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SODIUM-COOLED REACTORS CORE ENGINEERING PROGRAM

FORTY-SIXTH QUARTERLY REPORT
FEBRUARY-APRIL 1973

U.S. ATOMIC ENERGY COMMISSION
CONTRACT AT(04-3)-893 TASK 11

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AEC Research and
Development Report
May 1973

SODIUM-COOLED REACTORS CORE ENGINEERING PROGRAM

Forty-Sixth Quarterly Report

February—April 1973

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Prepared for the
United States Atomic Energy Commission
Under Contract No. AT(04-3)-893
Task 11

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ABSTRACT

This report is the forty-sixth in a series of quarterly progress reports published under the Core Engineering 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 February, March and April 1973 is described for tasks related to fuel rods, fuel rod assemblies, core restraint, control and blanket assemblies, and defected fuel.

INTRODUCTION AND SUMMARY

The development of the liquid metal cooled fast breeder reactor depends on inter-disciplinary, complex technology. One of the essential tasks in this development is the complete engineering design of the reactor core. This includes the design of fuel rods, fuel rod assemblies, control and blanket assemblies, and the core restraint system. Closely related to these tasks is the study of defected fuel, to minimize its occurrence, and enable operation with a small, unavoidable incidence of defects.

This report is the forty-sixth quarterly progress report published under the Core Engineering Program. It describes the progress on these subtasks during February, March and April 1973. A comprehensive list of prior reports produced under Task 11 is contained in Appendix A and a list of abbreviations, symbols, and acronyms is contained in Appendix B.

SUBTASK 1—ENGINEERING COORDINATION AND LIAISON

The final contract modification covering GFY-73 was executed for the Core Engineering Program.

Preliminary program planning for GFY-74 was initiated and discussed with RDT representatives.

Program review meetings were held on Subtasks 3, 4 and 5 in February and March at AEC headquarters.

SUBTASK 2—FUEL ROD ENGINEERING

Revision 3 of the LIFE code is operational. Parametric calculations using Revision 2 indicate that interpretation of LIFE predictions of cladding inelastic strain due to steady fuel growth are clouded by prediction of a hydrostatic stress state in the fuel after closure of the fuel-cladding gap at start of life, which prevents inward creep of the fuel. A multi-node simulation of LIFE (SIMPLIFE) indicates that this problem is reduced by increasing the number of radial fuel nodes to twelve from the current three.

Analysis of the recent fuel cladding failures after installation of the reflector in EBR II highlights the need for LIFE model development to account for cladding primary creep and plastic flow under a short term load.

The LIFE code parametric calculations of the predicted cladding inelastic strain as a function of design and operating variables are complete. Interpretation of the results is in progress.

The national working committee has reached agreement on recommendations for revision of the thermal performance models in the LIFE code.

Predictions of the thermal performance of the fuel rods of the F20 power-to-melt experiment using the probabilistic design code "PEFT" were completed. Sample calculations of the structural response were completed with the LIFE-II code.

A preliminary draft of the report "Fuel Failure Predictions and Consequences" was delivered to RDT for initial comment.

Thermal and structural design analyses of the B9D experiment were completed.

The clad loading sensitivity study is now 80% complete.

Local indentations in the cladding due to contact loads on a bundle-wide basis are large enough to significantly reduce the contact loads which would be predicted assuming no local indentations.

During recent EBR II interim examinations, about 30 encapsulated test fuel rods were found to have failed, including 10 rods from this program: 2 rods from the F0 test, 4 rods from the F3B test, 1 rod from the F5 test, and 3 rods from the F8A test. All these rods experienced a significant change in axial fission rate profile when the stainless steel reflector was added to the EBR-II core.

Requirements for the interim examination of the F9A and F9D experiments were forwarded to the EBR-II Project.

Based on interim examination results and calculations from the EBR-II Project, a test plan for continuation of low power experiment F8B was formulated. Irradiation is scheduled to resume in Run 63. Addition of the stainless steel reflector to the EBR-II core caused significant changes in axial power profile in the outer regions of the core where this low power experiment operates.

Interim examination of the F9B experiment at 9.3 at.% burnup showed the rods to be in good condition. The experiment is scheduled to continue irradiation in Run 63.

Encapsulation and final inspection of the fuel rods for the F20 experiment was completed.

Analysis of the ANL "slit-tube" data indicates that, within the scatter in the data, the results can be adequately reproduced using currently available irradiation creep and swelling correlations for Type-304 stainless steel, without invoking concepts of swelling-dependent creep or stress-affected swelling.

Analysis of the EBR-II driver fuel rod failures shows, using currently available thermal creep and irradiation creep correlations, that the thermal creep component of the cladding strain at failure is sufficiently large to have caused the cladding failure. However, use of the "swelling enhanced irradiation creep" approach would indicate that all of the failure strain can then be attributed to this mechanism, thus violating the concept of superplastic irradiation creep.

The four L-15 capsules have been disassembled. All mechanical properties samples are available for mechanical and microstructural examination at National Laboratories. Disassembly proceeded very smoothly. This work was terminated and a report prepared describing the L-15 capsules, materials, and irradiation history.

The L-16 capsules in S/A X098A are continuing irradiation in EBR-II. A request was approved by RDT to extend the irradiation beyond Run 62 and through Run 65 in order to allow time for the reencapsulation capability to be demonstrated at HFEF. The ten L-16 capsules from S/A X098 were shipped to LASL for examination. A test plan was approved by both GE and LASL, and work will begin on a noninterfering basis as hot cell time permits. The L-16E capsules have been designed and engineering drawings and fabrication specifications were issued. Six capsules will be fabricated. The Type-321H vacuum melted tubing used in the L-16 capsules has been inspected. The tubing meets the dimensional requirements of the specification to which it was purchased.

SUBTASK 3—FUEL ASSEMBLY ENGINEERING

The EBR-II 37-rod grid spacer assembly tests F9E and F9F are continuing irradiation. At the end of reactor run 62A, F9E has accumulated a burnup of 4.2 at.%, which represents 37% of target burnup, and F9F has accumulated 2.4 at.% which is 26% of target.

A comprehensive review meeting was held at AEC-RDT headquarters on March 1 and 2, 1973 to discuss the GE FTR grid spaced assembly design and development plans.

A redesign of the grid spacer-to-channel mechanical attachment is being performed to increase the design margin against the force tending to overcome the locking mechanism. This is backed by a series of mechanical tests.

Development work has been initiated on the Electrical Discharge Machining (EDM) method for fabricating the grid spacer retaining recesses on the inside hexagonal channel walls.

Final reports have been prepared on the 37-rod sodium flow endurance tests and the 217-rod hydraulic test assembly which sustained grid spacer dislodgement during fabrication.

Preliminary work has been initiated to define the work scope required for the wire-wrap assembly development program.

The PECS (Probabilistic Evaluation of Cladding Stress) code was applied to the analysis of a typical LMFBR core using updated material property correlations. This analysis indicates that the case analyzed represents an untenable design position: the mean number of failures in a batch was predicted to be 285 and the peak-rod failure probability was 7.2%. The analysis established that cladding temperature is the most influential parameter; that reduction of nominal temperature, or reduced uncertainties in temperature, increase cladding reliability dramatically. Time-to-rupture appears to be next in order of importance.

SUBTASK 4—CORE RESTRAINT ENGINEERING

The Phase I test results and analysis were documented in GEAP-10798, "Core Restraint System Tests and Analysis, Phase I: Planar Externally Clamped Core," submitted to RDT. The most significant conclusions of this study are the following:

1. Comparison with previous HEDL PCM tests shows that, in general, less core compaction is obtained with high friction ($f = 0.68-0.75$) surfaces on load pads than with low friction ($f = 0.25-0.33$) surfaces. However, the tests demonstrated that even cores with high friction load pads can be adequately compacted by proper selection of the compaction sequence.
2. The optimum method for compacting the disarrayed core was found to be sequential loading of opposite yoke pairs.
3. Statistical analysis of the average pad forces shows the load distribution to be hydrostatic (i.e., equal forces on all pad faces of a channel in a given radial position) except for the outermost row of channels in contact with the yokes.

Phase II tests of an internal tightened core restraint system were completed. Tests included interchannel load distribution measurements, determination of the core compressibility, and examination of various internal tightener actuation sequences. The data are now being analyzed.

Modifications are required to the Planar Clamping Model to prepare it for the Phase III tests of a demonstration plant-size core (361 channels). Fabrication of parts was completed and installed in May and Phase III tests will be conducted in June.

SUBTASK 5—CONTROL AND BLANKET ASSEMBLY ENGINEERING

This subtask has recently been redirected toward designing and qualifying Europium oxide absorber assemblies for use in the FTR and LMFBR program. The subtask is focused on evaluating a procurement package for an FTR-Eu₂O₃ assembly including specifications, drawings, and design support documents. The scope also includes the definition of experimental programs to support the design and qualification effort. The boron carbide control assembly and blanket assembly engineering activities were terminated during the reporting period.

The B₄C control assembly design was evaluated for the case of no internal clamps to determine the feasibility of accommodating generated helium gas with sealed plenums. The required plenum length was determined for a 19, 37, and 61-rod assembly.

Requirements and bases for B₄C shim or primary safety control assemblies compiled to date are summarized in Appendices C and D.

The coolant temperature and flow distribution in the radial blanket assemblies were calculated with an improved FULMIX computer code that considers laminar, transitional, or turbulent flow. The results were compared with ORNL-FFM test data. In all cases the computer results for coolant temperatures are higher than the experimental results on the hot side and lower on the cold side.

The assembly power for the blanket region has been determined by two methods, an X-Y and R-Z diffusion theory method. The cylindrical representation of core and blanket region boundaries gives a 10 to 24% higher peak along the core boundary than the hexagonal model that is considered to be more accurate.

SUBTASK 6—DEFECTED FUEL TESTING

Capsules B11A and B11B have been irradiated as planned and the post-irradiation examination of each capsule is in progress. The irradiation of capsule B11C has continued.

Examination of the B11A neutron radiographs indicate that a maximum diameter increase of 0.0462 inch (21.1%) was attained approximately 30 hours after failure of the fuel rod. These results agree with the maximum swelling of an earlier test with similar fuel parameters (B9A-1).

Comparison of the B11B and B11C data indicates that axial migration of oxygen within the fuel column does not substantially influence the maximum fuel swelling or the rate of reaction product formation.

The sodium coolant in each of the B11 capsules was maintained at <1 ppm of oxygen. This high sodium purity did not prevent the formation of sodium-fuel reaction product. However, deterioration of the reaction product was observed in B11A and B11B following approximately 10 to 12 days of irradiation. The behavior of the reaction product suggests that it is thermodynamically unstable in high purity sodium.

QUALITY ASSURANCE

A review of the F-20 capsules status was held March 23, 1973 and was judged satisfactory.

A review was conducted of interim examination planning documentation for GE experiments to be examined at HFEF. Subsequent meetings at ANL-West between BRD-QA and HFEF personnel resulted in satisfactory agreements on examination work planning.

Several nonconformances were reported on work received from CDS Engineering. An audit of the vendor resulted in suspension of further purchase orders until corrective action is taken to assure future quality fabrication practices.

1. ENGINEERING COORDINATION AND LIAISON

Subtask Leader — J. H. Terhune

The objective of this subtask 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 final contract modification was executed covering the government fiscal year 1973 period (FY-73). As a result, the Core Engineering Program was redesignated Task 11 of Contract AT(04-3)-893, and the major subdivisions of the program are now subtasks. Reduced work scope and revised funding allocations were recommended to the contracting officer in response to mid-year guidance. Following discussions with RDT and SAN, revised program levels were established and efforts initiated to prepare modified work scopes and cost estimates.

Tentative funding guidance for FY-74 was received and preliminary program planning initiated. The impact of the guidance on manpower, schedule, and scope was evaluated, and summary recommendations for the FY-74 program were prepared. E. E. Kintner, G. W. Cunningham and R. E. Kosiba of RDT met with BRD management in Sunnyvale, on March 30 to discuss FY-74 program plans. Task 11 plans were presented by K. M. Horst, P. R. Pluta, and C. N. Spalaris.

Scope guidance on Subtask 5 was received by TWX, dated March 15, 1973, which terminated the blanket assembly work and re-oriented the control assembly toward design of an advanced Eu_2O_3 system. Present B_4C control assembly work will be phased out and documented.

P. R. Pluta completed his assignment as chairman of the task force investigating the failure of the XX05 irradiation creep experiment in EBR-II. A final report was transmitted to HEDL, EBR-II, and RDT.

Program review meetings were held with RDT on Subtasks 3, 4, and 5 at HEDL on February 1, 1973 and at AEC headquarters on February 5, 6, 7, 8, and 9, and March 1 and 2, 1973.

The following topical reports were released.

GEAP-13851, *Loss of Flow Equipment in an Electrically Heated Sodium Cooled Test Facility*, H. C. Pfefferlen and R. C. Nelson (October 1972).

GEAP-13925, *Capsule B9D Irradiation and Post-Irradiation Examination*, P. E. Bohaboy, C. N. Craig, G. L. Stimmell, J. J. Regimbal, P. E. Novak (December 1972).

GEAP-13928, *Evaluation of Irradiated Fuel Rods with Pulsed Eddy Currents*, R. R. Asamoto, R. F. Bacon, A. E. Conti, G. P. Wozadlo (December 1972).

GEAP-13935, *Stoichiometry Effects on $\int kd\phi$ of $(\text{Pu}, \text{U})\text{O}_{2-x}$ in Pile*, D. A. Cantley, B. F. Rubin, and C. N. Craig (January 1973).

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2. FUEL ROD ENGINEERING

Subtask Leader — J. D. Stephen

The objective of the Fuel Rod Engineering Subtask is to develop the design basis for the LMFBR fuel rod. 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 the development of material properties correlations.

2.1 FUEL ROD DESIGN ENGINEERING—L. M. McWETHY

The objective of this activity 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 material properties correlations.

2.1.1 Fuel Performance Modeling (LIFE)

The near term objectives of the fuel performance modeling program are to evaluate the LIFE computer code as a design tool and to recommend and participate in 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.

2.1.1.1 Evaluation of LIFE

Parametric studies and comparison with experimental data for important fabrication and operating parameters and material properties form the basis for the recommendations on desirable revisions to LIFE that are currently being prepared.

Evaluation of the capability of LIFE to predict the cladding inelastic strain due to fuel-cladding mechanical interaction as a function of fabrication parameters, operating variables, material properties and code models is complete. The results of the sensitivity calculations and the comparison with the trends indicated by the experimental data are summarized in Table 2-1. The LIFE code, Revision 2 options, were used for these predictions. In the absence of a complete calibration to the actual rod power history, the fuel swelling rate was adjusted for this study to give near the measured inelastic strain of F9C-31 for a simplified power history. Reactor power shutdowns, restarts, operation at partial power, etc., were neglected. The calibration is thus not exact. However, it is felt that the relationships of cladding temperature and cladding stress due to steady fuel growth were established adequately. The more important observations are being spot-confirmed with Revision 3.

The apparent agreement between the predicted trends and the experimental data shown in Table 2-1 in several areas does not necessarily confirm the validity of the models. Investigation shows that in the LIFE code the two inner zones of the fuel (equiaxed and columnar grain zones) are in hydrostatic compression after closure of the fuel-cladding gap. In this stress state, the load on the cladding produced by fuel-cladding mechanical interaction cannot be relieved by inward creep of the fuel. It was found that the dependence of cladding inelastic strain on most of the variables (i.e., density, power level, fuel creep rate) is due mainly to their effect on the rate of gap closure, and not to their effect on the fuel-cladding system response after gap closure. The results of typical cases are shown in Figures 2-1 and 2-2. The exponential relationship of cladding inelastic strain to burnup is essentially the same in all cases after gap closure. In the case of fuel creep, this difficulty leads to the prediction by LIFE that the weaker the fuel the greater the fuel-cladding mechanical interaction, which is contrary to the current understanding of fuel rod behavior. These difficulties are believed to be due largely to the current version of LIFE limitations of the three radial ring simulation of the fuel.

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

Table 2-1
SUMMARY OF LIFE CODE PREDICTIONS AND
COMPARISON WITH EXPERIMENTAL DATA AT EBR CONDITIONS

LIFE Predictions	Comments and Comparison with Experimental Data
Fabrication Variables	
1. Cladding inelastic strain the same for solid and annular pellet fuel with 5-mil fuel-cladding gap and fuel smeared density constant.	1. Consistent with data, and consistent with predicted insensitivity of cladding inelastic strain to pellet density (below) in this range of fuel-cladding gap.
2. Cladding inelastic strain not sensitive to fuel pellet density for constant smeared density and 4- to 6-mil gap over the range of $80 < \rho_{\text{smeared}} < 90\%$ TD.	2. Consistent with data, from which no dependence of cladding inelastic strain on smeared density can be discerned for this range of pellet density and fuel cladding gap.
3. Cladding inelastic strain increases with increasing smeared density with 3-mil fuel-cladding gap.	3. No dependence on smeared density evident in the data.
4. Cladding inelastic strain increases with decreasing fuel-cladding gap with constant fuel smeared density.	4. Consistent with very preliminary indications from experiment. Data indicate a threshold of increased cladding inelastic strain near a 3-mil gap.
5. Cladding inelastic strain decreases sharply with 6.5-mil gap and 90% TD smeared fuel density.	5. Marked departure from predicted trend, and believed due to instability in code when as-fabricated pellet density approaches input density of equiaxed grain growth region.
Operating Variables	
6. Cladding inelastic strain increases with increasing cladding temperature from 1000 to 1400°F.	6. LIFE is predicting significant cladding restraint of fuel growth, diminishing as cladding strength decreases. Data show no dependence of cladding inelastic strain on cladding temperature up to 1200°F.
7. Cladding inelastic strain increases with increasing power level at same burnup.	7. Not consistent with hypothesis that fuel strength decreases and gas release increases as power level increases, with consequent decrease in cladding stress, if fuel swelling less sensitive, although definitive data not available.
8. Cladding inelastic strain not affected immediately by increase in power.	8. Current LIFE does not include cladding primary creep or plasticity models, and increased cladding stress due to power increase must be relieved by secondary creep.
Material Properties and Model Effects	
9. Cladding inelastic strain increases with decreasing fuel-cladding gap conductance.	9. Not consistent with hypothesis that fuel strength decreases and fission gas release increases as fuel temperature increases, with consequent decrease in cladding stress if fuel swelling less sensitive, although definitive data not available.
10. Cladding inelastic strain increases with increasing fuel creep rate.	10. Not consistent with expectation that cladding stress decreases with decreasing fuel strength.
11. Cladding inelastic strain increases with increasing fuel swelling rate.	11. Consistent with expectation.

Table 2-1 (Continued)

LIFE Predictions Material Properties and Model Effects (Continued)	Comments and Comparison with Experimental Data
12. Cladding inelastic strain increases with increased healing of cracks in fuel.	12. Consistent with expectation.
13. Cladding inelastic strain not sensitive to fuel-cladding friction coefficient. LIFE is predicting no relative axial displacement between fuel and cladding.	13. Consistent with expectation at fuel-clad interface; elsewhere due to limitation of plane strain model.
14. Fission-gas release approaches 100% with increasing burnup, with the rate dependent on rod power level.	14. Consistent with expectation.

Work is in progress to determine the extent of the improvements to be expected from increasing the number of radial nodes, and to determine what further modifications of the structural model are necessary. The SIMPLIFE code has been written to solve the structural analysis problem for up to 20 radial cladding and fuel nodes, with the temperature distribution, fuel swelling rate, and material properties as input. The Illushyn method of solution is used, as recommended in the last quarterly report. Preliminary results with 12 radial fuel nodes indicate that the multinode solution does predict inward creep of the fuel, and as a result the predicted cladding stress due to fuel-cladding mechanical interaction is markedly reduced.

It was reported in the 44th quarterly report of this program (GEAP-10028-44, November 1972) that different results were calculated with the earlier LIFE revisions on the GE and ANL computer systems for the same case. This problem was checked with Revision 3 by comparing the LIFE prediction by ANL of the performance of fuel rod GE-F9C-31 with the LIFE prediction by GE using identical input. As shown in Table 2-2, there is a significant difference in the predictions of cladding thermal creep strain. The extent and timing of cladding thermal creep is a key parameter in predicting fuel rod lifetime. The values of the cladding thermal creep strain for F9C-31 are small because the cladding temperature was low (<1000°F). In cases where the cladding temperature is high (1200-1400°F), relative differences in cladding thermal creep strain on the order of these in Table 2-2 would produce markedly different predictions of fuel rod failure probability between sites with different computer systems. This problem is being discussed with ANL. Two higher cladding temperature cases have been forwarded to ANL with complete input decks and printout for further comparison.

Table 2-2
COMPARISON OF LIFE-II, REVISION 3
PREDICTIONS OF GE-F9C-31 ON GE AND
ANL COMPUTER SYSTEMS

Time at Power (hours)	4745/4745	6072/6072	8631/8631
Cladding Equivalent Stress (ksi).....	22.8/26.5*	25.8/26.2	25.5/26.1
Cladding Irradiation Creep Strain (%).....	0.12/0.12	0.20/0.21	0.37/0.39
Cladding Thermal Creep Strain (μ in./in.).....	1.7/2.9	5.5/8.4	14/20

*GE/ANL

Figure 2-1. LIFE Code Prediction of Cladding Inelastic Strain as a Function of Fuel-Cladding Gap

Figure 2-2. Effect of Fuel Creep Rate on LIFE Code Prediction of Cladding Inelastic Strain

Review of the recent failures of encapsulated fuel rods in EBR (discovered after addition of the stainless-steel reflector) using the LIFE code highlighted the need for a cladding plasticity model. A power change similar to that experienced by the failed rods was analysed with LIFE using the design parameters of F9C-31. The power history for this calculation was 6 kW/ft to 8.9 at.% burnup with a step change at this point to 11 kW/ft. The power change did not produce an immediate increase in cladding inelastic strain. LIFE does not account for cladding primary creep or plastic flow under a short-term load. The differential fuel-cladding thermal expansion instead produced a step increase in cladding stress, which led to an increased cladding secondary creep strain.

The material property correlations that were chosen as the preliminary reference properties for the LIFE code at the Fuel Modeling meeting at RDT-HQ in October 1972 have been reviewed in detail. The results are summarized in Table 2-3. A report has been issued and will be transmitted to ANL.

2.1.1.2 LIFE Model Development

The LIFE thermal performance working committee, consisting of representatives of GE, HEDL, WARD and AI, has reached preliminary agreement on recommendations for revision of the thermal performance models. These recommendations are that: 1) the reference version of the code should contain a theoretical fuel restructuring model based on void migration and grain growth, and a gap conductance model based on the calculated gap size at power, as proposed by AI and WARD, respectively, 2) that the code also contain empirical options for restructuring based upon input grain growth temperatures, and gap conductance based upon as-fabricated gap and factors for power, burnup and cladding temperature, as proposed by HEDL and GE, respectively, 3) that the GE recommendation for fuel thermal expansion be accepted, and 4) that the WARD fuel thermal conductivity correlation be accepted, with the GE correlation accepted as an option.

Discussions between GE, ANL, and WARD indicate a readiness to proceed with a focused model development for revision of the LIFE mechanical analysis models. GE can contribute in the modeling areas of least cladding wastage, cladding plasticity, cladding lifetime (criteria), and cladding structural simulation that will be compatible with other design tools for analysis of local, nonaxisymmetric loads due to spacers, bowing, etc., and for analysis of upset performance.

The GE correlations for creep of solution annealed and 20% cold worked Type 316 and swelling of 20% cold worked Type 316 recommended in the last quarterly report are being encoded as options for the LIFE code.

2.1.2 Steady-State Thermal Performance

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

The principal objectives of the F20 experiment in EBR II are to reduce the uncertainties in the thermal performance characteristics of LMFBR fuel for a range of design and operating parameters, and to determine the effects of high power operation on the fuel and cladding mechanical performance.

