Irradiation Parameters and Examination Results for Fuel Rods F9C-31 and -13	2-15
2-8	Ductility of Irradiated Stainless Steel	2-29
2-9	L-15 Tensile Specimens	2-31
3-1	Typical Operating Conditions for a Peak LMFBR Fuel Rod	3-16
3-2	Definition of Cladding Cases Considered	3-18
3-3	Significant Results of Cladding Analysis	3-18
3-4	Variables to be Biased in PECS	3-23
6-1	Fuel and Fission Products in Sodium Samples from Capsule B9D	6-4
6-2	Fuel and Fission Products Deposition on Capsule Walls from B9D Capsule	6-5
6-3	Total Fuel and Fission Products Released from B9D Fuel Rods	6-5
6-4	Fraction of Fuel and Fission Product Inventory Released from B9B Fuel Rods	6-6
6-5	Capsule B9D Fuel Burnup	6-6

ABSTRACT

This report is the forty-fourth in a series of quarterly progress reports published under the Fast Ceramic Reactor Development Program. The objective of the work reported here is to provide the engineering design basis for the core of a liquid metal cooled fast breeder reactor.

Progress during August, September and October 1972 is described for tasks related to fuel rods, fuel rod assemblies, core restraint, control and blanket assemblies, and defected fuel.

SUMMARY

This report is the forty-fourth in a series of quarterly progress reports published under the Fast Ceramic Reactor Development Program. The objective of the work reported here is to provide the engineering design basis for the core of a liquid metal cooled fast breeder reactor.

Progress during August, September and October 1972 is described for tasks related to fuel rods, fuel rod assemblies, core restraint, control and blanket assemblies, and defected fuel.

TASK 1 – ENGINEERING COORDINATION AND LIAISON

Final revisions to the FY-73 189a forms were completed, and work scope and cost estimate proposals were submitted to AEC/SAN.

The PA-10 program was reviewed in depth August 15 to 17 with C. E. Webber, L. S. Rubenstein, and D. R. Riley of RDT. Other reviews were held with K. M. Zwilsky and R. Richter of RDT.

TASK 2 – FUEL ROD ENGINEERING

Preliminary studies using LIFE-II, Revision 2, with options chosen to be consistent with the calibrated version, show that the model of fuel-cladding mechanical interaction does not adequately simulate the performance expected of an LMFBR fuel rod, based on fuel pin irradiation experiments. In addition, major code engineering problems, apparently associated with the number of axial fuel nodes, have been identified. These results were discussed with ANL.*

Design criteria for development of a fuel rod defect mechanism were formulated to simulate an end-of-life fuel rod failure. Correlation of defected fuel swelling with an analytical model based on sodium diffusion is in progress.

Metallographic examination of F9C-13 showed the fuel rod to be in good condition. Attack at the cladding inside surface was small. The maximum thickness of any reaction layer was ~1 mil. No intergranular attack was observed. The maximum diameter increase was 1 mil (0.4% $\Delta D/D$). This unencapsulated fuel rod, with 87% theoretical smeared fuel density and clad with solution annealed Type-316 stainless steel, was irradiated in EBR-II at 12.5 kW/ft peak start of life power with 935°F maximum cladding temperature to 6.3% burnup (calculated).

Significant intergranular attack at the cladding inside surface was observed in fuel rods F4A and F4E. Metallographic examination showed 6-mil and 5-mil penetration, respectively. These encapsulated fuel rods, with 86% theoretical smeared fuel density and clad with solution annealed Types-304 and -316 stainless steel, reached 9 at.% burnup at >16 kW/ft peak start-of-life power with 1090°F maximum cladding temperature.

With the addition of a stainless steel reflector to the EBR-II core, the low power FO rods in subassembly XG04B were subjected to a significant change in axial power profile. Preliminary calculations supplied by the EBR-II project indicate that the power at the center and ends of rods FOB and FOD was approximately 22% and 58% higher, respectively, during Run 56 (with the reflector) relative to Run 55 (without the reflector). Analysis of the effect of this change in axial power profile on the overall objectives of the experiment is in progress.

Fabrication of four replacement rods for the high cladding temperature F11A experiment was completed. The rods were shipped to EBR-II for irradiation. The objective of the F11A experiment is to investigate the performance of 37 unencapsulated fuel rods clad in several alloys at high linear power and high cladding temperature.

Ductility of 20% CW Type-316 stainless steel was projected to temperatures of 700 to 1400°F, strain rates of (10^{-6} in./in./hr to 10^1 in./in./hr) and fluences up to 3×10^{23} , $E > 0.1$ MeV. The projection indicates that the nominal steady state uniform plastic strain is expected to be greater than 0.1% during the life of a Demo plant core.

Thermal analysis of the L-15 tensile specimens has been initiated.

* Refer to Appendix B for listing of abbreviations, symbols, and acronyms.

The second S/A (X098A) in the L16 series of four S/As was inserted in row 4 of EBR-II prior to Run 56. This S/A contains tubular specimens from Types-304, -316, and -321 in both annealed and cold-worked conditions.

The ten capsules discharged from the initial L16 S/A (X098) are in the HFEF at EBR-II awaiting a nondestructive post-irradiation examination to determine the growth of Types-304 and -316 specimens at a fluence of 3.3×10^{22} n/cm², $E > 0.1$ MeV-GE.

The two L-16D capsule inserts scheduled for insertion in S/A X167 (previously X148A) are in the HFEF at EBR-II for encapsulation. Plans are to encapsulate these inserts in time for reconstitution of S/A X167, which is scheduled to make Run 59. RDT approval to hold S/A X167 until Run 59 was granted.

A proposal ANL experiment designed to obtain data on the combined effects of EBR-II fluence and sodium exposure on the post-irradiation stress rupture properties has been reviewed with respect to GE design requirements. The review indicates that with certain basic modifications an experiment of this type would be very useful for design purposes.

TASK 3 – FUEL ASSEMBLY ENGINEERING

F9F, the second in a series of GE grid-spaced fuel assembly experiments planned for irradiation in EBR-II, has been loaded into the core for start of irradiation during reactor run No. 59. F9F was designed to provide fuel assembly test conditions and data in direct support of the optimized FTR grid-spaced driver fuel assembly.

Detailed design has been initiated on the FTR grid spaced assemblies. Four full size grid-spaced fuel assemblies have been proposed, GSA 1, 2, 3, and 4. These will be placed in open core positions, two in each fuel enrichment zone of the reactor.

A program of out-of-pile development work leading to the fabrication of the FTR grid-spaced fuel assemblies has been planned and initiated. This will include thermal-hydraulic testing of this assembly, mechanical tests of the grid-channel attachment, the development of channel characterization methods, the development of radiography as an inspection method, and the performance of sodium friction tests.

Existing structural design criteria for core components have been reviewed. A study has been completed on the application of current criteria to predict the creep rupture of 20% clad worked Type-316 stainless steel components. Recommendations to achieve a consistent material basis in creep rupture analysis have been made. Analyses are being conducted to establish for 20% cold worked Type-316 stainless steel, the temperature and stress levels at which damage due to creep becomes significant.

The two dimensional codes CREEP-PLAST and GOGO are now both fully operational. Both codes can now handle thermal creep, radiation creep and swelling behavior. The check-out case of an isothermal thick cylinder under internal pressure with all of the above material effects included has been run for both codes and the results agree within 1%. An analysis was made to evaluate the effect of temperature gradients in cladding on its response to fission gas pressure.

The probabilistic fuel rod design code, PECS (Probabilistic Evaluation of Cladding Stress), has been modified to include a Monte Carlo biased sampling procedure. In addition, the numerical integration logic in this code has been refined and the code has been reorganized on a modular basis. These changes have reduced, by a factor of approximately 700, the cost of running the code.

TASK 4 – CORE RESTRAINT ENGINEERING

A core-wide temperature study has been completed. The axial distribution of the channel wall temperatures were determined for all faces of the assemblies within a one-sixth segment of the core. The results show a large heat sink effect of the control assemblies on the temperatures of the adjacent fuel assembly walls.

The distribution of duct loads in the core is being studied with both an 8-hexagonal model and a full 60-hexagonal model which covers a one-sixth segment of the core. The modeling scheme employs a series of beams to

represent hexagon sides and contact point between the hexes. Body forces are applied at each hexagon, and the resulting compressive and shear contact loads between the interacting hexes are determined.

A first draft of the design basis document for the 3D test components was completed.

The Development Specification for the preliminary Phase 1 tests at HEDL has been issued. Renovation of the Planar Clamping Model (PCM) and coating of the test ducts with alumina is nearing completion.

The Development Specification for the Phase 2 tests of an external yoke system has been written. Those components required to modify the PCM have been designed. Preliminary test planning for the Phase 3 tests of the internal tightener system has been completed.

TASK 5 – CONTROL AND BLANKET ASSEMBLY ENGINEERING

The B^{10} (n, α) reaction rate radial distribution inside a control assembly was calculated for the range of B_4C enrichments from 40% to 90%. It appears that, due to the neutron flux depression in the control element, the reaction rate increases as the radial distance is increased. The heat generation due to B^{10} (n, α) reaction at the average power position was calculated to be 0.298 MW per control assembly for the 90%-enriched B_4C case and 0.185 MW per control assembly for the 40%-enriched B_4C case. The calculated initial helium gas generation rate at the average power position was 3.0149×10^{-5} liter/sec per control assembly for the 90%-enriched B_4C case and 1.8674×10^{-5} liter/sec per control assembly for the 40%-enriched B_4C case.

The feasibility of using a sealed plenum control rod design to contain the helium gas generated due to the B^{10} (n, α) reaction was investigated. Results indicate that sealed plenum control rods are not presently feasible if internal core clamps are used. However, there is a good potential for sealed plenum control rods if internal core clamps are illuminated as is the case in the FTR control assembly.

The effects of wire wrap pitch on the blanket rods were evaluated. The wire wrap around a rod helps promote thermal interchange to adjacent flow channels. This interchange reduces the variations in temperature between flow channels and also lowers the peak temperature. For a reduction in wire pitch from 6 inches to 4 inches, the peak coolant temperature is reduced by 6°F, and the greatest temperature difference around a rod is reduced from 37°F to 27°F.

TASK 6 – DEFECTED FUEL TESTING

The post-irradiation examination of the B9D capsule was completed. Metallographic examination of ten fuel samples shows fuel-sodium reaction product in the fuel cladding gap along the entire fuel column length. The reaction product thickness varied from a maximum of 0.009 inch to a minimum of 0.001 inch. Analysis of sodium samples taken from the capsule show that 84% of the uranium and plutonium released from the fuel rod is deposited on the capsule walls while 16% remained in the sodium. Of the total fission products found in the sodium and on capsule walls, 90.2% were Cs-137 and 8.4% were I-131.

Fabrication of the sodium fuel reaction study in capsules B11A, B and C continued. Fabrication of B11B is completed and final preparations for irradiation in the GETR are in progress.

The Run Beyond Failure test proposal (Request for Approval-in-Principle) has been forwarded to the AEC-RDT for review.

QUALITY ASSURANCE

The BRD QA Plan for development programs was issued October 2, 1972.

Surveillance of L16D activities and B11A, B, C fuel rod assemblies continued. Surveillance of final fabrication activities of the F9F, replacement of F11A fuel rods and F20 fuel fabrication was continued.

Quality document reviews were completed for LASL examination of irradiated fuel rods and interim capsule examinations at ANL.

Nonconforming items reported on B11, F9F, and F20 experiments were investigated and disposition was made. One nonconformance (NIR0290) was referred to AEC/SAN for concurrence.

Summaries of fourteen QA audits were prepared and submitted to RDT for information.

MAJOR CONTRIBUTORS

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1. ENGINEERING COORDINATION AND LIAISON

Task Leader — J. H. Terhune

The objective of this task is to manage the program, coordinate the technical work, including the development and application of the QA plan, and to provide liaison with the national LMFBR Program.

The program was reviewed at a meeting held in Sunnyvale, August 15 to 17, with C. E. Weber, L. S. Rubenstein, D. R. Riley of RDT and GE Site Representatives. PA-10 Task Leaders and Cognizant Engineers presented their respective program plans and reviewed progress made during the previous three months. RDT acknowledged a significant increase in the degree of integration and interaction between the design and experimental testing functions in BRD.

The status of all PA-10 tasks was reviewed with Mr. R. Richter, the new RDT site representative for PA-10. Dr. K. M. Zwilsky of RDT reviewed subtask 3 of Task 2 on September 28th in San Jose.

Revisions were incorporated into the final FY-73 189 forms, corresponding contract work scope proposals prepared, and detailed cost estimates generated for contract period 1973. Required management and legal review was completed and the proposal submitted to AEC/SAN.

C. N. Spalaris and P. R. Pluta visited HEDL for liaison regarding fuel rod engineering.

2. FUEL ROD ENGINEERING

Task Leader: J. D. Stephen

The objective of Task 2, Fuel Rod Engineering, is to develop the design basis for the LMFBR fuel rod. This is an integrated analytical and experimental program. The scope includes development of design methods and performance of analyses in the areas of both normal and defected fuel operation, irradiation of fuel rod and structural materials, and development of material properties correlations. The restructuring of PA-10, 1 July 1972, combined the activities of part of Task B (Defected Fuel Analysis), Task E (Fuel Material Properties and Performance Evaluation), Task F (Fast Flux Tests and Support), Task L (Cladding Development), and Task P (Fuel Element Modeling) under Task 2. The other part of Task B (Defect Fuel Testing) was restructured as Task 6. Close integration is maintained between Task 2 and Task 6, and with the other Core Engineering Tasks. In particular, design analyses are conducted under Task 3 (Fuel Assembly Engineering) and Task 5 (Control and Blanket Assembly Engineering) using methods developed under Task 2. The development of design criteria under Task 3 is closely coupled with the Task 2 methods development, and the requirements of all Core Engineering Programs are factored into the development of material properties correlations. In addition, active interfaces are maintained with other programs at GE and with the programs at ANL, HEDL, WARD, AI, and other LMFBR contractors.

2.1 FUEL ROD DESIGN ENGINEERING – L. M. McWETHY

The objective of this subtask is to develop the analytical design basis for the LMFBR fuel rod. Activities include experiment analysis, design methods development, fuel rod design, and generation of design requirements for materials properties correlations.

2.1.1 Fuel Performance Modeling (LIFE)

The near term objectives of the fuel performance modeling program are to evaluate the computer code LIFE as a design tool and to recommend modifications of the code, models, material properties, and calibration* as required, while maintaining close liaison with the center of the LIFE development program at ANL, and with similar programs at other sites. The scope includes extensive sensitivity studies and modeling to establish compatibility with design criteria. In the longer term, LIFE will be used in the analysis and design of the fuel for the Demonstration Plant.

This is the first report on the progress of a restructured fuel performance modeling program. Work under PA-10 is now focused on the development and use of the LIFE system as the reference fuel performance code of the national LMFBR fuel design program. The results of the earlier fuel modeling development work are being factored into the LIFE program.

Preliminary studies using LIFE-II, Revision 2, with options chosen to be consistent with the calibrated version, show that the model of fuel-cladding mechanical interaction does not adequately simulate the performance reasonably expected of an LMFBR fuel rod on the basis of extrapolation of experimental data. In addition, major code engineering problems apparently associated with the number of axial fuel nodes have been identified. These results were discussed with ANL.

Using LIFE-II, Revision 0, which was released in FY-72, a possible problem with different answers from different compilers was identified. As part of the program to convert the code from the ANL computation system to that at GE, Revision 0 was run on the IBM-360 at ANL, the IBM-360 at Boeing, and the H6070 at GE. The results for the identical input problem are shown in Table 2-1. The calculated results using the Boeing compiler are markedly different, including a change in sign. The differences between the ANL and GE results are smaller, but not negligible, particularly in the case of axial growth of the fuel, where the result using the GE computer is three times that using the ANL computer. During this quarter the potential problem of these differences has been discussed with ANL in detail. ANL has reported, in personal communication, that similar differences were observed between identical cases run before and after the change in computer systems at ANL. The potential problem is accumulation of the error. The input case for

*"Calibration" is used here to mean the adjustment of the numerical factors in the code to the values which best fit the properties of the materials which have been experimentally determined.

these runs was a sample problem with a simple irradiation history. A still greater error would be expected in more complex cases. If this problem cannot be resolved by observation and analysis, GE has recommended that a more complex case or one based on an irradiation experiment be compared in a round robin by contractors with different computers.

Table 2-1
DIFFERENT RESULTS FOR SAME-LIFE CODE CASE ON DIFFERENT COMPUTERS

	ANL (IBM 360)	BCC ^(a) (IBM 360)	GE (H 6070)
Burnup (at. %)	0.987	0.976	0.987
Gas Pressure (psi)	32.33	32.31	32.32
Fuel Cladding Interface Pressure (psi) J = 1 ^(b)	1240	1150	1280
Fuel Cladding Interface Pressure (psi) J = 2	723	691	734
Fuel Axial Strength (%) J = 1	0.033	0.045	0.027
Fuel Axial Strength (%) J = 2	0.112	0.012	0.036
Cladding Diameter Increase (%) J = 1	0.106	0.095	0.11
Cladding Diameter Increase (%) J = 2	0.568	0.624	0.602

(a) Boeing Computation Center.

(b) J is the number of axial nodes into which the fuel rod is divided for computational analysis.

LIFE-II, Revision 1 was used extensively this quarter. The code, the models, the calibration, and the material properties were reviewed. The performance of a typical high power LMFBR fuel rod was calculated. A major problem in convergence, found to be more serious with cases with more than one axial fuel node, was identified.

Convergence is time consuming when the fuel and cladding are just in contact. This occurs early in life as the fuel grows out to the cladding, and later in life as the cladding swells away from the fuel. As an example, the LMFBR fuel rod case with two axial fuel nodes had reached only 122 hours out of a 14,000 hour irradiation time after 18 minutes of computer time. In order to converge, the time steps had been cut by the code to 0.001 hour. When the case was reduced to a single axial node, the entire irradiation was completed within 18 minutes. This problem has been discussed with ANL and they are attempting to resolve it.

LIFE-II, Revision 2 was received from ANL in September and is now operational. Additional options for cladding swelling and cladding creep correlations are being programmed into the GE working version of the code to broaden the scope of the studies. A major problem in first startup has been identified which is also apparently associated with the number of axial fuel nodes. The startup history used is shown in Figure 2-1. The primary coolant temperature is raised to rated level while the reactor is at low power. The reactor is then raised to full power. This case did not compute correctly with two axial fuel nodes. Cladding stresses $>10^6$ psi were calculated in the second axial fuel node during startup. When the simulation was reduced to a single axial fuel node, the case ran without difficulty. This problem is being discussed with ANL.

Figure 2-1. Simulated LMFBR Startup Showing Sodium Temperature Rise to Rated Level with Reactor at Low Power, Followed by Rise to Full Power

The results of the typical high power fuel rod case using LIFE-II, Revision 2, are shown in Figures 2-2 and 2-3. The fabrication and operating parameters are given in Table 2-2. Options for fuel creep and cladding creep were chosen to be consistent with the earlier calibration of Revision 0. The model predicts closure of the fuel-cladding gap within five days after startup, followed by fuel-cladding mechanical interaction that produces high cladding stress (~16,000 psi) early in life. The cladding stress decreases monotonically with increasing burnup. Irradiation creep strain of the cladding under this load is negligible, but thermal creep strain is significant (~3%). These results are not consistent with the performance of fuel rods in irradiation experiments. The major conclusion is that the LIFE mechanical model (based on the early calibration) does not correctly describe expected LMFBR fuel rod performance.

Table 2-2
INPUT PARAMETERS FOR LIFE CODE PREDICTION
OF TYPICAL LMFBR FUEL ROD PERFORMANCE

LIFE-II, Revision 2 with options
consistent with 1971 calibration

Number of Axial Fuel Nodes	1
Cladding Outside Diameter (inches)	0.230
Cladding Thickness (inches)	0.015
Fuel Pellet Diameter (inches)	0.1945
Fuel Pellet Density (% theoretical)	90.4
Fuel Column Length (inches)	30.6
Plenum Length (inches)	54.0
Power (kW/ft)	15.6
Cladding Temperature (°F)	1026

The above observations were discussed at the 4th Fuel Modeling meeting at RDT-HQ on 11, 12 October 1972. Detailed comments on material properties to be used in LIFE were also generated for discussion. At this meeting, the first reference version of the (LIFE) LMFBR fuel performance code was defined, including models, material properties, and experimental data for calibration. Calibration of this reference version is in progress at ANL. Significant changes have been made in the fuel creep and cladding creep correlations. The sensitivity studies under this subtask will continue with the new reference version of LIFE.

2.1.2 Steady-State Thermal Performance

The objectives of this work are to predict and evaluate the results of the F20 power-to-melt experiment, coordinate the round robin predictions and update fuel rod thermal performance models.

The F20 experiment is designed to eliminate a major uncertainty in thermal performance by achieving sufficient burnup to permit an assessment of the experiment power independent of physics calculations. Extensive discussions were held on the F20 experiment to review thermal properties of the fuel, the experiment objectives, and the power-to-melt design criteria in preparation for final pre-irradiation thermal analysis. The detailed material properties requirements for analysis of thermal performance experiments were generated. These updated property correlations are being compiled under the Task 2 Materials Handbook effort. These properties will be used in the final pre-irradiation calculations for selection of the linear power range for the F20 experiment.

2.1.3 Defected Fuel Performance

The objective of this work is to correlate defected fuel performance, evaluate experimental results, and assess design consequences of defected fuel performance.

Design criteria for development of a fuel rod defect mechanism were formulated to simulate an end-of-life fuel rod failure. Correlation of defected fuel swelling with an analytical model based on sodium diffusion is in progress.

Figure 2-2. LIFE Code Prediction of Fuel-cladding Interface Pressure for Typical High Power LMFBR Fuel Rod

Figure 2-3. LIFE Code Prediction of Cladding Diameter Increase for Typical High Power LMFBR Fuel Rod

The detailed planning of this analytical task was completed in coordination with the experimental effort under Task 6. Task 2.1.3 participated in generating the B12 series request for Approval-in-Principle. This series of EBR-II irradiations will supply needed prototypical data on defected fuel performance in advance of FFTF irradiations at comparable burnup and in advance of design decision points for the Demonstration Plant fuel. The outline for the Design Basis Specification documentation was generated in conjunction with Task 6.

The proposed design criteria for the defect mechanism development program was transmitted to Task 6. The principal criteria for simulated end plug defects is to assure entrance of sodium to the fuel rod. The exact defect geometry is not considered critical since end plug defects are not expected to suffer degradation in reactor.

Fuel region defects are not expected early in the fuel rod life; therefore the principal defect design criteria is to simulate the expected cladding failure at high burnup. The program for the development of a defecting mechanism should thus attempt to defect the rods with an intergranular crack, running longitudinally under a wire wrap. This mode of cladding failure, which is expected at end-of-life, is due to a combination of fission-product cladding attack at the hottest cladding location under the wire wrap, fission gas pressure stress, possible fuel-cladding mechanical interaction, and longitudinal and local membrane and bending stresses due to thermal gradients, differential steel swelling, and spacer contact loads.

The principal potential hazard of continued operation with defected fuel is the fuel rod swelling due to formation of a low density fuel-sodium reaction product. Current understanding of the phenomena involved indicate that the swelling will be controlled by the rate of sodium diffusion into the fuel.

The problem of diffusion of sodium into mixed-oxide fuel through an axial line defect in the cladding was evaluated numerically. This solution was compared with a similar solution generated for diffusion from a line defect in a flat plate. For distances from the defect, small compared to the diameter of the rod, the two solutions were found to be very similar. The only differences encountered were near the flat plate boundary or cylindrical surface. In view of the similarities between the two solutions, the decision was made to concentrate on the flat plate solution due to its simplicity.

The solution for the flat plate geometry was extended to provide for a defect of finite width and length (of rectangular shape). Diffusion through a rectangular defect into a semi-infinite volume results in a time dependence of $t^{1/n}$ where n increases from 2 to infinity with the time. Thus, after long times, a steady state is approached where no further diffusion takes place.

The E1J-B2 swelling data were shown to fit this time dependence adequately for further analysis. Analysis of these experimental results is in progress prior to formulating a method of predicting future swelling experiments.

Fuel rod swelling is believed to be influenced by the change of defect geometry due to crack propagation. A literature survey was conducted to attempt to characterize the propagation of cracks in the cladding due to fuel-sodium reaction product swelling. No crack propagation data suitable for direct application were found, due to the specialized conditions of interest in high temperature austenitic stainless steel under a local strain limited loading mechanism. From the limited number of fuel failures experienced to date, the crack geometry has been characterized for use in the above swelling correlation effort.

2.2 FUEL ROD IRRADIATIONS – R. F. HILBERT

The objective of this subtask is to determine experimentally fuel rod behavior in three general design areas: High Burnup Irradiations, High Cladding Temperature Irradiations, and Operating Limits Irradiations. The status of the fuel rod irradiations in progress under this subtask is summarized in Table 2-3. This task includes correlations for use in fuel rod design based on data obtained from this program, other U.S. programs, and foreign programs.

2.2.1 High Burnup Irradiations

2.2.1.1 FO Experiment (Low Power Behavior of High Density Fuel)

The FO experiment consists of five encapsulated fuel rods containing fuel of 93 to 95% smeared density operating at a linear power of 5 to 8 kW/ft.

Rod FOE was destructively examined at 4 at.% burnup. Rods FOA and FOC completed irradiation to 9 at.% burnup during EBR-II Run 55 and have been shipped to LASL for post-irradiation examination.

Irradiation of rods FOB and FOD in an outer row of the core was continued after installation of the stainless steel reflector (after Run 55). Preliminary calculations supplied by the EBR-II project indicate that the power at the centers and ends of the rods was approximately 22% and 58% higher, respectively, during Run 56 relative to Run 55.

An interim examination has been requested to determine whether the change in power profile experienced by the rods during Run 56 relative to Run 55 has caused the rods to fail. If neither rod is determined to be failed by neutron radiography and gamma scanning, rod FOB will be destructively examined and rod FOD will continue irradiation. If one or both rods are determined to have failed, both rods will be destructively examined.

2.2.1.2 F4 Experiment (Behavior of Low Density Fuel at High Power)

The objective of the F4 experiment is to determine the performance of nine encapsulated fuel rods with 84 to 85% theoretical smeared fuel density, clad with solution annealed Types-304 and -316 stainless steel, and Incoloy 800, operating at high power.

Rods F4D and F4F were destructively examined at 4 at.% burnup. The remaining seven fuel rods reached 9 at.% peak burnup with >16 kW/ft peak start-of-life power and $\sim 1090^{\circ}\text{F}$ maximum cladding temperature. Neutron radiography and gamma scanning indicated that all seven rods were intact.

The destructive post-irradiation examination of rods F4A and F4E is in progress at LASL.

A summary of fabrication and operating parameters and partial examination results for rods F4A and F4E are presented in Table 2-4.

Results of the visual examination and gamma scanning were reported last quarter.²⁻¹ Visual examination showed both rods to be in good condition. Fuel rod F4A was bright and metallic in appearance while rod F4E was tarnished.

The gamma scans indicated the Zr-95 and Nb-95 distributions were nearly identical and conformed to the flux profile. The isotopes Cs-134, Cs-137, Rh-106 were found to be distributed nonuniformly over the length of each fuel rod.

Metallographic examination at the peak flux region of rods F4A and F4E indicated maximum cladding inside surface attack of 6 mils and 5 mils respectively. In contrast, metallography of the longitudinal sections from the peak temperature regions of each rod showed significantly less attack. Figure 2-4 shows the maximum attack observed in the peak flux region of fuel rod F4E. The maximum calculated cladding temperature of this section is 1080°F . The attack is intergranular with little or no general surface attack. Figures 2-5 and 2-6 show the observed attack in the peak temperature regions (1092°F) of rods F4E and F4A respectively. Little or no attack was observed in this region of rod F4E. Fuel rod F4A showed a maximum attack of 2.5 mils in the peak temperature region.

The observation of maximum attack in the peak flux region as opposed to the peak cladding temperature region contradicts results from past examinations. In previous post-irradiation examinations, metallography in the peak flux regions was performed on transverse specimens, while metallography in the peak temperature region was performed on longitudinal specimens. LASL personnel have indicated that longitudinal metallography samples typically show less cladding attack than do transverse samples even though sample preparation was identical. No explanation can presently be given for these observations either in terms of preparation techniques or in terms of fuel rod operating characteristics.

Immersion density measurements taken on sections of cladding from rods F4A and F4E are given in Table 2-5. These metal swelling data will be included in future design correlations.

Table 2-3.
SUMMARY OF TASK 2 FAST FLUX TESTING PROGRAM IN EBR-II^(a)

Group	Objectives	Total Number of Pins in Group	Date In	Projected Date Out	Actual Date Out	Number of Rods in S/A	S/A	Position	Range of Average Pin Powers ^(b) kW/ft	Target Burnup at. % MWd/Te	Average Calculated Burnup ^(c) at. % MWd/Te	Peak Calculated Fluence ^(d) (1E22 n/sqcm)	Total	E>0.1 MeV	Status	Group
F0	Low power shelling rate of high density fuel at various burnup levels	5	7/16/65		8/15/68 . . . 1		XG02	7A1	7.76	3.91 35000	3.34 30199	2.76 1.99			Post exam complete	F0
			7/16/65		8/8/70 . . . 2		XG03	7D1	5.47- 5.92	8.95 80000	6.88 62247	5.93 4.32			Reconstituted into XG03A	
			7/16/65		8/8/70 . . . 2		XG04	7B1	5.44- 6.24	11.18 100000	7.62 68916	6.62 4.90			Reconstituted into XG04A	
			9/14/70	4/23/72	. . . 2		XG03A	8D2	5.49- 6.12	8.95 80000	8.77 79354	7.71 5.58			Irradiation complete	
			9/14/70		. . . 2		XG04A	8A7	5.34- 6.08	11.18 100000	9.48 85760	8.40 6.16			Reconstituted in XG04B	
			7/ 1/72	12/22/72	. . . 2		XG04B	9C7	9.92-10.32	11.18 100000	9.85 89128	8.76 6.38			In reactor	
F1	Short time performance and proof test	6	5/ 6/65		5/23/65 . . . 6		XG01	4F2	12.32-14.49	0.22 2000	0.21 1882	0.17 0.15			Reported in GEAP-5570	F1
F2	Medium power scoping irradi- ation, major parameters include smear density, stoichiometry, cladding- coolant interactions and burnup	21	9/ 3/65		3/ 6/68 . . . 9		XG05	4C2	11.42-13.91	8.39 75000	6.69 60543	5.69 4.88			Post exam complete	F2
			9/ 3/65		2/20/67 . . . 12		XG06	4E2	8.48-14.83	5.59 50000	5.02 45451	4.22 3.62			Reported in GEAP-13549	
			2/23/69		5/18/70 . . . 4		XO50	4C2	10.73-12.86	11.19 100000	11.75 106358	10.34 8.87			Post exam complete	
F3A	Bare pin irradiation to evaluate fuel cladding and cladding-coolant interaction	16	8/15/68		6/30/69 . . . 16		XO40	5B2	12.87-14.65	5.62 50000	2.88 25959	2.36 2.00			Reconstituted into X040A	F3A
			10/ 4/69		5/18/70 . . . 16		XO40A	5B2	12.53-14.25	5.62 50000	5.17 46620	4.30 3.65			Reported in GEAP-13735	
F3B	Density and stoichiometry effects for encapsulated fuel pins	18	11/21/67		5/18/70 . . . 18		XO27	4B3	11.48-12.80	13.49 120000	8.86 79922	7.54 6.47			Six pins destructively examined	F3B
			3/10/72	7/16/73	. . . 8		X151	7A4	10.29-11.87	13.49 120000	10.12 91269	8.70 7.40			in reactor	
F4	Low density fuel behavior at high heat ratings	9	5/ 9/66		6/28/67 . . . 9		XO11	2F1	13.74-14.53	5.65 50000	3.64 32758	2.98 2.57			Reconstituted into X039	F4
			8/15/68		12/24/68 . . . 7		XO39	3C2	13.41-13.83	9.04 80000	4.85 43633	4.00 3.44			Reconstituted into X070	
			1/10/70		8/ 8/70 . . . 7		XO70	3E1	12.72-13.37	9.04 80000	8.25 74217	6.95 5.97			Post exam in progress	
F5	Fuel performance at peak for cladding temperatures	19	5/28/69		8/ 8/70 . . . 19		X064	4F2	11.63-13.75	11.24 100000	5.76 51970	4.80 4.12			Three pins destructively examined	F5
			3/10/72	7/16/73	. . . 11		X151	7A4	11.65-12.58	11.24 100000	7.05 63609	5.94 5.01			in reactor	
F6	Fuel-cladding interactions under accelerated swelling	4	3/24/66		4/24/69 . . . 4		XD10	7F3	8.59- 9.34	5.59 50000	5.71 51645	4.80 3.91			Post exam complete	F6
F7	Pre-irradiated axially restrained fuel prior to transient test, main variable is fuel density	6	12/23/67		12/23/67 . . . 2		X015	4A2	12.54-12.65	5.64 50000	5.40 48599	4.56 3.90			Transient tests in treat	F7
			1/23/69		5/18/70 . . . 4		X050	4C2	11.91-12.25	5.63 50000	5.43 48914	4.49 3.84			Transient tests in treat	
F8A	Effect of density on fuel swelling rate	16	1/13/67		8/ 2/69 . . . 7		X019	6D2	8.53- 9.15	5.62 50000	4.97 44777	4.12 3.42			Post exam complete	F8A
			1/13/67		1/31/70 . . . 9		X020	6B5	8.37- 9.11	11.25 100000	6.15 55398	5.24 4.38			Reconstituted into X081	
			5/24/70	10/10/72	. . . 9		X081	7E7	7.80- 9.82	11.24 100000	10.75 96961	9.36 7.51			In Reactor	
F8B	Effect of void deployment on fuel cladding reaction	19	7/25/68		8/24/70 . . . 19		XD36	7E1	4.99- 6.39	11.24 100000	4.73 42600	4.01 3.03			Reconstituted into X117 & X118	F8B
			2/24/71	6/17/75	. . . 9		X117	8B2	4.31- 5.49	11.25 100000	5.79 52161	5.01 3.65			In reactor	
			2/24/71	5/ 1/74	. . . 10		X118	8D7	4.97- 6.16	11.24 100000	6.72 60612	5.81 4.32			In reactor	

(a) All calculated power, burnup, and fluence numbers are based on the fission rates provided in the "Guide of Irradiation experiments in EBR-II."

(b) Values are the calculated axial average powers, ratio of peak-to-average power is 1.125.

(c) Burnup values are axial average values for the pin in the subassembly with the highest fuel burnup regardless of position in subassembly, all values are corrected for fuel depletion, burnup values in MWd/t are based on a value of 92 MeV/fission and calculated from data supplied by EBR-II.

(d) Fluence values reported are calculated for the peak axial flux for the pin in the subassembly located close to the center of the reactor core. Values are based on data supplied by EBR-II.

Table 2-3 (Continued)
SUMMARY OF TASK 2 FAST FLUX TESTING PROGRAM IN EBR-II

Group	Objectives	Total Number of Pins in Group	Date In	Projected Date Out	Actual Date Out	Number of Pins in S/A	S/A	Position	Range of Average Pin Powers ^(b) kW/ft	Target Burnup		Average Calculated Burnup ^(c)		Peak Calculated Fluence ^(d) (1E22 n/sqcm)		Status	Group
										at. %	MWd/Te	at. %	MWd/Te	Total	E>0.1 MeV		
F9A	High power, high burnup unencapsulated fuel pin irradiations	37	2/20/69		5/18/70	37	X043	4D2	12.06-13.36	11.25	100000	5.57	50189	4.64	3.98	Reconstituted into X043A In reactor	F9A
			10/27/70	2/27/73		37	X043A	7D4	11.00-12.06	11.25	100000	9.18	82783	7.87	6.66		
F9B	Medium power, high burnup unencapsulated fuel pin irradiations	37	5/23/69	3/16/73		37	X862	7F2	10.64-11.74	11.24	100000	8.76	78960	7.49	6.22	In Reactor	F9B
F9C	High power, high burnup unencapsulated fuel pin irradiations	37	4/ 3/69		7/16/70	37	X056	5C2	10.50-11.55	11.24	100000	5.67	51157	4.72	4.01	Reconstituted into X056A	F9C
			10/27/70		5/13/71	37	X056A	6B2	10.20-11.71	11.24	100000	6.68	60207	5.61	4.76	Reconstituted into X143 & X144	
			2/ 2/72	9/ 7/72		19	X143	6A1	11.50-12.48	11.25	100000	8.02	72293	6.80	5.75	In Reactor	
			2/ 2/72	4/29/73		19	X144	6B4	11.99-13.45	11.24	100000	8.25	74403	6.99	5.93	In Reactor	
F9D	Medium power, high burnup, unencapsulated fuel pin irradiations	37	4/24/69	6/18/73		37	X058	7C1	9.61-11.29	11.24	100000	8.38	75563	7.24	5.72	In Reactor	F9D
F10A	Cladding performance at peak cladding temperatures	19	2/ 2/72	7/18/74		19	X121	6D3	12.97-15.52	11.01	100000	2.22	20461	1.85	1.56	In Reactor	F10A
F10B	Cladding performance at peak cladding temperatures	19	12/ 5/71	6/ 6/74		19	X122	6D4									
F11A	Cladding performance at peak FOR cladding temperatures	37				37	X141									Shipped to EBR-II	F11A
F20	Irradiation to probe operating limits	19														Fabrication in Progress	F20

Table 2-4
SUMMARY OF ROD F4A AND F4E DESIGN AND IRRADIATION PARAMETERS

Fuel	F4A	F4E
Wt.% Pu (91% enriched in Pu-239)	20	30
Stoichiometry (O/M)	2.00	2.00
Fabrication Process	Coprecipitated	Coprecipitated
Configuration	Solid Pellet	Solid Pellet
Average Pellet Density (% Theoretical)	89.1	89.8
Smeared Density (% Theoretical)	86.4	86.6
Diametral Gap Range (mils)	2.6 to 4.9	3.2 to 5.3
Cladding		
Alloy	304 SA ^(a)	316 SA ^(a)
Wall Thickness Range (inches)	0.0141 to 0.0146	0.0149 to 0.0152
Nominal ^(b) Irradiation Conditions		
Peak Burnup (at. %)	8.9	9.22
Peak Linear Power, kW/ft (SoL/EoL)	17.6/14.4	18.7/14.8
Peak Fluence (>0.1 MeV) 10^{22} n/cm ²	5.87	5.97
Peak Inside Surface Cladding Temperature of (SoL/EoL)	1065/993	1092/1006
Postirradiation Examination Results		
Maximum Cladding Diameter Increase (mils %)	4.5 (1.8)	6.0 (2.4)
Maximum Cladding Chemical Attack Observed at Peak Flux Region (mils)	6	5
Fission Gas Release	93.3	81.6
Measured Peak Burnup (at. %)	7.7	8.0
Ratio, Measured Burnup/Calculated Burnup	0.86	0.87

(a) SA = solution annealed

(b) "Nominal" operating conditions are based on values recommended in the *Guide for Irradiation Experiments in EBR-II*, Revision 4, February 1972.

Figure 2-4. Maximum Cladding Attack Observed Near Peak Flux Region of Fuel Rod F4E. Maximum Cladding Inside Surface Temperature (at Start-of-Life) was 1080°F. It Occurred at Start-of-Life in This Region.

2B127

Figure 2-5. Typical Cladding Appearance in Peak Temperature Region of F4E. Maximum Cladding Inside Surface Temperature in this Region (at Start-of-Life) was 1092°F.

2B125

Figure 2-6. Maximum Cladding Attack Observed in Peak Temperature Region of Rod F4A. Maximum Cladding Inside Surface Temperature (at Start-of-Life) was 1065°F.

Table 2-5
CLADDING IMMERSION DENSITY MEASUREMENTS FROM FUEL RODS F4A AND F4E

Fuel Rod	Cladding Type	Specimen Number	Distance from Bottom of Fuel (in.)	Fluence (>0.1 MeV) 10^{22} n/cm ²	Density R/ρ^0	Midwall Temperature (°F) ^(a)	
						Start of Life	End of Life
F4A	Type-304 SA	B	3.5	4.86	∓ 2.78	975	922
		F	7.5	5.04	∓ 4.42	1018	954
		H	12.5	3.58	∓ 2.28	1025	958
F4E	Type-316 SA	B	3.5	4.93	∓ 3.40	995	931
		F	7.5	5.11	∓ 4.50	1040	965
		H	12.5	3.64	∓ 2.06	1047	967

(a) Calculated from values recommended in the *Guide for Irradiation Experiments in EBR-II*, Revision 4, February, 1972.

2.2.1.3 FBA Experiment (Effect of Fuel Density on Fuel Behavior Rate)

The objective of the F8A experiment is to evaluate fuel rod growth at medium power as a function of fuel density. The F8A experiment consists of sixteen encapsulated fuel rods clad with solution annealed Types-304, -316, and -347 stainless steel and Incoloy 800. Fuel smeared density ranged between 84 and 90% theoretical.

The irradiation of seven rods was terminated at 5 at.% burnup. At this time, four rods were destructively examined (the other three are in storage). The remaining nine rods are scheduled for discharge after Run 58 at a peak burnup of 12.2 at.%. The plan for destructive examination of these seven rods was completed (the remaining two will be stored).

2.2.1.4 F8B Experiment (Effect of Void Deployment on Fuel-Cladding Interaction)

The objective of the F8B test is to determine the effect of void deployment on fuel rod performance during low power operation. The F8B experiment consists of nineteen encapsulated fuel rods, with smeared densities of 83 to 92% theoretical, cladding with solution annealed Types-304 and -316 stainless steel, 20% cold-worked Type-304 stainless steel, and Incoloy 800. Fuel configurations include: solid pellets, annular pellets, dished pellets, and vipac fuel. The rods have operated at linear powers in the range from 5 to 10 kW/ft to ~ 7 at.% burnup.

Rod F8B-5 was discovered to be damaged during reconstitution of the subassembly after an interim examination at EBR-II. The capsule remained intact, but could not be reinserted into a subassembly. The rod is in storage at EBR-II pending shipment to LASL for destructive examination.

The F8B experiment, which now consists of two subassemblies each containing nine rods, has not operated in EBR-II since the stainless steel reflector was installed. Preliminary calculations obtained from the EBR-II project indicate that there will be a significant change in the axial power profile with the reflector, relative to Run 55 (without the reflector), in the outer rows of the core where this low power experiment operates. Estimates of the total increase of power, in the reflected core relative to Run 55, range as high as 33% near the ends of some rods. The possibility of rod failure after such power changes is sufficient to justify withholding one of the subassemblies from the core until an interim examination can be performed on the other subassembly after one cycle of irradiation in the reflected core.

2.2.2 High Cladding Temperature Irradiations

2.2.2.1 F11A Experiment (Fuel Rod Performance at High Cladding Temperature)

The objective of the F11A Experiment is to investigate the performance of unencapsulated fuel rods clad with Types-304, -316, -321, and -347 stainless steel in several metallurgical conditions at cladding temperatures near the peak expected in near term LMFBRs. The fuel rods will be irradiated in a wire-wrap spaced subassembly.

Fabrication of four replacement rods has been completed. The rods were shipped to EBR-II for inclusion in subassembly X141.

2.2.2.2 F10A and F10B Experiments (Fuel Rod Performance at High Cladding Temperature)

The objective of the F10A and F10B experiments is to investigate the performance of fuel rods clad in several alloys at high linear power (16 kW/ft) and high cladding temperatures (up to 1275°F). The F10A and F10B experiments had been irradiated to 2.2 and 2.8 at.% burnup, respectively, at the end of Run 56.

Each of these experiments utilizes the Mark J-19 subassembly as the irradiation vehicle. The Mark J subassembly provides coolant bypass flow using concentric hexagonal tubes with a gas-filled insulating space between them. During a recent interim examination of the HEDL P-23A experiment, which utilizes a Mark J-37A subassembly, sodium was found to be present in the gas annulus space. This experience raises concern as to whether such a problem is generic to the Mark J subassembly design.

An analysis was made to determine possible temperature distributions for fuel rods in assemblies F10A, F10B, and F11A. It was found that if sodium inadvertently fills the gas insulating gap, the total temperature drop for all cases is small, and that the experiment objectives will not be seriously affected.

A comparison of calculated temperature differences around an edge rod for intact and sodium filled insulating gas annuli is given in Table 2-6. Table 2-6 summarizes the maximum circumferential temperature difference in the coolant surrounding an edge rod in each subassembly. The increase in temperature difference with sodium in the gas annulus for experiments F10A, F10B, and F11A is 18°F, 6°F, and 45°F respectively. These increases are reasonably low and the experiments will proceed as scheduled.

Table 2-6
COMPARISON OF CALCULATED TEMPERATURE DIFFERENCES
IN THE COOLANT AROUND AN EDGE ROD FOR INTACT
AND SODIUM-FILLED INSULATING GAS ANNULUS
IN EXPERIMENTS F10A, F10B, AND F11A

	F10A	F10B	F11A
Gas Annulus Intact	33°F	45°F	183°F
Gas Annulus Filled with Sodium	51°F	51°F	228°F
Increase in Temperature Difference with Sodium in Gas Annulus	18°F	6°F	45°F

Figures 2-7, 2-8, and 2-9 show the temperature distribution in the sodium coolant across the fuel rod bundle for experiments F10A, F10B, and F11A. In these figures the solid lines labeled "with conduction" represent the case of sodium in the gas annulus. The dashed lines labeled "without conduction" represent the case of an intact gas annulus. Figures 2-7 and 2-8 for experiments F10A and F10B respectively show that the temperature distribution across the bundle is quite flat with a nominal drop off in temperature across the edge rod. [Note: In Figures 2-7, 2-8, and 2-9 only the left side of the subassembly has been analyzed for the case of a sodium filled gas annulus. The slight drop in temperature to the right across the bundle is due to the radial power profile.] It is noted that the points marked 0.0 and 1.0 do not represent the inner surfaces of the subassembly but rather the center of the coolant nodes next to the subassembly walls. Figure 2-9 shows a large temperature drop across the edge rod for experiment F11A. This calculated large drop in experiment F11A compared to experiments F10A and F10B is due to the presence of solid triangular spacers, which reduce the flow area in the edge coolant channels in experiments F10A and F10B. Experiment F11A does not have such flow restrictors.

The analysis indicates that the metal liners and annulus space provide a good thermal barrier independent of whether sodium or argon is in the gas space.

Figure 2-7. Coolant Temperature Distribution Across Subassembly in Experiment F10A.

Figure 2-8. Calculated Coolant Temperature Distribution Across Subassembly in Experiment F10B.

Figure 2-9. Calculated Coolant Temperature Distribution Across Subassembly in Experiment F11A.

2.2.3 Operating Limits Irradiations

2.2.3.1 F20 Experiment (Short-Term Irradiation to Determine Power-to-Fuel-Melting Parameters)

The F20 experiment, containing 19 encapsulated fuel rods to be irradiated at >16 kW/ft for several days is designed to determine fuel rod operating limits, power-to-fuel-center-melting, and characteristics of operation with fuel center melting as a function of as-fabricated fuel-cladding gap, fuel density, and void deployment for both fresh and pre-irradiated fuel.

Fabrication of the fuel and cladding was completed and loading of fuel is in progress.

2.2.3.2 F9C Experiment (Run-to-Failure)

The objective of the F9C experiment is to operate unencapsulated fuel rods clad in solution annealed Type-316 stainless steel until failure occurs. Currently, thirty-eight rods are being irradiated in two Mark E subassemblies as part of the EBR-II run-to-failure program. Prior to reconstitution, the fuel rods operated at 14 kW/ft with 1075°F peak cladding temperature to 7 at.% burnup. At this time, fuel rods F9C-31 and F9C-13 were removed for destructive examination. Destructive examination at the General Electric Vallecitos hot cells indicated that rod F9C-31 was in excellent condition. Rod F9C-13, also in excellent condition, was shipped to LASL for destructive examination. The completed examination package was received from LASL during the quarter and is being evaluated. Results of the nondestructive examination, including gamma scanning, Betatron radiography, and dimensional measurements, were reported previously.^{2-1, 2-2}

The fabrication and irradiation parameters and examination results for the F9C-13 and F9C-31 fuel rods are summarized in Table 2-7.

Metallography of samples taken from the peak flux and peak cladding temperature regions showed no significant attack on the inside surface of this cladding. The maximum thickness of any reaction layer was less than one mil. Alpha autoradiographs gave no obvious indication of plutonium concentration near the central void.

Figure 2-10 is a montage of the transverse metallographic specimen from F9C-13 taken near the longitudinal center of the fuel column. Only a small diametral gap was observed to remain. This is contrasted to F9C-31 which exhibited a gap of approximately 2 mils.

Figure 2-11 is a macro-photograph of the longitudinal metallography specimen taken from the peak cladding temperature region of rod F9C-13. The fuel was observed to be well broken up with no indication of central void formation.

The burnup determination yielded a value of 5.84 at.% compared to a calculated value of 6.52 at.%. Fission gas release was measured to be 68%.

The transverse section of fuel shown in Figure 2-10 was examined with an electron microprobe. The microprobe examination was directed toward determining: (a) the radial distribution of uranium and plutonium across the fuel, (b) the constituents comprising the metallic particles near the cladding, (c) the constituents comprising the metallic particles near the central void, and (d) any peculiarities in composition of the dark band appearing near the outer edge of the fuel (Figure 2-10).

Point count scans with counts for uranium M_{α} and plutonium M_{β} X-ray intensities at 100 μm intervals were made across a fuel radius. The results, Figure 2-12, indicated increase in Pu concentration and a decrease in U concentration near the central void consistent with previous experience.

Particles, metallic appearance, were observed in the outer portion of the fuel near the cladding (Figure 2-13). Two-dimensional X-ray images (Figure 2-14) showed them to contain iron, chromium, nickel and palladium. Cesium and molybdenum were detected in the fuel near the cladding but were not detected in the metallic particles. Elements which were analyzed for but not detected were: Mn, Nd, Pr, Ce, Ba, I, Xe, Te, Rh, Ru, Tc, Nb, Zr, Sr, and Si. However, etching before the final polishing may have altered the fission product distribution.