Preirradiation design predictions of the thermal performance of the F20 experiment were completed. The design code 'PEFT,' Probabilistic Evaluation of Fuel Temperatures,⁽²⁻¹⁾ was updated for this analysis to include the recently completed correlations of thermal properties.⁽²⁻²⁾

The power-to-melt results from the PEFT code for the standard rod are shown in Figure 2-3. These results show that the expected power-to-melt is between 18 and 20 kW/ft for the F20 conditions. The proposed criteria for selecting the experiment power level are a 90% probability of melting fuel. This results in a desired peak power level for the standard rod of 20.6 kW/ft (cladding outside surface temperature of 1080°F). It is also desired that fuel at the ends of the rods not melt. With a ratio of axial minimum to peak power in EBR II of ~0.8, there is about a 10% probability of melting fuel at the rod ends. A power level of 20.6 kW/ft thus should provide a melt boundary without excessive risk of melting fuel at the ends of the rods.

The center temperature or fuel melt fraction results are shown in Figure 2-4. For the peak power of 20.6 kW/ft, the expected melt fraction is 17% (at 1000°F cladding outside surface temperature). The melt fraction is seen to be a fairly sensitive indicator of the linear power. The current uncertainties in thermal properties and power level result in a large uncertainty in melt fraction. This uncertainty will be improved significantly for a wide range of design and operating parameters by the results of the F20 experiment.

The 'LIFE-II' code, Revision 3, ⁽²⁻³⁾ was exercised on two rods in the F20 experiment array. The code was run to determine if a dynamic fuel restructuring model would show solidification of molten fuel with continued operation. Figure 2-5 shows the columnar, melt and void diameters for two rods, the F20-C11 rod with a 3-mil fuel-cladding gap and the F8F rod, preirradiated to 5% B.U. The melt diameter is plotted beginning at the time when full power is attained. The LIFE-II code predicts significantly different behavior for the two rods as a function of burnup. The melt boundary is virtually constant on the preirradiated rod but the once molten fuel has solidified on the zero burnup rod in less than 20 hours. This suggests that the power level on unirradiated rods should be raised to assure retaining molten fuel for any extended operation.

The 'LIFE-II' code cladding structural results for the F8F rod are shown in Figure 2-6. For a constant cladding temperature of 1260°F at the top of the core (TOC) and 1125°F at the rod midplane (MP). The thermal hoop strain due to fuel-cladding interaction is quite large. Comparison of these results with the expected cladding ductility shows that cladding failure is probable. However, it is noted that even at the lower cladding temperature of the rod midplane, 'LIFE-II' predicts significant cladding strain (0.2%) in only ten minutes at power. Preliminary indications from the preirradiated P19 rods indicate that no cladding strain was observed. This comparison suggests that the 'LIFE-II' code may be significantly overpredicting the cladding stress and strain for this type of irradiation. The F20 experiment, with extended operation at high power, could provide a controlled test of fuel-cladding interaction to determine if significant cladding strain develops due to thermal creep strain.

The 'PEFT' code was also utilized in predicting the power-to-melt for the P20 experiment national round robin analysis being coordinated by HEDL. These results were transmitted to HEDL to document this prediction. The power-to-melt result is shown in Figure 2-7. This shows a probability of melting fuel between 4 and 64% at the maximum P20 power level of ~18 kW/ft for the considered range of fuel-cladding gap sizes at zero burnup. Complete fuel restructuring is expected to raise the power-to-melt by about 1 kW/ft at one percent burnup.

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.

In the previous two quarters, the correlation of defected fuel swelling was discussed in terms of the diffusion of oxygen and sodium in a defected fuel rod. A sodium diffusion limited model, based on the EIJ-B2 experiment, was used to predict the performance of a similar but higher temperature test, B11B. The initial swelling of B11B closely matched the prediction but continued swelling was much slower than predicted. It was believed that crack propagation made sodium available over the entire fuel rod length in contrast to a limited crack length used in the prediction. Consideration of sodium and oxygen availability led to a prediction of the swelling to be expected under typical LMFBR fuel rod conditions.

In this reporting period, the defected fuel correlation was expanded to include a general consideration of oxygen availability and transport in a defected fuel rod. This analysis was correlated with the B11-A, B, C, and B9D experiment results. This work is currently being documented as a topical report.

A preliminary draft of the report *Fuel Failure Predictions and Consequences* was delivered to RDT for initial comment. This report includes a first estimate of the fuel failure frequency to be expected in an LMFBR. In addition, this report includes discussions of possible failed fuel swelling, fuel failure propagation, fuel and fission product loss and system contamination. Additional analysis is being conducted in response to comments received on this draft.

Due to RDT guidance, the fuel rod design effort on the B12, A, B, C experiment has been postponed.

Table 2-3
REVIEW OF REFERENCE MATERIAL PROPERTIES IN LIFE CODE

Material Property	Acceptable	Not Acceptable		
		New Correlation Recommended	New Correlation In Progress	New Development Required
Fuel-Cladding Heat Transfer Coefficient		X		
Sodium-Cladding Heat Transfer Coefficient	X			
Cladding Thermal Conductivity	X			
Cladding Thermal Expansion	X			
Fuel Thermal Conductivity		X		
Fuel Restructuring			X	
Fuel Thermal Expansion	X			
Plutonium Redistribution			X	
Fission-Gas Release	X			
Gas Atoms Produced Per Fission	X			
Fuel Young's Modulus	X			
Fuel Poisson's Ratio	X			
Cladding Shear Modulus	X			
Cladding Poisson's Ratio	X			
Solid Fission-Product Volume per Fission	X			
Gaseous Fission- Product Swelling				X
Incompressible Gas Factor		Evaluation Not Possible		
Fuel Hot Pressing				X

Table 2-3 (Continued)

Material Property	Acceptable	Not Acceptable		
		New Correlation Recommended	New Correlation In Progress	New Development Required
Cladding Swelling				
Solution-Annealed Type-304 Stainless-Steel		X		
Solution-Annealed Type-316 Stainless-Steel		X		
20% Cold-Worked Type-316 Stainless-Steel		X		
Cladding Density	X			
Cladding Thermal Creep				
Solution-Annealed Type-304 Stainless Steel	X			
Solution-Annealed Type-316 Stainless Steel	X			
20% Cold-Worked Type-316 Stainless Steel	X			
Cladding Irradiation Creep				
Solution-Annealed Type-304 Stainless Steel	X			
Solution-Annealed Type-316 Stainless Steel		X		
20% Cold-Worked Type-316 Stainless Steel		X		
Fuel Creep	X			
Fuel Fracture Strength	X			
Fuel Crack Healing Temperature	X			
Fuel Brittle-to-Ductile Transition Temperature	X			
Fuel-Cladding Friction Coefficient				X
Cladding Time-to-Rupture			X

Figure 2-3. Probabilistic Power-to-Melt for F20 Standard Rod

Figure 2-4. Probabilistic Fuel Center Temperature or Melt Fraction for F20 Standard Rod

Figure 2-5. Fuel Structure from 'LIFE-II'

Figure 2-6. Cladding Loading from 'LIFE-II'

Figure 2-7. Power-to-Melt for P20 Fuel Rods

Design analyses in support of the B9D experiment evaluation were completed. The B9D experiment involved the intentional defecting of two fuel rods to determine the degree of fuel-sodium reaction product formation.

The formation rate of fuel-sodium reaction product is believed to be a function of the fuel temperature. Both diffusion rates of sodium through the fuel and/or fuel sodium reaction product and the rate of formation itself may be affected by fuel temperature. A thermal analysis was completed using the THTD code⁽²⁻⁴⁾ to characterize the fuel temperature both prior to rod defecting and at the time of rod defecting.

The fuel surface suffers a rapid temperature decrease when sodium bonding occurs. A surface temperature decrease of from ~350 to 470°F was calculated. These results indicate that the fuel surface temperatures may stabilize in the range of 1200 to 1300°F, from the bottom to the top of the assembly.

The fuel temperatures occurring after appreciable fuel-sodium reaction product has formed will be estimated using thermal conductivity data for the reaction product.

The formation of this thin layer of fuel-sodium reaction product between the fuel and cladding of the B9D fuel rods resulted in sufficient swelling to cause measurable cladding strain, believed to be a result of thermal creep.

The key unknown in analyzing the cladding structural response to fuel-sodium reaction product formation is the strain rate. There were no direct measurements of the strain rate of the B9D experiment. Other defected fuel experiments have provided such a measurement, notably E1J-B2 and B11A, B, and C. These experiments involved a localized reaction at a defect in contrast to this experiment in which there was a uniform reaction over all the fuel pellet surfaces. Nevertheless, the rate of formation may be comparable. The E1J-B2 and B11 experiments showed the major portion of the swelling to occur within the first day after defecting. On this basis, a lower limit on the B9D swelling time might be on the order of ten hours. The upper time limit is the total duration of the irradiation after defecting. The possible cladding stress and fuel pressure resulting from these assumptions are:

B9D Structural Analysis

	B9D-1	B9D-2
Average cladding strain %	0.2	0.2
Cladding temperature, °F	1165	1125
Maximum swelling time, hours	500	313
— Cladding strain rate, 1/h	4×10^{-6}	6.7×10^{-6}
— Cladding stress, ⁽²⁻⁵⁾ psi	22,000	28,000
— Effective fuel-cladding interface pressure, psi	2,800	3,600
Minimum swelling time, hours	10	10
— Cladding strain rate, 1/h	2×10^{-4}	2×10^{-4}
— Cladding stress, ⁽²⁻⁵⁾ psi	35,000	39,000
— Effective fuel-cladding interface pressure, psi	4,500	5,000

The thermal creep rate is sufficient to maintain the cladding stress well below the cladding yield strength (>60 ksi) even if the cladding strain occurs in about 10 hours. The stress dependence of the thermal creep rate results in less than a factor of two increase in stress for a factor of 50 increase in the creep rate. It would appear from these results that thermal creep is the mode of cladding deformation in the B9D experiment.

2.1.4 Cladding Loading Sensitivity Study

The objective of this work is to evaluate the relative contributions of all cladding loading mechanisms to failure. The loading mechanisms being analyzed are the internal pressure due to fission gas, fuel-cladding mechanical interaction, cladding thinning due to wastage, contact loads from the spacer system and the effect of prototypic cladding temperature distributions. The work reported here was previously reported under Subtask 3 (Section 3.4.2 of the 44th and 45th Quarterly Reports).

Work conducted in previous quarters has included the preparation of a masterplan for the study of clad loading sensitivity, the collection of data on the preparation of materials, temperatures, fluxes and other rod-related parameters to be used in conducting the sensitivity study, the development and modification of the GOGO and the CREEP-PLAST computer codes, the development of finite element models for the rod to be used in the contact load study and some preliminary analysis to scope the importance of loading history.

2.1.4.1 Combined Effect of Fission Gas Pressure, Cladding Wastage and Radial, Circumferential and Hot Spot Temperature Variations

The purpose of this work is to establish the correct procedures for modeling cladding wastage and the effect of prototypic temperature distributions on the cladding behavior for steady and nonsteady power operation.

During this quarter, analyses were conducted using the GOGO computer code on typical LMFBR hot, peak power, average power and minimum power rods at both the top of the core and the core midplane axial locations. Both steady and variable power cases were run. Also the effect on the results of two different models of the cladding wastage mechanism was investigated.

The results show that while differential swelling due to temperature differences in the cladding may cause failure in localized regions of the cladding, it is not expected to cause a through-the-wall crack in the cladding. For the loading cases considered, through-the-wall cracks are expected to occur only for the case of the hot rod at the top of the core. In such cases the principal cause of damage is the internal pressure and the cladding failure may be reasonably, if somewhat nonconservatively, predicted by considering only the internal pressure and assuming the cladding to be an isothermal thin wall tube with a temperature equal to the maximum midwall temperature in the cladding.

During variable power and flow operation, it has been found that the presence of the temperature gradients in the higher temperature rods ($>1200^{\circ}\text{F}$) gives rise to significant low-cycle fatigue damage to the cladding. This effect was first reported in the 45th Quarterly Report. The extent of the fatigue damage is dependent on the average cladding temperature, the magnitude of the thermal stresses which result from the power and flow change, the time spent at each power level and the number of power and flow cycles experienced.

The inclusion of cladding wastage in the analysis is straightforward. The only complex aspect of cladding wastage effects is the way in which strain history in the cladding is handled. The simplest approach is to consider that the strains are "node dependent." In this case, as the cladding wall thins and the nodes move to different locations, the strains move with them. This is obviously a nonrigorous approach which in most cases is conservative. The alternative approach is more nearly rigorous. In this case the strains at each node point are recalculated each time the node points move, based on the strain distribution in the cladding, i.e., the strains are "geometry dependent." Analysis has been run using both approaches and it has been found that for typical cladding problems the difference between the two approaches is not great. The "node dependent" solution as expected predicts larger peak strains and damage values whereas the "geometry dependent" solution requires additional calculational time. It has been concluded that in general the "node dependent" approach is acceptable and should be used for most design calculations.

2.1.4.2 Spacer Contact Loading

The differential swelling which occurs between the fuel rod bundle and its surrounding channel can cause significant cladding contact loads. Analyses have been carried out using a three-dimensional MARC-CDC finite element model. The conclusions to date are reported below.

At the lower temperatures ($<1050^{\circ}\text{F}$), at which the peak loads occur, it has been found that local indentations of the order of 1-2 mils will occur due to contact loads which increase to 14 pounds over a 6000-hour period. Although such a local indentation is small, the sum of the indentations across a 217-rod bundle is significant and would be expected to reduce the peak contact load calculated based on room temperature bundle compression tests by about 25%.

It is expected that the limiting condition for contact loads at temperatures less than 1050°F will be local yielding due to high local stresses. The presence of typical fission product intergranular cracks at the inside surface of the cladding while a contact load is acting is not expected to initiate unstable crack propagation even for fluences as high as 2×10^{23} nvt (>0.1 MeV) in stainless steels.

2.1.4.3 Fuel-to-Cladding Mechanical Interaction

The GOGO code has been modified so that it will handle either a radial displacement or a pressure as the boundary condition at the inside of the cladding. The code will now be used to analyze the effect of circumferential variations in the cladding temperature on the cladding strains which accumulate due to fuel swelling. This work is being closely associated with the LIFE work.

2.2 FUEL ROD IRRADIATION—R. F. HILBERT

The objective of this work is to determine experimentally fuel rod behavior in three general design areas: High Burnup Irradiations, High Cladding Temperature Irradiation, and Operating Limits Irradiations. This includes development of correlations for use in fuel rod design based on data obtained from this program, other U.S. programs, and foreign programs. The status of the fuel rod irradiations in progress under this activity is summarized in Table 2-4.

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 irradiated in EBR II at a linear power of 5 to 8 kW/ft to study the performance of high density fuel at low power levels.

Rod FOE was destructively examined at 4 at.% burnup. Rods FOA and FOC completed irradiation to 9 at.% burnup at the end of EBR II Run 55. The rods were found to be intact and the post-irradiation examination was recently completed at LASL. The results will be reported after the data have been received from LASL.

Rods FOB and FOD were irradiated in an outer row of the core after installation of the stainless steel reflector during Run 56. A comparison of Runs 55 and 56 power and cladding temperature profiles for Rod FOB, Figures 2-8 and 2-9, indicate that the rod experienced a considerable change in operating conditions at 9.9 at.% burnup. Power increases ranged as high as 110% and cladding temperatures approached 1300°F. The change in operating conditions of Rod FOD was similar. Neutron radiographs, Figure 2-10, indicate that after this severe change of conditions both rods failed at many positions along the rod length. A summary of fabrication and irradiation parameters for Rods FOB, FOD, and other recently failed rods is given in Table 2-5. (The other rods are discussed in later sections of this report.)

The rods have been shipped to LASL for post-irradiation examination.

2.2.1.2 F8A Experiment (Effect of Fuel Density on Fuel Swelling Rate)

The objective of the F8A experiment is to evaluate fuel swelling at medium power as a function of fuel density and cladding alloy. 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 remaining nine rods were discharged from EBR II after Run 58 at a peak burnup of 12.2 at.%.

Neutron radiography and gamma scanning which were completed in April indicate that three of the nine rods have failed. Fabrication and irradiation parameters for these rods which experienced a significant change in axial fission rate profile when the stainless steel reflector was added to the EBR II core, are given in Table 2-5. Figures 2-11 and 2-12 show the power and cladding temperatures profiles for Rod F8N during Runs 55 and 56. There were significant increases in linear power (~50%) and peak cladding temperature (~100°F). The power and cladding temperature changes in the companion F8A rods were similar.

Neutron radiographs shown in Figure 2-13 show that rod F8P failed near the top of the rod, Rod F8N failed near the middle of the rod, and Rod F8L failed near the bottom of the rod. Thus, as in the case of rods FOB and FOD, it was not possible to relate the region of failure to a specific region of power or cladding temperature.

Table 2-4
SUMMARY OF SUBTASK 2 FAST FLUX TESTING PROGRAM IN EBR-II (a)

Group	Objectives	Total Number of Rods in Group	Date In	Projected Date Out	Actual Date Out	Number of Rods in S/A	S/A	Position	Range of Average Rod Powers (b) kW/ft	Target Burnup at. % MWd/Te	Average Calculated Burnup (c) at. % MWd/Te	Peak Calculated Fluence (d) (1E22 n/cm ²) Total E>0.1 MeV	Status	Group
F0	Low power swelling 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		4/7/72	2	XG03A	8D2	5.49- 6.12	8.95 80000	8.77 79354	7.71 5.58	Post exam in progress	
			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		8/3/72	2	XG04B	9C7	9.03- 9.40	11.18 100000	9.35 89128	8.76 6.38	Irradiation complete	
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 irradiation, 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	X050	4C2	10.73-12.86	11.19 100000	11.75 106358	10.34 8.87	Post exam complete	
F3A	Bare rod irradiation to evaluate fuel cladding and cladding-coolant interaction	16	8/15/68		6/30/69	16	X040	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	X040A	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 rods	18	11/21/67		5/18/70	18	X027	4B3	11.48-12.80	13.49 120000	8.86 79922	7.54 6.47	Six rods destructively examined	F3B
			3/10/72		1/8/73	8	X151	7A4	10.10-11.65	13.49 120000	11.62 104758	10.00 8.40	Interim exam	
F4	Low density fuel behavior at high heat ratings	9	5/ 9/66		6/28/67	9	X011	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	X039	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	X070	3E1	12.72-13.37	9.04 80000	8.25 74217	7.15 5.94	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 rods destructively examined	F5
			3/10/72		1/8/73	11	X151	7A4	11.44-12.33	11.24 100000	8.56 77261	7.15 5.92	Interim exam	
F6	Fuel-cladding interactions under accelerated swelling	4	3/24/66		4/24/69	4	X010	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		10/14/72	9	X081	7E6	7.77- 9.76	11.24 100000	11.22 101189	9.73 7.54	Irradiation complete	
F8B	Effect of void deployment on fuel cladding reaction	19	7/25/68		8/24/70	19	X036	7E1	4.99- 6.39	11.24 100000	4.73 42600	4.01 3.03	Reconstituted into X117 & X118	F8B
			2/24/71		4/7/72	9	X117	8B2	4.31- 5.49	11.25 100000	5.79 52161	5.01 3.65	Reconstituted into X117A	
			9/14/72	5/ 1/74		10	X118	8D7	4.97- 6.16	11.24 100000	6.72 60612	5.81 4.32	Interim exam	
			9/14/72		10/14/72	9	X117A	9C8	8.23-10.03	11.25 100000	6.21 55901	5.35 3.83	Interim Exam	

(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 rod 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 192 MeV/fission and calculated from data supplied by EBR-II.

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

Table 2-4 (Continued)
SUMMARY OF SUBTASK 2 FAST FLUX TESTING PROGRAM IN EBR-II

Group	Objectives	Total Number of Rods in Group	Date In	Projected Date Out	Actual Date Out	Number of Rods in S/A	S/A	Position	Range of Average Rod Powers ^(b) kW/ft	Target Burnup		Average Calculated Burnup ^(c)		Peak Calculated Fluence ^(d) (1E22 n/cm ²)		Status	Group
										at. %	MWd/Te	at. %	MWd/Te	Total	E>0.1 MeV		
F9A	High power, high burnup unencapsulated fuel rod 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 Interim exam	F9A
			10/27/70		2/19/73	37	X043A	7D4	10.93-11.98	11.25	100000	9.76	87974	8.31	7.02		
F9B	Medium power, high burnup unencapsulated fuel rod irradiations	37	5/23/69		8/3/72	37	X862	7F2	10.04-11.07	11.24	100000	8.79	79217	7.49	6.22	Interim exam	F9B
F9C	High power, high burnup unencapsulated fuel rod irradiations Run to failure	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		10/14/72	19	X143	6A1	11.43-12.40			8.59	77389	7.25	6.11	Reconstituted into X143A	
			2/ 2/72	4/11/73		19	X144	6B4	11.64-13.08			10.58	95373	7.58		In Reactor	
F9D	Medium power, high burnup, unencapsulated fuel rod irradiations	37	4/24/69		9/7/72	37	X143A	6B1	11.21-12.69			9.09	81911	7.68	6.46	In Reactor	F9D
						37	X058	7C1	9.61-11.29	11.24	100000	8.43	76014	7.24	5.72	Interim exam	
F10A	Cladding performance at peak cladding temperatures	19	2/ 2/72	7/12/74		19	X121	6D3	12.62-15.06	11.01	100000	4.71	43369	3.81	3.23	In Reactor	F10A
F10B	Cladding performance at peak cladding temperatures	19	12/ 5/71		2/19/73	19	X122	6D4	12.78-14.75	11.01	100000	5.35	48186	4.30	3.64	Interim exam	F10B
F11A	Cladding performance at peak cladding temperatures	37	12/13/72	4/23/75		37	X141	6B4	13.04-14.91	11.25	100000	1.27	11442	0.97	0.82	In Reactor	F11A
F20	Irradiation to probe operating limits	19														Fabrication complete	F20

Table 2-5
SUMMARY OF FAILED FUEL ROD PARAMETERS

Rod	Cladding Material	Fuel* Type	% PuO ₂	Fuel† Density (%)	Smeared Density (%)	Fuel-Cladding† Diametral Gap (mils)	O/M†	EOL Av Burnup (at.%)	EOL Av Power‡ (kW/ft)
F0B	347SA	SP	20	93.3-96.0 94.2	92.6	1.4-2.4	1.995-2.002 1.998	9.9	9.9
F0D	347SA	SP	20	93.5-98.1 95.7	92.7	1.1-5.9	1.996-2.000 1.999	9.1	10.3
E1H-25	316SA	AP	25	90.9-96.9 93.7	86.4	2.5-8.3	1.962-1.98 1.975	11.6	11.0
E1H-28	304 13%CW	SP	25	93.5-95.6 94.9	91.7	2.9-4.4	1.972-1.972 1.973	11.2	11.6
E1H-32	304 20%CW	SP	25	93.4-96.5 95.2	92.1	2.2-4.5	1.963-1.977 1.971	11.2	11.3
E1H-34	304SA	SP	25	94.3-96.3 95.4	92.6	2.6-4.0	1.964-1.979 1.971	10.9	11.7
F5E	I-800	VC	25	84.3 84.3 84.3	84.3	—	1.960-1.961 1.961	8.4	11.6
F8N	316SA	SP	20	88.5-92.0 90.1	86.9	2.7-5.2	2.00-2.00 2.00	10.3	8.6
F8P	316SA	VC	25	84.0 84.0 84.0	84.0	—	1.974-1.974 1.974	10.5	7.8
F8L	316SA	SP	25	87.7-90.7	86.3	2.7-4.8	1.973-1.977 1.975	10.7	8.4

*SP = Solid Pellet

AP = Annular Pellet

VC = Vibration Compacted

†Densities and O/M Values are Maximum, Minimum and Average.

Gaps Values are Maximum and Minimum.