Table 2-7
FABRICATION AND IRRADIATION PARAMETERS AND
EXAMINATION RESULTS FOR FUEL RODS F9C-31 and -13

	F9C-31	F9C-13
Fuel		
Wt % PuO ₂ (91% Pu-239)	24.1	25.4
U-235/U (%)	92.9	92.4
Stoichiometry (O/M)	1.97	1.98
Fabrication Process	Physically Mixed	Coprecipitation
Configuration	Solid Pellet	Solid Pellet
Pellet Density (% Theoretical)	88.4	92.9
Smear Density (% Theoretical)	84.3	87.4
Diametral Gap Range (mils)	3.8 to 6.0	6.0 to 7.5
Cladding		
Type	SA 316	SA 316
Fabrication Method	Seamless	Seamless
Wall Thickness (mils)	15	15
Outside Diameter (inches)	0.250	0.250
Irradiation ^(a) Conditions		
Calculated Peak Burnup (at. %)	7.26	6.52
Calculated Peak Fluence (10 ²² n/cm ²) > 0.1 MeV	4.64	4.13
Calculated Peak Linear Power, SoL/EoL (kW/ft)	13.4/12.7	12.5/11.3
Calculated Maximum Cladding Temperature, SoL (°F)	1033	935
Examination Results		
Measured Peak Burnup (at. %)	6.26	5.84
Measured Burnup/Calculated Burnup	0.86	0.90
Maximum Cladding Diameter Increase (mils)	1.6	1.0
Maximum Cladding Attack (mils)	<1	<1
Fission Gas Release (%)	74	68

(a) Nominal operating conditions are based on values recommended in the *Guide for Irradiation Experiments in EBR-II*, Revision 4, February 1972.

Figure 2-10. Transverse Metallographic Section Near Peak Flux Region of Rod F9C-13

Figure 2-11. Longitudinal Metallographic Section Taken at Top of Fuel Column on Rod F9C-13

A. DISTRIBUTIONS OF URANIUM AND PLUTONIUM AS INDICATED BY X-RAY INTENSITIES OBTAINED DURING A RADIAL SCAN ACROSS THE FUEL.

B. RATIOS OF URANIUM TO PLUTONIUM X-RAY INTENSITIES (U/Pu) CORRESPONDING TO SCAN OF ABOVE

Figure 2-12. Electron Microprobe Analysis for Radial Plutonium Distribution at Peak Flux Region on Rod F9C-13.

Other metallic appearing particles were observed throughout the columnar-grained region and into the equiaxed region. These particles were found to contain Mo, Tc, Ru, and Rh as shown by the two-dimensional X-ray images of Figure 2-15. Molybdenum was the major constituent; the other three elements were not detected in some of the smaller particles (Figure 2-15). Zirconium was also detected in some of the particles. Elements which were analyzed for but not detected were: Fe, Ni, Mn, Cr, Nd, Pr, Ce, Ba, Cs, Xe, I, Te, Pd, Nb, Sr and Si.

A dark ring was visible in the outer portion of the fuel (Figure 2-10). The darkened appearance is attributed to the presence of fine porosity too small to reflect light. Point count scans across this region showed Zr to be the only element which differed in concentration in the dark region compared to adjacent fuel (Figure 2-16).

Elements which were analyzed for and showed no appreciable concentration difference in the dark band were: U, Pu, Mo, Ru, Rh, Pd, Te, Cs, Ba, Ce, Pr, and Nd. The elements Si, Sr, Nb, Tc, I, Xe, and Mn were analyzed for but not detected in this region. The appearance of a dark band was also noted in fuel rod F1D²⁻³ but was much more marked than in F9C-13. Examination of F1D did not give a clear indication of the cause of the dark band.

2.2.4 Fuel Rod Design Correlations

This portion of the fuel rod irradiations work is aimed at providing design correlations as well as evaluating current design correlations and models in many aspects of fuel rod behavior for use in LMFBR core design. Correlations are based on data obtained from this program and other U.S. and foreign programs. Since, at the present time, the main LMFBR fuel rod design tool is the LIFE-II code, a major focus of this effort will be to evaluate the correlations and deterministic models to be used in LIFE-II in terms of available experimental data.

Figure 2-13. Photomicrograph Showing Metallic Particles in Fuel Near Cladding. Square Indicates Area Covered by Scans to Produce X-ray Images Shown in Figure 2-14.

Figure 2-14. X-ray Images of Iron, Nickel, Chromium, Palladium, Molybdenum, and Cesium Distributions in Region Indicated in Figure 2-13. Elements Analyzed for, but not Detected by this Technique: Mn, Nd, Pr, Ce, Ba, I, Xe, Te, Rh, Ru, Tc, Nb, Zr, Sr, and Si.

A. PHOTOMICROGRAPH OF FUEL NEAR CENTRAL VOID. SQUARE INDICATES AREA COVERED BY X-RAY IMAGES IN B BELOW.

Mo

Tc

Ru

Rh

→ | ← 25 MICRONS

B. X-RAY IMAGES SHOWING PRESENCE OF MOLYBDENUM, TECHNETIUM, RUTHENIUM, AND RHODIUM IN PARTICLES OF METALLIC APPEARANCE. ELEMENTS ANALYZED FOR, BUT NOT DETECTED: Ni, Fe, Mn, Cr, Nd, Pr, Ce, Ba, Cs, Xe, I, Te, Pd, Nb, Sr, AND Si.

Figure 2-15. Electron Microprobe Analysis of Fuel Near Central Void at Peak Flux Region on Rod F9C-13

Figure 2-16. Distribution of Zirconium X-Ray Intensities Across Portion of Fuel Including Dark Ring. (Rod F9C-13).

2.3 MATERIAL PROPERTIES – H. J. BUSBOOM

The technical and economic feasibility of ceramic-fueled fast reactors depends on the attainment of a high performance level for core structural members. The materials properties program has two major objectives in ensuring the economic performance of fast reactor core materials:

1. To maintain a continuous interface with the core design to identify the principal areas of concern in materials performance and provide material properties correlations to establish structural design criteria, and
2. To identify the critical experiments necessary to provide data for input to materials properties correlations and perform those experiments as necessary.

2.3.1 Design Correlations

The development of structural material properties correlations has been combined into one work element in order to provide a sharp interface with the development of design criteria. The initial effort was placed on ductility of irradiated Type-316, 20% cold worked stainless steel. In the near future, it is planned to provide or update design correlations on:

1. Creep Rupture
2. Irradiation Creep
3. Irradiation-Induced Swelling
4. Probability Distributions on Irradiated Properties
5. Fuel Cladding Attack

1.1 In-Reactor Ductility

The ductility of 20% cold worked Type-316 stainless steel has been projected as a function of fluence (to 3×10^{23} n/cm²), temperature (700 to 1400°F) and strain rate (10^3 h⁻¹ to 10^{-6} h⁻¹), as shown in Figures 2-17 to 2-27. At the present time, a total of 10 tensile tests have been performed on 20% cold worked Type-316 stainless steel, which had been irradiated in EBR-II and tested at the same temperatures.^{2-4, 2-5, 2-6}

The highest fluence received by these specimens was 2.3×10^{22} n/cm² ($E > 0.1$ MeV), but most of the specimens received a fluence less than 1×10^{22} n/cm². The limited data available required a large extrapolation to be made to target demonstration plant exposures (2.3×10^{23} n/cm²). This extrapolation indicates that the nominal steady-state plastic strain will be greater than 0.1% during the life of the Demo plant core.

In projecting ductility, the following judgments have been made:

1. Uniform elongation is the significant measure of ductility for design purposes. Since plastic instability occurs at strains above this, the ductility beyond uniform strain is not useful or significant in design.
2. Irradiation at temperatures above 800°F appears to improve the post-irradiation uniform elongation of 20% cold worked Type-316 stainless steel due to thermal annealing (Figure 2-17).²⁻⁵ However, it is felt that this improvement is only temporary. As higher fluences are reached, the uniform elongation will continuously decrease with increasing fluence. Therefore it is assumed that as the cold work is annealed out during elevated temperature irradiation, the 20% cold worked material will exhibit ductilities similar to annealed Type-316 stainless steel irradiated to high fluences. For a conservative design, it is recommended that the uniform elongation of the unirradiated 20% cold worked Type-316 stainless steel should be used (Figures 2-19 through 2-26).
3. At this time it is necessary to project ductility based on the presumption that post-irradiation measurements are representative of in-pile steady-state behavior.

There are several uncertainties in even the initial unirradiated tensile properties. The tensile properties of unirradiated 20% cold worked Type-316 stainless steel as tested by ORNL²⁻⁵ and HEDL²⁻⁴ are quite different. For example, at 800°F the ORNL data give an ultimate strength of 139,000 psi while HEDL data show an ultimate strength of 101,000 psi. At all testing temperatures the ORNL 20% cold worked Type-316 stainless steel is stronger and less ductile. HEDL is presently doing some work to establish the properties of 20% cold worked Type-316 stainless steel as a function of the fabricating procedure.²⁻⁷ In the present study the HEDL uniform plastic strain is used as a base line.

2.3.1.2 Effects of Neutron Fluence

In developing the projected uniform strain for irradiated 20% cold worked Type-316 stainless steel, the following procedure was used:

1. Uniform plastic strain for both annealed and 20% cold worked Type-316 stainless steel was plotted as a function of fluence for each of the temperatures. (See Figures 2-19 to 2-26). In cases where uniform plastic strain was not given, the elastic strain was subtracted by using Figure 2-18. Recent experiments at GE-BRD have shown that irradiation-induced swelling decreases the elastic modulus. The decrease in elastic modulus is approximately twice that of swelling; e.g., for a density decrease of 5% the elastic modulus is reduced 10%.²⁻¹ Since the difference in elastic strain is rather minimal, compared to the other approximations, the effects of irradiation in the elastic strain are neglected in these calculations.
2. In cases where insufficient data for Type-316 stainless steel were available some Type-304 stainless steel data were also incorporated.²⁻⁸ These data came from EBR-II thimbles which have received the highest fast fluence (1.1×10^{23} n/cm²) of any stainless steel to date.
3. A "nominal" straight line was drawn through the data points on each of these graphs for extrapolation to higher fluences. The direction of this line is highly subjective. However, the decision regarding direction was

Figure 2-17. Uniform Strain of 20%CW Type-316 Stainless Steel as Function of Irradiation and Thermal Annealing at Different Temperatures. (2.5)

Figure 2-18. Elastic Strain of Unirradiated 20%CW Type-316 Stainless Steel as Function of Stress and Temperature.

weighed heavily by the direction of ductility trends at higher fluences and was not influenced much by ductilities at lower fluences or by ductilities of unirradiated material. This approach was taken because a mechanism like helium embrittlement is a function of helium content and at low fluences not enough helium would be present for the mechanism to be operable.

4. A "lower limit" line was drawn approximately parallel to the "nominal" line. This represents an engineering judgment that 95% of the data would have uniform plastic strains higher than the lower limit line.
5. Continuity of ductility trends as understood in terms of presently known mechanisms was maintained. For example, the data showed uniform plastic strains of 1.2, 4.0 and 0.4% at 1200, 1300 and 1400°F, respectively, for an extrapolated fluence of 1×10^{23} n/cm². However, one expects the degree of helium embrittlement to increase with increasing temperature, and these data are judged to be in error. Therefore, the uniform plastic strain at 1300°F was obtained by linear interpolation between the values for 1200 and 1400°F (to give 0.8).

The uniform plastic strains as a function of fluence are given in Figures 2-19 through 2-26. These strains are for strain rates of 0.02 min^{-1} . The actual data used are tabulated in Table 2-8.

2.3.1.3 Effects of Strain Rate

Steichen and Paxton²⁻⁹ have shown that at temperatures of 1000°F and below the uniform elongation of ferritic stainless steel is not dependent on strain rate. However, at temperatures above that the uniform elongation decreases with decreasing strain rate.²⁻¹⁰ Long-time creep-rupture tests on unirradiated Type-316 stainless steel have also shown lower ductilities (primary creep strain + secondary creep strain) with long (5000 hours) rupture lives.²⁻¹¹

Figure 2-19. Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 700°F.

Figure 2-20. Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 800°F.

Figure 2-21. Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 900°F.

Figure 2-22. Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1000°F.

Figure 2-23. Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1100°F.

Figure 2-24. Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1200°F.

Figure 2-25. Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1300°F.

Figure 2-26. Uniform Plastic Strain of Irradiated Type-316 Stainless Steel at 1400°F.

There are absolutely no data available on effects of strain rates on ductility of irradiated 20% cold w Type-316 stainless steel, and only a few data points on irradiated annealed Type-316 stainless steel. Consequently use was made of tensile and creep strain data on strain-rate effects concerning unirradiated annealed Type-316 stainless steel. The tensile and creep data were incorporated into Figure 2-27. Here the uniform plastic strain for the different temperature has been normalized for a strain rate of 0.02 min^{-1} (most irradiated specimens have been tested at that strain rate).

Some recent post-irradiation creep-rupture tests on irradiated Type-316 stainless steel have shown larger strains than those exhibited by irradiated Type-316 stainless steel in tension tests at equivalent test temperatures.²⁻¹² However, it is believed that these high strain values are due to annealing of radiation damage during the long time testing at elevated temperatures.

Since no data on strain rate effects are available for the irradiated materials, or even for unirradiated 20% cold worked Type-316 stainless steel, it is recommended that the correction factor given in Figure 2-27 be applied to strain limits for irradiated 20% cold worked Type-316 stainless steel as given in Figures 2-19 through 2-26 if prediction of behavior at rates other than 0.02 min^{-1} is required.

2.3.2 L-15 Experiment

The L-15 experiment is designed to reduce uncertainties in design limit criteria related to the decrease in ductility caused by neutron fluence. Precut tensile specimens are irradiated and then tested.

The four L-15 capsules identified as LOA, LOB, LOC, and LOD were returned from EBR-II after accumulating a fluence of approximately $3 \times 10^{22} \text{ n/cm}^2 \text{ E} > 0.1 \text{ MeV-GE}$ in a temperature range of 1150 to 1350°F and are awaiting disassembly at Vallecitos Nuclear Center, Pleasanton, California. The capsules contain 288 precut tensile specimens fabricated from Types-321, -316, -347, and -304 stainless steels and Incoloy 800.

Figure 2-27. Effect of Strain Rate on Uniform Plastic Strain of Irradiated 20%CW Type-316 Stainless Steel at Different Temperatures.

Table 2-8
DUCTILITY OF IRRADIATED STAINLESS STEEL

Test Temperature (°F)	Material Condition	Fluence (10^{22} n/cm ²) E>0.1 MeV	Irradiation Temperature (°F)	Uniform Elongation (%)	Elastic Strain ^(b) (%)	Uniform Plastic Strain (%)	Reference
700	316-A	0.68	820	18.7	0.34	18.4	2.4
700	316-A	2.3	825	5.0	0.50	4.5	2.4
700	316-A	1.3	900	9.0	0.36	8.6	2.4
700	304-A	10.7	750	0.3	(a)	0.3	2.9
700	304-A	9.4	715	0.65	(a)	0.65	2.9
700	304-A	6.6	700	0.5	(a)	0.5	2.9
700	304-A	4.5	700	0.5	(a)	0.5	2.9
700	304-A	2.8	700	0.5	(a)	0.5	2.9
700	304-A	7.6	730	0.3	(a)	0.3	2.9
700	304-A	7.1	710	0.8	(a)	0.8	2.9
700	304-A	5.9	700	0.7	(a)	0.7	2.9
700	304-A	4.1	700	0.6	(a)	0.6	2.9
700	304-A	2.8	700	0.6	(a)	0.6	2.9
700	304-A	3.0	750	0.7	(a)	0.7	2.9
700	304-A	1.6	704	2.7	(a)	2.7	2.9
700	304-A	0.9	704	3.1	(a)	3.1	2.9
800	316-A	0.31	700	36.3	0.29	36.0	2.4
800	316-A	0.52	700	14.2	0.34	13.9	2.4
800	316-A	0.12	720	43.5	0.30	43.2	2.4
800	316-A	1.3	740	9.7	0.36	9.3	2.4
800	316-A	1.6	740	4.5	0.38	4.1	2.4
800	316-A	3.4	740	1.1	0.46	0.64	2.4
800	316-A	2.3	750	5.2	0.41	4.8	2.4
800	316-A	3.2	750	1.4	0.47	0.93	2.4
800	315-A	2.5	760	1.7	0.43	1.3	2.4
800	316-A	0.9	820	18.1	0.38	17.7	2.4
800	316-A	1.3	850	9.8	0.38	9.4	2.4
800	316-A	2.4	850	3.0	0.42	2.6	2.4
800	316-20 CW	0.74	1225	5.2	0.39	4.8	2.4
800	304-A	9.4	795	0.5	(a)	0.5	2.9
800	304-A	6.6	840	0.4	(a)	0.4	2.9
800	304-A	5.9	780	0.8	(a)	0.8	2.9
800	304-A	4.1	815	1.1	(a)	1.1	2.9
800	304-A	2.8	845	2.0	(a)	2.0	2.9
900	316-A	0.12	720	42.1	0.31	41.8	2.4
900	316-A	0.68	730	28.3	0.34	28.0	2.4
900	316-A	0.17	775	41.3	0.30	41.0	2.4
900	316-A	1.3	825	8.8	0.36	8.4	2.4
900	316-A	2.4	850	2.2	0.40	1.8	2.4
900	316-A	1.9	850	4.9	0.41	4.5	2.4
900	316-A	1.3	860	12.7	0.35	12.3	2.4
900	316-A	1.3	860	12.8	0.35	12.4	2.4
900	316-A	1.6	860	11.4	0.35	11.0	2.4
900	316-A	0.84	870	28.0	0.29	28.6	2.4
900	316-A	0.58	900	10.5	0.38	10.1	2.4
900	316-A	0.75	900	4.4	0.39	4.0	2.4
900	316-A	0.75	935	27.8	0.31	27.5	2.4
900	316-A	0.33	935	23.2	0.31	22.9	2.4
900	316-A	1.6	960	12.2	0.38	11.8	2.4
932	316-25 CW	2.2	752	3.8	(a)	3.8	2.6

Table 2-8 (Continued)
DUCTILITY OF IRRADIATED STAINLESS STEEL

Test Temperature (°F)	Material Condition	Fluence (10^{22} n/cm ²) E>0.1 MeV	Irradiation Temperature (°F)	Uniform Elongation (%)	Elastic Strain ^(b) (%)	Uniform Plastic Strain (%)	Reference
1000	316-A	0.64	870	17.8	0.28	17.5	2.4
1000	316-A	2.3	870	7.9	0.35	7.5	2.4
1000	316-A	0.9	820	11.7	0.36	11.3	2.4
1000	316-A	0.64	870	18.1	0.28	17.8	2.4
1000	316-A	2.3	885	8.5	0.35	8.1	2.4
1000	316-A	0.58	945	30.4	0.33	30.1	2.4
1000	316-A	1.6	960	7.9	0.36	7.5	2.4
1000	316-A	2.4	1000	12.8	0.32	12.5	2.4
1000	316-A	2.1	1020	6.3	0.31	6.0	2.4
1000	316-A	0.75	1050	10.2	0.31	9.9	2.4
1000	316-20 CW	0.72	1225	3.3	0.39	2.9	2.4
1100	316-A	1.6	960	2.5	0.33	2.2	2.4
1100	316-A	3.0	1110	3.6	0.25	3.3	2.4
1100	316-A	0.63	1225	7.4	0.26	7.1	2.4
1100	316-A	0.64	870	11.0	0.25	10.8	2.4
1100	316-A	7.0	750	0.7	(a)	0.7	2.10
1100	316-A	6.8	910	2.0	(a)	2.0	2.10
1100	316-A	3.8	910	13.3	(a)	13.3	2.10
1100	316-A	7.0	750	0.3	(a)	0.3	2.10
1150	316-A	6.8	1000	2.9	(a)	2.9	2.10
1100	316-20 CW	0.64	1225	1.0	0.42	5.8	2.4
1200	316-A	0.58	1050	3.4	0.24	3.2	2.4
1200	316-A	2.7	1080	9.5	0.16	9.3	2.4
1200	316-A	2.7	1080	2.0	0.23	1.8	2.4
1200	316-A	2.1	1085	3.1	0.22	2.9	2.4
1200	316-A	2.7	1150	8.3	0.20	8.1	2.4
1200	316-A	0.64	1225	3.8	0.23	3.6	2.4
1200	316-A	3.7	1230	2.8	0.26	2.5	2.4
1200	316-A	4.4	1230	1.7	0.24	1.5	2.4
1200	316-A	1.1	1320	8.5	0.28	8.2	2.4
1200	316-A	2.4	1000	3.6	0.25	3.3	2.4
1200	316-A	1.6	1010	1.9	0.24	1.7	2.4
1200	316-A	5.6	1396	3.8	0.20	3.6	2.4
1200	316-20 CW	0.64	1225	0.9	0.32	0.58	2.4
1300	316-A ^(c)	1.1	1320	5.4	0.18	5.2	2.4
1300	316-A ^(c)	1.6	1320	6.2	0.17	6.0	2.4
1300	316-A ^(c)	2.3	1320	5.6	0.17	5.4	2.4
1400	316-A	4.4	1396	0.27	0.11	0.26	2.4
1400	316-A	5.6	1396	2.6	0.13	2.5	2.4
1400	316-A	7.0	1396	0.66	0.10	0.56	2.4
1400	316-20 CW	0.72	1225	0.68	0.22	0.66	2.4

A — Annealed

(a) Reported uniform strain is equivalent to uniform plastic strain

(b) Elastic strain obtained from Figure 2.3.2

(c) These data are believed to be grossly in error; see 2.3.1.2.

The thermal environment of these tensile specimens is complex since the capsules were irradiated in three different subassemblies. The irradiation history is shown below:

Subassembly	Grid Position	Reactor Runs	Reactor Power (MW)	Total Exposure (MWd)
X039	3C2	30A→32B	50	2532
X080	6E5	48C, D and E	62.5	5351
		49A, B, C and D		
X148	6B3	52B and C, 53, 54, and 55	62.5	5579

Total fluence accumulated by the capsules is 3×10^{22} n/cm², $E > 0.1$ MeV-GE. The irradiation temperature range is estimated to be 1150 to 1350°F (621 to 732°C). The number and types of specimens available for testing are shown in Table 2-9. All the specimens having a 1-inch gauge length are 0.1-inch wide and 0.015-inch thick. A precise definition of the test program awaits the results of the capsule disassembly and the thermal analysis. At the present time only 25 specimens are scheduled for testing. The 21 25% cold-worked Type-316 stainless steel specimens will be given first priority. A tensile test grip has been designed for use in the planned tests.

Table 2-9
L-15 TENSILE SPECIMENS

		Number of Specimens of Each Alloy				
Code	Condition	I-800	304	316	321	347
A	HT 1800°F 15 minutes	12	4	12	4	4
B	HT 1800°F 15 minutes plus 1650°F 24 hours	20	10	20	10	10
D1	25% CW longitudinal section	12		12	12	
D2	25% CW transverse section			9		
E3	HT 1800°F 15 minutes, longitudinal section, fine grain size	16		16	14	
E4	HT 1900°F 15 minutes, longitudinal section, course grain size	13		13	13	
F1	25% CW plus Garofalo HT, longitudinal section	15		15	13	
F2	25% CW plus Garofalo HT, transverse section			9		

HT = Heat Treatment

CW = Cold Worked

Garofalo HT refers to 900°F for 24 hours plus 1300°F for 216 hours.

2.3.3 L-16 Experiment

The objective of the L-16 experiment is to identify the temperature dependence of alloy swelling by irradiating prototypical fuel cladding tubing at various temperatures and monitoring the growth of these tubes as a function of fluence. The L-16 experiment has been designed to provide quantitative data on the swelling behavior of candidate cladding alloys including Types-304, -316, and -321 stainless steel as a function of temperature of irradiation, metallurgical condition, and fluence. The L-16 program plan has approval to utilize at least four consecutive EBR-II subassembly irradiations with an interim examination between each subassembly irradiation. Thus, new capsules will be incorporated into the next subassembly to provide a means of maintaining up-to-date experiments on cladding alloy swelling. The initial subassembly in the L-16 program is X098. All future subassemblies will be designated X098A, X098B, etc. The first subassembly, X098, irradiation was completed in April, 1972.

2.3.3.1 Subassemblies X098 Capsules

These initial 19 (L-16B type) capsules contain Type-304H and Type-316H tubular specimens. The irradiation exposure for the subassembly was completed after Run 55 at 12,041 MWd which resulted in an accumulated total fluence of 3.8×10^{22} n/cm²-GE. The subassembly was discharged, dismantled, and four capsules containing Type-304H, four containing Type-316H, and one containing Type-321, Type-347 and I-800 were included with ten fresh cold capsules for reconstitution into subassembly X098A. The other ten capsules discharged from subassembly X098 are in storage at the HFEF in Idaho and will be examined during the next quarter.

2.3.3.2 Subassembly X098A Capsules

The ten new L-16 capsules were incorporated into subassembly X098A. These capsules have been selected to provide a broader base for the L-16 program. Nine of these capsules contain a prime, stabilized grade stainless steel Type-321H which was double vacuum-melted, having been processed to optimize the creep properties for Type 321. Air-melted Type 321H will also be included in these L-16B capsules as a minor variable. The remaining capsule was one of the original three L-16D capsules; it was a cold capsule containing sodium-exposed ring samples. The two hot capsules containing highly irradiated ring samples from previously irradiated cladding were not available for subassembly X098A and will be incorporated into a later EBR-II MARK A-19 subassembly. The identification and operating characteristics for the capsules in subassembly X098A were given in the last quarterly report²⁻¹ under Task-L.

To date subassembly X098A has accumulated ~4,500 MWd's of EBR-II exposure which amounts to an accumulated fluence of 4.5×10^{22} n/cm² E > 0.1 MeV-GE on the Types-304 and -316 specimens and 1.2×10^{22} n/cm² E > 0.1 MeV-GE for the Type-321H specimens.

2.3.3.3 L-16D Capsules

The L-16D experiment is designed to investigate swelling saturation effects and the effect of pre-exposure to flowing sodium on metal swelling. Encapsulation of only one of the original three L-16D capsules was completed in time for reconstitution into subassembly X098A as described above. The remaining two hot inserts were shipped to HFEF in Idaho for encapsulation.

The capsule inserts are presently being encapsulated hopefully in time for insertion in subassembly X167 which is scheduled for irradiation during Run 59.

2.3.4 L-17 Experiment

The objective of the L-17 experiment was to define the effects of both sodium and neutron fluence on the mechanical properties of fuel rod cladding. This experiment was deferred by AEC-RDT on the basis that the types of data will be generated by other experimenters, specifically ANL. At the recent RDT Review of PA-10 Tasks 2 and 6 (August 15 and 16, 1972), it was pointed out that the present ANL concept was largely concerned with sodium effects and the effect of stress was not being considered. As a result of the ensuing discussion, an agreement was reached whereby GE will provide constructive comments with respect to the ANL concept and suggestions or recommendations for change which would satisfy LMFBR Demonstration Plant design requirements.

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3. FUEL ASSEMBLY ENGINEERING

Task Leader: J. R. Punches

The overall objective of this task is to contribute to the design of the LMFBR fuel assembly. All aspects of fuel assembly design are considered and include the following work areas:

- Grid-Spaced Assembly Design
- Wire-Wrap Assembly Design
- Major Fuel Assembly Hardware Development
- Fuel Assembly Design and Operating Limits

Focus is placed on the priority needs of the fuel assembly design by treating the fuel rod, fuel rod support, and channel as an integrated system of components.

3.1 GRID-SPACED ASSEMBLY – J. MARSHALL

The main purpose of this subtask is to develop a grid-spaced fuel assembly for the fast breeder reactor. The scope and objectives of the development program are to:

- Develop and build four grid-spacer driver assemblies for initial FTR core loading (GSA 1, 2, 3, and 4)
- Build two complete prototypic mock-up assemblies for trial assembly and cell handling practice
- Plan and execute an irradiation test program aimed at achieving driver fuel peak burnup capability in excess of 100,000 MWd/Te
- Provide a backup or replacement grid-spaced driver fuel assembly design for FTR
- Confirm the grid-spacer design by a program of out-of-pile testing and development, and by irradiation tests in EBR-II
- Perform the analyses necessary to back up the design and program objectives

This subtask is a continuation of work previously performed and reported under PA-10 Task G-6.

3.1.1 Diagnostic Grid Spacer Experiment

The F9E group irradiation is the first in-pile test of a grid-spaced fuel assembly utilizing the wave-beam grid-spacer concept, which was conceived by General Electric. F9E employs three different variations of the basic wave beam concept to obtain test information on the effect of these design alternatives.

In addition, F9E is a "diagnostic" irradiation test of an LMFBR grid-spaced assembly. "Diagnostic" is used in the sense that the experiment is designed to produce conditions in the EBR-II assembly that simulate design limiting conditions in the LMFBR. Assembly design methods can be evaluated and improved based on a comparison of predicted and actual grid assembly performance.

In order to obtain diagnostic information for grids irradiated to a high fluence, the loads, stresses and strains have been amplified through design and material selection. Post-irradiation observations will be compared to analytical predictions to increase understanding of grid performance.

The F9E test will provide a direct comparison of three grid spacer concepts:

- Full Grid
- Staggered Grid
- Full Grid Modified Profile

-1

The F9E experiment commenced irradiation in Reactor Run 56 (start-up date July 1, 1972) and, at the end of Run 58 (October 15, 1972), had achieved a peak pellet burnup of 2.0 A/O.

F9F is the second in a series of GE grid-spaced fuel assembly experiments planned for irradiation in EBR-II (F9E was the first). F9F was designed to provide fuel assembly test conditions and data of direct support to the optimized FTR grid-spaced driver fuel assembly being developed by General Electric.

The F9F assembly was designed primarily to determine:

- The adequacy of the design to withstand mechanical interaction between the bundle and the channel
- The ability of the complete staggered-grid system to restrain rod bowing
- The adequacy of the tab-notch concept of fixing grids to channels
- The behavior of grids made of cold-worked Type-316 stainless steel in a reactor environment.

In the F9E design, the principal modeling variation was to substitute annealed steel for cold-worked steel to obtain high relative rod-to-grid swelling on a shortened time scale. In the design of F9F, the principal modeling variation is to increase the distance between the edge fuel rods and the channel to cool the channel and obtain high beam-to-channel and rod-to-channel relative growth caused by swelling. This will be the first grid test to experience appreciable amounts of these growths. The structural material used for the fuel rods, the grid spacers, and the channel will be 20% cold-worked, Type-316 stainless steel. F9F will use the staggered grid concept exclusively in the axial core region; this is the reference design for the FFTF optimized grid-spaced driver fuel assembly being developed under this task. All of the fuel rods will be similar to the FFTF design. The target operating conditions will be similar to previous Group F9 series irradiation, i.e., maximum cladding outside temperatures in the range of 1050 to 1200°F and linear powers in the range of 14 to 16 kW/ft.

Figure 3-1 shows the complete F9F assembly. The fuel rod lower support was modified from that previously used to support the F9E bundle; the new support is compatible with the EBR-II irradiation subassembly MK-S1. Parts numbered 7 and 8 on Figure 3-1 replace the grid bar fuel rod support assembly used for F9E. The lower end plug of the fuel rod is also changed. The end plug is engaged in a tapered hexagonal opening in the fuel rod support plate. This results, in total, in positioning the end plug axially, laterally and against rotation. The hold-down comb, part number 8, engages the 37 rods, restricting the axial motion of the rods. Removal of the hold-down comb allows the removal of any fuel rod without removing and dismantling the inner channel, which contains the grid-spaced subassembly. The hold-down comb also secures the inner channel subassembly to the total assembly, allowing the inner channel subassembly to be attached to or separated from the total assembly without welds.

This experiment has been loaded into the core of EBR-II for commencement of irradiation in Reactor Run 59 (October 29, 1972).

3.1.2 Grid Spacer Assembly Experiment

It is proposed to irradiate, in the FTR, four full-size grid-spaced fuel assemblies. These will be placed in normal core driver fuel positions, two in each fuel enrichment zone of the reactor.

The purpose of the test is to demonstrate, under prototypic LMFBR conditions, that the performance of the fuel assembly is not limited by the grid-spacer support system, and that the inherent geometrical and thermal hydraulic characteristics will permit a grid-spaced assembly to achieve higher burn-up than a corresponding wire-wrap design.

Figure 3-1. EBR-II Fuel ASM-(F9F)

The out-of-pile development work leading to the fabrication of the reactor assemblies includes the fabrication of two full-scale mock-up assemblies, one of which will be used for hydraulic testing and the other for practice in performing interim examination on irradiated assemblies. Other development work includes mechanical tests to prove the grid-spacer design, a full-scale friction test in sodium where the effects of rod to grid movement will be investigated, and a high-temperature sodium flow endurance test.

3.1.2.1 Design and Fabrication

A detailed redesign of locking tabs which attach the grid spacer to the channel is being performed to overcome the grid dislodgement experienced during the assembly of the previous 217-rod hydraulic test assembly. This redesign is being backed up by a series of mechanical tests. Preliminary design of the assembly is rescheduled for completion in January, 1973.

Material has been ordered from the FFTF Project (HEDL) from which GE will fabricate two complete mock-up grid-spaced assemblies. One of these is scheduled to be a hot cell practice assembly, to verify interim and final examination procedures. The second assembly is to be used in full scale hydraulic testing.

3.1.2.2 Mechanical Testing and Development

A program of mechanical tests has been planned and initiated. These are aimed at improving the mechanical design of the attachment of the grid spacer to the channel wall. Preliminary tests will investigate locking tab design parameters on single grid beams. In parallel with these, further development work on channel wall recessing and other grid support concepts is being performed.

A program of work for development of hexagonal channel characterization methods has been initiated. The objective of this work is to develop fixtures, tooling, and methods that can be used to dimensionally characterize an FTR-size hexagonal channel and to visually observe the internal surfaces and features of a channel.

A program has been prepared, and development work initiated, on the use of radiography as a tool to inspect grid-spaced channel assemblies. X-ray inspection has been used successfully to locate the axial position of grid spacers within the channel after complete assembly with simulated fuel rods. This has been done for assemblies of 37, 127, and 217 fuel rods. Some information on the engagement of grid locking tabs in their channel wall recesses has been gained from X-ray inspection, but present techniques are inadequate for using this inspection method as a criterion for acceptance or rejection. It is the objective of the development program to assess the full capabilities of this method and to improve existing techniques. Destructive examination of the 217-rod grid-spaced (hydraulic flow test) assembly which experienced grid dislodgement during assembly has been completed. Detailed profilometry of the inside of the channel revealed that it was sufficiently above nominal size on the across flats dimension to account for a substantial part of the problem.

3.1.2.3 Fuel Rod/Grid Spacer Cyclic Friction Test Sodium

This test has been devised to assess the mechanical performance of a 217-rod grid-spaced assembly in high-temperature flowing sodium. Fuel rods are to be mechanically moved through the grid spacers in a cyclic manner to simulate reactor temperature transients. The integrity of the grid spacers and their attachment to the channel wall will be assessed under simulated environmental conditions. The design of the test rig is in progress, and is scheduled for completion in December, 1972.

3.1.2.4 Sodium Flow Test

A long-term sodium flow test has been conducted in the General Electric Breeder Test Facility Control Drive Test Loop to evaluate the endurance characteristics of the reference FTR Grid-Spacer Design and to provide engineering information that will support the design bases for the F9E, F9F, and the reference FTR grid-spaced fuel assembly. The test assembly consisted of two separate 37-rod dummy fuel elements positioned in series. The FTR shield/orifice region was simulated for both assemblies to generate prototypical inlet turbulence in each bundle. One 37-rod assembly utilized staggered grids, the reference for the GE grid-spaced FTR driver assembly. The other 37-rod assembly utilized full grids, the backup concept for the FFTF driver fuel assembly.

The prime objectives of this endurance test were to:

- Demonstrate the mechanical integrity and hydraulic stability of a model FTR fuel assembly operating under conditions of turbulent flow at 1000°F
- Determine the mechanical integrity of the reference FTR grid support system under typical operating conditions
- Determine the effects of extended periods of flow-induced vibration on the clad-grid interface
- Provide general information in support of the F9E and F9F EBR-II irradiation experiments

The secondary objectives were to determine:

- The effects of various grid/channel clearances on the mechanical integrity of the support system
- The stability of a grid spacer unsupported by the channel

Details of the test assemblies are shown in Figure 3-2. A complete description of the test parameters and test assemblies has been reported previously.³⁻¹

The test was completed, having logged a total of 4408 hours of operation at 1000°F with a peak coolant velocity of 22.3 fps. Both assemblies were examined by X-ray after being removed from the test loop and cleaned. Some of the grid spacers were observed to be out of their axial locations in the channel. It is believed that this occurred during assembly and that the flow had no effect on the grid displacement. The X-ray inspection techniques used after the assembly of the F9E and F9F irradiation tests were not available at the time of assembly of the flow tests; therefore, the grid movement was not detected prior to running the test.

Examination of the assembly containing the full grid spacers has been completed. A measurement of the force required to move individual rods through the grid spacers was made. This showed an average value of three times that measured before the test. The higher force has not been attributed solely to contact surface effects in light of the grid spacer movement. Some visual evidence of galling has been detected on rod/grid contact surfaces and is being further investigated.

3.2 WIRE WRAP ASSEMBLY — J. R. PUNCHES

The broad purpose of this subtask is to develop the engineering basis for a wire-wrap fixed assembly for the LMFBF Demonstration Plant.

The scope and detailed objectives are to:

- Assess the FTR wire-wrap assembly design, design basis, and test program relative to the requirements and needs of the demonstration plant
- Plan a series of engineering diagnostic experiments for EBR-II and FTR to provide information on wire-wrap assembly design, fabrication, and performance
- Design and document the first of these diagnostic experiments; the 37-rod, F11B assembly for irradiation in EBR-II
- Fabricate the F11B 37-rod assembly

This is the first reporting quarter for this subtask under the new PA-10 program alignment.

3.2.1 F11B Diagnostic Wire-Wrapped Fuel Assembly

F11B is the first of a series of diagnostic wire-wrap fuel assemblies designed to investigate the design-limiting conditions of an LMFBR fuel rod assembly. The object of F11B is to obtain in-reactor test data from a fuel assembly designed to experience, in EBR-II, bundle-to-channel mechanical interactions like those predicted to occur in an LMFBR fuel assembly. This results in an assembly in which fuel rod wire wrap spacer contact loads, rod bow restraint stresses, and fuel bundle response are simulated. In addition, this experiment can complement the existing FTR/LMFBR fuel testing program and can provide a means of gaining additional in-reactor experience with second generation fuel rod hardware and materials.

The design basis for F11B, or any other "diagnostic" assembly, is to simulate, in EBR-II or FTR, the stresses, strains, and interactions which are prototypical of an LMFBR fuel assembly at rated conditions. Assembly design methods can be evaluated, improved, and updated based on a comparison of predicted and actual assembly performance. The design of such an assembly is based on achieving certain design-limiting conditions at selected key locations relative to present LMFBR design criteria. This approach to experiment design requires greater emphasis on pre-irradiation engineering and results in fuel assembly designs and operating conditions which are not necessarily prototypical because of unavoidable mechanical and environmental differences which exist between the selected test facility and the LMFBR.

A specific study of the justification and feasibility for the F11B wire-wrap assembly has recently been completed. It was established that the test objectives can be met fully with the proposed test assembly. This will be accomplished by duplicating in EBR-II the balance between relative bundle-to-channel growth, irradiation creep, and the compression characteristics of the bundle such that prototypical LMFBR end-of-life wire-wrap loadings are achieved. This requires an assembly with a reduced axial wire-wrap pitch which increases the stiffness of the 37-rod F11B bundle to that of a 217-rod LMFBR bundle, an increased bundle-to-channel temperature difference to stimulate increased bundle-to-channel growth, and operation of the assembly at low linear power (~ 7 to 8 kW/ft) to achieve the desired end-of-life fluence ($\sim 1.5 \times 10^{23}$ n/cm²). The mixed mean bundle outlet temperature, accounting for edge bypass flow, is to be approximately 1130°F with a peak nominal temperature at the inside surface of this cladding of approximately 1310°F. This combination of parameters will result in an assembly which will provide the data needed in the design of LMFBR fuel assemblies for target conditions.

3.3 FUEL ASSEMBLY HARDWARE TESTING – L. S. LEE

The objective of this subtask is to perform development testing (including thermal, hydraulic, mechanical, irradiation, and life tests in sodium) of fuel assembly hardware. The scope of work for the current fiscal year includes a review of the FTR work, development of a master plan, and formulation of a test plan and specification for tests of nose-piece, orifice, and shield.

The development of the master plan and schedule is nearing completion.

A request has been sent to HEDL for final reports of assembly hardware and tests associated with FTR development. Review of these reports will begin as soon as they are available.

3.4 FUEL ASSEMBLY DESIGN AND OPERATING LIMITS – R. G. SIM

The overall objective of this subtask is to develop the criteria and methods for structural design of the complete core assembly for an LMFBR. This is the first reporting quarter for this subtask.

3.4.1 Structural Design Criteria for Core Components

The object of this work is the development of the structural design criteria which are required to design the highly irradiated core components of LMFBRs.

This is the first quarter during which work on structural design criteria for core components has been specifically funded by the AEC under PA-10. Under the G6 Project, a draft document defining structural design criteria for core components was prepared. These draft criteria are similar in form to the ASME Code Case 1331-5 with suitable

modifications to account for the embrittlement which occurs in metals subjected to irradiation and flowing nonisothermal sodium. These draft criteria have been fully reviewed. A number of modifications in the criteria have been made and they will be incorporated in the revised version of the criteria which is scheduled for issue in February, 1973. The material data used in the criteria were reviewed in light of current information, and certain elements of the properties of materials are scheduled to be revised. To date, new limits of inelastic strain have been proposed and the creep-rupture properties are scheduled to be revised in November 1972. This work is being done under Task 2, Subtask 3, of the PA-10 Project.

A review of the FTR structural design criteria for core components³⁻² was initiated. Arising out of this review were a number of unresolved questions concerned with the FTR criteria which were referred to RDT for resolution. The completion of this review will be possible once these questions are answered.

Perhaps the most probable cause of fuel failure occurrence in an LMFBR core will be the creep-rupture of fuel rod cladding. Consequently, a significant effort was made in this quarter to examine in detail the current basis for making creep-rupture predictions.

Both the FTR structural design criteria and the GE structural design criteria include a strain-fraction- and a life-fraction-rule for creep rupture. These rules take the following form:

$$\text{Strain Fraction Rule} \quad \sum \frac{\epsilon_i}{\epsilon_{\text{Limit}}} \leq 1$$

$$\text{Life Fraction Rule} \quad \sum \frac{t_i}{t_{\text{Limit}}} \leq 1$$

Where ϵ_i and t_i are the inelastic strain and time, respectively, associated with conditions $\sigma_i, T_i, \phi t_i$, stress, temperature and fluence, respectively.

ϵ_{Limit} and t_{Limit} are the limiting inelastic strain and time, respectively, associated with rupture at the same conditions, $\sigma_i, T_i, \phi t_i$.

The purpose of this investigation was to better understand these two rules and the material data correlations which support them.

The practical problem of a peak power rod in an LMFBR core loaded by fission gas pressure was chosen as the basis for comparing the results of the different analytical approaches. The assumed history of temperature and stress of the cladding for the sample problem are shown in Figure 3-3. Failure was assumed to occur when the accumulated damage by the life fraction rule equalled unity or the thermal creep loop strain equalled 0.2% (this is the FFTF strain allowable limit for normal operation). The cases for which results were obtained are as follows:

1. GE Life Fraction (annealed data)

The stress-rupture expression used is based on annealed Type-316 stainless steel rupture data except that an order of magnitude reduction in rupture time due to irradiation has been included. This expression is the currently recommended design basis for 20% cold-worked Type-316 stainless steel components.

$$t_r = \left(\frac{\alpha B}{\sigma} \right)^\eta \exp \left(\frac{C}{T + 460} \right)$$

where

t_r = rupture time in hours

σ = maximum principal tensile stress (psi)

T = temperature of the material ($^{\circ}\text{F}$)

α = multiplier for taking account of sodium effect

Figure 3-3. Sample Problem Used in Analyses.

The calculations were performed with

$$\alpha = 0.48 \text{ (0.6 for sodium effects and 0.8 design margin)}$$

$$B = 430,400$$

$$C = -32,350$$

$$\eta = 34.57 - 0.022T \text{ for } 1000 \leq T \leq 1300^\circ\text{F}$$

2. GE Strain Fraction Approach (Annealed Data)

The currently recommended thermal creep strain expression for 20% cold-worked Type-316 stainless steel is given below; it is in fact based on annealed Type-316 stainless steel data.

$$\bar{\epsilon} = 7.75 \times 10^{21} t e^{-55359/T} [\sinh(4.31 \times 10^{-5} \bar{\sigma})]^{6.16}$$

Where

$\bar{\epsilon}$ = equivalent creep, strain, in./in.

t = time, hour

T = temperature, °K, and

$\bar{\sigma}$ = equivalent stress, psi

3. Blackburn Strain Fraction Approach

The Blackburn creep expressions apply for annealed Type-316 stainless steel and were developed for use on the FFTF project. The equations are as follows:

$$\bar{\epsilon} = \epsilon_t [1 - \exp(-rt)] + \dot{\epsilon}_m t$$

$$\dot{\epsilon}_m = A \left[\sinh \left(\frac{\beta}{\eta} \bar{\sigma} \right) \right]^\eta \exp \left(- \frac{67,000}{1.987T} \right), \text{ \% per hour}$$

$$r = B \left[\sinh \left(\frac{\beta}{\eta} \bar{\sigma} \right) \right]^\eta \exp \left(- \frac{67,000}{1.987T} \right), \text{ hours}^{-1}$$

or

$$r = 3 \times 10^{-3}, \text{ hours}^{-1}$$

whichever yields the larger value of r

$$\epsilon_t = C \frac{\dot{\epsilon}_m}{r} \%$$

where

$\bar{\epsilon}$ = equivalent creep strain

$\bar{\sigma}$ = equivalent stress

The constants used in the above expression are well documented³⁻³ and they are not repeated here.

4. Life Fraction Using CW Data

Reference 3-4 contains correlations for the 20% cold-worked Type-316 stainless steel creep rupture generated by HEDL on prototype FTR cladding. The time to rupture for irradiated material ($\phi t > 10^{22}$ nvt) is given the following expression.

$$t_r = 0.1 \exp(-62.49 + 116000/RT) [\sinh(1.79 \times 10^{-5} \sigma/\alpha)]^{-8.81}$$

Where

$\alpha = 0.48$ in the design calculations (designed margin x sodium effects factor).

$\sigma = \text{psi}$

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

$t = \text{hours}$

5. Strain Fraction Using CW Data

Reference 3-4 also contains design correlations for the thermal creep strain rate of 20% cold worked Type-316 stainless steel based on the data generated by HEDL on prototype FTR cladding. The expression is as follows:

$$\dot{\epsilon} = \exp(59.64 - 116000/RT) [\sinh(1.79 \times 10^{-5} \sigma)]^{8.82}$$

where

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

$\sigma = \text{psi}$

$\dot{\epsilon} = 1/h$

Considering only the fission gas pressure, failure was predicted to occur as follows:

	Predicted Lifetime
1. GE life fraction approach (annealed data)	4,400 hours
2. GE strain fraction approach (annealed data)	13,700 hours
3. Blackburn strain fraction approach (annealed data)	12,800 hours
4. Life fraction using cold-worked data	11,300 hours
5. Strain fraction using cold-worked data	approximately 20,000 hours

There is a factor of 3 difference between the lifetime predicted using the GE life fraction approach and the lifetime using the GE strain fraction approach. This inconsistency is discussed in detail below. Both strain fraction approaches based on annealed data (results 2 and 3) agree well. This is not surprising, since both strain rate expressions were derived by the same person and are presumably based on the same data. The difference between the two results is due to primary creep being included in results 3 and not in 2. Results 4 and 5 are both based on cold-worked data and again the lifetime predictions are found to be inconsistent. Figure 3-4 illustrates how the strain and the damage accumulate with time for the five results given above. The strains based on cold-worked data are approximately two orders of magnitude less than those based on annealed data. The difference in the lifetime prediction obtained by using either the strain fraction or the life fraction approach is large when the material correlations are based on annealed data or cold-worked data. Strictly speaking for 20% cold-worked Type-316 stainless steel, which exhibits small amounts of primary reason for the present difference in the lifetime prediction is inconsistency in the extent of conservatism in the design correlations used to support these two approaches.

Consistency between the life fraction and the strain fraction approaches can only be maintained if the following equation is satisfied:

$$\epsilon_{\text{Limit}} = \dot{\epsilon}_s t_R$$

Analysis of GE material data generally shows that

$$\epsilon_{\text{Limit}} \gg \dot{\epsilon}_s t_R$$

Figure 3-4. Damage Accumulation and Inelastic Strain Buildup with Time
for a 20% CW Type-316 Stainless Steel Fuel Rod

Since $\dot{\epsilon}_s$ is already conservatively high, it must be concluded that either our present strain limits are an order of magnitude too high or our creep rupture times are an order of magnitude too low. Reduction of the strain limits by an order of magnitude would reduce the allowable inelastic strain to almost negligible levels. This would be in contradiction to present experimental evidence which indicates that the minimum recorded rupture strains are of the order of 0.2% and not 0.02%. This suggests that the inconsistency lies in the present design approach to evaluating the stress vs time to rupture design curves.

After close examination of the material data and the results reported here, it has been concluded that a more consistent design approach for 20% cold-worked Type-316 stainless fuel rods is as follows:

- Use present estimates for inelastic strain limits (~ 0.2 to 0.5% for fluences $> 5 \times 10^{22}$ n/cm² at 1250°F).
- Base secondary creep rates on annealed data.
- Base the time to rupture on cold-worked data.
- Use factors to modify the creep rupture data to take account of irradiation and flowing sodium effects. Typically, such factors would be an order of magnitude reduction in time to rupture and a 60% factor on stress for a peak rod at its end of life.

If our present strain limits are correct, then the approach outlined above will provide conservative and reasonably consistent estimates of creep rupture lifetimes independent of whether the strain fraction or the life fraction criteria are used.

The most significant criteria for channels are probably stress limits based on the yield stress and the ultimate tensile stress for the material. However, since the channels operate at temperatures above 800°F, analysis to assess creep rupture damage is required by current high-temperature criteria. Work has been initiated during this quarter to try to establish the importance of thermal creep rupture for fuel channels under prototypic loadings. It is hoped that it may be possible to establish material limits dependent on temperature and stress under which it may be possible to neglect the thermal creep damage in the channels and hence significantly simplify the required analyses.

The channel with severe flux and temperature gradients such as occur in the outer rows of a LMFBR core was chosen for analysis. The case was analyzed using the computer code CRASIB with and without thermal creep. For the nominal-swelling, nominal-irradiation-creep case the results are shown in Figures 3-5 and 3-6. Figure 3-5 shows the maximum bending stress over the lifetime of the channel for both cases. There was found to be negligible difference in the maximum bending stress whether or not thermal creep was included. The thermal creep damage due to this stress history was computed on a life fraction basis and was found to be approximately equal to 0.03. The damage was computed based on HEDL 20% cold-worked Type-316 stainless steel data extrapolated to the lower temperatures based on a Larson-Miller correlation. The time to rupture was reduced by a factor of ten to account for radiation damage. Figure 3-6 shows the axial temperature distribution on the channel and also the maximum creep strain at each axial location, with and without thermal creep.