‡Calculated on the Basis of EBR-II Guide Fission Rates

Figure 2-8. FOB Axial Power Profile for EBR-II Runs 55 and 56

Figure 2-9. FOB Cladding Axial Temperature Profile for EBR-II Runs 55 and 56

Figure 2-10. Neutron Radiographs of Rod FOB and FOD After Operation During Run 56

Figure 2-11. F8N Axial Power Profile for EBR-II Runs 55 and 56

Figure 2-12. F8N Cladding Axial Temperature Profile for EBR-II Runs 55 and 56

Figure 2-13. Neutron Radiographs of Failed Group F8A Rods

2.2.1.3 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 19 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 configuration included solid pellets, annular pellets, dished pellets, and vipac fuel. The rods have operated at linear power in the range from 5 to 10 kW/ft to ~7 at.% burnup.

Rod F8B-5 was damaged during reconstitution of the subassembly after interim examination at EBR II. The capsule remained intact, but could not be reinserted into a subassembly. The rod was recently shipped to LASL for destructive examination.

The F8B experiment now consists of two subassemblies (X117 and X118), each containing nine rods. Preliminary calculations obtained from the EBR II Project before the stainless steel reflector was added to the core indicated 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. Because of the possibility of rod failure after such a power increase, only one subassembly (X117) was irradiated during one EBR II run with the stainless steel reflector (Run 58). Based on results of interim examination of subassembly X117, which indicated no failures after operation in the reflected core, and detailed axial fission rate calculations from the EBR II Project, a plan for continuation of the F8B experiment was formulated. Three rods from each subassembly will be destructively examined. The remaining 13 rods are to continue to goal burnup with emphasis on evaluating fuel rod performance at low linear power. Because of the inadvertent power increase, a secondary objective of the test is to determine the effect of power changes on fuel rod performance. The subassemblies are scheduled to continue irradiation in Run 63.

2.2.1.4 F9A Experiment (Evaluation of Unencapsulated Fuel Rods at High Power)

The objective of the F9A experiment is to evaluate the performance of fuel rods at high linear power (11 to 14 kW/ft). The test consists of 37-rods with solid pellet, annular pellet, and powder fuel, clad in solution annealed Types-304, -316, and -321 stainless steel.

The subassembly was removed from the core and placed in the reactor basket after Run 57 (September 1972). Then, the subassembly was irradiated in Run 61 and discharged for interim examination after Run 61. Interim examination requirements were forwarded to the EBR II Project and the examination is scheduled for early May 1973.

2.2.1.5 F9B Experiment (Evaluation of Unencapsulated Fuel Rods at Medium Power)

The objective of the F9B experiment is to evaluate the performance of unencapsulated fuel rods operating at medium power (8 to 11 kW/ft). The test consists of 37 rods with solid pellet, annular pellet, and powder fuel, clad in solution annealed Types-304, -316, and -321 stainless steel.

Interim examination of the F9B experiment (subassembly XO62) at a peak rod burnup of 9.6 at.% (26,285 MWd exposure) showed the rods to be in good condition. The experiment is scheduled to continue irradiation in Run 63 (mid May, 1973).

Significant observations made during the interim examination included: (1) indications of the onset of interaction between Rod 9B-266A and the wire on an adjacent rod (Figure 2-14), and (2) loose and irregularly-pitched spacer wires on eight rods (Figure 2-15).

2.2.1.6 F9D Experiment (Evaluation of Unencapsulated Fuel Rods at Medium Power)

The objectives of the F9D experiment are to evaluate the performance of unencapsulated fuel rods at medium power (8 to 11 kW/ft). This test consists of 37 rods, with solid pellet fuel (coprecipitated and physically mixed) and powder fuel, cladding in solution annealed Type-316 stainless steel. Since all 37 rods are clad in the same alloy and channel dimensions and bundle-to-channel clearances were characterized before irradiation, examination of the F9D experiment will focus on the evaluation of rod-to-rod and bundle-to-channel mechanical interaction.

8F P10

~ 2X

Figure 2-14. Onset of Interaction Between Rod F9B-266A and the Wire on an Adjacent Rod

8F P7

~ 2X

Figure 2-15. Loose Spacer Wire With Indication of Some Spacer Wire Movement on Rod F9B 201S

The irradiation was temporarily discontinued in September 1972. The peak rod in this subassembly is at a burnup of 9.3 at.%. The subassembly is scheduled for interim examination in early May 1973.

2.2.2 High Cladding Temperature Irradiations

2.2.2.1 F5 Experiment (Fuel Rod Performance at High Cladding Temperatures)

The objective of the F5 experiment is to evaluate fuel rod performance at high cladding temperatures. The experiment consists of 19 encapsulated fuel rods clad in solution annealed Types-304, -316, and -321 stainless steel, Incoloy 800, and cold-worked Type-316 stainless steel.

Of the 11 rods that operated in the reflected core (post Run 56), one rod was found to have failed during a recent interim examination. Power and cladding temperature profiles for the failed rod, F5E, during Runs 55 and 56 are shown in Figures 2-16 and 2-17. The rod experienced $\sim 50^\circ\text{F}$ increase in peak cladding temperature. This rod did not exhibit failure to an extent observable on neutron radiographs. Detection of Cs-137 within the capsule above the fuel rod on gamma scanning indicated that it had failed. Fabrication parameters for Rod F5E are given in Table 2-5.

2.2.2.2 F3B Experiment (Fuel Rod Behavior at High Cladding Temperatures)

The major objective of the F3B test is to evaluate fuel rod performance at high cladding temperatures. The experiment consists of 18 encapsulated fuel rods clad in several alloys focused on the effects of void deployment during long term operation at high temperatures.

During interim examination at ~ 12 at.% burnup, 4 of 8 rods that operated in the EBR II core since insertion of the reflector were found to have failed.

Figure 2-16. F5E Axial Power Profile for EBR-II Runs 55 and 56

Figure 2-17. F5E Cladding Axial Temperature Profile for EBR-II Runs 55 and 56

Fabrication parameters for the four failed F3B rods are given in Table 2-5. A comparison of Runs 55 and 56 power and cladding temperature profiles for rod E1H-32, which is typical of the eight F3B(E1H) rods, is shown in Figures 2-18 and 2-19. The ends of the rods experienced $\sim 20\%$ power increase, while the peak cladding temperature increase was $\sim 50^\circ\text{F}$. Neutron radiographs of the failed F3B rods, Figure 2-20, show failed regions near the top of rods E1H-32 and E1H-34. Failure of rods E1H-25 and E1H-28 was detected only by gamma scanning upper capsule regions for Cs-137, not by neutron radiography.

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 $>16\text{ kW/ft}$ 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 preirradiated fuel.

The F20 rods have been encapsulated and inspected. Encapsulation of the F20 rods was completed and final inspection completed. The design basis report and the experiment safety evaluation are being prepared.

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, 38 rods are being irradiated in two Mark E subassemblies as part of the

Figure 2-18. E1H32 Axial Power Profile for EBR-II Runs 55 and 56

Figure 2-19. E1H32 Cladding Axial Temperature Profile for EBR-II Runs 55 and 56

Figure 2-20. Neutron Radiographs of Failed Rods from the F3B Experiment (Series E1H)

EBR II run-to-failure program. Prior to reconstitution (more than a year ago), the fuel rods operated at 14 kW/ft with 1075°F peak cladding temperature to 7 at.% burnup. At that time fuel rods F9C-31 and F9C-13 were removed for destructive examination. Destructive examination at the GE Vallecitos hot cells indicated that Rod F9C-31 was in excellent condition.⁽²⁻⁶⁾ Rod F9C-13, also in excellent condition, was shipped to LASL for destructive examination. Results of the examination of Rod F9C-13, including gamma scanning, betatron radiography, dimensional measurements, metallography, and electron microprobe analysis were reported previously.^(2-7, 2-8)

Interim examination of subassembly X143 at an average rod burnup of approximately 9 at.% indicated that all the rods were in good condition. The irradiation continued in Run 61.

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 desing. Correlations are to be 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 used in LIFE II in terms of available experimental data.

Design correlations activity in the areas of fuel rod thermal performance and fuel-cladding chemical interaction continued. A draft report on the results of analyses on the experimentally observed cladding attack was completed and submitted for review. Correlations for mixed-oxide thermal conductivity, restructuring, fuel thermal expansion, and fuel-cladding gap conductance were also completed and submitted to the LIFE thermal performance subcommittee for evaluation. Evaluation of the significance of the recent fuel rod failures (discovered after the installation of the stainless-steel reflector in EBR II) is in progress.

2.3 MATERIAL PROPERTIES—H. J. BUSBOOM

The material properties program has three major objectives:

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

2.3.1 Design Applications

2.3.1.1 Irradiation Creep of SA-304 Stainless Steel from Residual Stress Measurements

The presence of gradients in neutron flux produces swelling gradients in LMFBR core components which can be expected to lead to distortion. In the case of nonfuel core components the principal means of stress relaxation is irradiation induced creep. Pennell proposed⁽²⁻⁹⁾ that the measurement of the residual stress in capsule tubes of experimental fuel rods irradiated in EBR II could be used to establish the interaction between irradiation creep and swelling in-reactor. These tubes were irradiated with a substantial temperature gradient through their wall thickness in the radial direction. The method consists of mounting tubular sections of irradiated capsule tubing in a rigid medium. The tube section is then slit longitudinally and the slit width measured with the section restrained in the mounting material. The slit width is then remeasured after removal of the restraint. The degree of slit opening or closure then indicates the magnitude and the sign of the residual stresses within the tube. If the differential stresses were relieved in-pile, the slit should close, and vice versa, as shown in Figure 2-21. Analysis of data from twenty-three of these tests is complete. The specimens analyzed were solution-annealed Type-304 stainless-steel sections 0.375 inch outside diameter by 0.020 inch wall thickness (Table 2-6).

Table 2-6
DATA FROM RESIDUAL STRESS DETERMINATION SAMPLES

S/A No.	Capsule No.	Sample No.	Axial Position	Outside Temperature (°C)	Inside Temperature (°C)	Total Fluence n/cm ² X 10 ²²	Fluence >0.1 MeV n/cm ² X 10 ²²	Density (g/cc)	Density Change (%)	(Feeler Gage) Mounted Silt Width (in.)
XO11	SOV-3	45C-B	1-1/2	385	429	1.9	1.6	7.873	-0.15	0.015
		45C-C	2	389	433	2.0	1.7	7.855	-0.38	0.017
		45C-E	3-1/4	397	440	2.2	1.9	7.849	-0.46	0.017
		45C-F	3-3/4	403	446	2.2	1.9	7.844	-0.52	0.0165
		45C-H	6	416	460	2.4	2.1	7.827	-0.74	0.015
XG05	SMV-2	49A-B	1/4	385	430	3.1	2.7	7.864	-0.27	0.016
		49A-C	3/4	389	434	3.2	2.8	7.846	-0.49	0.015
XO11	SOV-1	45G-B	3	399	449	3.1	2.7	7.822	-0.80	0.016
		45G-C	3-1/2	403	453	3.1	2.7	7.819	-0.84	0.014
		45G-E	5-1/2	418	471	3.4	2.9	7.822	-0.80	0.015
		45G-F	6	422	475	3.4	2.9	7.888	-0.98	0.016
XO12A	C-1	66C-D	1/2	381	411	5.9	5.1	7.825	-0.76	0.014
	C-15	66B-D	-1-1/4	—	—	4.5	3.9	7.862	-0.29	0.014
XO50	W8X	67B-B	5	403	471	4.2	3.6	7.801	-1.06	0.015
		67B-D	6	415	483	4.3	3.7	7.774	-1.41	0.014
XG05	SMV-2	49A-E	4	419	467	4.2	3.6	7.779	-1.34	0.016
		49A-F	4-1/2	424	472	4.3	3.7	7.777	+1.37	0.014
XO80	C-10	78A-B	-1-1/2	—	—	5.0	4.3	7.880	-0.06	0.016
	C-19	78B-B	-1-1/4	—	—	5.2	4.5	7.895	+0.13	0.016
XO12A	-1	66C-F	1-1/2	387	419	6.4	5.5	7.671	-2.71	0.014
	C-15	66B-F	2	389	417	6.3	5.4	7.772	-1.43	0.015
	C 1	66C H	1 1/4	406	430	7.6	6.1	7.637	3.14	0.010
	C-15	66B-H	5-1/4	407	437	7.3	6.3	7.532	-3.21	0.015
136272	Archive	1	—	—	—	—	—	7.835	—	0.016
		2	—	—	—	—	—	7.885	—	0.016

Figure 2-21. Possible Stress Histories as a Function of Fluence. σ_t is the Stress on the Outer Radius of the Tube as a Consequence of Differential Thermal Expansion on Start-up

The analysis was conducted using the computer code GOGO.⁽²⁻¹¹⁾ The code analyzes either one- or two-dimensional, generalized plane strain, nonlinear, time dependent, structural problems. The code predicts the inpile stress history in the tube and calculates the residual stress distribution for out-of-pile conditions. It integrates numerically the residual stress distribution to obtain the bending moment and hence the slit opening or closure.

The current multiplicity of correlation expressions relating swelling to fluence and temperature leads to some question of the reliability of residual stress values based on them. To explore this question, swelling, residual stress, and change in slit width were predicted for the same values of fluence and temperature by using several different swelling and creep equations. The resulting great variation in calculated slit width is apparent in Table 2-8.

The following cases were examined using the swelling and creep correlations listed in Table 2-7. The results are tabulated in Table 2-8.

- Case 1. Change in slit width calculated using the HEDL swelling equation and creep equation 1.
- Case 2. Change in slit width calculated using the ARD swelling equation for Type 304 and creep equation 2.
- Case 3. Change in slit width calculated using the ARD swelling equation increased by a factor of two and creep equation 2.
- Case 4. Change in slit width calculated using the ARD swelling equation and creep equation 2 but assuming that the average temperature was 50°F higher than calculated.

Table 2-7
THE 304 SWELLING AND CREEP CORRELATIONS
USED IN THE ANALYSIS OF SLIT-TUBE DATA

A. SWELLING

(1) WARD Swelling Equation SA 304L

$$\% \Delta V/V = A (\phi t)^n$$

with ϕt in units of 10^{22} n/cm² E > 0.1 MeV

For T ≤ 429°C n = 1.6

For T = 429 → 454°C, n = 0.221T - 7.831

For T ≥ 454°C n = 2.25

For T ≤ 420°C, A = 10⁻² exp [10.868 ln (T/100) - 12.726]

For T = 420 → 450°C A = 10⁻² exp [2.902 - [(T/100) - 4.26]²/0.135]

For T > 450°C A = 10⁻² exp [-3.479 ln (T/100) + 7.625]

(2) HEDL Swelling Equation SA 304 (Ref. 2-12)

$$\% \Delta V/V = A (\phi t)^m + B$$

where A = 2.65 X 10⁻⁸ (α³) - 1.54 X 10⁻⁵ (α²) + 2.24 X 10⁻³ (α)

$$\alpha = T - 348$$

$$m = 0.872 + 2.98 \times 10^{-3} T$$

$$B = 0.2 [1 - \exp(-1.12\phi t)] / [1 \exp 0.1 (T - 480)]$$

ϕt = Fast fluence E > 0.1 MeV n/cm²/10²², applicable for $\phi t < 10^{23}$ n/cm².

T = Temperature -C, applicable range 360-640°C

(3) GE Swelling Equation SA 304 (Ref 2-10) (Based Principally on SA 304L Data)

$$\% \Delta V/V = (\phi t)^{1.57} \exp (-35.01 + 2.968 \times 10^4/T - 6.567 \times 10^6/T^2)$$

ϕt is neutron fluence for E > 0.1 MeV X 10²² n/cm²

T is the irradiation temperature °C

The data used to develop this equation covered the temperature range 370°C to 700°C (700°F to 1290°F) and fluences up to 7.5 X 10²² n/cm², E > 0.1 MeV.

B. IRRADIATION CREEP

Creep Equation 1 (Ref. 2-13)

$$\epsilon_h = 2.65 \times 10^{-11} \sigma_h - 2.0 \times 10^{-8}$$

where

ϵ_h is hoop strain/hour

σ_h is hoop stress psi

Experimental conditions:

flux = 1 X 10¹⁵ n/cm²/sec, E > 0.1 MeV

Temp = 380°C

Creep Equation 2 (Ref. 2-2)

$$\bar{\epsilon} = 0.8 \times 10^{-8} \bar{\sigma} + 9.8 \times 10^{-30} \bar{\sigma} \phi t$$

Table 2-8
RESULTS OF SLIT TUBE DATA ANALYSIS

Calculated Change in Slit Width (mils)

Sample No.	Outside, Inside Temperature Differential (°F)	Fluence E > 0.1 MeV 10^{22} n/cm ²	Estimated Midwall Temperature (°F)	Change in Slit Width After Unmounting (mils)		ARD 304 Swelling Equation Using Midwall Temperature $\Delta V/V$ (%)	HEDL Swelling Equation & Creep Equation No. 1	ARD 304 Swelling Equation & Creep Equation No. 2	ARD 304 Swelling Equation & Creep Equation No. 2	ARD 304 Swelling Equation & Creep Equation No. 2 Using Av Temperature +50°F	GE 304 Swelling Equation & Creep Equation No. 1	GE 304 Swelling Equation & Creep Equation No. 2	GE 304 Swelling Equation Using Creep Equation No. 2 Reduced 20%	GE 304 Swelling Equation & Creep Equation No. 2 Using Av Temperature -20°F
45C B	79	1.6	765	2	(-1)	0.231	-11.4	-4.1	-0.56	-8.7	-5.85	-5.2	-4.2	-4.5
45C C	79	1.7	772	-1.5	(-2)	0.321	-13.2	-5.7	-0.67	-12.5	-7.37	-6.6	—	-5.6
45C E	77	1.9	784	2.5	(-1)	0.462	—	-4.8	+1.59	-13.6	-7.74	-7.1	-6.1	-5.7
45C F	77	1.9	796	-0.5	(-2.5)	0.501	-12.56	-6.7	-2.2	-12.9	-8.59	-8.2	—	-6.5
45C H	79	2.1	820	-6	(-7.5)	0.642	-11.7	-11.4	-11	-15.1	-10.77	-10.8	-10.3	-8.7
49A B	81	2.7	765	-1	—	0.615	-32	-2.1	+7.5	-4.5	-5.6	-4.5	—	-3.3
C	81	2.8	773	-1	—	0.726	—	-3.4	+5.2	-7.7	-6.0	-5	—	-3.4
45G B	90	2.7	796	2.5	(1)	0.882	-17.4	-3.39	+6.5	-10.5	-9.22	-8.6	-7.5	-6.1
C	90	2.7	757	4.5	(1)	0.546	-23.8	-3.8	+5.05	-4.1	-5.82	-4.4	—	-3.5
E	95	2.9	832	6	—	1.17	-28.8	-11	-7.5	-18.1	-14.4	-14.7	-14.6	-11.6
F	95	2.9	839	-4	(-4)	—	-11.26	-11.4	-8.4	-20.6	-15.34	-15.8	—	-12.7
66C D	54	5.1	745	0	—	—	-26.8	+0.17	+8.22	+3.4	-0.29	+1.2	+3.3	+1.3
67B B	122	3.6	819	-2	—	1.716	—	-5.6	-27	-16.5	-15.9	-15.9	-15.2	-11.3
D	122	3.7	841	-0.5	—	—	—	-6.8	+3.9	-26	-20	-20.6	-20.9	-16.2
49A E	86	3.6	829	-1.5	—	—	—	+1.6	-16.1	-17	-12.8	-13.1	—	-9.7
F	86	3.7	838	-5.5	—	1.896	—	-6.7	+0.26	-21.2	-14	-14.5	—	-11.2
66C F	58	5.5	757	0	—	—	—	+2.4	-12.9	+0.17	-0.49	+1	+3.2	+1.9
66B F	50	5.4	757	3.5	—	1.662	—	+1.9	-11.2	+8.4	-0.46	+0.83	—	+1.6
66C H	61	6.4	742	0	—	—	—	+23	-53	+1.9	-3.4	-2.6	-1.1	+0.44
66B H	54	6.3	792	8.5	—	3.42	—	+17.3	+41.7	-2.3	-3.1	-2.3	—	+0.36

- Case 5. Change in slit width calculated using the GE swelling equation for Type 304 and creep equation 1.
- Case 6. Change in slit width calculated using the GE swelling equation for Type 304 and creep equation 2.
- Case 7. Change in slit width calculated using the GE swelling equation for Type 304 and creep equation 2 reduced by a factor of 0.8.
- Case 8. Change in slit width calculated using the GE swelling equation for Type 304 and creep equation 2 but assuming that the average temperature was 20°F lower than calculated.

The results of the analysis for the combinations of materials correlations considered are given in Table 2-8 which lists the various predictions of slit opening or closing. In addition, Cases 2 and 6 are illustrated in Figures 2-22 and 2-23. The measured values for change in slit width on removal of the restraint are plotted against the calculated values for the slit width change. A perfect fit would be represented by a line drawn at 45 degrees through the origin. The following are noteworthy:

1. The scatter in the slit width predictions is considerable.
2. The analysis is highly sensitive to the swelling equation selected. For example, the ARD equation predicts substantial slit opening for the high fluence specimens, whereas the GE equation predicts slit closing.

It can be seen from Table 2-8 that in general all the treatments predict too much slit closure in comparison with the experimental results, indicating that the tubes had larger residual swelling stresses than predicted. This implies that the correlations used predicted either too much creep or too little swelling. Measurements were made of the average swelling in some of the specimens by immersion density. Figure 2-24 compares these measurements with those values predicted by GE correlation. The agreement indicates that the GE correlation should give a good estimate of swelling.

The evidence in favor of swelling dependent creep lies in an earlier analysis of these data⁽²⁻¹⁴⁾ which indicated that the measured slit openings for the higher fluence specimens were inconsistent with the values calculated using the ARD swelling equation (Case 2). It was therefore concluded that more creep had occurred than would be predicted by the conventional creep equations.

However, the concept of enhanced creep at higher fluences where swelling occurs is only one of several means of rationalizing the data. If the residual stress were less than anticipated, then instead of postulating that large stresses were built up and subsequently relaxed, it is equally plausible to suggest that such large stresses never built up in the first place. Recent work at ANL-Idaho⁽²⁻¹⁵⁾ has demonstrated the existence of stress effects on swelling in Type-304 stainless steel at temperatures as low as 500°F (932°F). It can be argued that components, such as capsule tubes, subjected to temperature gradients and potential differential swelling will self-adjust the extent of swelling to minimize developing stresses.

The present analysis has shown, however, that while either of these conjectures may be perfectly valid, it is quite possible to adequately describe the measured results by using the available swelling and irradiation creep correlations for Type-304 without invoking the concept of swelling dependent creep. The analysis has shown that the predictions of slit behavior are highly dependent on the correlation used to calculate swelling. Results are markedly influenced by the shape of the swelling versus temperature curve, the shape and position of the peak, and the dependence of swelling on neutron fluence. It should be noted that the influence of none of these parameters is adequately defined at present.

2.3.1.2 Analysis of EBR II Driver Fuel Rod Failures

EBR II driver fuel rods clad with solution-annealed Type-304 stainless steel have experienced cladding failures.⁽²⁻¹⁶⁾ These results were examined to determine whether they support currently used LMFBR fuel element failure criteria.