The maximum difference between the two strain distributions at any location is 0.062%. For the assumed temperatures, this is a conservative estimate of the thermal creep which will accumulate since the creep rate expression is based on annealed data which will give creep rates approximately an order of magnitude higher than would be expected for 20% cold-worked material. These results indicate that for the nominal bowing behavior of the channel case considered, the creep damage is low. Additional analyses are being conducted for other channels with other temperatures and bowing behavior to further evaluate the relationship between the temperature, the stress history and the thermal creep damage. Analysis is also being conducted to evaluate the effect of channel loads in the circumferential direction, e.g., internal pressure. To do this, a finite element model of the complete channel cross section has been set up using the computer code CREEP-PLAST.

Figure 3-5. Variation with Time of Maximum Bending Stress in a Channel Located Near The Edge of LMFBR Core and Subjected to Nominal Swelling – Nominal Radiation Creep Behavior.

3.4.2 Structural Design Codes

The objective of this work is to acquire, adopt and make operational the computer codes necessary to allow the implementation of the structural design criteria for core components under development.

CREEP-PLAST is an elastic-plastic-creep finite-element computer code capable of analyzing axi-symmetric and plane-strain structures. This code has been modified to include both swelling and radiation creep effects in addition to those material effects already in the code. The code treats the swelling strains in the same way as it treats thermal strains. The radiation creep strains are added to the thermal creep strains and treated similarly. Two new subroutines were written to handle the material data input associated with the swelling and radiation creep behavior. The program has also been updated to include current Type-316 stainless steel data.

Cases have been run using the code in order to check its validity. Uniaxial cases were run and the material data results for thermal creep, plasticity, radiation creep, and swelling all agree with the results of closed form solutions. Typical cladding loading cases have been run both on a one-dimensional cladding model and also on a 180-degree model. The plane strain solution in CREEP-PLAST allows a constant thermal and swelling strain in the direction of plane strain, but limits the creep and plasticity strains in this direction to be zero. For the case of a cylinder under internal pressure, such a solution does not give axial equilibrium except at steady state when creep strains are of the order of twice the elastic strains, or when Poisson's ratio approaches 0.5. As Poisson's ratio approaches 0.5, the computation is unstable. It has been found that if the Poisson's ratio is not greater than 0.49, satisfactory results are obtained. The difference between the results obtained at steady state for a typical cladding problem where Poisson's ratio is initially equal to 0.3 or 0.49 is small. The plane strain procedure used by CREEP-PLAST is judged not to be a problem for cladding analyses. The code is not ready for use for studies of cladding loading sensitivity.

GOGO is a computer code specifically designed to handle two-dimensional generalized plane-strain cladding problems. By the use of analysis, the solution procedure is reduced to a simple numerical integration. Details of the

Figure 3-6. Axial Profile of Temperature and Maximum Creep Strain Occurring in a Channel Bowing Due to Nominal Swelling and Nominal Creep.

codes's capability have been reported.³⁻⁵ During the current quarter, the code has been modified to include Type-316 stainless steel data and to compute the creep rupture damage on a life fraction basis on either an equivalent stress basis or a maximum principal tensile stress basis.

Check out runs made using the code are satisfactory, and the code is presently being used to run sample problem cases.

The computer codes GOGO and CREEP-PLAST were both used to analyze the same sample problem. The sample problem was the case of an isothermal thick cylinder under a constant internal pressure with thermal creep, radiation creep, and swelling. After a lifetime of 13,150 hours, the results from the two codes were compared. It was found that all the stresses and strains agreed to within less than 1%. This agreement between the results of the two codes suggests that both codes are correctly formulated and giving accurate results.

Details of the planned approach for a sensitivity study have been published under Task P in the forty-third quarterly progress report.³⁻⁵ Analyses are being conducted in order to evaluate the significance of a selection of prototype cladding loads. The effect of cycles of loading is also to be studied. The planned analyses will use one-dimensional, two-dimensional and in some cases, three-dimensional models of the cladding.

GOGO is presently being used to analyze typical cladding problems. Initial efforts have been concentrated on understanding the behavior of a typical peak LMFBR rod at its hottest point under normal operating conditions. Typical operating conditions for such a case are defined in Table 3-1. In many simplistic cladding calculations, the information contained in Table 3-1 would be considered sufficient to characterize the creep rupture behavior of the cladding. Calculations have been carried out to determine, for a given maximum overall temperature history, the significance of the temperature gradients acting in the cladding. Figure 3-7 defines the type and form of the temperature gradients considered in the analyses. Also shown in Figure 3-7 is the model of the cladding used in the analyses. Typically there were 11 nodes through the wall thickness and at each of 21 locations around the 180-degree segment of cladding. In Figure 3-7 location B is where the maximum midwall temperature occurs.

Table 3-1
TYPICAL OPERATING CONDITIONS FOR A PEAK LMFBR FUEL ROD

Lifetime	Maximum Midwall Temperature History	Fission Gas Pressure History	Wall Thickness Variation
Hours	(°F)	(psi)	(mils)
13,000	1272/1218 ^(a)	50.5/727 ^(a)	15/10 ^(a)

(a) Beginning of Life/End of Life

Four cladding cases which correspond to the conditions in Table 3-1 are reported here. For each of these four cases, different linear temperature gradients have been assumed to exist in the cladding. The particular temperature distributions in each case are defined by Table 3-2. The material data correlations used in GOGO correspond to the current design correlations used by GE for 20% cold-worked Type-316 stainless steel. These correlations are as follows:

Swelling

$$100 \frac{\Delta v}{v} = 8.90 \times 10^{-2} \left(\frac{\phi t}{10^{22}} \right)^{1.50} e^{\left[-3.14 + \frac{5.04 \times 10^3}{C} - \frac{1.66 \times 10^6}{C^2} \right]}$$

Thermal Creep

$$\bar{\epsilon} = e^{-55359/T} [\sinh(4.31 \times 10^{-5} \bar{\sigma})]^{6.16} 7.75 \times 10^{21} \times t$$

tion-Enhanced Creep

$$\bar{\epsilon} = 0.18 \times 10^{-8} \alpha \bar{\sigma} [1 - e^{-\phi t / 1.47 \times 10^{21}}] + 2.20 \times 10^{-30} \alpha \bar{\sigma} \phi t$$

Definitions of terms in the above expressions are given below:

$100 \frac{\Delta v}{v}$ = The percent change in volume

ϕ = Fast flux ($E > 0.1$ Mev), neutrons/cm²/hour

t = Time, hours

T = Metal temperature, °K

C = Metal temperature, °C

$\bar{\epsilon}$ = Equivalent creep strain, in./in.

$\bar{\sigma}$ = Equivalent stress, psi

$\alpha = e^{(1.405 - 0.0027 T)}$

The maximum elastic stresses on initial loading and the maximum thermal creep strains at the end of life are shown in Table 3-3. As stated earlier, location B is where the maximum midwall temperature occurred. The initial elastic stresses due to the thermal gradients are quite high. However, both radiation and thermal creep act quickly to relax these stresses. The thermal creep strains are considered in detail here since they are generally considered to be more damaging, radiation creep being super-plastic. Figures 3-8 and 3-9 show that significant thermal creep strains

Figure 3-7. Temperature Gradients and Cladding Model Used in Analyses.

Table 3-2
DEFINITION OF CLADDING CASES CONSIDERED

Case	Temperature Gradients	Solution Procedure
1	None	Simple model assuming a thin wall
2	ΔT (through wall) = 40°F ΔT (across rod) = 70°F	GOGO
3	ΔT (through wall) = 40°F ΔT (across rod) = 70°F ΔT (hot spot located 180° removed from the location of the maximum midwall temperature = 40°F	GOGO
4	ΔT (through wall) = 40°F ΔT (across rod) = 70°F ΔT (hot spot located at the location of the maximum midwall temperature = 40°F	GOGO

Table 3-3
SIGNIFICANT RESULTS OF CLADDING ANALYSIS

Case	Beginning of Life Maximum Elastic Stress (psi)	End of Life			
		Circumferential Thermal Creep Strains	Peak (%)	Equivalent Thermal Creep Strains	Peak (%)
		Maximum Midwall (%)		Maximum Midwall (%)	
1	396	0.0165		0.0143	
2	17,250(C)*	0.018 (B)	0.02 (A)	0.021 (B)	0.037(A)
3	±14,940(A)	0.017 (B)	0.023(A)	0.02 (B)	0.06 (A)
4	±21,290(A)	0.035 (B)	0.038(A)	0.049 (B)	0.058(A)

* The letters (A), (B), and (C) designate the location of stress or strain as indicated on Figure 3-7.

occur within 100 hours of going on power in response to the thermal stresses in the cladding. From Figure 3-7 it is seen that the differences in the maximum midwall circumferential thermal creep strains in Table 3-3 are due to the differences in the initial creep required to relax the thermal stresses. Since these creep strains occur early in life the damage would not be expected to be as great as it would be had they occurred later in life. The maximum equivalent thermal creep strains plotted in Figure 3-9 are more difficult to understand. The initial creep strains, which occur in the cases where there are thermal gradients, are quite large. The way in which the equivalent thermal creep strains vary with time is complex, being dependent on the balance between the radiation creep and the thermal creep and also on what is happening in other parts of the cladding at the same cross section. However, it is evident that the introduction of thermal gradients can lead to significantly higher thermal creep strains at the end of life. This result emphasizes importance of using damage criteria which are fluence dependent.

Figure 3-8. Maximum Midwall Circumferential Thermal Creep Strain Occurring in Cladding with Time. (See Table 3-2)

Figure 3-9. Maximum Equivalent Thermal Creep Strain Occurring in Cladding as Function of Time.

3.4.3 Probabilistic Core Design Tools

The objective of this activity is to develop computational tools for core design analysis which allow adequate, but not excessive, margins for uncertainties in dimensions, material properties, environment, and material behavior. It is also the objective of this activity to identify core design and development requirements through the use of these tools in conducting sensitivity studies.

The current fiscal year is the first time the development of probabilistic core design tools has been AEC funded under PA-10. Previous work in this area was funded by General Electric, and resulted in development of the PEFT (Probabilistic Evaluation of Fuel Temperature) and PECS (Probabilistic Evaluation of Clad Stress) codes. The work performed during the current quarter was directed toward continued development of these codes.

Development of the PECS code is scheduled to be completed in two segments, the second of which is to be initiated after some user experience has been accumulated with the first. In order to provide a preliminary estimate of the adequacy of the present fuel rod design at the earliest possible time, this user experience will be obtained by performing a parametric sensitivity analysis during November and December 1972. Sensitivity studies using the PECS and PEFT codes are to be directed toward identification of the effect on probabilistic calculations of the shapes of probability distribution used as input to the calculations. It is intended that these results be used to guide further effort in material property characterization. The PECS code is to be documented early in 1973.

Acquisition of material property data and probability distributions is to occur on a continuous basis. The need for reliable creep-rupture information was identified as a priority item during the past quarter, and the statistical information required to characterize fully the uncertainties in material property data was defined.

The PEFT code is to undergo major modification during November and December 1972. Documentation of the revised code is to be completed by July 1973. The revised code will be used to perform parametric and sensitivity analyses and will be available for post-irradiation analysis of the F20 experiment.

The PECS code evaluates core-wide primary membrane stresses in sealed-plenum fuel rods using a Monte Carlo simulation procedure to describe the effect of material property, temperature and design uncertainties. Cladding failure is assumed to be predictable by a cumulative, creep-rupture damage fraction:

$$D. F. = \int_0^{t_0} \frac{dt}{T(\sigma, t)} \quad (3-1)$$

where

D. F. = Cumulative damage fraction at time t_0 .

$T(\sigma, t)$ = Time-to-rupture assuming cladding stress remains at a constant value of σ .

$\sigma(t)$ = Primary membrane stress due to gas pressure at time t .

The output of the PECS code includes the probability of failure of each of a (user-specified) number of individual fuel rods as a function of irradiation time. The expected number of failures in the core is also provided as output, along with a probability distribution for the value of the cumulative damage fraction for each rod at a specified time (e.g., the design lifetime).

The primary effort during the quarter was directed toward reduction of running costs of the PECS code in order to make this a practical design tool. Two modifications were conceived and implemented in order to accomplish the objective. First, the method of numerical integration used to evaluate Equation (3-1) was refined. Second, a simple form of biased sampling was applied in order to reduce the number of Monte Carlo samples required to obtain statistically significant results. The first modification has been found to reduce the running cost per Monte Carlo sa by a factor of 7. The second modification reduces the number of samples required by a factor of approximately 10

The biasing technique is used to increase the proportion of Monte Carlo sample values indicating failure that fall in some time interval of interest. In the PECS code the time interval of interest is the range of irradiation times between zero and the design lifetime. That is, the ability to accurately estimate failure probability is of greatest importance at times (and burnups) when rods are designed to make failure probability small. Without the use of biasing, a very large number of Monte Carlo samples would be required to obtain the desired accuracy. With biasing, however, most of the Monte Carlo samples calculated provide failure data in the region of interest, and the code does not spend a large amount of time generating failure probabilities at very high burnups.

The biasing scheme employed in PECS is illustrated schematically in Figure 3.10. In that figure, X is a random variable which is required as input to PECS. The probability density function that describes the actual uncertainty in X is shown as a solid line. This distribution is provided as input to PECS in the form of a cumulative distribution function. The code modifies the distribution specified by the user by removing part of the area under one side of the probability density function and adding an equal area to the opposite side of the distribution. This is shown on Figure 3-10, where the shaded area on the left side of the median has been moved to the right side. The area on the right side has been increased because high values of the variable X (e.g., cladding temperature) are assumed to lead to greater failure probabilities. Clearly, the same procedure can be applied in the opposite direction to variables for which low values of X lead to high failure probabilities (e.g., creep-rupture strength). The procedure described above results in the biased probability density function shown by the dashed line on Figure 3-10. An important feature of this biased distribution is that the shape of the distribution is a horizontal line on the side to which area is added; this means that all values of X on this side of the distribution are equally likely to be sampled, and that very high (or low) values of X are sampled much more frequently than in the unbiased case. The shape of the curve on the side from which area is subtracted is the same shape as the input distribution.

Figure 3-10. Biased Sampling Scheme For PECS

In order to correct for the distortion of the probability density function that is introduced by biasing, the of the damage calculation for each sample are not weighted equally as they are in an unbiased calculation. Instead, each sample calculation is given a weight of

$$W_n = \frac{1}{\prod_{i=1}^N} W_{i,n} \quad (3-2)$$

where

W_n = the weight assigned to the result of the n^{th} calculation (i.e., the n^{th} Monte Carlo sample)

and

$$W_{i,n} = \frac{P_i(X_{i,n})}{P_i^*(X_{i,n})} \quad (3-3)$$

The quantity $W_{i,n}$ is the weighting factor for the i^{th} random variable, and the n^{th} Monte Carlo sample.

In Equation (3-3), the nomenclature is:

$P_i(X_{i,n})$ = the *unbiased* probability of obtaining a value of X_i between $X_{i,n}$ and $X_{i,n} + \Delta X_{i,n}$ in a single sample (e.g., the n^{th}),

$P_i^*(X_{i,n})$ = the *biased* probability of obtaining a value of X_i between $X_{i,n}$ and $X_{i,n} + \Delta X_{i,n}$ in a single sample (e.g., the n^{th}).

The weights calculated from Equation (3-2) are treated as scores; all calculations that result in a damage fraction at the end of the design lifetime that falls in the range DF to $DF + \Delta DF$ add an increment of W_n to the total score for that damage fraction interval. Thus, the total score for each damage fraction interval is given by

$$\text{Total Score (DF to DF + } \Delta DF) = \sum_N W_N \text{ (DF to DF + } \Delta DF) \quad (3-4)$$

where the summation extends over all N Monte Carlo samples. The probability that a given damage fraction will occur is given by the score [from Equation (3-4)], normalized by the sum of all weights (W_N). The probability that a damage fraction equal to 1.0 will occur in the time interval t to $t + \Delta t$ is found in the same manner.

For present usage of the code, 11 variables have been selected for biasing; these variables were selected based on analysis and evidence supplied by a sensitivity study which was performed during the quarter. This study utilized an analytical model which is identical to that included in PECS, and is available as a non-probabilistic computer program. The primary criterion used to select a variable for biasing was that the time-to-rupture vary monotonically with the variable in question. The 11 variables identified and the direction in which each is biased, are listed on Table 3-4.

The integration scheme that has been incorporated into the PECS code uses a composite Simpson's Rule with time-step sizes that are dependent on the values of the random variables for each Monte Carlo sample. The integration procedure is iterative; the time-step size is reduced until the value of the integral converges to the accuracy specified by the user. Convergence is required at the end of each operating cycle, since cladding temperature and time-to-rupture are discontinuous functions at the end of each cycle. The cladding swelling, cladding wastage, and damage fraction integrations are performed independently, so that convergence criteria for each of these integrals can be specified independently. The integration procedure calculates cladding swelling, cladding wastage, and damage fraction at a maximum of 32 points in each operating cycle. Interpolation between these points is accomplished using a third-order polynomial and is consistent with the Simpson's Rule integration method.

As part of the modifications described above, the basic structure of the PECS code was changed; the code is now organized in a modular fashion in order to provide flexibility in changing material properties and the analytical model.

Table 3-4
VARIABLES TO BE BIASED IN PECS

	Variable	Biasing Direction High or Low ^(a)
1	Uncertainty factor in creep rupture strength magnitude	Low
2	Uncertainty factor in the rupture stress-versus-time slope on log-log plot	High
3	Uncertainty factor in the cladding temperature	High
4	Uncertainty factor in the burnup rate	High
5	Number of gas atoms per fission	High
6	Uncertainty factor in the plenum temperature	High
7	Uncertainty in gas release fraction (atom released/atom produced)	High
8	Cold clad outside diameter	Low
9	Uncertainty factor in sodium corrosion	High
10	Uncertainty factor in fuel cladding attack equation	High
11	Defect depth	High

(a) High = Biased to give large values

Low = Biased to give low values

This feature is expected to be particularly valuable, since it will allow different indices of cladding damage (e.g., creep-rupture damage fraction and cladding strain) to be included in the code and compared probabilistically.

The PECS code is currently being checked for accuracy and consistency, and minor changes are being made to clarify the input requirements. This work was begun during the reporting period and is expected to continue as user experience is accumulated.

No modifications were made to the PEFT code during the quarter. It is planned to expand the capability of the code from the analysis of a single rod in the core to analysis of the entire core. In order to do this, a core-wide probabilistic design criterion must be selected. Several different criteria have been drafted, and this draft is currently under review. An engineering specification for the revised version of the code will be written early in the next quarter, and programming work is expected to begin at that time.

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4. CORE RESTRAINT ENGINEERING

Task Leader: P. M. Magee

The objective of this task is to develop a core restraint system for the LMFBR. A master plan will be prepared for the engineering analysis and detailed mechanical design and testing. A design basis will be established for the out-of-pile test components and a design for the test components to be built will be provided. Core restraint system and component tests will be conducted as required to develop a viable core restraint system.

A core restraint system is required to maintain precise location of nuclear reactor core components relative to each other, and to other parts of the reactor, so as to ensure no significant shifts in power or otherwise contribute to any possible power excursion. The system must, at the same time, accommodate changes in dimensions of core components caused by thermal expansion and by irradiation-induced swelling and creep. A further requirement is that core loosening must be readily accomplished to allow efficient refueling.

The two primary candidate systems under consideration are external yoke loading and internal tighteners. The first system consists of six yokes at the core periphery which actively load the outmost ring of assemblies and thus constrain the core to its minimum diameter; this is the basic approach used on the FTR. The second system employs a set of actuating internal core clamps located on selected assemblies to effect final core tightening. A spring load is utilized at the periphery of the core to accommodate thermal expansion and irradiation induced swelling. Thus, all compliance is external to the core.

In order to facilitate selection of a suitable restraint system and to demonstrate its ability to meet the functional requirements, a development program which emphasizes early comparison of the performance of the two alternative restraint systems has been initiated. These comparative tests, using simplified two-dimensional models of the reactor core, will provide, along with concurrent analysis, timely operational information to aid in the selection of the most suitable core restraint system.

4.1 CORE RESTRAINT ANALYSIS—J. V. MARRON

The objective of the core restraint analysis subtask is to evaluate the requirements of the core restraint system including the assessment of core response to operational environmental conditions, and to establish the relationship between out-of-pile test results and LMFBR core design requirements.

4.1.1 Core-Wide Channel Wall Temperature Distribution

A preliminary core-wide temperature study has been completed. The axial distribution of the channel wall temperatures were computed for all faces of the assemblies within a one-sixth segment of the core. The results show a large heat sink effect of the control assemblies on the temperatures of the adjacent fuel assembly walls. This often results in a gradient opposite in direction to that produced by the power gradient. Almost all of the fuel wall temperatures are the results of actual computer calculations, but temperatures have been estimated on the core-blanket interface and in the radial blanket. Future work will investigate the effects of the control rod axial position and will better define temperatures in the blanket.

The axial distribution of the channel wall temperature not only depends on the flow rate and power profiles of the assembly, but also on the temperatures of the adjacent channel walls. These temperatures are a function of time since the power varies throughout the assembly residence.

In the fuel assemblies, the power is greatest at the beginning-of-life, while the end-of-life is the time of greatest power for the radial blanket assemblies. The peak wall temperatures would normally occur at a time corresponding to the peak power, but this can be affected by the adjacent assemblies. All of the assemblies in the reactor have at least a two-year residence (two cycles). The power in the fuel assembly decreases linearly throughout each cycle, but there is a step change in the power at the cycle change.

To simplify the analysis, the temperature is assumed to be constant throughout the assembly residence time, and the temperature after one cycle is assumed to be representative of the temperature throughout life for assemblies with a two-year residence. Conditions representing the end of the first cycle are used rather than those for the start of the second cycle since the temperatures are higher for end-of-cycle. The conditions for the middle of the second cycle are used for the blanket assemblies with a three-year residence, while end of the second cycle conditions are used for those with a four-year residence.

Adequacy of assuming a constant temperature with time depend on the importance of the various mechanical phenomena. Those that are a function of maximum temperature will be underestimated by the assumption, while those that are a function of time and temperature will be over-estimated in the fuel assemblies and underestimated in the blanket assemblies.

The temperature calculations are based on the orificing scheme shown in Figure 4-1. In this scheme, groups of assemblies have the same flow rate, and the flow is based on the requirements of that assembly in the group which generates the maximum power. Such a scheme results in overcooling of many assemblies.

The bundle geometry used in this analysis is representative of the initial core, and the fuel rods along the channel wall are wrapped with the same diameter wires as the interior rods. This results in more flow along the edge rods of the bundle than is needed, and causes higher temperatures in the interior. The high flow along the edges also reduces the channel wall temperatures and the gradients across the channel. It is expected that advanced cores will utilize reduced edge spacings between the outer fuel rods and the channel wall in an effort to minimize cladding temperatures. This would result in higher channel wall temperatures for comparable conditions.

The following assumptions were used in the calculation:

1. Temperature at mid-life *is typical* throughout assembly residence.
2. A one-dimensional radial power profile is representative.
3. Control rods are fully withdrawn.
4. The temperature at mid-width and mid-thickness of the channel wall is representative of the temperature of the face.
5. The gap between assemblies is uniform both axially and radially.
6. Leakage flow between assemblies is 0.05% of total reactor flow.
7. Film coefficient for the channel wall is the same as that for the fuel rods.
8. Swirl of the flow between outer rods and channel wall is ignored.
9. The effect of banding rods on the outer blanket assemblies is the same as another row of blanket assemblies.

The calculated wall temperatures at one elevation are shown in Figure 4-2, and the resulting gradients are illustrated in Figure 4-3. These results show the great influence of the control assemblies on the temperature of the adjacent walls.

Ordinarily, the power gradient in the reactor would cause the temperatures to be higher in the channel wall closer to the assembly centerline than in the opposite wall; this effect is shown in the radial blanket where there are control assemblies. However, the temperature gradients in the assemblies surrounding each control assembly are pointing away from the control assembly. The control assembly is acting as a heat sink because of the relatively large flow rate and low power.

Figure 4-1. Reference Orificing Scheme.

Figure 4–2. Channel Mid-Wall Temperatures at 136 in. Elevation.

Figure 4-3. Channel Wall Temperature Gradients.

Besides the effect of the control assembly on the gradients, much of the variation in the gradients is due to the effects of zone orificing. Since two adjacent assemblies can be in different orificing zones, they can have much different coolant temperatures. The temperatures generated in this study will be used in channel lifetime studies involving the effects of temperature, swelling and creep.

4.1.2 Hexagon Load Distribution

The hexagon load distribution is being studied in both an 8-hexagon sample and a full 60-hexagon study which covers a one-sixth segment of the core. The modeling scheme, employing a series of beams to represent hexagon sides and to represent contact points between the hexagons, is shown in Figure 4-4. In these elastic studies on computer program, SAP, the body forces at each hexagon (representing the beam reactions at loading pads on the channels, obtained from computer program CRASIB) are applied, and the resulting compressive and shear contact loads between the interacting hexagons are determined. The following parametric studies are underway on the 8-hexagon model:

1. Study of the internally-clamped array (both 4-sided and 6-sided hexagon loading) with rigid ties between hexagons. This gives an equivalent infinite coefficient of friction.
2. Internally clamped array with frictionless ties between hexagons. The beam-ties (indicated in Figure 4-4) are assumed to have an end condition simulating rollers.
3. Externally-clamped array with frictionless ties. All hexagons will have six-sided loading on the first solution and those ties found to be in tension will be removed on the next iteration.