Figure 2-22. Case 2. Change in Slit Width Calculated Using the ARD Swelling Equation for Type 304 and Creep Equation No. 2

Figure 2-23. Change in Slit Width Calculated Using the GE Swelling Equation for Type 304 and Creep Equation No. 2

2

Figure 2-24. Comparison of Experimentally Determined Swelling Values and GE Predicted Swelling Values (Equation Ref. 2-2)

Current fuel rod design criteria assume irradiation creep to be superplastic. This approach is based largely on the linear stress dependence of irradiation creep and the theory for the creep mechanism. The ANL driver fuel-failure results were examined to determine how much of the measured strain could be attributed to irradiation creep. The stress in the fuel cladding is not known, but is considered to increase in an approximately linear manner with reactor exposure. Reactor life-time is estimated at 10,000 hours, based on a flux of 1.5×10^{15} n/cm² second and taking a representative fluence of 5.8×10^{22} n/cm². Since the incremental stress level is not known, the approach taken in this analysis was to assume a constant stress acting for one-third the total reactor life (Case 1) and one-half total reactor life (Case 2). Peak cladding temperature ranged from 1007°F to 1078°F. For calculation purposes a midwall temperature of 1000°F was assumed. Measured inelastic strain values ranged from 1.1 to 2.7%. Irradiation creep strains and thermal creeps strain were calculated for a range of stress levels for Cases 1 and 2 to determine which combination of irradiation and thermal creep would produce 2.0% strain. The equations used to calculate thermal and irradiation creep strain are given below.

Irradiation Creep

$$\epsilon_h = 5.96 \sigma_h \times 10^{-9} + 7.432 \times 10^{-30} \sigma_h \phi t$$

$$\epsilon_h = \text{hoop strain} \quad \sigma_h = \text{hoop stress, psi} \quad \phi t = \text{fluence n/cm}^2, E > 0.1 \text{ MeV}$$

Thermal Creep

$$\epsilon_{th} = \epsilon_t (1 - e^{-rt}) + \epsilon_m t$$

where

$$\epsilon_t = 8.66 \times 10^{-14} \sigma^{2.58}$$

$$r = 6.84 \times 10^{-2} \sigma^{5.7} \exp(-100,000/1.987T)$$

$$\epsilon_m = 7.4 \times 10^{-16} \sigma^{8.28} \exp(-100,000/1.98T)$$

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

The irradiation creep equation was obtained by analysis of the ANL CREEP I in-pile creep test results on Type-304L.⁽²⁻¹³⁾ The thermal creep equation represents the creep rate of unirradiated Type-304 steel⁽²⁻¹²⁾ and for this reason would overpredict the actual creep rate, since at these temperatures and fluence levels irradiation hardening would be significant. To obtain a conservative value for thermal creep strain the transient strain was reduced by a factor of 0.5 in agreement with the existing data concerning the effect of irradiation on thermal creep for similar conditions. Transient creep for these stress levels accounts for the majority of the creep strain measured.

It was found that the majority of the creep strain was attributable to thermal creep. Taking Case 1 (assuming a constant stress acting for one-third the fuel rod reactor life), a hoop stress of 33,250 psi (effective stress 28,800 psi) would be necessary to produce a hoop strain of 2%. The irradiation creep strain is calculated as 0.0045 in./in. and the thermal creep strain as 0.0159 in./in.; that is, the thermal strain constitutes 78% of the total strain experienced by the rod.

For Case 2 (assuming a constant stress acting over half the fuel rod reactor life) a hoop stress of approximately 31,000 psi is required to give 2% strain, 70% of which would be thermal creep strain.

In either case the thermal creep strain is greater than 1%, which is consistent with the expected failure strain value for the conditions under consideration (i.e., fluence 5.8×10^{22} n/cm², temperature 1000°F and a slow strain rate).

Since very substantial swelling was measured for these specimens, the data were also analyzed in terms of swelling-dependent creep. Recently, theories have been proposed which indicate an increase in creep rate as a function of swelling.^(2-17, 2-18, 2-19)

The opportunity was taken, therefore, to analyze the driver fuel data using a swelling-dependent creep term. Analysis of slit tube data by WARD⁽²⁻²⁰⁾ resulted in values for D (the swelling dependent term) ranging from 1.7 to $14 \times 10^{-5} \text{ psi}^{-1}$. As recommended, a value of 2×10^{-5} was used in this analysis. Consideration of the swelling measurements for these fuel rods and the swelling curves for this alloy at these temperatures indicated that swelling would become appreciable ($>1\%$) at a fluence of about $2 \times 10^{22} \text{ n/cm}^2$. The current swelling enhanced creep equation multiplies the fractional swelling by the stress and a factor (the D factor given above). Time is not considered separately.

The equation for the swelling enhanced component of creep is therefore:

$$\epsilon_s = 2 \times 10^{-5} \sigma \Delta v$$

$$\epsilon_s = \text{swelling enhanced creep component} \quad \Delta v = \text{swelling fraction}$$

$$\sigma = \text{stress, psi}$$

On this basis, for a stress as low as 7.3 ksi all the measured strain can be attributed to swelling enhanced creep using input values of

$$\text{Measured strain} = 2\%$$

$$\text{Average swelling fraction for all driver fuel elements } \Delta v = 0.137$$

The conventional irradiation and thermal creep contributions can be ignored at these stress levels.

It appears that the concepts of superplastic irradiation creep and swelling enhanced creep cannot both be valid (unless they are substantially more complicated than this simple treatment allows).

On the one hand, if swelling enhanced creep occurs, the claddings failed after 2% irradiation creep, while if one considers only "conventional" irradiation creep the concept of irradiation creep superplasticity is not invalidated, since in this case the claddings failed after $\sim 1.4\%$ thermal creep.

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.

The four L-15 capsules have been disassembled. Disassembly proceeded very smoothly. All tensile coupons and strain cycle coupons were easily removed from the threaded coupon spacers, indicating that the inert atmosphere had been retained in the capsule throughout life.

This experiment provided a wide variety of LMFBR candidate cladding alloys irradiated in most cases to a peak fast fluence-GE ($E > 0.1 \text{ MeV}$) of at least $2.5 \times 10^{22} \text{ n/cm}^2$ at temperatures in excess of 1700°F . Strain cycle coupons of Incoloy-800 and Inconel-625 received greater than $1 \times 10^{22} \text{ n/cm}^2$ -GE, $E > 0.1 \text{ MeV}$ at temperatures less than 1100°F . All strain cycle coupons and tensile coupon packets have been retained intact in numbered vials preparatory to further examination at the National Laboratories should testing these coupons be deemed fruitful. This effort has been terminated and a report prepared giving full details of the L-15 irradiation.

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 program plan will utilize at least four consecutive EBR II subassembly irradiations with an interim examination between each subassembly irradiation. The initial subassembly in the L-16 program is X098. All future S/A's will be designated X098A, X098B, etc. The first S/A(X098) irradiation was completed in April 1972.

2.3.3.1 Subassembly X098 Capsules

The ten capsules discharged from S/A X098 were shipped to LASL for examination. A post-irradiation test plan was sent to LASL. The examination will be done on a noninterfering basis as hot cell time permits, since mixed oxide fuel rods were given higher priority.

2.3.3.2 Subassembly X098A Capsules

The S/A X098A contains nine Types-304H and -316H capsules from S/A X098 and several Type-321H capsules which began irradiation with S/A X098A. To date S/A X098A has accumulated ~10,600 MWd's of EBR II exposure which amounts to accumulated fluences of 6.1×10^{22} n/cm² E>0.1 MeV-GE on Type-304H and -316H capsules and 2.8×10^{22} n/cm² E>0.1 MeV-GE for Type-321H capsules. A request was approved by RDT to extend the irradiation beyond Run 62 through Run 65 by which time a reencapsulation capability should be demonstrated at the HFEF.

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 swelling. Two hot inserts containing rings of fuel cladding tubes cut from earlier PA-10 fuel and materials capsule experiments were shipped to HFEF in FY-72 for encapsulation. Irradiation will be delayed until HFEF can encapsulate these inserts.

2.3.3.4 L-16E Capsules

The L-16E experiment, an in-pile stress relaxation experiment, has been initiated. This study will compare various techniques for determining irradiation creep by stress relaxation. Of foremost concern is the generation of specific data applicable to core components such as grid spacers and channels. Capsule design has been completed and out-of-pile proof testing initiated. Fabrication drawings and specifications were issued.

2.3.3.5 Type-321H Tubing

Incoming inspection was completed on the Type-321H vacuum melted tubing purchased previously from Carpenter Technology Corporation to provide a stabilized grade of austenitic stainless steel cladding for in-pile experiments. The tubing was manufactured by unique, closely controlled fabrication parameters utilizing nontypical thermal and mechanical treatments as outlined in Figure 2-25 to enhance the high temperature mechanical strength of the alloy.

The tubing consists of six lots identified A through F. Lots A, B, and C make up a group that evolved from a high temperature (2200°F) annealing process to annealed, 10% cold worked and 20% cold worked, respectively. Similarly, Lots D, E, and F were derived from a low temperature (2000°F) annealing process to annealed, 10% cold worked and 20% cold worked.

Nondestructive incoming inspection (Table 2-9) of the tubing was completed to evaluate the vendor's conformance to ordering specification. Twelve tubes from each of six lots were examined which represents approximately 20% of the tubing order.

The following quantities of tubing were received:

Lot A	23 sections	184 feet total
Lot B	33 sections	264 feet total
Lot C	32 sections	256 feet total
Lot D	21 sections	168 feet total
Lot E	33 sections	264 feet total
Lot F	28 sections	224 feet total

Based on the above receiving inspection, the Type-321H vacuum melted tubing was shown to meet specified requirements and accepted for irradiation experiments.

Table 2-9
NONDESTRUCTIVE RECEIVING INSPECTION
OF K-4014G TUBING

Measurement	Type of Test	Tube Order Specification Limits	Test Procedure Employed	Accuracy of Test
Outside Diameter	Air Gage	0.249 to 0.251 in.	22A1803	± 0.0001 in.
Inside Diameter	Air Gage	0.219 to 0.221 in.	22A1803	± 0.0001 in.
Wall Thickness	Resonant Ultrasonics	0.014 to 0.016 in.	22A1804	± 0.0002 in.
Surface Finish	Visual Comparison With Standard	32 rms	—	Not Determined
Surface Defects	Visual Examination	Defects Not To Exceed 0.0015 In. Depth	22A1887	± 0.0002 in.
Flaw Detection	Ultrasonic	Defects Not To Exceed 0.0015 In. Depth	22A1805	95% Confidence

Figure 2-25 Flow Diagram for CarTech Type -321H Tubing

72EP632 (A)

72EP635 (D)

GRAIN SIZE ASTM 8 AND <10

72EP638 (B)

GRAIN SIZE ASTM 7

72EP640 (E)

72EP639 (C)

72EP641 (F)

A) LOT A; B) LOT B; C) LOT C; D) LOT D; E) LOT E AND F) LOT F [100X]

Figure 2-26. Microstructure of Vacuum Melted Type-321 Stainless Steel Tubing (HN93132)

The microstructure typical of each tubing lot is presented in Figure 2-26. A duplex grain size (ASTM 8 and ASTM less than 10) exists in Lot A. This lot, along with lots B and C, received a 2200°F-15-minute anneal preceding the final 50% cold reduction (anneal step of the processing sequence) in an attempt to dissolve all TiC. The grain size resulting from this high temperature treatment was quite large, ASTM 1, and apparently one subsequent cold work-anneal step is insufficient to uniformly refine the grain size.

The purpose of the high temperature anneal is to dissolve the TiC present so that carbide precipitation will occur during elevated temperature deformation (creep) and improve the elevated temperature mechanical properties of the alloy. Thus, to add another cold-work anneal step subsequent to the 2200°F anneal would probably be self defeating in that the TiC would reprecipitate during the recrystallization anneal. In addition, the duplex grain size is not believed to be in any way deleterious to the tubing's performance.

The microstructure of the cold worked lots is typical of 10- and 20% cold worked austenitic stainless steels; slip bands becoming increasingly evident with increased level of cold work.

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. Surveillance continues of the ANL sponsored program, "An Investigation of the Relative Effects of Fast Neutron Fluence and High-Temperature Sodium on the Mechanical Properties of FFTF Cladding and Structural Materials."

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3. FUEL ASSEMBLY ENGINEERING

Subtask Leader — J. R. Punches

The overall objective of this work is to contribute to the engineering basis for LMFBR fuel assembly design. All aspects of fuel assembly design are considered including:

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

- Develop and build grid-spaced driver assemblies for initial FTR core loading;
- Build two complete prototypic mockup assemblies for trail assembly and cell handling practice;
- Plan and execute an FTR irradiation test program aimed at: (1) comparing the performance of grid spaced and wire-wrap assemblies, and (2) achieving 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.

3.1.1 Diagnostic Grid Spacer Experiments in EBR-II

The first two of a series of GE grid-spaced fuel assembly experiments, F9E and F9F, are continuing irradiation in EBR-II.

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 was 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 growths.

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-recess concept of fixing grids to channels;
- the behavior of grids made of cold-worked Type-316 stainless steel in a reactor environment.

The support grids in both of these assemblies are bi-planar, line-of-sight type using wave-shaped support beams. F9F contains the later version of staggered grid which has alternate spacer beams omitted. The staggered grid system is being used in the design of the 217 rod FTR grid spaced assembly.

To the end of reactor run 62A, F9E accumulated a burnup of 4.2 at.%, which represents 37% of its target burnup of 11.4 at.%. At that same time, F9F had accumulated a burnup of 2.4 at.%, which is 26% of its goal burnup of 9.4 at.%. F9E was inserted into the EBR-II core at the beginning of Run 56, and F9F at the beginning of Run 59.

Cladding temperature calculations for both F9E and F9F due to decay heat generation, assuming natural convection and radiation heat transfer, have been performed in preparation for interim and final examination of the assemblies.

3.1.2 GSA—Grid-Spaced Assembly Experiment in FTR

It is proposed to irradiate in the FTR, full-size 217-rod grid-spaced fuel assemblies. These will be placed in normal core driver fuel positions in each of the fuel enrichment zones 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 burnup than a corresponding wire-wrap design.

The out-of-pile development work leading to the fabrication of the reactor assemblies includes the fabrication of two full scale mockup 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 confirm the grid spacer design, a full scale hydraulic flow test and a high temperature sodium flow endurance test.

3.1.2.1 Design and Fabrication

A review of grid-spaced assemblies was held at RDT on March 1 and 2, 1973. GE presented plans for resolution of critical issues identified at a GE/RDT meeting on February 9, 1973. The major issues involved the design of the mechanical attachment of grid spacers to the channel wall and the type of spacer (staggered or full) to be used. GE adopted Electrical Discharge Machining (EDM) as a reference process for producing grid retaining recesses in the hexagonal channel wall. The design has been further modified to use the rail-type axial fixing at the bottom ends of the fuel rods. This is basically the same as for the FTR wire-wrap assembly, but structurally strengthened to carry the greater cumulative friction loads from the grid-spaced bundle.

The grid-spacer-to-channel mechanical attachment is being redesigned to improve the design margin against the force tending to overcome locking mechanisms. This activity is being backed by a series of mechanical tests.

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 grid-spacer-to-channel wall attachment. The first test in this series will assess the strength of the grid/channel mechanical attachment by the use of single grid beams.

A third series of single-beam tab-geometry tests, No. 28 through 46 has been completed. The wave beam is shown in Figure 3-1. Tests 38 to 46 deal with two new tab concepts; the circular tab (Figure 3-2) and the hooked tab (Figure 3-3). Both concepts have the advantage of centering the reaction load on the beam axis. This effectively eliminates a twistout mode of failure, due to off-center loading, that characterized the reference beam concept. The circular and hooked designs are about equal in pushout strength. They are twice as strong as the strongest reference design beam (Test No. 28) and four times as strong as the previous reference design (Test No. 1, Ref. 3-1). Table 3-1 lists the parameters of Tests 28 to 46 and Table 3-2 the results.

Figure 3-1. FTR Wave Beam Detail

Figure 3-3. Hooked Locking Tab

Table 3-1
TESTS 28—46

Test	Tab Depth (mils)	Tab Length (mils)	Penetration (mils)	Undercut		Shelf Angle (deg)
				Yes	No	
28	200	100	031	X		5
29	200	100	027	X		5
30	200	100	035	X		5
31	200	087	020	X		5
32	200	087	016	X		5
33	200	087	024	X		5
34	200	125	031	X		5
35	200	075	031	X		5
36	140	160	020	X		0
37	200	160	020	X		0
38*	200	160	031	X		5
39*	200	160	031		X	5
40*	200	160	031	X		0
41*	200	160	020	X		5
42*	200	160	044	X		5
43*	170	160	031	X		5
44*	140	160	031	X		5
45*	200	160	031	X		5
46*	140	160	031	X		5

Table 3-2
POUNDS FORCE FOR TAB FAILURE

Test	Specimen No.					Average Force
	1	2	3	4	5	
28	73	71	69	67	68	71.6
29	50	49	64	—	—	54.3
30	72	71	68	—	—	70.3
31	42	39	24	40	37	36.4
32	12	14	9	—	—	11.7
33	30	50	45	—	—	41.7
34	70	75	72	—	—	72.3
35	67	73	67	—	—	69.0
36	50	19	28	—	—	32.3
37	81	74	78	—	—	77.7
38	148	143	155	127	143	143.2
39	179	133	143	—	—	151.7
40	145	141	141	—	—	142.3
41	120	140	135	—	—	131.7
42	140	137	139	—	—	138.7
43	142	126	134	—	—	134.0
44	110	80	96	—	—	95.3
45	139	144	148	—	—	143.7
46	105	102	104	—	—	103.7

*Using the circular tab, Figure 3-2.

*Using the hooked tab, Figure 3-3.

The second phase of mechanical testing will involve evaluating the force required to overcome the mechanical retention of a complete spacer grid from the channel. The third phase will measure rod/grid friction forces, thus enabling the design margin to be assessed. A test specification has been issued for the second phase and one is in preparation for the third phase.

After technical reevaluation, it has been decided to discontinue the planned mechanical 217-rod/grid cyclic friction test in high temperature sodium. It is considered that the mechanical air tests described in the foregoing, used in conjunction with a conservative scaling factor to account for the sodium environment, will provide evidence of a more than adequate design margin.

Further development work on forming recesses on the inside of the hexagonal channel walls for grid spacer retention is being performed. The EDM method of machining is being actively pursued. Contracts have been let to three vendors for developmental machining of a flat plate and a short length of hexagonal channel. Upon successful completion of this work, a full length channel will be machined.

3.1.2.3 Sodium Flow Test

The final report on the 37-rod sodium flow test bundles is in draft.

The test assembly consisted of two separate bundles of 37-rod dummy fuel elements positioned in series. The FFTF shield/orifice region was simulated for both assemblies to generate prototypical inlet turbulence in each bundle. One 37-rod assembly contained staggered grids, the reference for the GE grid spaced FFTF driver assembly; the other 37-rod assembly contained full grids. Both assemblies were subjected to flowing sodium at 1000°F for a period of 4408 hours.

3.1.2.4 Hydraulic Test Assemblies

The final report on the destructive examination of the 217-rod assembly, intended for hydraulic flow test, has been completed. It has been concluded that the dislodgement of the grid spacers from their mechanical attachments to the channel wall was due to the across flats dimension of the channel being above the acceptable tolerance margin. Design work has been started on the test loop and fittings for a second assembly. The second assembly will incorporate the final revisions in grid spacer, FTR orifice, nosepiece and handling socket designs.

3.2 WIRE-WRAP ASSEMBLY—J. R. PUNCHES

The broad purpose of this work is to develop the engineering basis for a wire-wrap fuel assembly for the LMFBR Demonstration Plant.

The scope and objectives are:

- To implement a series of out-of-pile mechanical tests to evaluate bundle mechanical interactions and performance at end-of-life conditions;
- To plan and conduct a water flow test program to evaluate the thermal-hydraulic and vibration performance of an end-of-life bundle configuration;
- To plan, design, and implement an advanced 217-rod wire-wrap fuel assembly, utilizing 20% cold-worked Type-316 stainless steel for testing in the first core of FTR. The design will be based on the results of detailed wire-wrap assembly optimization analysis, out-of-pile tests carried out under this program, and work completed by others.

The design limits and engineering basis for optimization of LMFBR wire-wrap fuel assemblies must be understood, including the necessary mechanical, hydraulic, and irradiation tests to support this objective. To advance the state-of-the-art to target LMFBR conditions, the following fuel assembly needs are planned to be addressed: fuel assemblies capable of reaching high optimized burnup with a specified reliability; fuel assemblies capable of reaching higher optimized core breeding ratios; fuel assemblies capable of operating at higher specific powers; fuel assemblies capable of operating at higher coolant temperatures and/or coolant ΔT ; and fuel assemblies capable of accommodating swelling and distortion at target conditions.

The operating characteristics and limitations of FTR as a fuel assembly test facility are being evaluated for fuel assembly test planning purposes. The tradeoffs required in linear power, fast fluence, and fuel rod burnup are being investigated. Test planning consistent with overall FTR and LMFBR schedules are being considered.

Plans are in preparation for a series of out-of-pile mechanical tests to evaluate bundle mechanical performance at end-of-life conditions. The objective of these tests is to obtain a better understanding of the mechanical behavior of wire-wrapped assemblies when subjected to the high differential swelling which is predicted to occur at end-of-life conditions. The dispersive and compressive characteristics of such bundles will be investigated.

Following the mechanical tests, a series of water flow tests will be run to determine the hydraulic performance of end-of-life bundle configurations. Tight, distorted bundles, typical of end-of-life conditions at the bundle midplane, will be investigated as to flow distributions, thermal mixing and pressure drops. Very loose configurations typical of end-of-life conditions near the bundle exit will also be investigated, principally for flow-induced vibrations.

A series of FTR in-pile irradiations will be planned and executed considering best utilization of driver fuel positions, fueled open test assembly positions, and closed loops to obtain in-pile performance data on optimized fuel assemblies.

Initially GE will plan, design, and implement an advanced 217-rod wire-wrap fuel assembly for test in the first core of FTR when the core operates at full power. This assembly will utilize the results of the detailed wire-wrap assembly optimization analysis and out-of-pile tests carried out under this program and others. The in-pile tests will address assembly operation at end-of-target burnup life, higher operating temperatures, higher reliability and optimization of parameters affecting breeding ratio.

3.2.1 F11B Diagnostic Wire-Wrap Fuel Assembly

F11B was planned as the first of a series of wire-wrap fuel assemblies to be used to investigate the design-limiting conditions and operation for an LMFBR fuel rod assembly. These assembly tests were planned for both EBR II and FTR.

The original design basis for F11B was discussed with RDT on December 1, 1972. As a result of these discussions, GE was requested to provide further justification of the proposed F11B experiment as it related to overall wire-wrap fuel assembly requirements. In response to this request, a report, *Wire-Wrap Fuel Assembly Technology Requirements*, was prepared and discussed with RDT on February 6 and 7, 1973.

As a result of this meeting with RDT, the specific scope and objectives of this work have been revised as summarized in this subsection.

While it was agreed that the original F11B test as proposed by GE was of merit, RDT believed that alternate sources of comparable information may be available and that timing for obtaining this information from F11B did not appear to be critical (especially considering the large uncertainties in extrapolating the present swelling and creep correlations to goal LMFBR fluences).

GE was requested to plan, design, and implement a series of out-of-pile mechanical tests to evaluate bundle mechanical interaction at end-of-life conditions. These tests are to be followed by a series of water flow tests to evaluate the hydraulic performance of end-of-life bundle configurations.

Finally, it was agreed that GE would plan, design, and implement a 217-rod wire-wrap fuel assembly (GEWA-1) for test in the first core of FTR. The design of this assembly would utilize the results of detailed wire-wrap assembly design evaluations to assess assembly optimization and design operating limits and the results of the above out-of-pile tests.

3.3 FUEL ASSEMBLY HARDWARE TESTING—L. S. LEE

The objective of this work is to perform development testing (including thermal-hydraulic, mechanical, irradiation, and life test 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 test plan and specification for tests of nosepiece, orifice and shield.

The formulation of requirements and bases for orificing as well as the delineation of considerations for effective orifice design has been completed.

The main requirements of orificing are that the coolant flow through the assemblies be such that:

- temperature of the cladding and fuel do not exceed design limits;
- mixed mean coolant temperature is within specified values, and
- radial temperature variation throughout the core is minimized.

Major considerations for the effective design are: scheme of orificing, range of pressure reduction, existence of radial pressure gradient and limitation of flow dynamic effects.

Included in the consideration of orificing scheme are the number of zones needed and whether the orifice should be adjustable. Consideration of range of pressure reduction and the existence of radial pressure gradient focus on the maximum pressure the orifice in each zone is required to reduce. In the consideration of flow dynamic effects, limitations imposed by such phenomena as cavitation, erosion, vibration, and noise are assessed.

The bases for the requirements and those needed for effective design are specified wherever possible. A set of tests aiming at obtaining the specific information needed for setting the bases are to be proposed.

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

The overall objective of this activity is to improve LMFBR Core Design Methods and Structural Design Criteria.

3.4.1 Structural Design Criteria for Core Components

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

A draft document of the structural design criteria for core components has been completed. This document is scheduled for delivery to RDT in May.

The creep rupture life for 20% cold-worked stainless steel during simultaneous in-pile irradiation and creep has been examined. Based on a model whereby radiation contributes to void nucleation and creep contributes to void growth, predictions for time to rupture under in-pile conditions have been generated. These predictions are that the lifetime will correspond to the unirradiated case for rupture life <100 hours and will approach post-irradiation life for failure times greater than 10,000 hours.

3.4.2 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 sensitivity studies.

The work reported here is an extension of the development work performed during the first half of FY 1973; primary emphasis during the quarter was placed upon review and updating of materials data for PECS (Probabilistic Evaluation of Cladding Stress) input, and in using PECS to study the relative influence of each of the input uncertainties.

3.4.2.1 Master Plan

The Master Plan for development of probabilistic core design tools was completed and sent to AEC-RDT for review.

3.4.2.2 PECS Code Development

PECS development continued as necessary to accomplish the objectives of the sensitivity study. In this context, the capability for numerical integration of fuel-cladding attack rate was added to the code. Damage fraction, steel swelling, sodium corrosion, and fuel-cladding attack are now integrated using a modified technique.

3.4.2.3 PEFT Code Development

PEFT code development continued with major emphasis placed upon formulation of a core-wide code structure that is capable of representing spatial variations in power, burnup, coolant temperature, fuel composition, etc. A method for incorporating control rod power peaking effects was formulated, and methods were examined for limiting the core-wide Monte Carlo calculation for each sample to core regions in which some melting occurs.

3.4.2.4 Code Applications

The input data for the PECS code were reviewed and modified; major changes include a new creep-rupture correlation with fluence and temperature dependence, a correlation for the effect of sodium exposure on creep-rupture strength, and a revised correlation for the effective loss of cladding strength caused by fuel-cladding attack. The following correlations replace those given on pages 3-33 and 3-41 of GEAP-10028-45:

$$t_r = \exp [-74.78 + 82,800/T(K)] [\sinh (B\sigma)]^{-4.4105} \quad (\phi t < 1 \times 10^{22}) \quad (3-1)$$

$$t_r = \frac{4F}{\omega} \quad (\phi t \geq 1 \times 10^{22}) \quad (3-2)$$

where

$$\begin{aligned} t_r &= \text{rupture time (h)} \\ T &= \text{cladding temperature (°K)} \\ \sigma &= \text{stress (psi)} \\ B &= 0.849697 \times 10^{-4} \exp [-0.001534 (T-977)] \\ F &= A_1 (\phi t)^{-A_2} \exp [-A_3 T(^{\circ}\text{C}) + A_4 T^2 (^{\circ}\text{C})] \\ A_1 &= \exp [115.2801] \\ A_2 &= 0.59524 \\ A_3 &= 0.265969 \\ A_4 &= 1.9476 \times 10^{-4} \\ \phi t &= \text{Neutron fluence (E > 0.1 MeV) (n/cm}^2\text{)} \end{aligned} \quad (3-4)$$

and

$$\omega = \frac{\exp [74.78 - 82800/T(^{\circ}\text{K})] [\sinh (B\sigma)]^{5.15105}}{4.4105 (B\sigma) \cosh (B\sigma)} \quad (3-5)$$

It should be emphasized that the units of temperature are degrees Kelvin everywhere except in equation (3-4). These equations include the effect of irradiation on time-to-rupture, but do not include the effect of exposure to flowing sodium. The reduction in strength caused by flowing sodium is characterised by:

$$F_2 = 1.0 - 0.05 \exp [0.011945 (T-1150)] t/13000, \quad (3-6)$$

where

$$T = \text{temperature (°F)}$$

$$t = \text{time (h)}$$

and σ is divided by F_2 in equation (3-1) or (3-2) to represent the loss in creep-rupture strength.

The effective cladding wastage caused by fuel-cladding intergranular attack is given by

$$X = 216 F_1 \exp [-12720/R(T+273)] \quad (3-7)$$

where

$$F_1 = 1.0 - \frac{(100-M)}{2000} [\phi t - 10.0] \quad (\phi t \geq 1 \times 10^{23}) \quad (3-8)$$

$$F_1 = 1.0 \quad (\phi t < 1 \times 10^{23})$$

$$M = \exp [0.69664 + 1.2807 \times 10^{-2} T - 1.0561 \times 10^{-5} T^2]. \quad (3-9)$$

and

$$X = \text{effective loss of cladding thickness (mil/at.\%)}$$

$$T = \text{temperature (°C)}$$

$$\phi = \text{neutron fluence (E > 0.1 MeV) (n/cm}^2\text{ X } 10^{-22}\text{)}.$$

Improved estimates of the probability distributions of these parameters were made during the quarter period. Fuel-cladding attack uncertainty is represented by a log-normal distribution with a deviation from the mean at ± 2 standard deviations equal to a factor of 2. Uncertainty in time-to-rupture is represented by a skewed distribution; the time given by the correlation equations is approximately equal to the median of this distribution, while the range of the ratio of the actual time to the median time is 0.01 to 10 (see Figure 3-4). The effect of sodium exposure on creep-rupture

Figure 3-4. Probability Distribution of the Time to Rupture Uncertainty Factor

strength is treated as an independent uncertainty; this variable (from Equation 3-6) is described by a normal distribution with a mean of 1.0 and a standard deviation of 0.16. Table 3-3 shows a complete list of the PECS input distributions currently in use, while Table 3-4 defines the nomenclature used on Table 3-3.

The mean number of failures in a fuel batch at end-of-life was calculated using the revised properties and revised probability distributions described above. The core was divided into six rod groups, which are defined in Table 3-5, the operating conditions and fuel rod geometry assumed for this analysis are shown on Table 3-6. The results, shown on Table 3-5, indicate that the mean of the distribution describing the number of failures in a batch is 285, while the failure probability of the peak-temperature rod is 7.5%. These values are higher than the 29 failures and 0.7% failure probability presented in the 45th Quarterly (GEAP-10028-45), and are believed to be more realistic. The mean number of failures predicted by this analysis represents a design position which is untenable by a wide margin. In view of this, further analysis and material property development is required. If reduction of uncertainty in creep-rupture strength for 20% cold worked, Type-316 stainless steel fails to indicate increased strength over the present correlation, then a cladding material with greater strength is required, or the fuel rod design and/or operating conditions must be modified substantially.

Following this base-case analysis, the sensitivity of rod failure probability and mean number of failures in a batch to reductions in the variance of each input parameter was investigated by arbitrarily reducing the standard deviation of each distribution. Therefore, the results of this study are conditioned by the assumption that future work will tend to confirm present estimates of material properties and the present distribution means will remain fixed. Standard deviations were reduced, for one parameter at a time, by 10, 25, 50, and 90%.

The results of the probabilistic analysis indicate that cladding temperature uncertainties are of overriding importance. Statistically significant evidence of sensitivity to reductions in the standard deviations of input variables was found for only two parameters; cladding temperature was established as the most sensitive parameter, and time-to-rupture also displayed significant influence on the results. For all other parameters, reduction of the standard deviations by as much as 90% produced no statistically significant effect. Figure 3-5 shows how the single-rod failure probability

Table 3-3
PROBABILITY DISTRIBUTIONS PROVIDED
AS INPUT TO PECS

Variable	Type of Distribution	Mean	Standard Deviation	Truncation Points
U_{σ}	Normal	1.0	0.16	$\pm 3\sigma$
U_{TR}	Arbitrary	(see Figure 3-4)		
U_{SW}	Normal	1.0	0.10	$\pm 3\sigma$
ϕ (n/cm ² sec)	Normal	4.49E15	0.449E15	$\pm 3\sigma$
U_{ID}	Log Normal	0	0.15045	$\pm 3\sigma$
U_{OD}	Normal	1.0	0.2	$\pm 3\sigma$
G(atoms/fiss.)	Normal	0.2758	0.00405	$\pm 3\sigma$
C_o	Normal	215.2	2.0	$\pm 3\sigma$
d_i (in.)	Normal	0.200	0.001	$\pm 1\sigma$
d_o (in.)	Normal	0.230	0.001	$\pm 1\sigma$
d_v (in.)	Normal	0.1945	0.0003	$\pm 3\sigma$
δ (in.)	Normal	0.0005	0.0003	0 and 0.001
ℓ_i (in.)	Normal	54.0	0.3	$\pm 3\sigma$
B_1	Normal	1.0	0.15	$\pm 3\sigma$
$T_{F, 1}$	Normal	1.0	0.065	$\pm 4\sigma$
U_{TC}	Normal	1.0	0.059	$\pm 3\sigma$
U_p	Normal	1.0	0.070	$\pm 4\sigma$

Table 3-4
PROBABILISTIC INDEPENDENT VARIABLES OF THE PECS PROGRAM

Variable	Definition
Global Variables:	
U_{σ}	Uncertainty factor applied to the sodium effect on cladding material strength
U_{TR}	Uncertainty factor applied to time to creep-rupture of irradiated cladding material
U_{SW}	Uncertainty factor applied to steel swelling correlation
ϕ	Core average neutron flux (n/cm ² -sec)
U_{ID}	Uncertainty factor applied to cladding inside surface attack correlation
U_{OD}	Uncertainty factor applied to cladding outside surface corrosion correlation
G	Number of fission gas atoms produced per fission
C_o	Constant of the fission gas release correlation
Local Variables:	
d_i	Cladding inside surface (cold)
d_o	Cladding outside surface (cold)
d_F	Fuel pellet outside surface (cold)
δ	Local cladding defect depth
l_1	Effective plenum length (cold)
$B_g, g = 1, 2, \dots, NG$	Burnup rate (MWd/Te/h) per rod group
$T_{F, g}, g = 1, 2, \dots, NG$	Fuel temperature per rod group
$U_{TC, g, i},$ $g = 1, 2, \dots, NG$ $i = 1, 2, \dots, NG$	Uncertainty factors applied to cladding temperature (see Equation 6) per rod group per cycle
$U_{p, g, i},$ $g = 1, 2, \dots, NG$ $i = 1, 2, \dots, NG$	Uncertainty factors applied to plenum temperature per rod group per cycle

Table 3-5
DEFINITION OF ROD GROUPS USED
IN BASE-CASE PECS ANALYSIS

Group No.	Number of Rods	Single-Rod Failure Probability (%)	Cladding Midwall Temperature at Beginning/End of Life (°F)	Axial Average Burnup (MWd/Te)	Mean Number of Failures in Group
1	501	7.5	1244/1160	120,000	37
2	441	5.2	1215/1150	118,000	23
3	1,161	4.0	1217/1145	116,000	46
4	2,730	3.7	1215/1140	113,000	101
5	2,517	1.9	1212/1130	107,000	48
6	<u>10,227</u>	0.3	1160/1090	102,000	<u>30</u>
Total	17,577				285

Table 3-6
NOMINAL GEOMETRY AND OPERATING CONDITIONS
USED FOR BASE-CASE PECS ANALYSIS

Fuel Rods per Assembly.....	217
Fuel Rods per Batch.....	17,577
Number of Batches.....	2
Fuel Residence Time (full-power hours).....	13,150
Average Rod Discharge Burnup (MWd/Te).....	106,000
Peak Rod Discharge Burnup (MWd/Te).....	120,000
Active Fuel Length (in.).....	30.6
Effective Plenum Length (in.).....	54
Reactor Mixed Mean Outlet Temperature (°F).....	1000
Peak Neutron Flux ($E > 0.1$ MeV) (n/cm^2 sec).....	5.3×10^{15}
Peak Neutron Fluence ($E > 0.1$ MeV) (n/cm^2).....	2.5×10^{23}
Cladding Outside Diameter (in.).....	0.230
Cladding Thickness (in.).....	0.015
Rod Pitch (in.).....	0.286
Spacer Wire Diameter (in.).....	0.056
Cladding Material.....	20% Cold Worked, Type-316 Stainless Steel
Number of Coolant Orificing Zones.....	9
Peak Cladding Inside Surface Temperature (°F).....	1266

Figure 3-5. Reduction of Rod Failure Probability and Mean Failures in Core with Reductions in Cladding Temperature Variance

in each rod group changes as the standard deviation of the cladding temperature distribution is reduced. Also shown is the mean value of the probability distribution for the number of failed rods in a fuel batch as a function of the standard deviation of the cladding temperature distribution. Figure 3-6 is a similar plot showing the variation in failure probability produced by reduction of uncertainty in time-to-rupture.

Figure 3-6 indicates that large reductions in failure probability cannot be achieved by reducing only the uncertainty in time-to-rupture if present estimates of the mean of the time-to-rupture distribution prove to be correct. Thus, cladding alloys with greater creep-rupture strength than that of 20% cold-worked Type-316 stainless steel appear to be required for successful operation of this fuel rod design at the high cladding temperatures and burnup expected. Clearly, if nominal temperatures are reduced or temperature uncertainties can be shown to be smaller, significant improvement in cladding reliability will occur. Conversely, because of the extreme dependence of cladding reliability on cladding temperature, reduction of uncertainty in time-to-rupture will not enable the designer to increase temperatures significantly. With present values of operating temperature and alloy creep rupture strength, a 40% reduction in burnup would be required to reduce the number of failures to 70.

As a result of the work described above, two areas can be identified in which further work is highly desirable. First, thermal-hydraulic core design should be carefully optimized to reduce cladding temperature, and temperature uncertainty analysis should be refined; reductions of 10 to 20°F provide significant increases in reliability. Second, uncertainties in the creep-rupture behavior of cladding materials should be reduced, particularly for highly irradiated material (the large uncertainties presently assigned to the creep-rupture strength reflect the fact that data for highly irradiated material is sparse). The effect on cladding reliability of optimizing thermal-hydraulic design is discussed below.

It was shown above that the probability of cladding failure caused by fission gas pressure loading is an extremely sensitive function of cladding temperature uncertainty. It was also apparent that cladding failure probability is sensitive to changes in the mean value of the cladding temperature distribution. The relationship between mean cladding temperature and cladding failure probability has been developed explicitly, and the effects of some thermal-hydraulic design options have been expressed in terms of cladding failure probability. In addition, the effect on cladding reliability of changing a principal rod design variable (plenum length) was investigated. Finally the effect of flattening the core-wide cladding temperature profile, which reduces peak temperatures but increases the number of rods at peak conditions, is analyzed in this section and the result expressed in terms of the mean number of failed rods in a batch.

Figure 3-7 shows how single-rod failure probability varies as the nominal cladding temperature (the mean of the temperature distribution) is reduced for each rod group in the base case of Table 3-5. For this series of calculations, the standard deviation of the cladding temperature distribution was held constant at its base-case value. Figure 3-7 confirms that cladding failure probability is very sensitive to cladding temperature changes, with changes in temperature on the order of 10°F producing a significant effect.

Two important thermal-hydraulic design options involve choosing the number of coolant orificing zones in the core and selection of the spacing between the fuel assembly channel wall and the adjacent fuel rods. A partial measure of the effectiveness of various design choices is the reliability of the peak-temperature fuel rod in the core. Given a reduction in peak cladding temperature, Figure 3-7 provides an estimate of the corresponding increase in cladding reliability.

Table 3-7 shows how the peak cladding temperature in the core can be reduced by increasing the number of orificing zones. A nine-zone orificing scheme was assumed for the base case in which orificing is fixed throughout life and peak cladding temperatures in each assembly are equalized at beginning of life. Peak rod cladding failure probabilities estimated from Figure 3-7 are also shown on Table 3-5. Although peak-rod failure probabilities are not completely descriptive of the core-wide effect of orificing, these probabilities do provide a qualitative indication of the effect. For example, Table 3-7 indicates that increasing the number of orificing zones from 9 to 43 decreases peak-rod failure probability by a factor of 2. Thus, significant increases in reliability can be achieved in this manner, but the cost may be prohibitive.

Figure 3-6. Single-Rod Failure Probability and Mean Number of Failures per Batch vs Variance in Time-to-Rupture

Figure 3-7. Single-Rod Failure Probability and Mean Number of Failures per Batch vs Mean Cladding Temperature

Table 3-7
EFFECT OF THE NUMBER OF ORIFICING ZONES IN THE
CORE ON RELIABILITY OF THE PEAK-TEMPERATURE FUEL ROD

Number of Orificing Zones	Peak Cladding Inside Surface Temperature in Core (mean value) (°F)	Peak-Temperature Rod Failure Probability (%)
5	1340	17.0
6	1322	14.6
7	1313	13.4
8	1285	9.7
9	1266	7.2
12	1260	6.4
43	1240	3.7

Optimization of the spacing between peripheral fuel rods in an assembly and the channel wall was also investigated as a means of reducing cladding temperatures and increasing cladding reliability. As shown on Table 3-6, the diameter of the wire spacer in the base case was 0.056 inch. This wire diameter was assumed to be changed throughout the rod bundle and the wire in contact with the channel wall was assumed to be flattened as a means of changing edge spacing. When wire diameter is increased to 0.060 inch and the edge spacing reduced from 0.056 inch to 0.031 inch, the peak cladding temperature in a typical assembly is reduced by 30°F. From Figure 3-6, such a decrease produces a 55% decrease in the failure probability of a peak-temperature rod. From Table 3-6, 501 rods in a batch experience a decrease in failure probability of this magnitude. Many rods in a batch, however, experience no increase in reliability, since their temperatures are unchanged by this design modification. Thus, an accurate assessment of the effect of this (or any other) design change on core reliability requires consideration of all the rods in the core. The core-wide effect can be expressed in terms of the mean number of cladding failures in the core, as described in the following paragraph.

The situation in which fuel-rod-to-channel-wall spacing was optimized to reduce peak cladding temperatures is but one example of the way in which cladding temperature peaking in the core can be reduced. In general, a reduction of peak cladding temperature is accompanied by an increase in the number of rods operating at the new, lower peak temperature. To estimate the effect of this behavior for the edge spacing optimization study, we assign 75% of the fuel rods in rod groups 1 to 4 (Table 3-5) to rod groups 5a, 5b, 5c and 5d as shown on Table 3-8; the remainder of these rods are assigned to rod group 6a. The single-rod probability of failure and the mean number of failures is also shown for each group on Table 3-8; probability values were estimated from Figure 3-6. The data shown on Table 3-8 indicate that the mean number of cladding failures in the batch is reduced by 35% as the probability of failure of the peak-temperature rod is reduced by 55%. Thus, some of the effect of a reduction in peak temperatures is "damped out" by the existence of a large number of unaffected rods. For the same reason, we find that the core outlet temperature can be increased by only 15°F (when high-temperature rods are redistributed in the manner assumed here) before the mean number of failures in a batch is returned to its original value (i.e., its value with peak temperatures 30°F higher). This behavior is caused by the fact that all the rods experience a change in reliability when the core outlet temperature is modified, whereas reduction of peak temperatures is not expected to affect the large number of rods operating near the average temperature in the core.

It has been shown that the most effective way to reduce the mean number of cladding failures in a batch is to change the operating environment of all rods. It follows that modifying the fuel rod design or operating conditions in every rod is the most effective method of influencing core reliability. Figure 3-8 shows the effect on failure probability in each rod group of increased plenum length. Also shown is the plenum length effect on the mean number of failures in a batch. As expected, cladding reliability is sensitive to plenum length, but not as sensitive as it is to the analogous thermal parameter, the core outlet temperature.

Figure 3-8. Single-Rod Failure Probability and Mean Number of Failures per Batch vs. Plenum Length

Thus design temperature must be reduced or cladding alloys with greater creep rupture strength than the present estimates of 20% cold-worked Type-316 stainless steel appear to be required for successful operation of this fuel rod design to the burnup desired.

Table 3-8
EFFECT OF 30°F REDUCTION IN CLADDING TEMPERATURE
PEAKING ON MEAN NUMBER OF FAILURES IN CORE

Group No.	Number of Rods	Cladding Midwall Temperature at Beginning/End of Life (°F)	Axial Average Burnup (MWd/Te)	Single-Rod Failure Probability (%)	Mean Failures
1	0(501)	1244/1160	120,000	— (7.25)	0(36)
2	0(441)	1215/1150	118,000	— (5.20)	0(23)
3	0(1161)	1217/1145	116,000	— (4.05)	0(47)
4	0(2730)	1215/1140	113,000	— (3.50)	0(95)
5	2517(2517)	1212/1130	107,000	1.92(1.92)	48(48)
5a	375 (0)	1212/1130	120,000	3.20 (—)	12 (0)
5b	331 (0)	1212/1130	118,000	3.00 (—)	10 (0)
5c	878 (0)	1212/1130	116,000	2.80 (—)	25 (0)
5d	2040 (0)	1212/1130	113,000	2.50 (—)	51 (0)
6	10,277(10,227)	1160/1090	102,000	0.30(0.30)	31 (31)
6a	1209 (0)	1160/1090	120,000	0.50 (—)	6 (0)
Total	17,577 (17,577)				183 (280)

Note: Values in parentheses describe base case (i.e., before 30°F reduction in peak temperatures).

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4. CORE RESTRAINT ENGINEERING

Subtask Leader — P. M. Magee

The objective of this work is to develop a core restraint system for the LMFBR. A master plan has been prepared for the engineering analysis and detailed mechanical design and testing. Two of three planned tests of core restraint systems and components have been conducted. Analyses will be performed to assess the core response to operational environmental conditions and to establish the relationship between out-of-pile test results and LMFBR core design requirements.

In order to facilitate selection of a suitable restraint system and to demonstrate its ability to meet functional requirements, a development program which emphasizes early comparison of two alternative restraint systems, externally clamped and internally tightened, 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. N. FOX

The objective of the core restraint analysis activity 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

The results of the core-wide channel wall temperature distribution study discussed in the 45th Quarterly Report, have been forwarded to RDT in a memo report, *Core-Wide Distribution of the Channel Wall Temperatures in the GE Conceptual Design of the Demonstration Plant*, NEDG-13942. The modeling techniques and assumptions employed in this study were reported in the 45th Quarterly and are also included in the above memo report. These results are being directly applied to the CRS comparative performance study as described in Subsection 4.1.3.

4.1.2 Hexagonal Load Distribution Analysis

Additional hexagonal load distribution studies were performed which have altered some of the conclusions reported in the 45th Quarterly. The revised conclusions, additional case studies, and the corresponding supporting information were forwarded to RDT in the memo report, *Hex Load Distribution Analysis*, NEDG-13943. The pertinent results and conclusions of this memo report are discussed below.

An internally tightened core inherently produces four-face loading as opposed to six-face loading in an externally clamped core. To assess the significance of the load distribution in an internally tightened core, the load distribution is compared to the corresponding load distribution of an externally clamped core with the same applied loads. The specific comparison is for loads* applied to the outer surfaces of Row 4 as shown in Figure 4-1. The other applied loads shown are those required to meet the "sector" boundary conditions. Also shown in Figure 4-1 are the ratios of the maximum (normal to the hex faces) loads for four-face loading to the corresponding loads for six-face loading. Only the "nontightener" channels are included since the tightener channel (pad) stresses are not comparable to the six-face loading or externally clamped system. The ratios shown in Figure 4-1 were calculated by averaging techniques as shown in Sketch A.

The load ratios determined in this fashion are nearly equal, and one can conclude that the maximum forces normal to the surface in the internally tightened core are about two times the corresponding loads for the externally clamped system.