Three additional studies are planned which will: (1) vary the amount of load applied at certain of the boundary elements; (2) vary the hexagon wall thickness; and (3) change the contact points in the hexagon side from the quarter points to other locations such as the corners.

The input data on the 60-hexagon model have been prepared and submitted to the computer but solution of the problem has not yet been achieved. Due to possible bandwidth limitations it may be necessary to solve this problem in two parts which are more amenable to the core size of the computer.

These analyses are being undertaken to determine the contact loads in channel load pads and internal clamps for structural analysis, and for correlation with test results from the up-coming 2D and 3D tests on the core restraint system.

4.1.3 Passive Core Restraint

A feasibility study of a passive core restraint system was made, based on the work to date on the extrapolated EBR-II approach. The assessment was completed in preliminary stage, with proposals for a recommended program and is now being reviewed within BRD.

4.2 CORE RESTRAINT MECHANISM—N. P. HANSEN

The scope and objectives of the mechanism design subtask are to:

- Complete the design basis for the 3D test assemblies.
- Establish the reference design for the 3D core restraint tests.
- Complete the conceptual design of the components for the 3D test.
- Initiate the fabrication drawings for the 3D test components.

4.2.1 Design Basis for 3D Test Components

A first draft of the design basis for the 3D test components was completed. The report will be the basis for design of the core restraint components to be tested in a full scale, three-dimensional mockup of a complete Demonstration

Figure 4-4. One-Sixth Sector of Core Array For Use in Hexagon Load Distribution Problem.

Plant core. The design of the core restraint system must account for large uncertainty in predicted values of the radiation-induced swelling and creep and for unpredictable fuel assembly interactions when the assemblies are in a clamped condition. The selected system establishes an overall configuration that can cope with the most unfavorable development results, yet be capable of modification to the simplest restraint concept in the event that core restraint proves to be relatively straightforward.

The core restraint system to be tested will consist of three basic components:

peripheral restraint mechanisms, spring banding rods, and internal tighteners.

The design basis report describes the system design requirements which follow:

1. The test components must accurately model the core restraint system intended for the LMFBR
2. The test components must simulate an entire core restraint system and be full scale
3. The test components must provide a good simulation of all core boundary conditions including those of the core support structure where the lower end restraint of the boundary rods and external tighteners must be simulated.
4. The test components must have the capability of adjusting the clamping loads applied to the core assembly.

The design requirements for each of the three basic components of the core restraint system are discussed in greater detail in the design basis report.

4.2.2 Low Friction Coating Material Study

Work is progressing on the review and evaluation of low-friction coating materials. A review meeting on the FFTF Wear and Friction Program was held at LMEC, Canoga Park, on October 11-12, 1972, to review the current status of on-going tests and establish criteria for future tests. Organizations represented included HEDL, ARD, AI, LMEC, ANL, USAEC, and GE-BRD. The interest of GE-BRD in this particular program is in the area of low friction coatings for Demo Plant channel pads.

FFTF work on low friction coatings for channel pads has, over the past year, concentrated on metal carbides. These included chromium, tungsten and titanium carbides. Work to date has revealed that only chromium carbide remains as a viable coating. Chromium carbide with a nichrome binder is acceptable for use in FFTF in all test parameters except breakaway friction. The coefficient of friction of chromium carbide against itself was measured to be 0.4 maximum in the temperature range of 1000 to 1160°F.

A comprehensive review of the work reported in the conference has been made with attention to its applicability to LMFBR design, and recommendations for additional work will be made as appropriate.

4.3 CORE RESTRAINT TESTING—V. R. MARIAN

The objective of the core restraint testing subtask is to conduct basic tests on a two-dimensional mockup of core restraint systems in order to assess the effects of friction on core compaction and to obtain an experimental comparison of external clamping and internal tightening core restraint systems.

Two dimensional planar mockup tests using the HEDL Planar Clamping Model Facility (PCM) will be conducted in FY-73. These tests will provide needed information on core restraint problem areas including core compaction, channel alignment, and component loads. The tests will investigate the effect of core size and inter-channel friction coefficient and will provide a direct experimental comparison of the internal tightener system and an external clamping system.

Three phases of tests are planned using the Planar Clamping Model. The first phase will use the unmodified PCM with high friction alumina coated channels. Loads will be applied by six yokes external to the core. Channel load distributions and optimum yoke actuation sequences will be experimentally determined in this 199 channel FTR sized array. Comparison of the experimental results from these tests with those from previous HEDL tests using low friction channels will be used to assess the effects of friction on core restraint.

Phase 2 tests will use the PCM modified to accommodate a Demonstration Plant sized array of 361 one-foot long high-friction channels. As in Phase 1, the external clamping restraint system will be used. Load channel distributions and optimum yoke actuation sequences will be determined. Comparisons of Phase 1 and Phase 2 results will be used to assess the effect of core size on core restraint.

Phase 3 tests will use the PCM modified to accommodate the internal tightener core restraint system. This novel system has not been tested. Optimum yoke and internal tightener actuation sequences and channel load distributions will be experimentally determined. The relative merits of the internal tightener and the external clamping core restraint systems will be assessed using the results of Phase 2 and Phase 3 tests.

The experimental results from the three phases of testing as well as the results of concurrent analytical effort will be the basis for specification of a core restraint system for testing in the WARD 3D mockup.

4.3.1 Phase 1 Testing

The specific objectives of the Phase 1 tests will be to determine:

1. The distribution of channel forces and distortions within a compacted array.
2. The optimum sequence, rate, and magnitude of restraint load application for core compaction.
3. The dependence of the degree of compaction on applied yoke loads.
4. The effects of azimuthal dislocation of the channels on compaction.
5. The effects of channel tolerance on core compaction, interchannel gaps, and component loads.
6. The effect of friction on core compaction.
7. The feasibility of using the existing HEDL lateral force channels to mock up the effects of warped channels.
8. The distribution and size of interchannel gaps subsequent to compaction attempts.

The first test will be run with a compacted array of standard alumina-coated channels— 4.719 ± 0.002 inches across flats. The load cell will be sequentially positioned to each of the 33 positions in a $1/6$ pie-shaped (60°) segment of the core. The compacted core will be compressed with the six yokes loaded simultaneously; first up to 2,500 pounds, then to 5,000 pounds, and finally to 10,000 pounds. The load cell will be at one location during each loading sequence. The data on pad force, yoke load, and yoke position will be recorded at each force level using a data acquisition system. Distortions on those channels along a single radial line of the core will be measured using an inside micrometer. A second test will be run that is identical to the first except that an oversized channel, 4.734 ± 0.002 inches, will be positioned three rows in from the yoke. The data will be used to evaluate the effect of a known channel distortion on local load distributions.

The core will be compacted by applying inward forces to the six yokes. The diametral gap which is the diameter difference between the resultant core and the ideal compacted core (no inter-channel gaps) will be measured after each experiment by monitoring the position of each yoke using linear potentiometers. In all cases the gap will be measured when the inward force applied to each yoke is 10,000 pounds. In addition, the interchannel spaces remaining in the loaded core will be measured using feeler gages. Prior to the compaction experiment, each yoke will be moved radially

outward one inch from its position at compaction. Two initial channel position patterns will be tested. The first one, which is expected to be more easily compacted, will contain random inter-channel spaces of up to 1/8-inch. Care will be taken to maintain the correct azimuth alignment of each channel. The second pattern will not only contain random inter-channel spaces but also have random azimuth misalignments up to a maximum of 10° in the 60 channels.

A special channel has been designed and fabricated by HEDL that is capable of imparting lateral forces to neighboring channels in the core compaction experiments. This device that is fixed to the PCM base plate is designed to mock up the effects of warped fuel assemblies on core compaction. This channel has not been previously tested at HEDL in the PCM, apparently for lack of time. A test will be run using this device in a position four rows in from the yoke. Prior to its installation, the core will be compacted in its proper position relative to the base plate. Five yokes will be kept in their proper positions while the sixth will be withdrawn one inch. The appropriate channels in the affected segment will be moved outward so as to permit installation of the special channel. The channel will be adjusted so that it is displaced outward from its "correct" position. Attempts to compact the loose segment of the core by providing inward loads on yoke should provide important information on the operation of the lateral force channel.

Phase 1 requires renovation of the PCM facility as well as coating the 4-inch long channel with alumina using a flame spraying technique. This preparation of the PCM as well as several preliminary tests has been completed. The Development Specification has been written, reviewed, and issued. The scheduled six-week testing period is expected to be completed in December.

4.3.2 Phase 2 Testing

Upon completion of the Phase 1 test effort, the PCM facility will be modified to accommodate 361 hexagon channels, 4.715 inches across flats and 1-foot long. The diameter of the core will, as a result, increase from 70.725 inches to 94.31 inches. The external-clamping core-restraint system will be retained. The following components are being designed for the PCM facility conversion:

1. **Grid Plate**—The existing grid plate is being enlarged to accommodate the larger core through addition of six identical plates. The resultant hexagonally shaped grid plate will be 98 inches across flats.
2. **Yokes**—A new yoke is being designed to accommodate the larger core size. six identical yokes will be required, each of which is 4-inches thick and loads 11 peripheral channels. The yoke-to-channel geometry of the existing narrower yokes is being retained as much as possible. Tracks are incorporated in the yoke mounts to constrain each to move along a straight line intersecting the center of the PCM. This will minimize the effort required to keep the core centered on the grid plate during compaction experiments. Previously, a servo system was used for this purpose. A hydraulic cylinder is included in the design for rocking the yokes in a horizontal plane about the vertical axes of the tracks. Simultaneous and parallel rocking of the opposite yokes was found to be quite effective in compacting a partial scale mockup of the core and the core restraint system.
3. **Instrumented Channel**—A special instrumented channel, illustrated in Figure 4-5, capable of independently measuring the inward forces being applied to each of its six faces has been designed. The device, which uses strain gage load cells, will correctly model the mechanical characteristics of a standard 12-inch channel (compliance and Poisson effect). This device will be used in exactly the same manner as the HEDL "load cell" which has been successfully used in previous PCM tests. The HEDL load cell was designed to have the same compliance as the 4-inch high aluminum channels; however, and thus does not have sufficient rigidity for the Phase 2 and Phase 3 tests. It is also noted that the HEDL load cell does not correctly model the Poisson effect of a standard channel.
4. **Channels**—The channels for Phase 2 and Phase 3 testing will be 1-foot long, 4.715 inches across flats (outside) and will be fabricated from mild steel 0.215 inch thick. The channels will be flame sprayed with alumina to give them a channel-to-channel coefficient of friction greater than 0.6.

The primary reason for the channels being 12 inches long instead of the 4 inches used in the existing design, is the requirement for internal tighteners in the Phase 3 tests. The actuating internal tighteners which have been designed are 12 inches long.

5. Balance of PCM—The existing hydraulic system (no servo system), yoke load cells, instrumentation and PCM structure will be retained for Phase 2 and Phase 3 tests. The installation of the components described above should not require any more effort than was required for installation of the original components that they replaced. They are designed to interchange with the existing components.

The specific objectives of the Phase 2 tests are identical to those of the Phase 1 tests. Evaluation of Phase 1 and Phase 2 results will be used to:

1. Compare the distribution of channel load and distortion in the 199 and 361 channel cores.
2. Assess the increased complexity of clamping the 361 channel core.
3. Compare the dependence of compaction on the applied yoke load for the 199 and 361 channel cores.
4. Evaluate the effectiveness of yoke "rocking" for core compaction.

The experimental techniques used in Phase 1 will be used again, taking into account the increased core size. Specific tests found to be non-productive in Phase 1 will not be repeated. If the lateral force device feasibility tests are successful, seven of the devices will be used in the Phase 2 test effort to determine their effect on the channel load distribution and on core compaction.

The Development Specification for the Phase 2 testing effort has been written, reviewed, and will be issued in November. Those components required to modify the PCM have been designed. Several bids on the prime long lead item (380 channels) have been received. Procurement of all PCM conversion components, fabrication and check out is expected to be completed during February 1973. It is estimated that six-weeks will be required to accomplish the scope of the Phase 2 tests.

4.3.3 Phase 3 Testing

The major differences between Phase 3 and Phase 2 tests will be the complexity of the compaction experiments. The degree of compaction achieved in the internally tightened core is expected to depend not only on the yoke actuation sequence but on the internal tightener actuation sequence. The data collection is expected to be more complex as well since the positions of the tighteners subsequent to a compaction experiment must be determined.

The channels, grid plate, hydraulic system, instrumented channel and the balance of the PCM used in the Phase 2 tests will be used in the Phase 3 tests without modification. In addition, the following components will be either modified or built for this test series:

1. Yokes—Shims will be installed on the existing yokes to accommodate the periphery of the internally tightened core. It is noted that the expanded internal tighteners change the channel pitch from the external clamped configuration.
2. Active Internal Tightener—A hydraulically actuated internal tightener has been designed which replaces a standard channel, Figure 4-6. Upon actuation, the across flats hexagon size will increase from 4.715 inches to 4.890 inches. Seven of these devices will be built.
3. Fixed Internal Tightener—Shims, 0.087 inch, will be attached to each face of 48 standard hexagon channels using counter-sunk machine screws. These modified channels will replace the expanded actuating tightener which will result in a considerable reduction in costs. A total of 55-internal tighteners are required for the core.

4-13

4. Tightener Removal Tool—A removal tool will be built which permits an expanded active internal tightener to be replaced with a fixed tightener.

The specific objectives of the Phase 3 tests will be similar to those of the Phase 1 tests which are listed above. Evaluation of Phase 2 and Phase 3 results will be used to:

1. Assess the relative merits of the internal tightener and external clamping systems with respect to core compaction complexity.
2. Compare the magnitudes of the component (channel) loads and distortion in the internal clamping and external clamping systems.

The potential of several techniques for compacting the core will be evaluated in the experiments. The candidate techniques include:

1. Fixed yokes and actuation of the tighteners from the center of the core out towards the yokes.
2. Fixed yokes and actuation of the tightener from the periphery of the core towards the center.
3. Fixed yokes, actuation of tightener, then movement of yokes.
4. Retracted yokes, actuation of tightener, then compaction by moving the yokes inward.

Measurement of both yoke positions and internal tightener positions will be required. Measurement of tightener position is required in this restraint system since proper positioning of the yokes does not necessarily indicate that the channels and tighteners will be in their correct positions.

The load distribution in a 60 degree segment of the core will be measured using the special instrumented channel. Of particular interest will be the loads on those channels surrounding an expanded internal tightener. In addition to load measurements on a loaded, compacted core, a limited number of load measurements are planned for the non-compacted core.

Those tests described above in the description of Phase 2 testing using the lateral force device will be repeated in the Phase 3 tests if useful data have been obtained.

The fixed and active internal tighteners and the removal tool have been designed. The preliminary test planning has been completed. It is estimated that a six-week testing period will be required to complete the scope of this testing. Phase 3 testing is expected to start in the second quarter of 1973.

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5. CONTROL AND BLANKET ASSEMBLY ENGINEERING

Task Leader: A. J. Lipps

The objective of this task is to identify test and data needs for control and blanket assemblies applicable to LMFBR design.

5.1 CONTROL ASSEMBLY ENGINEERING—W. J. MARGETANSKI

The control assembly engineering work effort was initiated in July under PA-10 and is reported for the first time in this quarterly report. The objective of this work element is to establish specific concept design test planning relative to Demonstration Plant needs. This will be accomplished by evaluating the control assembly design in the following areas:

- Boron carbide (B_4C) rod design
- Accommodation of swelling and gradients
- Bundle thermal hydraulics
- Interface with overall control drive system
- Integration with the HEDL FTR control development program, the AI tantalum control development program, and information from other tasks on B_4C properties, and the B_4C /cladding/Na interaction.

The current design focus for the control assembly is on the B_4C rod design. The work is based on a control assembly design that is limited in diameter by an internal core restraint system that utilizes the control assembly channels to provide internal clamping of the core. The base design incorporated 19 wire-wrapped, 0.483-inch i.d. rods, each with a B_4C section 34.6 inches long (4 inches of which is above the core).

5.1.1 Control Assembly Nuclear Engineering

The control assembly is designed to provide the necessary reactivity control for plant operation. There are a total of 19 control assemblies in the core. Each control assembly has a movable portion, the control element, that consists of 19 control rods that contain neutron-absorbing material. Boron carbide (B_4C) enriched in B^{10} has been chosen to be the neutron-absorbing material. The axial movement of the control element into or out of the reactor core causes the reactivity change, and thus serves the control purpose.

Since B_4C is used as the neutron-absorbing material, the reaction $B^{10}(n, \alpha)$ is of primary interest. Each reaction not only releases 2.3 MeV energy but also produces an alpha particle that is the nucleus of a helium atom. The heat released from this reaction is totally absorbed in the control assembly, and the helium gas generated by this process builds up inside each control rod. It is necessary that both heat release and gas generation be accommodated in the control assembly design. For this reason calculations utilizing one-dimensional diffusion theory calculations were made to determine the $B^{10}(n, \alpha)$ reaction rate distribution, the heat release, and the helium gas generation.

In the calculations, cylindrical geometry was employed for the calculational model that consists of one central control assembly and six peripheral fuel assemblies. The calculations were made for the range of B_4C enrichment from 40% to 90%.

The calculated $B^{10}(n, \alpha)$ reaction rate radial distribution inside the control assembly is shown in Figure 5-1. It should be noted that the equivalent radius of the control element is 3.15646 cm, and the relative reaction rate is normalized at approximately 2.6 cm from the center for both 40% and 90%-enriched B_4C cases. It appears that, due to the neutron flux depression in the control element, the reaction rate increases as the radial distance from the center is increased. The heat generation due to $B^{10}(n, \alpha)$ reaction at the average power position in the core was calculated to be 0.298 MW per control assembly for the 90%-enriched B_4C case and 0.185 MW per control assembly for the

Figure 5-1. Relative Distribution of $B^{10} (n, \alpha)$ Reaction Rate

Figure 5-2. Heat Generation by Control Rods.

40%-enriched B_4C case. If the control assembly is located at a position where the surrounding fuel assemblies generate a power density either higher or lower than the average power density, then the heat generation due to $B^{10} (n, \alpha)$ reaction will be different as shown in Figure 5-2. The initial helium gas generation rate at the average power position in the core was calculated to be 3.0149×10^{-5} liter/second per control assembly for the 40%-enriched B_4C case. Based on the calculated results, it appears that, for one-year irradiation with 0.75 load factor at the average power position, the accumulated gas at standard pressure and temperature conditions is 670 liters per control assembly for the 90%-enriched B_4C case and 404 liters per control assembly for the 40%-enriched B_4C case.

5.1.2 B_4C Rod Design

One of the reaction products of the B^{10} -neutron absorption reaction is an alpha particle (the nucleus of a helium atom). The helium gas generated by this process must be accommodated in the control rod design. In this light the feasibility of a sealed plenum control rod design was investigated. The bases used for the investigation were:

- Channel orificing is provided to allow control rod overcooling so that rod temperatures will not exceed $1100^\circ F$.
- Two assemblies were examined, one with 19 control rods having inside diameters of 0.483 inch, the other with 37 control rods with 0.331-inch diameters.
- The helium gas generation rate is 596 liters (at STP) per assembly in 274 fully inserted days (required B_4C enrichment = 70%). Gas release is assumed equal to 100%.
- No cladding waste is assumed. This is justified by the assumption that thickness losses due to wastage will be a small fraction of the net wall thickness.

- No corrosion of the outside surface of the cladding is assumed. The justification is the same as for cladding wastage.
- Linear stress buildup is assumed with only generated helium gas contributing to the cladding stresses.
- No credit is taken for cladding swell.
- The reference cladding material is Type-316 20% CW stainless steel.
- The plenum space availability is 100%.
- There is no mechanical interaction between the B_4C and the control cladding.
- The plenum size may be established by the lesser of: 80% of the primary membrane stress required for creep-rupture following a linear cumulative stress damage criterion, 67% of the nominal yield strength of the cladding material at operating temperature; 33% of the nominal ultimate strength of the cladding material at operating temperature.
- For sealed plenum control rods the design objective is established on the concept of non-failure.
- Material properties used for the sealed plenum control rod studies are from Kangilaski⁵⁻¹ and Ring.⁵⁻² They are shown graphically in Figures 5-3 and 5-4. Differences in assumptions between this document and Reference 5-2 deal with the change in geometry (this document examines smaller rod diameters), material (Type 316 CW rather than Type 321), and gas generation rate per assembly (present control assemblies have three times the previous rate due to increased assembly worth and decrease in neutron importance).

The required control rod wall thickness was based on an assumed fixed rod size and the specified amount of generated helium gas. Figures 5-5 and 5-6 show the required thickness of the 0.483 i.d. rod using the 1/3 UTS criteria as the stress limit for 950 and 1100°F, respectively. Plenum length and time vary parametrically. Figures 5-7 and 5-8 show the identical information for the 0.331 i.d. rod. Figures 5-9 through 5-12 are the same as Figures 5-5 through 5-8 except that 80% of the primary membrane stress required for creep rupture following a linear cumulative stress damage criterion is used as the structural limit. No calculations were done using 67% of the yield stress as a criterion since this was always greater than 33% of the UTS limit.

Calculations were made on the basis of primary membrane stress due to internal pressure only. Other primary stresses are considered to be negligible. For each criterion the stresses computed were the maximum principal stresses based on the thick wall cylinder formula. These values were taken for the slight added conservatism. The 1/3 ultimate tensile strength criterion was more limiting than the 80% stress rupture strength criterion, and was thus the controlling fraction. Because thicknesses were determined by the 1/3 ultimate tensile strength criterion, the results are time independent. That is, design life is limited by the maximum metal temperature during the service life of the rod and not by the time at temperature.

The results show that sealed plenum control rods do not appear to be practical for Demonstration Plant use, based on the currently established geometry limitations (caused by internal core clamps) and operating conditions. With a plenum length of 14-inches and a 20-mil cladding, the allowable fully inserted operation is approximately 17 days. If the plenum length were allowed to increase to 50 inches, the limit is raised to approximately 60 days. In either case, the times are far short of the objective of 274 fully inserted days (one year at 0.75 load factor). These results are based on the concept of non-failure (i.e., the cladding is not breached), and operation beyond these times would only indicate that the likelihood of gas leakage was high.

Although these results indicate that sealed plenum control rods are not practical, extensive redesign could change this conclusion. If the number of rods were increased, the inside diameters increased, and the wall thickness increased, sealed plenum rods might achieve the design objectives. Whether these changes are feasible will be the subject of subsequent studies.

Figure 5-3. Minimal Ultimate Strength of 20% Cold Worked Type-316 Stainless Steel

Figure 5-4. Stress Rupture Life of 20% Cold Worked Type-316 Stainless Steel

Figure 5-5. 1/3 UTS Criteria, 950°F, 0.483 in. i.d. Rod

Figure 5-6. 1/3 UTS Criteria, 1100°F, 0.483 in. i.d. Rod

Figure 5-7. 1/3 UTS Criteria, 950°F, 0.331 in. i.d. Rod

Figure 5-8. Sealed Plenum Wall Thickness—1/3 UTS Criteria, 1100°F, 0.331 in. i.d. Rod

Figure 5-9. Sealed Plenum Wall Thickness—Creep Rupture, 950°F, 0.483 in. i.d. Rod

Figure 5-10. Sealed Plenum Wall Thickness—Creep Rupture, 1100°F, 0.983 in. i.d. Rod

Figure 5-11. Sealed Plenum Wall Thickness—Creep Rupture, 950°F, 0.331 in. i.d. Rod

Figure 5-12. Sealed Plenum Wall Thickness—Creep Rupture, 1100°F, 0.331 in. i.d. Rod

For this scoping study the FTR control assembly geometry was assumed. The FTR control assembly consists of 61 rods with an o.d. of 0.475 inch and an i.d. of 0.369 inch. This means a wall thickness of 53 mils. There apparently has been no attempt to optimize this wall thickness.

For a given o.d., as the wall thickness increases, the stress for a constant internal gas pressure decreases. However, if it is the volume of the gas that is held constant, the gas pressure will increase as the thickness is increased due to a shrinkage of plenum volume and this will cause the wall stress to increase. Therefore, there are two competing effects, and there should be some optimum wall thickness at which any increase or decrease in wall thickness will lead to an increase in wall stress. This optimum thickness is found as follows:

For thick-walled cylinders:

$$\sigma = \frac{Pr_i^2}{r_o^2 - r_i^2} \left(1 + \frac{r_o^2}{r_i^2} \right)$$

where

σ = Stress

P = Pressure

r_o = Outside Radius

r_i = Inside Radius

If r_o is fixed and r_i varied, the pressure can be defined as:

$$P = P_o \frac{r_o^2}{r_i^2}$$

where P_o = pressure for radius r_o of a given volume of gas.

Combining the two equations:

$$\sigma = \frac{P_o r_o^2}{r_o^2 - r_i^2} \left(1 + \frac{r_o^2}{r_i^2} \right)$$

To find the value of r_i to make σ minimum, we set $d\sigma/dr_i = 0$ and solve for r_i . Performing these operations we find that $r_i = 0.643 r_o$.

Thus, if the o.d. is 0.475 inch, the optimum i.d. is 0.305 inch or an 0.085-inch wall thickness. Using this geometry, a calculation of the needed plenum length to last the 274' fully inserted days of operation was performed for a set of given temperatures. The results are shown in Figure 5-13. This figure indicated that, for the FTR geometry, it would be possible to design a workable sealed plenum control rod.

5.2 BLANKET ASSEMBLY ENGINEERING—A. J. LIPPS

The objective of this work effort is to identify, plan and conduct the development tests which are required to design a blanket assembly for an LMFBR. Focus will be placed on thermal hydraulics, consequences of shuffling and orificing, with close coordination with on-going development programs.

5.2.1 Blanket Assembly Engineering

The blanket assembly engineering work effort was initiated in July under PA-10 and is reported for the first time in this quarterly report.

Figure 5-13. Plenum Temperature versus Plenum Length—0.475 in. o.d. Rod, 0.305 in. i.d., 9.44 Liters of Gas

The work under this program will include:

1. Identification of design requirements, basis, and criteria.
2. Determination of the nuclear, thermal-hydraulic and mechanical operating characteristics.
3. Evaluation of the nuclear, thermal-hydraulic, and mechanical effects of rotation and shuffling of assemblies.
4. Identification of testing requirements and data needs.

In order to focus the design evaluations conducted under this program the work is based on a typical Demonstration Plant radial blanket design that is made up of a total of 180 radial blanket assemblies, each containing 61 rods. Each rod consists of a stainless steel tube containing a column of UO_2 pellets that extend from 15 inches below to 15 inches above the active fuel of the core.

Work on establishing the nuclear engineering operating characteristics for the blanket has been initiated. Power and burnup distributions are being established considering various blanket shuffling and rotation schemes.

The wire wrap on the blanket rods is necessary to maintain spacing and provide support, but it can also be used to promote mixing. The temperature distribution in the blanket assembly depends on the flow and power distributions. Without wire wrap the flow along a blanket rod is somewhat restricted to the cusps formed by the triangular array of three rods with only a small amount of turbulent interchange. This is due to the tight rod spacing and low flow rates. The wire wrap around a rod alternately sweeps through each of the flow channels surrounding a rod and helps promote interchange between them. This interchange reduces the variations in temperature between flow channels and also lowers the peak temperature. The more often that the wire sweeps through the flow channels, the greater the interchange and the lower the temperature variations. The amount of sweeping is basically a function of the wire wrap

pitch, where the pitch is defined as the axial distance traversed for the wire to return to the same azimuthal position. Figure 5-14 shows the effect of wire pitch on the coolant temperatures in a typical radial blanket assembly for an elevation near the top of the core. The reduction in wire pitch from 6 inches to 4 inches has reduced the peak coolant temperature by about 6°F and has reduced the greatest difference in coolant temperatures around a rod from 37°F to 27°F.

The temperatures in Figure 5-15 were calculated with the FULMIX computer code, which handles mixing due to the wire wrap as an added term in the turbulent interchange equation. It does not consider any directional effect of the wire. There is an option in the code that models the directional sweeping effect of the wire, but it has not been sufficiently verified to be the recommended model and the model also has inconvenient limitations on the nodal structure. The results of the directional sweeping model show the asymmetric temperature profile around the edges of the bundle that have been seen in experiments; it also predicts higher peak temperatures ($\sim 10^\circ\text{F}$) and a shift in the location of the peak. This difference in the temperature prediction resulting from the different modelings of the wire wrap effect is greater than the difference resulting from a change in the wire pitch from 6 inches to 4 inches.

There is also an uncertainty in the temperatures resulting from an uncertainty in the amount of mixing for a given wire pitch. The mixing coefficient can be related to the fraction of the coolant in the flow area swept by the wire that is transported into the adjacent flow channel (the computer variable is named FRACT). Experimental work has shown that this factor typically varies from 1/3 to 2/3, but the data were not typical of blanket assembly conditions. The peak cladding temperature will change about 6°F for a change in the factor from 1/3 to 1/2; this change is about the same magnitude as the change resulting from a change in the wire pitch from 4 inches to 6 inches.

Although the change in wire pitch from 4 inches to 6 inches does not seem to have a significant effect on the peak coolant temperature when compared to the uncertainties, it does have a large effect on the bundle pressure drop. As the wire pitch gets smaller, the pressure drop increases. The effects of wire pitch and flow rate are shown in Figure 5-15. Since the peak radial blanket flow rate is in the range of 70,000 to 80,000 lb/h, the pressure drop can increase about 50% for a decrease in the wire pitch from 6 inches to 4 inches. A change of this magnitude could have a large effect on the reactor design. The radial blanket is fed from a low pressure plenum, whose pressure is governed by the peak blanket or control assembly pressure drop. A high pressure in the low pressure region will have an adverse effect on the hydraulic hold-down of the fuel assemblies and the design of the support structure.

The need for a low pressure drop across the blanket assembly may be the governing factor in the selection of the wire pitch. However, this would not be the case if the blanket assembly pressure drop was low (due to a lower flow rate) or if the peak blanket assemblies were fed from the high pressure plenum.

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Figure 5-14. Effect of Wire Pitch on Computed Temperature Distribution in a Blanket Assembly

Figure 5-15. Effect of Wire Pitch on Computed Pressure Drop in Radial Blanket

6. DEFECTED FUEL TESTING

Task Leader: P. E. Bohaboy

6.1 Defected Fuel Testing—P. E. Bohaboy

The use of a sealed-plenum fuel rod of a conservative design with proven quality and built with careful attention to detail provides a high level of assurance that the fission products in the fuel will be isolated from the primary cooling system. However, it is also desirable to understand the consequences of failure so that, should failure occur, adequate information is available to the plant operator to permit him to decide upon the appropriate mode of continued plant operation. It is therefore necessary to develop information on the post-failure performance of failed fuel in support of the reactor design, operating limits, and safety analysis.

The specific purposes of Task 6 are to:

1. Determine experimentally the consequences of continued reactor operation after failure of a fuel rod under normal or near normal LMFBR operating conditions;
2. Provide experimental and analytical data to permit selection of fuel rod design parameters and operating conditions which limit the consequences of fuel failure;
3. Provide experimental data on failure kinetics to permit modeling and prediction of failed fuel behavior for the projected range of LMFBR operating conditions; and
4. Identify the fuel and fission-product species distribution in the reactor coolant and cover gas as a function of the fuel rod failure mode.

6.1.1 B9B Capsule

Capsule B9B was designed to investigate the performance of a mixed-oxide fuel rod irradiated to 25,000 MWd/Te, defected and then operated for an additional 8,000 MWd/Te. Recently completed was a series of out-of-pile tests to determine the rate of fission gas release to be expected from the defected fuel rods. Fabrication of the capsule was terminated in July, but final analysis of the gas release test data has continued.

A detailed description of the gas release test apparatus, the experimental data, and the method of data analysis was presented in the 43rd Quarterly Report.⁶⁻¹ Post-test calibration of the flow meters has been completed. The calibration data required that adjustments and reanalysis of the raw test data be performed. The reanalysis does not significantly change the previous conclusions. Figure 6-1 shows the data from the eleven gas flow tests. For low Reynold's numbers ($Re \leq 1000$) the average friction factor, f , shows good agreement with the correlation $f = 16R_e^{-1}$. The Reynold's number (R_e) is based upon the pellet-cladding gap. For intermediate Reynold's numbers ($2000 \leq R_e \leq 10,000$), the average friction factor is essentially independent of the calculated Reynold's number. The friction factor however does increase as the pellet-cladding diametral gap decreases.

Preparation of a final report covering the gas release tests and analysis is in progress.

6.1.2 B9D Capsule

The purpose of the B9D capsule experiment is to investigate the performance of mixed-oxide fuel rods defected at power in high purity (≤ 5 ppm oxygen), high temperature (1050 to 1100°F) sodium and operated in the defected condition for several days at typical LMFBR peak power conditions.

The B9D capsule was irradiated in the GETR during November and December 1971. The post-irradiation (P.I.) examination of the capsule sodium, hardware and fuel has been completed and an analysis of the experimental data is in progress. Preparation of an interim report which summarizes the irradiation conditions and the post-irradiation examination data of the B9D experiment was initiated.

Figure 6-1. Calculated Average Friction Factor as a Function of Reynold's Number

Post-irradiation results of the fuel rod examination and fission gas analysis have been reported previously.^{6-1, 6-2} Additional information from the P.I. examination is given below:

6.1.2.1 Sodium Analysis (Fuel & Fission Products)

Three sections of capsule hardware containing sodium were analyzed to determine the quantity of fuel and fission products released to the sodium and deposited on the capsule walls. The locations of the three samples were as follows:

Sample B—Located between the top of the sodium electromagnetic pump and the bottom of the sodium deflector. (The gross gamma scan of the capsule indicated a higher than normal concentration of Cs-137 in this region.) The sodium temperature in this region was 1040 to 1090°F.

Sample G—Located directly above the fuel and uranium getter regions. The sodium temperature in this region was 1040 to 1090°F.

Sample L—Located at the bottom of the capsule, directly under the flowmeter. The sodium temperature in this region was 1000 to 1040°F.

The configuration of the capsule sections prevented taking a core sample of sodium from each section. Therefore, sodium samples were obtained by immersing each capsule section in hexane and slowly adding methyl alcohol and water to dissolve the sodium. The walls of the hardware were then leached with nitric and hydrochloric acids. The solutions were individually analyzed for uranium, plutonium and selected fission products.

Sodium from Sample B was removed in two stages to determine the effect of rinsing the capsule walls with methyl alcohol and water prior to the acid leach. The results indicated that the solvent action of the alcohol was minor in regard to premature removal of deposited material on the capsule walls.

The results of the analyses are reported in Tables 6-1 and 6-2. For all reported data, the isotopic decay was corrected back to the last day of the B9D irradiation on December 16, 1971. Table 6-1 gives the uranium, plutonium and fission products found in each sample. The data are presented in terms of atoms per gram of sodium sample. Variation between the samples was quite large and averaged about 50%. Sample L, however, consistently showed a greater concentration of fuel and fission products than the other samples. The reason for these differences between samples is not obvious since the sodium was flowing until solidification occurred at the conclusion of the irradiation.

The isotopic analysis of the uranium in all the samples indicated 3 to 9 times as much U-238 as U-235. Since the fuel was 93% enriched in U-235, the majority of the measured U-238 is most likely due to the dissolution of the uranium metal getter located directly above the fuel region.

Table 6-2 shows the uranium, plutonium and fission products leached from the walls of each of the capsule sections. Data have been presented in terms of atoms per square inch of wall surface. Sample L again indicated a greater concentration of fuel and fission products than Samples B and G. The average sodium temperature in Sample L was about 50°F cooler than the other samples and this region therefore may have acted as a cold trap resulting in enhanced deposition.

Table 6-3 shows the total fuel and fission products released to the sodium and capsule walls. Data were calculated from the averages of the four solutions shown in Table 6-1 and a capsule sodium inventory of 1000 grams. Wall deposition data were calculated from the averages of the three solutions shown in Table 6-2 and a capsule surface of 1240 square inches. Conclusions drawn from the data in Table 6-3 are:

- Of the uranium and plutonium released, 84% was deposited on the walls while 16% remained in the sodium.
- Most of the fission products were deposited on the capsule walls. Exceptions were Cs-137, SB-125 and Sr-90; 96%, 71% and 47% respectively of these isotopes were retained in the sodium.
- Of the total fission products found in the sodium and on the capsule walls 90.2% were Cs-137 and 8.4% were I-131. All other fission products contributed 1.4%.

Table 6-1
FUEL AND FISSION PRODUCTS IN SODIUM SAMPLES
FROM CAPSULE B9D

	Capsule Section			
	B-1 ^(a)	B-2	G	L
Size of Sodium Sample (grams)	27.5	52.1	15.7	13.6
Isotope				
U-235	1,200 ^(b)	650	1,300	2,400
U-238	7,300	5,400	9,500	12,000
Pu	580	230	630	1,800
Ce-141	4	2.1	2.4	4
Ce-144	1.1	0.4	1.5	1.3
I-131			380	2,400
Pm-147	6.2	4.4	6.4	5.4
Sb-125	3.6	4.6	7	8.1
Ru-103	0.007	0.004	0.34	0.06
Ru-106	0.005	0.002	0.47	0.12
Zr-95	1.3	0.91	2	4.6
Nb-95	2.7	1.8	3.4	2
Cs-137	20,000	5,800	13,000	19,000
Cs-134	8.4	2.9	5.6	9.5
Sr-89	2	2.5	1.7	13
Sr-90	23	18	2.7	44
Y-91	5.5	3.1	3.9	12

(a) 10^{12} atoms per gram of sodium.

(b) Sodium from Sample B was removed in two stages to determine effect of alcohol and water rinse prior to acid leaching of hardware walls.

Table 6-4 shows the fraction of fuel and fission products released to the sodium and capsule walls relative to the total capsule inventory. The fuel inventory was 38.5 grams of uranium (U-235) and 13.4 grams of plutonium. The inventory of uranium (U-238) from the getter was 1.8 grams. The fission product inventory was based upon an average burnup of 7000 MWd/Te. Fission yields used in the calculation are shown in Table 6-4. Conclusions drawn from the data in Table 6-4 are:

- In most cases the percentage release of fission products is comparable to the percentage release of fuel (~0.01%). A significant portion of these fission products probably were released with the fuel or were produced by fissioning of the fuel after it was released to the sodium.
- The percentage of fuel released from the failed rods was less than 0.015% of the total inventory.
- The percentage of plutonium released from the fuel was twice the released uranium. (The source of U-138 is presumed to be the uranium metal getter.)
- Cs-137, I-131, Sb-125 and Te-129m were released in significantly greater proportions than the fuel and other fission products. Of the total Cs-137 yield, 26% was released from the failed fuel rods.

Table 6-2
FUEL AND FISSION PRODUCT DEPOSITION ON CAPSULE WALLS
FROM B9D CAPSULE

Surface Area of Sample (in. ²)	Capsule Section		
	B	G	L
	10.2	10.6	8.6
Isotope			
U-235	1,200 ^(a)	6,100	11,000
U-238	6,200	17,000	99,000
Pu	440	4,100	5,800
Ce-141	4.7	4.4	15
Ce-144	1.1	4.4	7.3
Pm-147	11	<2.8	<3.5
Sb-125	2.2	1.6	<3
Ru-103	2.4	2.5	0.5
Ru-106	0.95	1.5	0.6
Zr-95	0.78	4.4	16
Nb-95	4.9	14	51
Cs-137	69	130	1,200
Te-129m	30	—	—
Sr-89	11	<9.5	<19
Sr-90	25	<130	<240
Y-91	3.2	6.3	33

(a) 10^{12} atoms per square inch of capsule wall.

Table 6-3
TOTAL FUEL AND FISSION PRODUCTS RELEASED
FROM B9D FUEL RODS

Isotope	Total Atoms in Sodium	Total Atoms on Walls	Total Walls ^(a) plus Sodium	Percent in Sodium	Percent on Walls
U-235	1,400	7,600	9,000	16	84
U-238	8,600	51,000	59,600	15	85
Pu	810	4,200	5,010	16	84
Ce-141	3.1	9.9	13	24	76
Ce-144	1.1	5.3	6.4	17	83
I-131	1,400	—	1,400	—	—
Pm-147	5.6	14	19.6	30	70
Sb-125	5.8	2.4	8.2	71	26
Ru-103	0.1	2.2	2.3	4	96
Ru-106	0.2	1.2	1.4	14	86
Zr-95	2.2	8.8	11	20	80
Nb-95	2.5	29	31.5	9	91
Cs-137	14,000	580	14,580	96	4
Te-129m	—	37	37	—	—
Sr-89	4.8	14	18.8	26	74
Sr-90	28	31	59	47	53
Y-91	6.1	17	23.1	26	74

(a) Total atoms of fission products in sodium and on walls = 1.66×10^{19} .

Table 6-4
FRACTION OF FUEL AND FISSION
PRODUCT INVENTORY RELEASED
FROM B9B FUEL RODS

Isotope	Fission Yield (%)	Percent Released
U-235	—	00.009
U-238	—	1.3
Pu	—	0.015
Ce-141	6.0	0.024
Ce-144	6.0	0.012
I-131	3.1	4.8
Pm-147	2.7	0.073
Sb-125	0.021	4.4
Ru-103	3.0	0.012
Ru-106	0.38	0.035
Zr-95	6.2	0.02
Nb-95	6.2	00.48
Cs-137	6.15	26.0
Te-129m	0.35	1.2
Sr-89	4.8	0.042
Sr-90	5.8	0.11
Y-91	5.4	0.046

6.1.2.2 Fuel Burnup Analysis

Fuel sections from each failed rod were submitted for fuel burnup analysis using the Nd-148 method (ASTM E321-69). The results are given in Table 6-5 along with burnup values calculated from sodium calorimetry and flux profile data. The values from this latter calculation represent a minor adjustment (downward) of previously reported burnup data.⁶⁻¹

The values obtained by the Neodymium-148 method and the sodium calorimetry method are in good agreement.

6.1.2.3 Fuel Metallography

A total of ten fuel specimens were prepared and photographed in the polished condition to show the extent of the reaction phase in the fuel-cladding gap. Figure 6-2 shows the location and condition of each of the eight transverse sections and the two longitudinal sections after a 600 grit grinding. Representative samples of the fine polished microstructure (unetched) are given in Figures 6-3, 6-4, and 6-5. High magnification shots of the fuel-cladding gap region are included to show the structure of the reaction product. Review of all the microstructure photographs yields the following information:

Table 6-5
CAPSULE B9D FUEL BURNUP

Method of Analysis	Average Fuel Burnup (MWd/Te)	
	B9D-1	B9D-2
Nd-148 (ASTM E321-69)	7000	5800
Sodium Calorimetry	7150	6000

Figure 6-2. Metallography Sectioning Diagram (B9D Fuel Rods)

Figure 6-3. Transverse Fuel Section 1/4 Inch Below B9D-2 Defect

A937-08

A937-02

0° DEFECT ORIENTATION

25X

Figure 6-4. Transverse Fuel Section 1/16 Inch Below B9D-2 Defect

Figure 6-5. Longitudinal Fuel Section of Bottom 1 Inch of B9D-2

- Power skewing due to the fully enriched fuel and the thermal flux of the GETR is indicated by the off-center location of the central void in the fuel.
- The microstructural changes and central void formation show that the top fuel rod (B9D-1) operated at a higher linear power than B9D-2. These results confirm the evidence of the fuel rod gamma scans and fuel burnup analyses.
- Fuel-sodium reaction product was found in the gap along the entire fuel column of each rod.
- No evidence of reaction product was found extruding into the annulus between the blanket uranium dioxide pellets and the cladding.

A detailed analysis of the reaction phase thickness in each transverse fuel section was performed. The measurements were taken at 25 equally-spaced locations around the circumference and are plotted in Figure 6-6. In general, the reaction phase thickness was larger and more uniform in the fuel samples from B9B-1.

The maximum and minimum thickness of the gap phase was 0.009 and 0.001 inch, respectively. The average reaction product thickness for each transverse and longitudinal fuel section is plotted in Figure 6-7. No trends are evident from the data which could possibly correlate reaction product thickness with fuel surface temperature or localized linear power.

6.1.2.4 Sodium Analysis (Oxygen Purity)

A niobium wire (6 inches long by 0.010 inch diameter) was included in the capsule to provide a means of measuring the equilibrium oxygen activity in the sodium during irradiation. The wire was attached to the exterior of the sodium flowmeter and was retrieved during the P.I. examination. Analysis of the wire was performed by personnel of Argonne National Laboratory using an internal friction technique. The results indicate that the sodium purity during irradiation was about 0.25 ppm of oxygen, and that it was considerably below the design level of ≤ 5 ppm oxygen. This number agrees with pre-irradiation measures of sodium purity that were previously reported.⁶⁻³

6.1.3 Sodium Fuel Reaction Study (B11 Capsule Series)

The B11 series of capsules are designed to measure the diameter increase of defected fuel rods as a function of time after defecting. Parameters to be investigated in the series are total oxygen availability, fuel burnup, operating power, and cladding temperature. B11A, B, and C are the first set of capsules in the series, and they are designed to investigate the effect of total oxygen availability on the rate of failed fuel rod diameter increase. Details of the capsule and fuel rod design have been presented previously.^{6-1, 6-3}

The following fabrication milestones have been completed during the current reporting period:

- Machining of all capsule and neutron radiographic standard parts.
- Encapsulation of the B11A, B and C fuel rods and uranium getters into the primary containments.
- Sodium filling of the B11A, B and C primary containments.
- Assembly and NaK filling of the B11B secondary containment.
- Assembly and NaK filling of the neutron radiographic standard.

Fabrication of capsule B11B has been completed and final preparations are being made to start irradiation in the GETR. Assembly of capsules B11A and B11C secondary containments is in progress.

The irradiation plan and schedule for the B11A, B and C capsules are given in Figure 6-8. Each capsule will initially be irradiated in the trial cable facility of the GETR. At selected intervals following fuel rod failure the capsules

Figure 6-6. Variation in Thickness of Reaction Phase in B9D Rods (Transverse Sections).

Figure 6-6, Cont. Variation in Thickness of Reaction Phase in B9D Rods (Transverse Sections).

B9D FUEL RODS;
POST-IRRADIATION
CONDITION

Figure 6-7. Variation in Thickness of Reaction Phase along B9D Fuel Rods

Figure 6-8. Capsule B11A, B, C Irradiation Plan

will be removed and neutron radiographed along with a fuel rod standard. Expected data from the neutron radiographs will provide information on the kinetics of the fuel-sodium reaction product formation. Capsule B11B will continue irradiation in a pool position of GETR until the neutron radiographs indicate that the formation of reaction product and the resulting fuel rod diameter increases have stopped. The irradiation plan provides for a possible irradiation of B11B fuel rod to 40,000 MWd/Te in the failed condition.

6.1.4 Run Beyond Failure Tests (B12 Series)

The purpose of the Run Beyond Failure (RBF) tests is to experimentally determine the consequences of continuing to operate with failed fuel rods in the core of an LMFBR under normal or near normal operating conditions. Experimental data obtained may potentially influence the fuel rod design and plant operating and safety limits.

Specific objectives of the RBF experiments are to obtain information concerning:

1. Determination of the amounts and species of fuel and fission products released from failed mixed-oxide fueled rods.
2. Characterization of the response of the failed fuel rod detection and location instrumentation to fuel rod defects of different geometry and location.
3. The extent of fuel rod deterioration (i.e., fuel rod diameter changes, fuel fragments released to the sodium coolant, etc.) which can result from fuel-sodium chemical reactions.
4. Determination of whether failed rods will deteriorate if operation is continued and affect the integrity of surrounding fuel rods.
5. Determination of whether deterioration of failed rods can be detected and characterized by the failed rod detection and location instrumentation.
6. Measurement of the levels of contamination of reactor systems and equipment as a result of continued operation with failed fuel.

The experiment proposal (Request for Approval-in-Principle) was prepared and submitted to the AEC-RDT for approval. A program to develop a self-actuating defect mechanism for these fuel rod tests was initiated. The development program will test a number of different defect designs in a flowing sodium environment.

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7. QUALITY ASSURANCE

7.1 QUALITY ASSURANCE PROGRAM PROGRESS AND ACCOMPLISHMENTS

The Quality Assurance Plan for Development Programs, BRD document NEDG-13881, was issued October 2, 1972, to establish the quality assurance program for BRD development programs, including PA-10. Prior to issue of NEDG-13881, the previous version of the PA-10 QA Plan, dated February 28, 1972, was in effect for all development programs.

The major activities of QA during this period have been the review of and comment on program planning, engineering planning, engineering documentation, and procurement documents for PA-10 tasks, and the preparation of QA planning for these tasks. Also, surveillance of fabrication, review of data packages, surveillance of shipping activities, and coordination of quality-related activities at EBR-II were performed for specific hardware items.

Surveillance of L16D experiment activities was performed by BRD-QA to verify the adequacy of hardware and documentation prior to shipment to EBR-II. BRD-QA personnel visited EBR-II to coordinate agreements for encapsulation and post-irradiation examination. Procedures for assembly of the L16 experiment at EBR-II were reviewed, and a conditional concurrence was granted. The conditional items were corrected and the procedures are now acceptable.

Surveillance of B11A, B, and C fuel rod assembly was performed, and the detailed assembly instructions for encapsulation were reviewed. Surveillance of encapsulation activities and sodium fill operations were in progress at the end of October.

Surveillance was performed over the final fabrication of the F9F experiment. The F9F engineering document package was reviewed to verify adequacy of hardware and records prior to shipment to EBR-II. Trial fits of the F9F assembly operations were completed. Surveillance activities were performed on the assembly hardware, fuel and dummy assembly (the latter for checkout of handling operations at EBR-II) for preparation for shipping. The EBR-II site was visited and receiving inspection of the F9F experiment hardware was performed at the site.

Surveillance of replacement F-11A fuel rods (through loading, welding, and wire wrapping) was performed, and QA review of data packages for shipping was accomplished. Samples and supporting documentation representing the fabrication of the F11A fuel were prepared for shipment to EBR-II.

Planning for the restart of fabrication for the F20 experiment was initiated, and surveillance of the F20 experiment restart fuel fabrication activities is being performed. Preparation of planning procedures for encapsulation was initiated by IPO.

Review of engineering documents is in progress to support the reassembly of the FTR Water Flow Test. In addition, engineering planning documents for the Sodium Flow Test are being reviewed.

Quality Engineering document reviews were completed for LASL examination of irradiated fuel rods E2R, F0A and F0C, and L-16 capsule interim examination at ANL. The F0A and F0C fuel rods were released for shipment from EBR-II to LASL for post-irradiation examinations.