*The loads are shown at the midpoint of the faces; however, the force shown is only the net force normal to the face.

NOTES

1. "*" ARROW INDICATES APPLIED LOADS
2. NUMBER SHOWN WITHIN THE HEXAGONAL IS LOAD RATIO
3. ONLY THOSE HEXAGONALS SUBJECTED TO BOTH FOUR-FACE AND SIX-FACE LOADS ARE SHOWN

Figure 4-1. Comparative Load Distribution

FOUR-FACE

$$\bar{F}_4 = 1/2 F_4 + 1/2 (F_1/2 + F_3 + F_2/2)$$

WHERE:

SIX-FACE

$$\bar{F}_6 = 1/6 (F_1 + F_2 + F_3 + F_4 + F_5 + F_6)$$

WHERE:

Sketch A

Stresses for four-face hexagonal loading are nearly 20 times higher than for six-face loading for the same maximum load.⁽⁴⁻¹⁾ This factor of 20 applies to the loads at the quarter points or for load applied at the hexagonal corners. Since the maximum loads (normal to the surfaces) for the four-face loading condition are about 2 times higher than the corresponding six-face maximum six-face loads, it is estimated that the peak stresses for the internal tightener system studied here are 40 times higher than the peak stresses for the externally clamped system.

Bridging fractions* corresponding to the load distributions shown in Figure 4-1 are plotted in Figure 4-2. Also shown in Figure 4-2 are the bridging fractions determined for idealized six-face loading.⁽⁴⁻²⁾ The six-face bridging fractions determined by the hexagonal load distribution analysis compare favorably with the idealized six-face loading model.

Bridging fractions for the four-face loading system are zero except for Row 4 which was determined to be 56%. The reason for the lack of bridging in the GE-BRD internal tightener arrangement is illustrated in Figure 4-3. As is shown, there are transverse gaps at every row of channels except for Row 4 and Row 13 (the banding rods). The bridge at the banding row is questionable since the banding rod channels bear directly upon the comparatively rigid yokes.

The major conclusions of this analysis are:

- For the same applied loads the GE-BRD internal tightener system results in maximum channel stresses about 40 times higher than a corresponding externally clamped system.
- Load build-up relief as a result of bridging is virtually nonexistent in the GE-BRD internal tightener system (bridging appears only in one row of channels).

*Bridging fraction is defined as the fraction of incoming loads on the outboard surfaces of a row that are diverted circumferentially.

Figure 4-2. Bridging Coefficients

Figure 4-3. Internal Tightener Arrangement for GE-BRD Reactor Core System

If these analytic conclusions are confirmed by the analysis (now underway) of the Phase II planar restraint test data, the externally clamped core restraint concept will be recommended for future development effort, to the exclusion of the internal tightener system.

4.1.3 CRS Comparative Performance Study

A CRS comparative performance study similar to that reported by WARD⁽⁴⁻³⁾ is in progress. Review of the WARD document preliminary case studies and Phase I testing results indicate that the effect of bridging between adjacent channels has a strong influence on the forces incident to refueling and must be included in the related structural analyses to properly assess the magnitude of refueling forces. To this end, bridging capability has been added to the ANSYS⁽⁴⁻⁴⁾ computer program. The GE study will include a detailed analysis of at least two candidate core restraint systems using the effect of bridging and the core environmental parameters (i.e., temperatures and fluences) developed for the GE conceptual design of the Demonstration Plant. Property correlations to be employed in the structural analysis will include the latest GE-BRD and the HEDL reference correlations. The study will compare the system operating characteristics to the CRS design requirements. In addition, the study will compare all of the relevant core restraint systems that have been considered in past core restraint development efforts.

4.2 MECHANISM DESIGN—N. P. HANSEN

The original scope and objectives of the mechanism design activity were to:

1. Complete the design basis for 3D test components.
2. Establish the reference design for 3D core restraint tests.
3. Complete the 3D test component design.
4. Initiate 3D test component fabrication drawings.

The design basis specification for the internal tightener Core Restraint System 3D tests was issued. Further work on this activity has been terminated as a result of changes in the subtask work scope.

4.3 CORE RESTRAINT TESTING—V. R. MARIAN

The objective of the core restraint testing activity is to conduct basic tests on the HEDL Planar Clamping Model (PCM) in order to assess the effects of friction, core size, and yoke manipulation sequence on compaction and to obtain an experimental comparison of the externally clamped and internally tightened core restraint systems. The test effort is in three phases. Phase I, completed in December, used the unmodified PCM to test the externally clamped system, with channels coated with alumina to increase their coefficient of friction to a value between 0.68 and 0.75. Phase II, completed this quarter, was a test of the internally tightened core restraint system using the PCM modified to the extent required to accommodate this core geometry. Phase III, to be conducted in June, will be a test of a demonstration plant-size core using either the internal or the external system depending on a comparative evaluation of Phase I and Phase II results. Phase III requires extensive modifications to the PCM.

4.3.1 Phase I Testing

The Phase I test results and analysis were documented in a draft report submitted to RDT in March.⁽⁴⁻²⁾

All Phase I tests were run using an externally clamped array of 199 hexagonal aluminum channels each 4.719 ± 0.001 inches across flats, 0.270 inch in wall thickness and 4 inches long. Each channel was flame sprayed with alumina (Al_2O_3) to increase the interchannel coefficient of friction to a measured value between 0.68 and 0.75. Six hydraulically actuated yokes imposed inward directed peripheral loads on the core tending to compact it. Interchannel forces were measured at 33 core locations using a special instrumented channel which had been used previously at HEDL.

Significant conclusions from the test program and subsequent analysis are as follows:

- Comparison with previous HEDL PCM tests shows that, in general, less core compaction is obtained with high friction (friction coefficient, $f = 0.68-0.75$) surfaces on load pads than with low friction ($f = 0.25-0.33$) surfaces. However, the tests show that even cores with high friction load pads can be adequately compacted by proper selection of the compaction sequence.
- The optimum method for compacting the disarrayed core was found to be sequential loading of opposite yoke pairs. In this scheme equal loads are applied to two opposite yokes while the remaining four are retracted. The two loaded yokes are then retracted and another pair loaded. This sequence is repeated six times before all six yokes are loaded simultaneously. It was observed that channels between the two opposite loaded yokes formed into columns that had little or no lateral interaction; formation of the columns caused rotation of the channels into their proper orientations. This compaction technique should be equally effective for larger cores.
- Statistical analysis of the average pad forces shows the load distribution to be hydrostatic (i.e., equal forces on all pad faces) except for the outermost row of channels in contact with the yokes. Row-to-row load bridging varies from a high of 72% near core center to 7% near the edge and is independent of load level. The empirical curve determined in the tests agrees very well with the theoretical one for hydrostatic loading.
- An oversized channel produces only a localized effect on the core, producing ~100% increase in load in hexagonal forces next to the channel, but does not significantly affect bridging or transmission of load to the centermost channel.
- A disarrayed core with channel rotations is significantly more difficult to compact than one with only channel translations. However, the optimum core compaction sequence fully compacted all disarrayed cores (maximum diametral gap of 9.8 mils at 10,000 pounds yoke load).
- The instrumented channel was found to have several deficiencies. Because of its construction, it does not distort in the same manner as a standard channel. The across-flats compliance does not match that of a standard channel and is not consistent for the three pairs of load pads. Interchannel forces are best determined by averaging data taken with the instrumented channel rotated to each of its six azimuthal positions. The nontypical compliance of the instrumented channels results in the total measured load on the hexagonals in contact with a yoke being about 30% less than the applied yoke load. However, excellent force balances are obtained within the core array (where all measurements are made with the instrumented channel).

4.3.2 Phase II Testing

The Phase II tests were performed using an array of 168 high-friction aluminium channels (from Phase I) and 31 actuating internal tightener mockups. As shown in Figure 4-4 the tighteners were located at the control rod positions and an extension of that pattern near the core periphery. The only PCM modifications required for these tests were the shimming of the yokes to accommodate the internally tightened core array and the installation of an existing optical micrometer over the PCM.

Phase II consisted of load distribution tests and core compaction tests. More specifically, the following tests were performed:

1. Measurement of interchannel loads in 41 channel locations in a 90-degree pie-shaped segment of the core. These measurements were made using the HEDL instrumented channel utilized in the Phase I test series. Interchannel force measurements were made on the compacted core with all the yokes loaded uniformly to 2500, 5000, and 10,000 pounds.

- = TYPICAL ALUMINUM CHANNEL
= TYPICAL INTERNAL TIGHTENER

Figure 4-4. Location of Actuating Internal Tighteners in the Phase 2 PCM Tests at HEDL

2. Measurement of the compressibility of the compacted core. The dependence of yoke position on the uniformly applied inward loads has been determined using the yoke load cells and position transducers.
3. Examination of various internal tightener actuation sequences for compacting (tightening) the disarrayed core. Five different sequences were tested, with the yokes held in fixed positions during the tests. Tightener locations subsequent to the compaction attempts were measured using an optical micrometer. In addition, the resultant yoke load cell values were measured after compaction.

The mechanically actuated internal tightener used in Phase II was described in the previous quarterly report.⁽⁴⁻⁵⁾ Its performance throughout this test series was quite satisfactory. No problems were encountered with the actuation mechanism or the technique of actuation.

A special instrumented channel was originally planned for use in the Phase II tests, but fabrication problems and a tight development schedule prevented its being operational in time for utilization. Accordingly the HEDL instrumented channel used in the Phase I tests was incorporated into the Phase II testing. This enabled the testing to remain on schedule. The GE developed instrumented channel will be available for the Phase III tests. A comparison of interchannel forces at the same location will be made between the two instrumented channels during the Phase III tests.

The dependence of the core diameter on the yoke loading was experimentally determined for the internally tightened core. Figure 4-5 illustrates the dependence of the core diameter on the yoke loading for both the Phase II and Phase I arrays. A comparison of these two curves shows the externally clamped core to be over three times as stiff as the internally tightened core. The full implications of Figure 4-5 are currently under investigation.

Figure 4-5. Dependence of Radial Core Compression on Yoke Loading;
Phase 1 and Phase 2 PCM Tests

The five different tightener actuation sequences tested are as follows:

1. Tightener actuation in concentric rings from the center to the periphery of the core.
2. Tightener actuation in concentric rings from the periphery to the center of the core.
3. Tightener actuation in alternating concentric rings.
4. Tightener actuation from center to periphery in opposite 60 degree segments of the core.
5. Tightener actuation from center to periphery in a spiral pattern.

The important calculated results and experimental observations are presented in Table 4-1. It is seen that no one tightener actuation sequence is significantly better than another. In all cases, the average yoke load is well above the 2500 pounds measured for a perfectly compacted core with the identical core diameter (i.e., yoke positions). The various actuation sequences did not result in positioning the tighteners to better than 30 to 50 mils on the average, with a maximum misposition of 70 to 97 mils.

**Table 4-1
SUMMARY OF RESULTS OF PHASE II COMPACTION TESTS**

Actuation Sequence	Average Tightener Displacement* (mils)	Standard Deviation of Displacement (mils)	Largest Displacement (mils)	Average Yoke Load (lb)	Number of Loose Channels
Concentric rings. center to periphery	44.32	20.15	86.31	5722	1
Concentric rings periphery to center	49.39	27.30	95.48	5152	0
Alternating concentric rings	33.72	19.21	75.54	4332	0
Center to periphery in opposite segments	31.04	18.53	71.39	5184	2
Center to periphery in spiral pattern	47.67	19.33	96.46	6036	1

*Displacement from calculated location.

4.3.3 Preparations for Phase III Tests

Modifications are required to the Planar Clamping Model (PCM) to prepare it for the Phase III tests of a Demonstration Plant sized core (361 channels). These modifications include base plate extension pieces, new yoke-clevis assemblies and new hexagonal channels. These parts are in preparation. Test rig modifications will be completed in May, and the Phase III tests will be conducted in June. Phase III core restraint configuration will be determined in early May after Phase II test data have been thoroughly reviewed.

Installation of the base plate extension pieces began at HEDL immediately following the conclusion of the Phase II tests. A minor difficulty was encountered in the alignment between the existing HEDL grid plate and the six extension plates provided by GE-BRD. Alignment will be accomplished by using the shim space provided beneath the grid plate extensions combined with jacking screws in the HEDL grid plate.

The yokes are to be nitrided and will then be returned for assembly to the clevis pieces. A slight amount of rework is required on five of the six yokes to bring the profile of the jaw face into tolerance. It is anticipated that the completed yoke-clevis assemblies will be shipped to HEDL by the end of April.

The new hexagonal channels have all been completed to their finished size and are now being coated with a layer of alumina to produce the desired high coefficient of friction. Shipment of the hexagonal channels to HEDL is scheduled prior to the end of April.

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- 4-1 Appendix 4A of *Project Definition Phase Follow-On II Final Report, Task CBC 3 Core Restraint System*, GEAP-13806, December 28, 1971.
- 4-2 *Core Restraint System Tests and Analysis Phase I: Planar Externally Clamped Core*, GEAP-10798 (Draft), March 12, 1973.
- 4-3 Brown and Yant, *Comparative Performance of Three Candidate LMFBR Core Restraint Systems Considering Uncertainties in Irradiated Material Effects*, WARD-3045T2B-4, October 1972.
- 4-4 ANSYS, Engineering Analysis Computer Program (proprietary) developed by Swanson Analysis Systems, Incorporated, Elizabeth, Pennsylvania.
- 4-5 *Sodium Cooled Reactors, Core Engineering Program*, Forty-Fifth Quarterly Report, November 1972 — January 1973, GEAP-10028-45.

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5. CONTROL AND BLANKET ASSEMBLY ENGINEERING

Subtask Leader — E. L. Gluekler

This subtask has recently been redirected toward designing and qualifying Europium oxide absorber assemblies for use in the FTR and LMFBF program. The effort on boron carbide control assembly and blanket assembly engineering has been phased out. Information reported here on control and blanket assembly engineering presents a conclusion of the work performed prior to the redirection of the subtask. Requirements and bases for B_4C shim or primary safety control assemblies compiled to date are reported in Appendix C: Functions, Design Requirements, and Basis, and Appendix D: Precautions and Safety Design Features.

5.1 CONTROL ASSEMBLY ENGINEERING (B_4C)—W. J. MARGETANSKI

The objective of this work was to establish a specific concept design plan relative to Demonstration Plant needs. In this regard, the reference B_4C control assembly was evaluated in such areas as B_4C rod design, bundle thermal hydraulics, and structural performance such as the accommodation of temperature gradients and volume changes due to irradiation-induced swelling.

The reference control assembly design was limited in diameter by an internal core restraint system that utilized the control assembly channels to provide internal clamping of the core. This base design incorporated 19 wire-wrapped, 0.483-inch inside surface rods, each with a B_4C section 34.6 inches long.

In addition, a control assembly design for the case of no internal clamps was evaluated to determine the feasibility of accommodating generated helium gas with sealed plenums. For this case the outer absorber assembly diameter was increased to fit into the fuel assembly duct geometry.

5.1.1 B_4C Rod Design

The feasibility of using a sealed plenum control rod design to contain the helium gas generated due to the $B^{10}(n, \alpha)$ reaction was investigated for the noninternal clamp design in a previous study.⁽⁵⁻¹⁾ In that study the required B_4C cross-sectional area was determined by maintaining the same number of B^{10} atoms as in the reference assembly but increasing the B^{10} enrichment in the B_4C pellets from 69.5% to 90%. The result is that a smaller B_4C volume is needed in the absorber assembly. The B_4C cross-sectional area obtained on a pure volume ratio basis was 2.72 sq. in. Subsequent calculations with SN1D code considered the increased absorber self-shielding for 90% B^{10} enrichment and resulted in a minimum required B_4C cross-sectional area of 3.25 sq. in. to meet the design reactivity worth.

The sealed plenum feasibility has been reassessed accordingly. Some of the previously used controlling structural criteria were found to be too conservative and were replaced by more appropriate criteria. The maximum cladding hoop stress is limited now to one-third the nominal instead of one-third the minimal ultimate tensile strength.

A comparison of the minimum and nominal ultimate tensile strengths for 20% cold-worked Type-316 stainless steel is given in Table 5-1. The revised fission gas plenum analysis for a B_4C cross-sectional area of 3.25 sq. in. provided the following cladding internal diameter:

Diametral Gap Between B_4C and Cladding (Inches)	Rod I.D. (Inches) For:		
	19-Rod Assembly	37-Rod Assembly	61-Rod Assembly
0.009	0.476	0.344	0.270
0.015	0.482	0.350	0.276

Table 5-1
COMPARISON OF MINIMAL AND NOMINAL
ULTIMATE TENSILE STRENGTHS FOR 20%
COLD-WORKED TYPE-316 STAINLESS STEEL

Cladding Temperature (°F)	Minimum UTS (ksi)	Nominal UTS (ksi)
950	87	99
1,000	83	95
1,050	78	90
1,100	63	78

Using a 0.25-inch spacing as the required radial clearance between the control rod wrapper and the guide tube together with an 0.050-inch thick hexagonal wrapper, a 0.30-inch assembly clearance and some reasonable wire-wrap diameters resulted in the following rod outside diameters:

No. of Rods In the Assembly	Outside Surface (Inches)
19	0.701
37	0.498
61	0.386

Using these rod outside diameters together with the inside diameters determined earlier, the required sealed plenum lengths were calculated. Results are presented in Figure 5-1 for the 19-rod assembly. Results for the 37-rod and 61-rod assemblies indicated longer plenum lengths required to maintain the same radial clearance (between guide tube and the hexagonal wrapper). A conceptual design of a sealed plenum B₄C control assembly was prepared on the basis of the described analytical evaluations and is given in Figure 5-2.

In conclusion, sealed plenum control rods appear to be only practical for Demonstration Plant use when the control assembly geometry is not limited by internal core clamps.

The fission gas plenum analysis was conservatively based on a 100% helium release rate. The rate would be less if it were based on a probability distribution, and sealed plenums would become more attractive since required plenum lengths would decrease in proportion to the helium release rate.

5.1.2 Control Assembly Requirements and Bases

Requirements and bases have been compiled that are, in general, applicable to B₄C control assemblies and, in particular, shim or shim/primary safety (dual-purpose) control assemblies. These have formed the basis for control assembly evaluations undertaken in this subtask and are expected also to be a basis for future control assembly evaluations. They are reported in Appendices C and D as a final documentation of work performed prior to reorientation of the task to Eu₂O₃ control assemblies.

5.2 BLANKET ASSEMBLY ENGINEERING—A. J. LIPPS

The objective of this work effort has been to identify, plan, and conduct the development tests that are required to design a blanket assembly for an LMFBR. The work under this program has included:

1. Identification of design requirements, bases, and criteria,
2. Determination of the nuclear, thermal-hydraulic, and mechanical operating characteristics,

3. Evaluation of the effects of rotation and shuffling of blanket assemblies,
4. Identification of testing requirements and data needs.

The work on the blanket assembly is based on a typical, early LMFBR radial blanket design that is made up of a total of 180 blanket assemblies, each containing 61 rods. Each rod consists of an 0.500-inch outside surface stainless-steel tube containing a 60-inch column of natural uranium oxide pellets.

5.2.1 Prediction of Coolant Temperatures in the Radial Blanket

The coolant temperatures in the radial blanket assemblies are calculated with the FULMIX computer code. This code used an assumption to calculate the flow distribution that is not valid for the low flow rates of the radial blanket assemblies along the outer edge of the reactor. The flow can be laminar, transitional, or turbulent depending on the flow rate. A model to determine flow distributions at low flow rates was discussed in the previous quarterly report;⁽⁵⁻¹⁾ however, the model had convergence problems in the transitional region. This problem was corrected by using a constant exponent for the friction factor throughout the transitional region.

The flow distribution model uses four flow regimes that were selected to correspond to the applicable friction factor correlations. The rod bundle is represented by three types of flow subchannels, interior, edge, and corner. The effect of the wire wrap on the effective friction factor is assumed to be the same for each subchannel type so that the flow distribution is not affected by the wire.

Figure 5-1. Required Sealed Plenum Length for 0.482 I.D. Rod Control Element

Figure 5-2. Conceptual Control Assembly

This model was incorporated into the FULMIX computer code, and the results were compared with the experimental results of the ORNL-FFM tests. The results of three cases are shown in Figures 5-4, 5-5, and 5-6, using the channel numbering scheme shown in Figure 5-3. The cases selected have a power gradient across the bundle where the highest power rods are three times higher than the low power rods, and the flows are in the laminar and transitional flow regimes. In all cases, the computer results are higher than the experimental results on the hot side and lower on the cold side. Free convection is ignored in the FULMIX code. However, the flow rates are high enough that the free convection is small relative to forced convection ($Gr/Re^2 \approx 0.02$). The disagreement may be due to a large effect of edge swirl around the periphery of the bundle. Edge swirl would lower the hot edge temperature with cooler flow from the cold side, and it would also raise the coolant temperature on the cold side. The FULMIX code has an option to consider flow sweeping around the fuel rod that has shown good agreement at high flow rates. However, the flow sweeping option does not appreciably change the results of the turbulent interchange option shown in the figures for the low flows because the sweeping around the bundle periphery cannot be adjusted separately from the sweeping around each rod. Figure 5-6 also shows the results of other computer codes⁽⁵⁻²⁾ for the same test conditions. The results of the codes that do not consider edge swirl separately are similar to FULMIX. Only the COTEC code, which considers edge swirl, gives good agreement with the test data.

5.2.2 Nuclear Analysis of the Radial Blanket

The assembly power for the blanket region has been calculated by two methods, an X-Y and an R-Z diffusion theory method. Different results for the power distribution were obtained with both methods. The discrepancy is attributed to the presence of the irregular core boundary included in one of the calculations. In each case, the isotopic compositions of the blanket assemblies, by region, were the same values. In Figure 5-7, the blanket power distribution was obtained directly from an X-Y diffusion theory calculation treating the individual assembly shapes as hexagonal. The core boundary appeared expressly as an irregular surface. In Figure 5-8, the radial power distribution from an R-Z diffusion theory calculation was used as the basis for power distribution among the individual assemblies. The distribution was calculated from the radial position and refueling region designated for each assembly. The core was represented as an equivalent cylinder with the blanket regions as equivalent annuli.

Figure 5-3. Channel Numbering Scheme

Figure 5-4. Comparison of Computer and Test Results for Inlet Temperature of 599°F and Flow Rate of 890 lb/h

Figure 5-5. Comparison of Computer and Test Results for Inlet Temperature of 614°F and Flow Rate of 930 lb/h

Figure 5-6. Comparison of Computer and Test Results for Inlet Temperature of 603°F and Flow Rate of 1775 lb/h

- RESIDENCE TIME-AVERAGED FUEL COMPOSITION
- NO CONTROL RODS IN CORE
- END OF CYCLE CONDITIONS

Figure 5-7. Blanket Assembly Power Peaking by X-Y Calculation

- RESIDENCE TIME-AVERAGED FUEL COMPOSITION
- NO CONTROL RODS IN CORE
- END OF CYCLE CONDITIONS

Figure 5-8. Blanket Assembly Power Peaking by R-Z Calculation

By comparing the differences in the calculations in Figure 5-9, sizable discrepancies in the power distribution are revealed. The cylindrical representation of core and blanket region boundaries gives a 10 to 24% higher peak along the core boundary than the hexagonal model.

The fuel cycle depletion was determined via a cylindrical core geometry with annular blanket regions. It is expected from the results shown in Figure 5-9 that a depletion calculation with hexagonal geometries will slightly change blanket assembly power distribution.

5.3 Eu_2O_3 ABSORBER ASSEMBLY DEVELOPMENT—E. L. GLUEKLER

The objective of this activity is to design and qualify an Eu_2O_3 absorber assembly for testing in FTR as a part of the FTR safety system. The work under this program will include the following activities:

- Definition of program requirements and priorities for work to be conducted by national laboratories in support of the FTR design basis, such as Eu_2O_3 material properties evaluation, irradiation testing, critical experiment and heating rate measurements, and flow and vibration testing of a Eu_2O_3 absorber assembly.
- Design of a Eu_2O_3 absorber assembly for FTR, including detailed analysis. The assemblies will be designed to meet the requirements of SDD-31 and to be compatible with possible use in demonstration or commercial LMFBF plants.
- Specifications and drawings suitable for procurement of a Eu_2O_3 assembly for use in FTR.

Figure 5-9. Power Peaking Percent Difference of R-Z Calculation Above X-Y Calculation

REFERENCES

- 5-1 *Sodium-Cooled Reactors*, Core Engineering Program, Forty-Fifth Quarterly report, November 1972 — January 1973, (GEAP-10028-45).
- 5-2 Radial Blanket Design and Development Quarterly Report for the period ending May 31, 1972, W—ARD-3045T2B-2.

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6. DEFECTED FUEL TESTING

Subtask Leader — P. E. Bohaboy

6.1 DEFECTED FUEL TESTING—P. E. BOHABOY

The use of a well built sealed-plenum fuel rod provides a high level of assurance that the fission products in the fuel will be isolated from the primary cooling system should failure occur. If failure occurs, adequate information must be immediately available to the plant operator permitting him a choice of alternatives for continued plant operation. Information on the post-failure performance of failed fuel in support of the reactor design, operating limits, and safety analysis is therefore of primary importance in determining what these alternatives should be.

The specific purposes of this subtask 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 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 and post-irradiation examination of the capsule was completed in September 1972. An interim report which summarizes the irradiation conditions and the post-irradiation examination data of the B9D experiment has been published.⁽⁶⁻¹⁾ Work has continued on a final topical report which will concentrate on an engineering analysis of the experimental data.

6.1.2 Sodium Fuel Reaction Study (B11 Capsule Series)

The B11 series of capsules is 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 capsules in the series; they are designed to investigate the effect of total oxygen availability on the rate of diameter increase of failed fuel rods. The fuel rod in each capsule contains a longitudinal notch defect in the cladding which permits defecting to occur at power. Differential thermal expansion of the fuel and cladding coupled with a small initial fuel-cladding gap is used to cause rupture of the fuel rod in the notched region. Details of the capsule and fuel rod design have been presented previously.⁽⁶⁻²⁻³⁾

At selected time intervals following failure of each fuel rod, the capsules were removed from the reactor and neutron radiographed along with a fuel rod standard of identical construction. Data from the neutron radiographs provided information on the rate at which the fuel rod diameter increased. The capsules were removed from the reactor after 2 minutes, 30 minutes, 2 hours, 2 days and 8 to 10 days of irradiation. The status of each capsule is summarized below:

- Capsules B11A and B11B have completed their scheduled irradiation and the post-irradiation examination of each capsule is currently in progress. Total fuel burnup accumulated in the failed condition was 3400 MWd/Te for B11A and 4100 MWd/Te for B11B.
- Capsule B11B has completed a 10 day post-failure irradiation. Additional irradiation of this capsule is scheduled in order to observe the effects of continued irradiation on the diameter increase of a failed mixed-oxide fueled rod.

Figures 6-1 through 6-6 show the measured diametral expansion of the B11A, B, and C fuel in both the 0-degree and 90-degree orientations at the end of each irradiation interval. The diametral expansion of the fuel at the center of the fuel column corresponds to the original defect location. Diametral changes observed beyond the boundaries of the original cladding defect indicate that the defect has propagated along the entire length of the fuel rods. In general, after the cladding split axially along the length of the fuel column, the newly exposed fuel shows the same swelling characteristics exhibited by the fuel located in the initial defect region (i.e., swelling peaks of comparable magnitude and with rates of formation similar to the original defect region are observed). This suggests that axial migration of oxygen within the fuel does not occur at a rate large enough to significantly affect the localized rate of fuel swelling. The axial concentration of oxygen in the fuel rod does not appear to be substantially altered by the presence of the original defect.

Fuel Rod B11A (O/M 2.00) showed no further expansion of the fuel along its length after completion of the 2 day irradiation interval. Figures 6-1 and 6-2 show that the B11A diameter profiles are nearly the same following the 2 day and 8 day irradiations. This result indicates that the last of the oxygen available for reaction was consumed between the 2-hour and 2-day irradiation intervals (see Figure 6-7).

Unlike the B11A fuel, the B11B fuel (1.97 O/M) was still increasing in diameter at the end of 10 days of irradiation, indicating that all of the oxygen available for reaction had not been consumed in the formation of sodium fuel-reaction product.

Comparison of Figures 6-3, 6-4, 6-5 and 6-6 show that the B11B (O/M 1.97 and 5-inch fuel column) and B11C (O/M 1.97 and 3-inch fuel column) fuel were swelling at essentially the same rate. Differences between the measured maximum fuel diameters for B11B and B11C at each of the irradiation intervals are almost within the ± 0.001 -inch accuracy of the measuring technique. This observation further substantiates the earlier conclusion that axial migration of oxygen within the fuel column does not significantly affect the rate of sodium fuel reaction.

Significant quantities of fuel fallout from the original defect region were observed in the neutron radiographs of B11A and B11B taken after the 8-day and 10-day irradiation intervals. Relocated fuel particles and/or fuel sodium reaction product are clearly visible in the sodium annulus around each fuel rod. This fuel fallout is evident in the diameter measurements reported in Figures 6-2 and 6-4. A significant localized reduction in the maximum fuel diameter in the region of the original defect for each fuel rod was measured. Since the sodium coolant in the B11 capsule design is stagnant, the fuel fallout is not the result of flowing coolant erosion. Throughout the irradiation the purity of the sodium was maintained below an estimated 1 ppm of oxygen by a uranium metal getter. The data indicate that a high purity environment does not prevent the initial formation of fuel sodium reaction product. However, the high purity sodium may be contributing to the deterioration of the reaction product by creating a thermodynamically unstable environment for the reaction product.

The maximum diametral expansions measured for each of the fuel rods at the end of each irradiation interval are given in Table 6-1 and plotted in Figures 6-7 and 6-8. Figure 6-7 shows a plot of the maximum diametral increase measured for fuel rod B11A. Included for comparison are the data from earlier defected fuel experiments E1J-B2⁽⁶⁻⁴⁾ and B9A.⁽⁶⁻⁵⁾ The E1J-B2 rod contained fuel of 2.00 O/M. As can be observed from the curves in Figure 6-7, the B11A and E1J-B2 fuel expanded at similar rates although the B11A curve is displaced upward compared to the E1J-B2 curve. The similar shapes to the curves indicate that the same mechanism controlled the rate of reaction product formation in both fuel rods. The displacement of the B11A curve with respect to the E1J-B2 curve is attributed to the higher surface temperature of the B11A fuel rod. The surface temperature in the B11A fuel rod was approximately 230°F (128°C) higher than the E1J-B2 fuel rod. Logically it would be expected that the higher temperature would result

Table 6-1
SWELLING RATE DATA FOR
FUEL RODS B11A, B, AND C

Fuel Rod	O/M	Post-Failure Irradiation Interval	Average of Maximum Diametral Increase Measured in the 0- and 90-Degree Orientations (Mils)	Increase in Fuel Diameter (%)
B11A	2.0	2 minute	11.2	5.1
		30 minute	19.5	8.9
		2 hour	28.9	13.2
		2 day	46.2	21.1
		8 day	46.2	21.1
B11B	1.97	2 minute	8.2	3.7
		30 minute	10.2	4.7
		2 hour	10.9	5.0
		2 day	16.3	7.4
		10 day	21.1	9.6
B11C	1.97	2 minute	6.1	2.8
		30 minute	12.8	5.8
		2 hour	13.8	6.3
		2 day	18.0	8.2

in an initially higher sodium-fuel reaction rate. The E1J-B2 fuel was irradiated in a sodium/potassium coolant while the B11 fuel rods are cooled by pure sodium. This is the only major design variation between the B11A and E1J-B2 experiments. From the results it appears that the rates of sodium-fuel reactions and the rate controlling mechanisms are similar for both NaK and sodium coolants.

The maximum fuel swelling found in the top fuel rod of capsule B9A (O/M=1.99) agrees with the maximum swelling obtained in B11A. Comparison of the data indicates that the oxygen in both the B9A and B11A fuel was consumed by formation of reaction product after approximately 30 hours (1800 minutes) of irradiation. The neutron radiographs of B11A indicates that no appreciable fuel swelling occurred beyond 30 hours of post-failure operation.

Figure 6-8 compares the maximum localized fuel swelling rates for the B11A, B and C fuel rods. As discussed previously, the rate of reaction product formation in B11B and B11C were essentially identical even though their fuel column lengths differed (5 inches and 3 inches, respectively). A major difference, however, does exist between the swelling rate for B11A and the rate for B11B and B11C. A curve of the experimental data shows the B11A swelling rate to be approximately twice that for B11B and B11C. This difference in reaction rates indicates that different rate-controlling mechanisms may be operative in these fuel tests. Since all three fuel rods operated at the same linear power (22 ± 1 kW/ft), the differences in performance are probably directly related to the differences in fuel stoichiometry. The mechanism(s) controlling the rate of fuel-sodium reaction product formation may be strongly influenced by the quantity of oxygen available for the reaction. It is possible that for the B11A fuel, the rate of reaction is controlled by the diffusion of sodium from the fuel to the reaction layer boundary. for the B11B and B11C fuel where the quantity of available oxygen is substantially reduced, diffusion of oxygen to the reaction layer boundary may be the dominant factor in controlling the rate of fuel-sodium reaction.

Figure 6-1. Measured B11A Fuel Profile in the Plane of the Defect as a Function of Time of Irradiation after Fuel Rod Failure

Figure 6-2. Measured B11A Fuel Profile 90 Degrees to the Defect as a Function of Time of Irradiation after Fuel Rod Failure

Figure 6-3. Measured B11B Fuel Profile in the Plane of the Defect as a Function of Time of Irradiation after Fuel Rod Failure

Figure 6-4. Measured B11B Fuel Profile 90 Degrees to the Plane of the Defect as a Function of Irradiation after Fuel Rod Failure

Figure 6-5. Measured B11C Fuel Profile in the Plane of the Defect as a Function of Time of Irradiation after Fuel Rod Failure

Figure 6-6. Measured B11C Fuel Profile 90 Degrees to the Plane of the Defect as a Function of Time of Irradiation after Fuel Rod Failure

Figure 6-7. Measured Maximum Fuel Diametral Change for Fuel Rods B11A, E1J-B2, and B9A-1

Figure 6-8 Measured Maximum Fuel Diametral Change for Fuel Rods B11A, B11B, and B11C

The post-irradiation examination of the B11A and B capsules was initiated. Gross gamma scanning of the two capsules was completed. The gross gamma scans were not atypical of irradiated fuel. The B11A gross gamma scan indicated the presence of a very small amount of fuel at the bottom of the primary fuel rod containment. As previously mentioned, the neutron radiographs of B11A and B11B indicated that some fuel fallout occurred at the center of the fuel column in both rods. The secondary containment has been removed from both capsules and the primary containment is in the process of being disassembled. Steps are being taken to ensure preservation of any reaction product within the fuel rods, as well as exterior to the rods during the examination.

6.1.3 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.

All work associated with this test program was stopped in February. This action is consistent with scope changes resulting from AEC-RDT guidance. Resources previously devoted to this experimental work have been redirected into the Sodium Fuel Reaction Study (Section 6.1.2) in an attempt to expedite and complete the post-irradiation examination of capsules B11A, B and C in FY-73.

REFERENCES

- 6-1 Bohaboy, P.E., et al., *Capsule B9D Irradiation and Post-Irradiation Examination*, December 1972, (GEAP-13925).
- 6-2 *Sodium-Cooled Reactors Fast Ceramic Reactor Development Program*, Forty-Third Quarterly Report, May-July 1972, August 1972, (GEAP-10028-43).
- 6-3 *Sodium-Cooled Reactors Fast Ceramic Reactor Development Program*, Forty-Fourth Quarterly Report, August-October 1972, November 1972, (GEAP-10028-44).
- 6-4 *Sodium-Cooled Reactors Fast Ceramic Reactor Development Program*, Thirty-Fourth Quarterly Report, February 1970 — April 1970, May 1970, (GEAP-10028-34).
- 6-5 Murata, R. E., et al., *Effect of Stoichiometry on the Behavior of Mixed-Oxide Fuel During Extended Operation in Failed Pins*, July 1971, (GEAP-13730).

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7. QUALITY ASSURANCE

7.1 QUALITY ASSURANCE PROGRESS AND ACCOMPLISHMENTS

The major activity of Quality Assurance during this period has been the review of program planning, engineering planning, engineering, and procurement documents for Task 893/11. Surveillance of fabrication, review of data packages, surveillance of shipping activities and coordination of activities at EBR II were performed for specific hardware items.

BRD-QA contributed to the investigation of Incident Report HFEF-35 by EBR II on F9C, Serial No. X/O 143, subassembly damage to element.

Quality Assurance Engineering participated in the L-15 Examination Review Meeting, F9A and F9D-Task 2 Review Meeting and Document Review of wire-wrap technology program for Subtask 3.

In addition, Quality Assurance Engineering participated in the final design review for the L-16E stress relaxation irradiation test.

Design and test documentation was reviewed for grid spacers and for grid beam and spacer prototype design testing. Plans were initiated for Quality Assurance participation in qualifying the electrode discharge machining (EDM) process for channel notching.

The status of F20 capsules prior to completing inspection was reviewed March 23rd. The fabrication documentation was reviewed by Quality Assurance and Engineering and judged satisfactory. An unsatisfactory condition, however, was found in the fabrication specification for the F20 encapsulation. X-rays before and after rolling the capsule closure welds were not required by the specification. A change to the EBR II Experimenter's Guide, May 1972, requires these X-rays if rolling is approved. A waiver of this requirement was granted by the EBR II Project.

H. Stock and R. Young visited HEDL and established quality interfaces with HEDL for Task 4 work.

Test plans for Phase II series A1-2 tests at HEDL were reviewed with HEDL QA. Initiation of tests was approved, with HEDL QA performing surveillance. A request was received from HEDL for as-built inspection dimensions on yokes for modifying the planar clamping model for Phase III tests. Planning was done to record the requested dimensions during inspections after part nitriding.

BRD-QA Source Inspection was performed on the preproduction run of aluminum hexagonal channel segments at the vendor's plant of Clark and Wheeler Engineering. This inspection identified the need to modify the vendor's manufacturing process to bring the channels within print tolerance. This source will be inspected in late April to assure that all processing problems have been corrected.

Fabrication of hardware for the reencapsulation of the L-16D experiment specimens (65 tube to rod plug assemblies) was completed. The final inspection of the hardware was completed under BRD-QA surveillance. The assemblies have been cleaned, bagged and are currently stored in a Quality Assurance Material Control Area awaiting establishment of shipping dates.

Documentation reviews were completed for post irradiation examinations of fuel at LASL and also for materials test capsules at LASL and RHO, Vallecitos. An Item Release was issued to provide for the shipment of ten L16B capsules from HFEF, ANL Idaho to LASL.

Review of planning for interim examination of GE subassemblies at ANL West, HFEF showed that minimum requirements of RDT Standard F2-2T, as interpreted by BRD-QA, were not being met. GE requested work plans that include instructions to be used in conducting the examinations and provisions for verification of operations and Quality Control. These were requested for review and approval to meet requirements of F2-2T. R. S. Young, BRD-QA Representative, met with R. J. Teunis, Manager HFEF and R. H. Lykken, Manager QA-ANL West during a trip to ANL-West. During these meetings agreement, satisfactory to all parties, and in accordance with F2-2T, was reached.

7.2 NONCONFORMANCES ACTIVITIES AND PROBLEMS

Quality System has modified the BRD Nonconforming Item Reporting Instruction to reflect the deletion of Amendment 1 to RDT Standard F2-2T. (Reference: Letter, C. V. Backlund to J. Barnard, Disposition of Nonconformances for PA-10, Contract AT(04-3)-189, dated January 24, 1973). The nonconformances listed below were determined to be "minor."

NIR 0253 dated February 8, 1973 reported six Rod Clevis received from CDS Engineering as exceeding drawing limits on thread depth, finish and outside surface not finished cut. First level disposition was made to reject and return material to the vendor for completion to drawing.

NIR 0254 dated February 12, 1973 reported rods received from CDS Engineering as one part having damaged lead threads and six parts exceeding surface finish on the 1.500 diameter. First level disposition was made to reject and return the one damaged thread part to the vendor and to accept the other six parts.

NIR 0255 dated February 12, 1973 reported two base plate segments received from CDS engineering as exceeded drawing length of $79 \pm 1/16$ inches by $1/4$ inch. First level disposition was made to accept both parts.

NIR 0258 dated March 23, 1973 reported one rod as having undersize threads (accepted the "NO GO" thread gage) on the first inch of the rod. This rod had been previously shipped by the vendor, CDS Engineering Inc., and subsequently rejected by BRD for thread problems on NIR's 0254 and 0257. First level disposition was made on NIR 0258 to accept.

As reported failure of fuel rods F0B and F0D in GE subassembly XG04B was received from the EBR II Irradiations Office based on examination of neutron radiographs. A meeting was held March 5, 1973 and established the occurrence as a failure with probable relationship to a power increase in EBR II during run 56 after installation of a stainless-steel reflector. An Interim Failure Report (001) was generated to document the failure and the corrective action which will result from the investigation. During subsequent interim examinations of three subassemblies at EBR II, eight more fuel elements were identified as failed. Due to the similarities in the suspected cause and anticipated corrective actions, all failures will be included in the investigation of Failure Report 001.

An audit was conducted at CDS Engineering to investigate this vendor's system for controlling product fabrication, inspection and acceptance. Emphasis was placed on those specific nonconformances found and reported during BRD Receiving Inspections. Deficiencies were found to exist in a number of areas. Future purchase orders will not be provided to this vendor until corrective action for the deficiencies noted has been verified by BRD-QA.

APPENDIX A. PRIOR REPORTS

Prior progress reports to the Commission under this contract include:

Monthly Progress Letters, Nos. 1 through 121, from July 1959 through March 1973.

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

GEAP-3888
First Quarterly Report
October–December 1961

GEAP-3957
Second Quarterly Report
January–March 1962

GEAP-3981
Third Quarterly Report
April–June 1962

GEAP-4080
Fourth Quarterly Report
July–September 1962

GEAP-4158
Fifth Quarterly Report
October–December 1962

GEAP-4214
Sixth Quarterly Report
January–March 1963

GEAP-4300
Seventh Quarterly Report
April–June 1963

GEAP-4382
Eighth Quarterly Report
July–September 1963

GEAP-4480
Ninth Quarterly Report
October–December 1963

GEAP-4601
Tenth Quarterly Report
January–March 1964

GEAP-4640
Eleventh Quarterly Report
April–June 1964

GEAP-4723
Twelfth Quarterly Report
July–September 1964

GEAP-4795
Thirteenth Quarterly Report
October 1964–January 1965

GEAP-4860
Fourteenth Quarterly Report
February–April 1965

GEAP-4931
Fifteenth Quarterly Report
May–July 1965

GEAP-4982
Sixteenth Quarterly Report
August–October 1965

GEAP-5098
Seventeenth Quarterly Report
November 1965–January 1966

GEAP-5158
Eighteenth Quarterly Report
February–April 1966

GEAP-5198
Nineteenth Quarterly Report
May–July 1966

GEAP-5292
Twentieth Quarterly Report
August–October 1966

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

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

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

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

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

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

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

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

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

GEAP-10028
Thirtieth Quarterly Report
February–April 1969

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

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

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

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

GEAP-10028-44
Forty-Fourth Quarterly Report
August–October 1972

GEAP-10028-45
Forty-Fifth Quarterly Report
November 1972–January 1973

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)

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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
ANSYS	=	A Finite Element Structural Analysis Code
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
CRS	=	Core Restraint System
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
EDM	=	Electrical discharge machining
EM	=	electromagnetic
ENDF/B-II	=	evaluated nuclear data file/B, version II
ENDRUN-2	=	computer code for cross section processing version 2
EOC	=	end of cycle
E.o.L.	=	end-of-life
ETOE-MC ²	=	computer codes for cross section processing
ETR-FEPL	=	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)
PEFT	=	Probabalistic evaluation of Fuel Temperature
PECS	=	Probabalistic Evaluation of Cladding Stress—A computer code

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
SNID	=	A computer code
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
wt %	=	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

APPENDIX C

FUNCTIONS, DESIGN REQUIREMENTS, AND BASIS FOR B₄C CONTROL ROD ASSEMBLIES (W. J. MARGETANSKI)

C.1 FUNCTIONS

Two types of control assemblies are defined: shim/primary safety and alternate shutdown. The distinction between each type of assembly is in its operation and drive design. The primary function of the shim/primary safety control assemblies is to control the reactor during all normal modes of operation (shim function) as well as provide rapid reactor shutdown during normal and abnormal operating conditions (safety function). The function of the alternate shutdown assemblies is to provide independent rapid reactor shutdown during normal and abnormal operating modes should the shim/primary safety assemblies fail to function properly.

The control assembly consists of the control element (absorber) and the duct assemblies. The control element (absorber assembly) is the movable portion of the overall assembly. Its motion is used in a control function to start up and shut down the reactor, to make adjustments in power level, and to compensate for reactivity changes during operation. The shim/primary absorber assemblies also provide an independent source of shutdown reactivity. The duct assembly is primarily used to isolate the absorber assembly in a separate container from the fuel assemblies. The duct assembly consists of a nosepiece similar to that of the fuel assemblies, which contains an orifice for regulating the flow through the assembly as well as acting as a neutron shield. A hexagonal outer container, of the same outer dimensions as the hexagonal fuel assembly channel, and an inner guide member provide the control element (absorber assembly) with a path for movement. Although many requirements would be the same for both types of absorber assemblies, the requirements and bases presented here are specifically addressed to the shim/primary safety control assemblies.

C.2 DESIGN REQUIREMENTS AND BASES

C.2.1 Configuration

Requirement—The control assembly configuration shall be compatible with the core design.

Each control assembly consists of an external, hexagonal, stainless-steel duct assembly that provides a separate coolant flow path for the control element that it contains. Its external dimensions shall be consistent with the blanket and fuel assembly hexagonal stainless-steel ducts. The duct assembly shall act as a guide for the movable absorber assembly providing a path for movement. (The guide and the hexagonal channel could be one and the same). The lower portion of the control assembly shall include the nosepiece and neutron shield sections. The nosepiece shall provide flow orificing for the control assembly and the shield section neutron shielding for the core support structure. The nosepiece shall be tapered and sized so as to provide for positioning and guidance of the control assembly as it is lowered into position in the lower grid plate.

The radial spacing between the duct or guide tube and the control element shall be maintained by utilizing wear pads located on the upper and lower ends of the control element. The control element shall consist of a hexagonal, stainless-steel tube that surrounds the neutron absorber bundle and provides a separate coolant flow path for the poison rods. This tube shall extend the full length of the rods and be welded to the control element end assemblies at both ends. All poison rods shall be spaced in triangular arrays utilizing wire wraps. The wire shall be wrapped around the rods in a helical form and welded at each end plug. Each rod shall consist of a hollow, cylindrical, stainless-steel tube containing B₄C pellets in its lower region and provisions for accommodating helium gas [generated by the B¹⁰(n, α) reaction] above and below the B₄C region. Stainless steel end plugs shall be welded at the top and bottom ends of the rods. The length of the neutron absorber (B₄C) section shall be at least as long as the fuel column length and also shall be such that the worth does not decrease in case the rod is decoupled from its drive, or in case of overtravel upon insertion.