7.2 NONCONFORMANCE ACTIVITIES

Nonconforming items reported during this period and their disposition were as follows.

One report covering measurement technique for determining B-11 end-plug hole alignment with the cladding defect notch required first level acceptance of the configuration as measured. Engineering investigation revealed that alignment tolerances as measured would have no significant effect on the experiment.

One report showed that the response time of B-11 thermocouples was slower than that specified for first level acceptance of thermocouples. Engineering investigation determined that the slower response time would not affect the intended application.

One report showed that the F9F outer hexagon tube dimensions exceeded the tolerances required for first level acceptance. Investigation disclosed that the discrepant dimensions were noncritical fit-up dimensions not affecting reactor assembly.

A report of out-of-tolerance dimensions of the F9F hold-down comb lead to reinspection of the comb, using machining setup jig, to determine the dimensions more accurately. Using this greater precision measurement, it was determined that the dimensions were actually in tolerance.

One report covering minor out-of-tolerance dimensions of grid spacer beams for the F9F subassembly required first level acceptance of the discrepancies after the responsible design component initiated an Engineering Change Notice to revise the applicable engineering documents to reflect acceptable dimensional tolerances.

One report covered tubing supplied for use as fuel cladding. Inspection samples tested proved to be marginal for ultimate tensile and burst pressure specifications. An MRB was held which resulted in the decision to accept the tubing and to issue an Engineering Change Notice, revising the acceptance limits to conform with engineering reevaluation of requirements.

Four nonconformances related to total gas content of pellets, cracks in pellets, high O/M ratio of pellets, and high iron and nitrogen content of fuel for F-20A were initiated and first level disposition made to accept. Review of the nonconformances by the Manager of QA resulted in an upgrading of the nonconformance reports to require full Material Review Board. MRB decision was, based on engineering investigation and justification, to accept as is. The disposition was submitted to AEC for concurrence before further processing and is currently awaiting final disposition.

7.3 PROGRAM AUDIT ACTIVITIES

Summaries of fourteen quality assurance audits of PA-10 activities performed during 1971 and 1972 were prepared and submitted to RDT for information, at RDT's request. These audits covered vendor audits, records audits, product and process audits and quality systems audits.

APPENDIX A. PRIOR REPORTS

Prior progress reports to the Commission under this contract include:

Monthly Progress Letters, Nos. 1 through 117, from July 1959 through September 1972.

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

GEAP-3888
First Quarterly Report
October–December 1961

GEAP-3957
Second Quarterly Report
January–March 1962

GEAP-3981
Third Quarterly Report
April–June 1962

GEAP-4080
Fourth Quarterly Report
July–September 1962

GEAP-4158
Fifth Quarterly Report
October–December 1962

GEAP-4214
Sixth Quarterly Report
January–March 1963

GEAP-4300
Seventh Quarterly Report
April–June 1963

GEAP-4382
Eighth Quarterly Report
July–September 1963

GEAP-4480
Ninth Quarterly Report
October–December 1963

GEAP-4601
Tenth Quarterly Report
January–March 1964

GEAP-4640
Eleventh Quarterly Report
April–June 1964

GEAP-4723
Twelfth Quarterly Report
July–September 1964

GEAP-4795
Thirteenth Quarterly Report
October 1964–January 1965

GEAP-4860
Fourteenth Quarterly Report
February–April 1965

GEAP-4931
Fifteenth Quarterly Report
May–July 1965

GEAP-4982
Sixteenth Quarterly Report
August–October 1965

GEAP-5098
Seventeenth Quarterly Report
November 1965–January 1966

GEAP-5158
Eighteenth Quarterly Report
February–April 1966

GEAP-5108
Nineteenth Quarterly Report
May–July 1966

GEAP-5292
Twentieth Quarterly Report
August–October 1966

GEAP-5433
Twenty-First Quarterly Report
November 1966–January 1967

GEAP-5491
Twenty-Second Quarterly Report
February–April 1967

GEAP-5522
Twenty-Third Quarterly Report
May–July 1967

GEAP-5541
Twenty-Fourth Quarterly Report
August–October 1967

GEAP-5584
Twenty-Fifth Quarterly Report
November 1967–January 1968

GEAP-5631
Twenty-Sixth Quarterly Report
February–April 1968

GEAP-5677
Twenty-Seventh Quarterly Report
May–July 1968

GEAP-5700
Twenty-Eighth Quarterly Report
August–October 1968

GEAP-5753
Twenty-Ninth Quarterly Report
November 1968–January 1969

GEAP-10028
Thirtieth Quarterly Report
February–April 1969

GEAP-10028-31
Thirty-First Quarterly Report
May–July 1969

GEAP-10028-32
Thirty-Second Quarterly Report
August–October 1969

GEAP-10028-33
Thirty-Third Quarterly Report
November 1969–January 1970

GEAP-10028-34
Thirty-Fourth Quarterly Report
February 1970–April 1970

GEAP-10028-35
Thirty-Fifth Quarterly Report
May 1970–July 1970

GEAP-10028-36
Thirty-Sixth Quarterly Report
August 1970–October 1970

GEAP-10028-37
Thirty-Seventh Quarterly Report
November–January 1971

GEAP-10028-38
Thirty-Eighth Quarterly Report
February–April 1971

GEAP-10028-39
Thirty-Ninth Quarterly Report
May–July 1971

GEAP-10028-40
Fortieth Quarterly Report
August–October 1971

GEAP-10028-41
Forty-First Quarterly Report
November 1971–January 1972

GEAP-10028-42
Forty-Second Quarterly Report
February–April 1972

GEAP-10028-43
Forty-Third Quarterly Report
May–July 1972

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

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

In addition, the following topical reports have been
issued:

GEAP-3287
Fast Oxide Breeder - Reactor Physics,
Part I - Parametric Study of 3000 MWe Reactor Core;
P. Greebler, P. Aline, and J. Sueoka
(November 10, 1959)

GEAP-3347
Fast Oxide Breeder - Stress Considerations
in Fuel Rod Design;
K. M. Horst
(March 28, 1960)

GEAP-3486
Fast Oxide Breeder Project
Fuel Fabrication

Part I - Plutonium-Uranium Dioxide Preparation
and Pelletized Fuel Fabrication;
J. M. Cleveland and W. C. Cavanaugh

Part II - Fabrication of
Plutonium-Uranium Dioxide Specimens by Swaging;
M. E. Snyder and W. C. Cowden
(August 15, 1960)

GEAP-3487
Fast Oxide Breeder -
Preliminary Sintering Studies
of Plutonium-Uranium Dioxide Pellets;
J. M. Cleveland and W. C. Cavanaugh
(August 15, 1960)

GEAP-3646
Calculation of Doppler Coefficient
and Other Safety Parameters
for a Large Fast Oxide Reactor;
P. Greebler, B. A. Hutchins, and J. R. Sueoka
(March 9, 1961).

GEAP-3721
Core Design Study for a 500 MWe
Fast Oxide Reactor;
K. M. Horst, B. A. Hutchins,
F. J. Leitz, and B. Wolfe
(December 18, 1961)

GEAP-3824
Fabrication Cost Estimate
for UO_2 and Mixed PuO_2 - UO_2 Fuel;
G. D. Collins
(January 24, 1962)

GEAP-3833
The Post-Irradiation Examination
of a PuO_2 - UO_2 Fast Reactor Fuel;
J. M. Gerhart
(November 1961)

GEAP-3856
Experimental Fast Oxide Reactor;
K. P. Cohen, M. J. McNelly and B. Wolfe
(November 27, 1961)

GEAP-3876
Plutonium Fuel Processing and Fabrication
for Fast Ceramic Reactors;
H. W. Alter, G. D. Collins, and E. L. Zebroski
(February 1, 1962)

GEAP-3880
Comparative Study of PuC - UC and PuO_2 - UO_2
as Fast Reactor Fuel,

Part I - Technical Considerations;
K. M. Horst and B. A. Hutchins
(February 15, 1962)

Part II - Economic Considerations;
G. D. Collins
(November 15, 1962)

GEAP-3885
Experimental Fast Ceramic Reactor Design,
Status Report as of October 31, 1961;
Edited by K. M. Horst
(April 24, 1962)

GEAP-3923
Resonance Integral Calculations
for Evaluation of Doppler Coefficients -
The RAPTURE Code;
J. H. Ferziger, P. Greebler, M. D. Kelly,
and J. Walton
(June 12, 1962)

GEAP-4028
A Fuel Reprocessing Plant
for Fast Ceramic Reactors;
H. W. Alter
(February 1, 1962)

GEAP-4058
Analytical Studies of Transient Effects
in Fast Reactor Fuels;
R. B. Osborn and D. B. Sherer
(August 1962)

GEAP-4090
FORE - A Computational Program
for the Analysis of Fast Reactor Excursions;
P. Greebler and D. B. Sherer
(October 1962)

GEAP-4092
Doppler Calculations for
Large Fast Ceramic Reactors,
Effects of Improved Methods
and Recent Cross Section Information;
P. Greebler and E. Goldman
(December 1962)

GEAP-4130
Experimental Studies of Transient Effects
in Fast Reactor Fuels
Series I, UO_2 Irradiations;
J. H. Field
(November 15, 1962)

GEAP-4426
Conceptual Mechanical Design
of 565 MWe Fast Ceramic Reactor;
A. G. Silvestor
(April 1963)

GEAP-4271

Measurement of Oxygen-to-Metal Ratio
in Solid Solutions of UO_2 and PuO_2 ;

W. L. Lyon

(May 31, 1963)

GEAP-4283

Experimental Studies of Sodium Logging in FCR Fuels;

G. L. O'Neill, P. E. Novak, M. L. Johnson,
and W. E. Baily

(September 1963)

GEAP-4350

Fast Reactor Meltdown Excursions-

Analysis and Computer Program FARM I;

B. C. Luci, D. R. Riley, and N. F. Friedman

(December 5, 1963)

GEAP-4410

Calculations of Doppler and
Sodium Reactivity Effects;

P. Greebler

(November 1963)

GEAP-4414

Reactor Safety and Fuel Cycle Economics;

K. P. Cohen

(November 1963)

GEAP-4418

Liquid Metal Fast Breeder Reactor Design Study,
1000 MWe PuO_2 - UO_2 Fueled Plant,

Vols. I and II;

M. J. McNelly, et al.

(January 1964)

GEAP-4420

Influence of Doppler Effect on Meltdown Accident;

N. F. Friedman

(December 1963)

GEAP-4466

Experimental Studies of

Plutonium and Fission Product Redistribution

in Fast Reactor Fuels;

T. Lauritzen, et al.

(July 1966)

GEAP-4469

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in Fast Reactor Fuels,

Series III,

Pre-Irradiated Mixed Oxide (PuO_2 - UO_2),

Irradiations Final Report,

Transient Irradiations;

J. E. Hanson, J. H. Field

(February 1968)

GEAP-4471

Calculated Nuclear Parameters and

Their Uncertainties in a 1000 MWe FCR;

P. Greebler, et al.

(October 1966)

GEAP-4472

User Requirements for Cross Sections

in the Energy Range from 100 eV to 100 keV;

P. Greebler and B. A. Hutchins

(1966)

GEAP-4580

FLUX-A Computer Code

for Analyzing the Reactor Response

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P. Greebler, B. A. Hutchins, and M. D. Kelley

(March 22, 1965)

GEAP-4630

The Effects of Resonance Overlap on

the Doppler Coefficient in a Fast Ceramic Reactor,

Computer Codes RAVE and DOPIE;

B. A. Hutchins

(August 1964)

GEAP-4675

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the Plutonium-Uranium Mixed Oxide System;

W. E. Baily

(July 1966)

GEAP-4770

A Technical and Economic Evaluation of

Vented Fuel for Sodium-Cooled Fast Ceramic Reactors;

G. L. O'Neill, J. Duffy, J. C. Gilbertson, F. W. Knight,

and D. B. Sherer

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An Analysis of Fast Reactor Transient Response and Safety in Selected Accidents;
W. Meinhardt, Jr.
(August 1966)
- GEAP-4804
Experimental Studies of
Transient Effects in Fast Reactor Fuels,
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J. E. Hanson, J. H. Field, and S. A. Rabin
(June 1965)
- GEAP-4809
Fast Reactor Meltdown Accidents
Using Bethe-Tait Analysis;
R. Meyer and B. Wolfe
(January 1957)
- GEAP-4819
Experimental Studies of
Transient Effects in Fast Reactor Fuels,
Series III,
Pre-Irradiated Mixed Oxide ($\text{PuO}_2\text{-UO}_2$) Irradiations,
Interim Report, Steady State Irradiations;
J. E. Hanson and S. A. Rabin
(September 1965)
- GEAP-4822
Studies of Alternate Core Design Concepts
for Fast Sodium Cooled Reactors;
C. L. Cowan
(November 1966)
- GEAP-48/U
Some Design and Irradiation Experience
with Capsules for Long Ceramic Fuel Elements;
F. W. Knight
(July 1966)
- GEAP-4877
Diffusion of Plutonium and Uranium
in Oxide Fuel at 2400°C ;
J. Davies and P. Novak
(August 1966)
- GEAP-4878
The Solid-Liquid Phase Diagram
for $\text{UO}_2\text{-PuO}_2$ System;
W. L. Lyon and W. E. Baily
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Thermal Conductivity of
Uranium and Uranium Plutonium Oxides;
F. J. Hestzler, T. E. Lannin, K. J. Perry and E. L. Zebroski
(January 1968)
- GEAP-4897
Irradiation Behavior of
Hyper- and Hypo-Stoichiometric Plutonia-Urania Fuel
at 70,000 MWd/Te;
E. L. Zebroski, W. E. Baily, and D. P. Hines
(March 1965)
- GEAP-4922
BISYN—
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M. D. Kelley
(December 1966)
- GEAP-4939
Safety and Economic Characteristics of
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K. P. Cohen and G. L. O'Neill
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- GEAP-5271
Implications of Recent Fast Critical Experiments
on Basic Fast Reactor Design;
P. Greebler
(March 1968)
- GEAP-5272
Evaluation and Computation of
Pu-239 Cross Section Data
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P. Greebler, P. Aline, B. Hutchins
(January 1967)
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FORE II, A Computational Program for
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B. E. Lawler, et al.
(September 1967)
- GEAP-5340
Neutron Spectrum Measurements in a Fast Plutonium
Assembly ZPR-III Assembly 48;
P. S. Brown and A. Weitzberg
(April 1967)

GEAP-5344

Electron Microanalysis of Radioactive Materials I,
Preliminary Experiments and Plans for a Shielded Facility;
H. Rosenbaum, et al.
(February 1968)

GEAP-5355A

Burnup: A FORTRAN IV Code for Computing U and Pu
Fuel Burnup from U, Pu, and Nd Mass Spectrometric
Measurements.
B. F. Rider
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GEAP-5411

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D. J. Bender, D. M. Switick, J. H. Field
(January 1968)

GEAP-5493

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of Uranium Dioxide;
R. Asamoto
(February 1968)

GEAP-5494

RAPFU—A Computer Code;
P. Greebler, C. Cowan
(November 1967)

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C. N. Craig, W. E. Baily
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GEAP-5583

The Role of Fission Products
in the Swelling of Irradiated UO₂-(U, Pu)O₂ Fuel;
F. Anselin
(January 1969)

GEAP-5633

Compatibility of LMFBR Fuel Pin Components;
T. A. Lauritzen
(June 1968)

GEAP-5635

Significance of Neutron Data Uncertainties
to Fast Reactor Economics and Power Plant Design;
B. A. Hutchins, et al.
(June 1968)

GEAP-5683

Thermodynamic Analysis
of Possible Chemical Interactions in the System,
UO₂-PuO₂ Fuel-Sodium-Stainless Steel;
E. A. Aitken
(July 1968)

GEAP-5744

Analytical Procedures and Applications
of Fluence Determinations from EBR-II Flux Wires;
A. Withop, B. A. Hutchins, G. C. Martin
(March 1968)

GEAP-5748

Fuel Cladding Heat Transfer Coefficients
for Oxide Fuel Rods;
C. N. Craig, et al.
(January 1969)

GEAP-5761

The Irradiation of Mixed Oxide Fuel
to 125,000 MWd/Te in a Thermal Flux;
B. F. Rubin and K. J. Perry
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of PuO₂-UO₂ Fuel Pins;
S. A. Rabin, W. W. Kendall, W. E. Baily
(February 1968)

GEAP-10046

BISYN—PERT:
A Two-Dimensional Perturbation Module
for the BISYN System;
M. D. Kelley and T. R. Jones
(April 1969)

GEAP-10052

Turbulent Heat Transfer in a Rod Bundle
with Liquid Metal Coolant;
D. J. Bender and P. M. Magee
(July 1969)

GEAP-10054

Compressive Creep Characteristics
of Stoichiometric Uranium Dioxide;
P. E. Bohaboy, R. R. Asamoto, A. E. Conti
(May 1969)

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Mechanical Properties of Irradiated Type-304
and Type-316 Stainless Steel in Fast Reactors;
F. A. Comprelli, H. J. Busboom, M. F. Gebhardt
(June 1969)

GEAP-10066

Mechanical Properties Evaluation of Austenitic Stainless Steels Irradiated in EBR-II;

T. Lauritzen, A. Withop, G. P. Ferguson
(July 1969)

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High Burnup Performance of Powder & Pellet Mixed-Oxide Fuel;

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GEAP-10403

Axial Expansion of Mixed-Oxide Fuel Measured In-Pile;
D. A. Cantley, G. R. Hull, C. N. Craig
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GEAP-13515

The Melting Point of Plutonia-Urania Mixed Oxides Irradiated to High Burnup;

J. L. Krankota, C. N. Craig
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GEAP-13517

Empirical Analysis of Swelling of Austenitic Stainless Steels in Fast Reactors;

F. A. Comprelli
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GEAP-13537

User's Manual for DOT2DB: A Two-Dimensional Multigroup Discrete Ordinates Transport/Diffusion Code with Anisotropic Scattering;

R. Protsik, E. G. Leff
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GEAP-13543

Molten Fuel Movement in Transient Overpower Tests of Irradiated Oxide Fuel;

T. Hikido
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GEAP-13549

Performance of Plutonium-Uranium Mixed Oxide Fuel Pins in a Fast Reactor (EBR-II) to 50,000 MWd/Te;

R. C. Nelson
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The Influence of Surface Defects on Short-Term Mechanical Properties of Austenitic Stainless Steel Tubing;

T. A. Lauritzen, A. Montes, A. Conti
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GEAP-13551

Effects of Nuclear Methods Approximations on LMFBR Analysis;

R. Protsik
(August 1969)

GEAP-13553

An Evaluation of the Quality of Seamless Austenitic Stainless Steel Tubing;

T. A. Lauritzen
(December 1969)

GEAP-13556

BICYCL - A Computer Code for Two-Dimensional Analysis of Equilibrium Fuel Cycles;

P. C. Vaughn
(December 1969)

GEAP-13562

Transient Overpower Irradiation of Axially-Restrained, Zero-Burnup Fast Reactor Fuel Specimens;

G. R. Thomas
J. H. Field
(September 1969)

GEAP-13571

Evaluation of Physical and Mechanical Properties of Type-304 Stainless Steel After Irradiation to 3.9×10^{22} n/cm² Total Fluence;

H. J. Busboom, R. R. Asamoto
(November 1970)

GEAP-13582

Summary of (U,Pu)O₂ Properties and Fabrication Methods (November 1970);

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GEAP-13591

Pu-239 Unresolved Resonance Data
for ENDF/B Version II;
J. T. Hitchcock and B. A. Hutchins
(April 1970)

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ENDRUN-1, A Computer Code to Generate
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GEAP-13599

FUMBLE: An Approach to Fast Power Reactor
Fuel Management and Burnup Calculations;
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K. E. Gregoire, P. E. Novak, R. E. Murata
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Testing Needs;
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GEAP-13639-1, -2

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Clad/Fuel-Sodium Thermal Interaction;
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M. D. Kelley
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R. C. Nelson, et al.
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GEAP-13703

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GEAP-13704

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B. A. Hutchins, C. L. Cowan, M. D. Kelley, J. E. Turner
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GEAP-13722

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GEAP-13729

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Oxide Fuel Element Modeling Code BEHAVE-1;
F. E. Gelhaus and J. D. Stephen
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S. Oldberg
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G. P. Wozadlo, B. F. Rubin, P. Roy
(August 1972)

APPENDIX B

LIST OF ABBREVIATIONS, SYMBOLS, AND ACRONYMS

AEC-RDT	=	Atomic Energy Commission—Division of Reactor Development and Technology
ANL	=	Argonne National Laboratory
Ann.	=	annealed
Ann. V	=	annealed at VNC
ARC	=	Argonne Reactor Computation (System)
ASTM	=	American Society for Testing Materials
at. %	=	atom percent
bcd	=	binary coded decimal
BEHAVE	=	fuel modeling code
BOL	=	bulk outlet
BRD	=	(GE) Breeder Reactor Division
BUBBLE	=	coolant boiling and channel voiding code, a subroutine of FREADM
co-ppt	=	coprecipitated
CSEWG	=	Cross Section Evaluation Working Group
C.W.	=	cold worked
D Plant	=	Demonstration Plant
DEMON	=	diffusion theory flux solution computer code
DFR	=	Dounreay Fast Reactor
DRL	=	Division of Reactor Licensing (AEC)
DTA	=	differential thermal analysis
EBR-II	=	Experimental Breeder Reactor II
EM	=	electromagnetic
ENDF/B-II	=	evaluated nuclear data file/B, version II
ENDRUN-2	=	computer code for cross section processing version 2
E.o.L.	=	end-of-life
ETOE-MC ²	=	computer codes for cross section processing
ETR-FEFPL	=	Experimental Test Reactor—Fuel Element Failure Propagation Loop
FCC	=	forced convection capsule
FCI	=	fuel coolant interaction
FFTF	=	fast flux test facility
FGAS	=	fission gas analysis system
FREADM	=	fast reactor excursion and accident dynamics model
FSAR	=	Final Safety Analysis Report
FTR	=	Fast Test Reactor
FUMBLE	=	fuel management computer code for startup cycles
GETR	=	General Electric Test Reactor
GMUG-2	=	Multigroup data file based on modified ENDF/B-II data

GT	=	Garafolo treated
GTV	=	Garafolo treated at VNC
HCD	=	hypothetical core disassembly
HEDL	=	Hanford Engineering Development Laboratory
HFEF	=	hot fuel examination facility
h_{gap}	=	fuel cladding gap conductivity
i.d.	=	inside diameter
I.P.	=	integrated power
kbar	=	thousands of atmospheres
k_{eff}	=	effective neutron multiplication factor
keV	=	kilo electron volts
ksi	=	thousands of pounds per square inch
ℓ	=	angular momentum states
lb _f	=	pounds force
lb _m	=	pounds mass
LASL	=	Los Alamos Scientific Laboratory
LMFBR	=	Liquid Metal Fast Breeder Reactor
MARS	=	computer code for core disassembly calculation
mCi	=	millicurie
MeV	=	million electron volts
MeV-GE	=	General Electric adjusted fluence for EBR-II
mg	=	milligram
Mod	=	modified
mole	=	gram molecular weight
MRB	=	Material Review Board
msec	=	millisecond
MWd/Te	=	megawatt-day per ton (metric, tonne, 2205 lb)
ND	=	not determined
n/cm^2	=	time integrated neutron flux
nvt	=	time integrated neutron flux
NOM	=	nominal
o.d.	=	outside diameter
O/M	=	oxygen-to-metal ratio
% Δk	=	percent reactivity addition
%TD	=	percent of theoretical density of fuel
PEC	=	pulsed eddy current (analyzer)
Phys Mix	=	physically mixed
P.I.	=	postirradiation
PISCES	=	Physics International multispatial code for engineering and science
P_L	=	linear power
P_{max}	=	peak power

PPS	=	plant protection system
PUNT	=	service code for maintaining GMUG data files
R	=	roentgen, unit of radiation dosage
RABBLE	=	computer code for special resonance self-shielding homogeneity
SA or S/A	=	subassembly
S Ann.	=	solution annealed
SEFOR	=	Southwest Experimental Fast Oxide Reactor
S.o.L	=	start of life
SS	=	stainless steel
(ST)	=	solution treated
Stab.	=	stabilized
STP	=	standard temperature and pressure
SUR	=	strong unbroken ring
S _x	=	standard deviation
SYN	=	combined synthesis-fuel cycle code system
TD or T.D.	=	theoretical density
TDOWN-2	=	computer code for cross-section processing, version 2
TEM	=	transmission electron microscopy
Theo	=	theoretical
THTD	=	transient heat transfer code (version D)
THTE	=	transient heat transfer code (version E)
TOFF	=	transient overpower fuel failure model
TREAT	=	transient reactor test facility
TRIAL	=	computer code for testing accuracy of Three-Dimensional (TRI) (A)xial (L)inking
UTS	=	ultimate tensile strength
VENUS	=	disassembly calculation code
VERA-11	=	critical assembly 11A in VERA facility
VNC	=	Vallecitos Nuclear Center
VOIDUM	=	systems hydraulics model, a subroutine of FREADM
vol %	=	volume percent
W/cc	=	watts per cubic centimeter
wl %	=	weight percent
XPORT	=	a computer routine to prepare code for shipment to outside users
ZEBRA-3	=	critical assembly 3 in ZEBRA facility
ZPR-2	=	critical assembly 2 in Zero Power Plutonium Reactor facility
ZPR-3	=	critical assembly 3 in Zero Power Plutonium Reactor facility
ZPR-6	=	zero power reactor facility 6
μm	=	micrometers

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