Basis—The control assembly configuration must be integrated into the core design. In order to provide a compact core arrangement capable of being clamped and responding to the individual coolant flow requirements, an array of hexagonal channels is used to house the fuel, blanket, and control assemblies. The size of these hexagonal channels is influenced by the size of the fuel assemblies. The hexagonal tubes in which the absorber rods are housed form the outer containment for the control assemblies. To this is attached the nosepiece (with orificing and shielding) and the guide tube which guides the movement of the control element. Optimum use of available space for absorber material indicates a hexagonal array of poison rods for the control element. Other features of the control assembly are consistent with the design requirements.

C.2.2 Performance

Requirement—The control assemblies in conjunction with the control drives shall provide the ability to regulate and control the reactor power output throughout the design lifetime of the control assemblies. The control assemblies shall be designed to operating limits consistent with the nuclear design specifications.

Basis—The control assemblies are responsible for regulating and controlling reactor power. Their performance must provide reliable protection for the public and adequate plant availability. In order to achieve these goals, the control assemblies must perform within specified limits and at specified conditions. These limits and conditions are specified once the plant and core designs are set.

C.2.3 Installation

Requirements—The control assembly shall be capable of withstanding the temperature transients that it undergoes during installation into the reactor without exceeding structural limits. The control assembly shall be designed to allow detection of and/or prevention of placing the assembly into an orificing zone not compatible with its cooling requirements. The control assembly shall be marked to assure positive identification before installation into the core.

Basis—Control assemblies will be replaced when the reactor is at refueling conditions. They should be capable of undergoing the thermal shock associated with insertion into a 400°F sodium pool without exceeding structural limits.

Because of the core design, control assemblies meant for different locations will have different worths and, hence, different B^{10} enrichments. It is, therefore, important that these control assemblies are not interchangeable either with each other or with the fuel assemblies. Each individual assembly, fuel or control, has unique coolant flow requirements that are based on the power generated for the particular assembly in its planned location. By appropriate mechanical design and procedural control, inadvertent substitutions can be avoided.

C.2.4 Thermal/Hydraulic/Shielding

The lower portion of the control assembly includes the nosepiece and neutron shield sections. The nosepiece shall provide flow orificing for the control assembly, and the shield section shall provide neutron shielding for the core support structure.

C.2.4.1 Cladding Hot Spot Temperature

Requirement—The operating temperatures of the control assemblies shall be controlled so as not to exceed a cladding hot spot temperature that would reduce the design lifetime.

Basis—Cladding thickness is set by consideration of cladding wastage allowances, internal and external loading, design lifetime, and design temperature. The cladding hot spot temperature is the maximum cladding temperature. This cladding hot spot temperature shall be verified as being less than the design temperature by analysis.

C.2.4.2 Cladding-Absorber Interface Temperature

Requirement—The normal operating cladding-absorber interface temperature shall be limited so that excessive absorber-cladding interaction does not occur.

Basis—Studies and tests on the compatibility of B₄C with stainless steel have shown the B₄C interacts with stainless steel to a depth that depends on the exposure time, interface temperature, and type of stainless (the higher the nickel content, the less compatible with B₄C). This interaction results in a surface scale of reaction products that is accompanied by regions of high porosity and carburization. These effects collectively reduce the cladding thickness available to carry the required loadings. The interface temperature is the dominant factor in determining the depth of penetration of the cladding surface. Therefore, in order to limit the effect on the structural capability of the cladding (which, in turn, affects lifetime), the interface temperature should be controlled to allow no more than a certain acceptable cladding reaction depth over the control assembly lifetime. This can be accomplished by adjusting the control assembly coolant flow to provide this condition.

C.2.4.3 Maximum Absorber Temperature

Requirement—The maximum temperature of the absorber during normal operation and design transients shall include a design margin resulting in a temperature below melting, taking into account uncertainties in the temperature prediction.

Basis—The maximum absorber temperatures must be maintained below the absorber melting point in order not to have a redistribution of the absorber material that would affect the worth of the rod and compromise the ability to shut down the core when required.

C.2.4.4 Shielding

Requirement—The control assembly will include neutron shielding in the lower axial position separating the core and support plate to limit the total neutron fluence at the upper section of the support plate to an acceptable level at the end of the projected 30-year plant lifetime.

Basis—The core support is designed so that it is functional, without requiring replacement for the 30-year plant lifetime. In order to prevent neutron streaming from the control assembly locations, the control assemblies will be required to have appropriate shielding in their nosepieces. Continued radiation exposure is known to cause loss of ductility and geometrical instability (swelling, creep). In order to prevent these types of damage, the amount of shielding shall be such that the total fluence the core support plate sees (from the control assembly locations) over its 30-year lifetime shall be limited to some acceptable value.

C.2.4.5 Control Assembly Vibrations

Requirement—The coolant velocities and velocity distributions required for cooling shall be less than those that would result in damaging vibrations in any part of the control assembly.

Basis—Depending on coolant flow velocities and control assembly design, flow-induced vibrations can approach natural frequency conditions in the control absorber assembly. Since a natural frequency vibration can be of large amplitude, the absorber assembly could fail if allowed to continue vibrating at natural frequency for any length of time. In order to prevent failure or damage by flow-induced vibrations, the control assembly design shall be analyzed to show that amplitudes resulting from such vibrations will be limited (to some safe value below the damage threshold) to prevent the interaction of rods, wires, or components that would cause damage to the control assembly and prevent the control element from performing its intended function.

C.2.4.6 Poison Rod Spacing

Requirement—The poison rod spacer system shall position the poison rods in such a manner as to provide adequate cooling to all poison rods, cause minimal increases in the local cladding temperature, and promote mixing of the coolant flow within the assembly.

Basis—The spacing of poison rods is important for proper distribution of the coolant flow. If the poison rods were allowed to bend and distort unrestricted by spacers, inevitably two or more rods would come in contact such that coolant flow in these areas would be considerably reduced. Rod temperatures in these flow-restricted areas would increase, resulting in larger thermal gradients. This, in turn, would result in more rod distortion, further reducing the coolant flow and increasing poison rod temperatures.

C.2.5 Mechanical and Materials

Each poison rod is a hollow, cylindrical tube of stainless steel containing a central region of B_4C control material. Above and below this is a sealed plenum that is designed to accommodate the helium gas generated by the $B^{10}(n, \alpha)$ reaction. The poison rods are sealed off at each end with end plugs. The bundle of poison rods is assembled in a triangular array with end plates and a hexagonal wrapper to form the movable absorber assembly. The absorber assembly end plates also provide end support for the poison rods and the hexagonal wrapper. Additional features of the assemblies are: springs within the rods to position the B_4C pellets and spacing to allow axial thermal expansion, control coolant holes penetrating through the end assemblies, and control element wear pads surrounding the end assemblies, which provide radial stability within the guide tube and minimize bypass flow. Additionally, a damper that arrests the motion of the absorber assembly at the end of a scram can be designed as part of the absorber assembly or part of the drive line.

C.2.5.1 Cladding Thickness

Requirement—Cladding thickness requirements shall be established so that operating and transient loads do not result in exceeding the structural design criteria.

Basis—Cladding thickness is set by consideration of cladding wastage allowances, internal and external loading, design lifetime, and maximum cladding temperature. Another consideration is the cross-sectional area left available for the absorber material (i.e., the thicker the cladding, the smaller the available absorber material cross-section). The intent is to make the cladding sufficiently thick as evaluated by the structural design criteria and analysis to prevent cladding failure any time during the life of the control assembly.

C.2.5.2 Control Assembly Nosepiece Design

Requirement—All control assembly nosepieces shall be designed to prevent insertion of an absorber assembly in a wrong location.

Basis—See basis for installation requirements (C.2.1).

C.2.5.3 Control Assembly Grapple

Requirement—The grapple used in handling the control assembly shall be of different design than the grapple used for the fuel and blanket assembly bundles.

Basis—See basis for installation requirements (C.2.1).

C.2.5.4 Control-Element-to-Guide-Tube Radial Spacing

Requirement—The control-element-to-guide-tube radial spacing shall be sufficient to enable the control assembly to move as required during reactor operation by precluding contact at other than the wear surfaces.

Basis—During reactor operation there are normally thermal and flux gradients that are set up across the control assemblies. The flux gradients are caused by the core flux shape, the location of the control assemblies within the flux shape, and local perturbations due to self-shielding. Thermal gradients are caused by temperature differences in the surrounding fuel assemblies and heat generation gradients in the control assemblies. These factors, in combination

with irradiation-induced swelling and creep and thermal expansion, cause the channel and duct (or guide tube) to bow. In like manner, flux and thermal gradients across the moving absorber element cause distortion.

In order to meet safety requirements, the control elements must be capable of scrambling from any axial position any time during operation of the reactor. Therefore, it is necessary that the radial spacing between the guide tube and control element be such that, even with the worst anticipated combinations of bowing and distortion any time during the life of the control assembly, the control element will not be restrained from scrambling.

C.2.5.5 Component Material

Requirement—The structural components of the control assembly shall be fabricated from Type-316 stainless steel, 20% cold worked or Type-316 annealed, as required for individual parts except where locally, specially hardened surfaces may be required to prevent galling.

Basis—These materials are compatible with the typical LMFBF core environment.

C.2.5.6 Absorber Material

Requirement—The absorber material shall be B¹⁰-enriched B₄C in pellet form, Eu₂O₃, or Tantalum.

Basis—There are two materials that have been investigated as prime candidates for the Demonstration Plant control material. These are B₄C and Tantalum. A third material, Eu₂O₃, has been recently included in development programs because of its attractive irradiation characteristics.

C.2.5.7 Structural Design Criteria

Requirement—The control assembly shall be designed to the core structural design criteria.

Basis—It is important for the integration of all core components upon which analyses were performed for the structural adequacy of particular components to be based on the same structural design criteria.

C.2.5.8 Transients

Requirement—The control elements shall be capable of withstanding, with an acceptable margin, all anticipated power and temperature transients within the core region.

Basis—The core nuclear and coolant flow mechanical design features and safety instrumentation are designed to reduce the probability of unplanned positive reactivity additions and/or local and bulk cooling flow reductions that cause an imbalance between power generation and heat dissipation to a practical minimum. However, there are transients that, by design, are within safety limits and are considered to have a definite probability for occurring during reactor operation.

The magnitude and rate of reactivity additions associated with potential mechanical and electrical failures, system malfunction; and operator errors must be limited to the end that the consequences of such faults do not lead to a reduction of the reliability and life expectancy of the control and fuel systems and core structures.

Operating limits should be established from a conservative interpretation of experimental and analytical information to assure that adequate and defensible margins exist between the expected transient operating range and any control element damage threshold.

C.2.5.9 Assembly Spacing

Requirement—Spacing between assembly members shall provide for the accommodation of swelling and thermal growth so as not to exceed the structural criteria for any portion of the assembly.

Basis—See basis for poison rod spacing (C.2.4.6) and control element to guide tube radial spacing (C.2.5.4).

C.2.5.10 Accommodation of Closure-to-Core Misalignment

Requirement—The coupling line shaft connects the upper end of the assembly to the control element coupling device (which is part of the drive mechanism) and allows removal of the control element. Its geometry is a hollow cylindrical tube with a special upper end to incorporate the coupling device. This coupling shall be designed to accommodate the expected closure-to-core misalignment during all modes of reactor operation to the extent that the control element can always be inserted into its position of greatest neutron absorption by scrambling.

Basis—The prime function of the control system in a reactor is its ability to successfully insert during the most adverse design conditions. Alignment of the control element and associated drive mechanism is affected by any of the following adverse conditions:

- As-built tolerance stackup of component parts
- Thermal Expansion
- Radiation-Induced Swelling
- Creep
- Flow-Induced Vibrations
- Seismic Events

The control element must be capable of insertion from any axial position during its lifetime considering the misalignment that is caused by the worst possible combination of these conditions.

C.2.5.11 Poison Rod Spring

Requirement—The poison rod shall contain a spring that maintains the absorber pellet column in position during preoperational handling and loading and permits axial absorber column expansion.

Basis—This design of individual poison rods requires that there be no absorber movement relative to the cladding and overall control element movement. This is needed to prevent transient conditions that could result from sudden relative movement of the absorber material.

Fabrication and required examination techniques are such that considerable clearance is required between the absorber column (or insulator) and poison rod final end plug weld. In addition, differential thermal expansion and radiation-induced swelling must be accommodated in the poison rod design. One additional design purpose for a long plenum is to accommodate helium gas generated within the rod.

A properly designed spring can accommodate the loadings caused by the above conditions in combination with dynamic loadings (due to scram and seismic events), such that the structural criteria are not exceeded and relative movement of the absorber and the core is prevented.

C.2.6 Design Life, Load Conditions, and Fluence Limits

The control assembly in-core lifetimes are determined from the time at which the margin of material strength, shock resistance, and reactivity is no longer sufficient to meet the design and safety requirements.

C.2.6.1 Absorber Assembly Design Lifetime

Requirement—The design lifetime for a fully inserted absorber assembly shall be at least one refueling cycle.

Basis—This requirement is set by the refueling interval. In order to increase plant availability and economics, downtime for control assembly replacement is eliminated by requiring control assembly replacement to be done during the planned refueling intervals.

C.2.6.2 Channel Structures Design Lifetime

Requirement—The design lifetime for the control assembly channel structures shall be at least two refueling cycles.

Basis—The lifetime of control assembly channel structures is expected to be determined by the effects of radiation-induced damage. It is desirable to have lifetimes as long as possible, however, a practical minimum is expected to be two years (or two cycles). Irradiation exposure of these channel structures causes material swelling and loss of ductility. It is the extent of these effects in combination with external loads from the core restraint system that limits channel structure lifetimes.

C.2.6.3 Control Assembly Flux Environment

Requirement—The control assembly shall be designed to operate in the flux environment of the LMFBR core described in the applicable power, burnup, and fluence performance specifications.

Basis—The neutron flux to which a control assembly is exposed depends on the position of the control assembly in the core as well as the operating time. The flux level changes during the reactor operating interval. As a reactor operates from the beginning to the end of an operating cycle, the fissile content changes; in particular it decreases in the core and increases in the radial blankets.

The neutron flux also changes radially and axially from the core center, such that flux gradients exist across and along each control assembly. These flux gradients cause distortion of the control assemblies due to differential swelling.

C.2.6.4 Control Assembly Structural Adequacy

Requirement—Analysis of the control assembly to prove structural adequacy shall include consideration of the following loads or loading mechanisms:

Shipping Loads	Design Transients
Seismic Loads	Coolant Pressure
Flow-Induced Vibration	Fission Gas Pressure

Refueling Loads: The maximum loads that can be applied to the control assembly shall be considered for the case where the handling grapple and socket are misaligned to their maximum extent.

Axial Ratcheting: Mechanical hold-down is provided to prevent the core components (fuel, blanket, and control assemblies) from ratcheting up out of the core more than a specified amount. The loads that result due to this thermal cycle-induced ratcheting shall be considered.

Core Clamping Loads: The clamping loads applied to the core by the restraint system should be considered.

Channel Bowing: The loads required to restrain channel bowing that results from temperature and flux gradients across the assemblies shall be considered.

Absorber Assembly Motion:	Loadings present during movement of the absorber assemblies shall be considered. These include frictional loads due to distortion of the absorber assembly and duct and misalignment of the drive and absorber assembly as well as acceleration loadings, especially during scram.
Dimensional Changes::	The channel, duct, rods, wire spacers, and B ₄ C pellets all suffer dimensional changes due to thermal expansion and volumetric swelling. Any loads that result due to these dimensional changes shall be considered.
Cladding Wastage::	Wastage allowances are defined to account for absorber-cladding attack, sodium corrosion, fretting and wear, scratching, and tolerances. The increased stresses that result due to these wastages shall be considered.

Basis—The control assembly must be analyzed on a component and local basis for all expected and projected loads in order to ensure structural soundness over the design lifetime. In order to perform the analysis, the temperature or temperature distribution of the particular structure needs to be specified for each loading. This can be done by defining the loadings under different categories following the ASME Section III load categorization.

C.2.7 Safety

The maximum reactivity worth of the control assemblies must be such that potential damage situations are minimized and that the worth of the assemblies still satisfies requirements. Both the total worth and the reactivity insertion rate must be controlled and assured in order to define the system response to potential damage situations.

C.2.7.1 Minimum Reactivity Worth

Requirement—The minimum reactivity worth of each control assembly shall be defined by the total worth of each system (shim/safety and backup safety) necessary to meet the operating reactivity requirements, including margins of uncertainty.

Basis—The optimum control configuration is defined as that which produces the lowest power peaking for a given quantity of reactivity inserted. The minimum reactivity worth of each control element is determined by both the worth required to shut down the reactor (with uncertainties) and the worth required to optimize control configuration.

C.2.7.2 Maximum Reactivity Worth/Element

Requirement—The maximum reactivity worth of any control assembly shall be limited such that a power excursion resulting from the accidental motion of such a component during the life of the core will be terminated by the Plant Protection System, so that the consequences are within or below the major incident* category.

Basis—Since the control elements are designed to be movable, it is not unreasonable to postulate unanticipated withdrawal motions of a control element. The combination of control element reactivity worth and rate of withdrawal determine the reactivity insertion effect.

C.2.7.3 Control Element Fail-Safe Features

Requirement—In the absence of absorber assembly drive forces, mechanical or hydraulic, the resultant forces normally applied on the absorber assemblies shall move the assembly in the direction of decreasing reactor multiplication.

*Defined in RDT Standard C16-1T.

Basis—In order to assure reactivity control at all times, the design of the control elements and associated drive mechanisms must be such that, in the absence of normal, externally caused drive forces, the control elements shall move to their scrambled position.

C.2.7.4 Control Element Coupling Status

Requirement—Means shall be provided to assure disconnection of the absorber assemblies from their drives.

Basis—Normal procedures during refueling require first that the absorber assemblies be disconnected from their drive lines. This disconnection must be assured before the drives can be moved in order to prevent damage to the absorber assemblies, core support structure, or drive lines.

C.2.8 Instrumentation

There is no instrumentation attached to the control assemblies. However, the control assembly design shall be compatible with the instrumentation requirements of the control drive system.

C.2.9 Interfaces

All interfacing systems shall be reviewed to ensure compatibility with the control assembly. These interfacing systems include the following:

- Nuclear Control System
- Main Primary Cooling System
- Core Clamping System
- Refueling and Handling System
- Core Support Structure

C.2.10 Operability

All control absorber assemblies are withdrawn above the core to add reactivity and all scram in the downward direction. The Alternate Shutdown System absorber assemblies are raised first and are not normally reinserted during power operation except for scram. The absorber assembly withdrawal rates must be controlled in order to control the reactivity insertion rates (see C.2.7). Additionally, any special procedure requirements or precautions during operation (including startup, power operation, and shutdown) must be written into the appropriate operating procedure.

C.2.11 Maintainability

Requirement—All control assemblies shall be designed as replaceable items at off-power conditions. All shim-safety control elements will normally be placed in the overtravel position and then decoupled from their drives prior to removal of the drives. The control assembly can then be removed and replaced as a unit, or the absorber assembly and duct assembly can be removed and replaced in separate units.

Basis—Control assembly lifetimes are small compared to plant lifetimes and, therefore, require periodic replacement. Replaceability of all portions of the control assemblies will reduce the negative impact on plant availability.

C.2.12 Codes and Standards

Requirement—Design, materials, fabrication, erection, installation, inspection, and testing shall conform to applicable sections of the following codes and standards.

1. AEC-RDT Standards—Material, fabrication, testing, and equipment standards.
2. ASME Boiler and Pressure Vessel Code, Section III, Nuclear Power Plant Components, including applicable codes cases such as 1331-6(7).
3. ASTM Recommended Practice for Surveillance Tests on Structural Materials in Nuclear Reactors, ASTM E 185-66.

Basis—A disciplined engineering approach requires utilization of appropriate codes and standards as normal practice in nuclear power plant component design.

C.2.13 Surveillance and In-Service Inspection

Monitoring of system parameters, including material properties, shall be done on a continuous or quasi-continuous basis during system operation, startup, or shutdown. Surveillance measurements shall be made over the lifetime of the reactor. Instrumentation and specimens for these measurements shall be replaceable, and space, access, and a means for replacing the surveillance instrumentation and specimens shall be provided.

In-service inspection shall be done during outages of the system. Space, access, and attachment provisions shall be included in the design so that instrumentation and equipment for performing the inspections can be installed. It is not intended that the reactor will ever be shut down solely for inservice inspection except to confirm or deny any indicated malfunction or damage. Otherwise, inservice inspection will be accomplished when the reactor is shut down for other reasons.

Requirement—The control assembly shall be designed to permit inspection of the control elements and spacers on site after shipping prior to insertion into the reactor.

Basis—This is intended to prevent the installation of a shipping-damaged control assembly.

Requirement—Control assemblies shall be designed to permit remote disassembly for nondestructive examination without damage to the elements.

Basis—This is necessary in order to permit surveillance and inspection of individual rods.

Requirement—The control assemblies shall be monitored to assure adequate cooling.

Basis—Inadequate cooling will result in high absorber assembly temperatures that, in turn, will reduce the design lifetime.

C.2.14 Quality Assurance

The control assembly and its components shall be designed, manufactured, and assembled in accordance with the Demonstration Plant quality assurance program plan.

APPENDIX D

PRECAUTIONS AND SAFETY DESIGN FEATURES FOR B₄C CONTROL ROD ASSEMBLIES (W. J. MARGETANSKI)

This section presents the expected potential hazards and discusses the design features of the control assembly that are influenced by these potential hazards.

D.1 HAZARDS

Potential hazards thought to significantly affect the control assembly design shall be considered throughout the design. The potential factors that are considered significant are:

- a. *Control Assembly Worth*—If too large and moved rapidly from the core, there is a potential for transient response beyond the capability of the safety system to correct. If too small, there would be an inability to shut the reactor down.
- b. *Propagation of the Effects of Local Rod Failures*—Although the poison rods are designed with allowances for uncertainties and a high degree of quality control will be exercised during fabrication and assembly, because of the large number of individual rods, there may be isolated failures that would allow a B₄C/Na/stainless steel reaction to take place that could propagate these isolated failures.
- c. *Breach of Poison Rod Integrity* (during Normal and Credible Transients).
- d. *Blockage of Coolant through the Absorber Assembly*.
- e. *Potential for Misplacement of Control Assembly* (into a Lower Enriched Location or a Fuel or Blanket Location).
- f. *Effect of Normal Modes of Assembly Distortion on Control Element Insertion*.

D.2 CONTROL ELEMENT WORTH

The maximum reactivity worth of any control element shall be limited such that a power excursion resulting from the accidental removal of the control element any time during the life of the core will be terminated by the Plant Protection System, such that the consequences are within or below the major incident* category (see D.2.7).

D.3 POISON ROD WRAPPER

The poison rods are contained within a hexagonal wrapper. The wrapper is contained within a guide tube that, in turn, is contained within a heavy, walled flow duct. These barriers should minimize the probability of propagating local rod failures.

D.4 CLADDING THICKNESS

The cladding tube wall thickness shall be sufficient to withstand the maximum cladding wastage allowance and maintain cladding integrity at the cladding hot spot temperature conditions to satisfy design criteria.

*Nomenclature is consistent with RDT Standard C18-1T.

D.5 CONTROL ELEMENT COOLING

The coolant inlet holes in the control assembly shall be designed to reduce the probability of flow blockage.

D.6 NOSEPIECE DESIGN, GRAPPLE DESIGN, AND CONTROL ELEMENT MARKING

All control assembly nosepieces shall be designed to prevent the insertion of a control assembly into a wrong location. The grapple used in handling the control assembly shall be different than the grapple used for the fuel and blanket assembly bundles. The use of this grapple shall aid in preventing placement of any control assembly into a fuel or blanket assembly location. In addition, the control elements shall be marked to indicate their B¹⁰ enrichment in order to aid in preventing the placement of a control element into a location reserved for one of lower enrichment.

D.7 RADIAL SPACING

The control-element-to-guide-tube radial spacing shall provide sufficient clearance to enable the control element to move as required by precluding contact at other than the wear surfaces during all modes of operation. Considerations shall be given to material swelling and distortion due to differential flux and temperature across the assembly.

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