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1000-MWe LMFBR ACCIDENT ANALYSIS
AND SAFETY SYSTEM DESIGN STUDY

— Topical Report —

Effects of Irradiation-Induced Metal
Swelling on the Reference Design

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PREFACE

This report was originally prepared as a technical note to document the work performed in a specific contract activity as soon as the work was completed. The technical editing was limited in order to meet the objective of timely reporting. The report was issued for USAEC-ANL use only, and the intent was to update and consolidate the information from all technical notes in a comprehensive phase report before final publication for public distribution at the end of Phase II.

This plan was changed when the contract was terminated in October 1970 for the convenience of the government. Instead, a final summary report will be prepared, and the previously issued technical notes will be published as formal topical reports. In accordance with the modified plan, this technical note is being published in its original form without further editing or modification except for minor technical corrections and changes in the title and date of issue. Even without updating and technical editing, the report provides detailed information that should be helpful in evaluating and resolving LMFBR safety questions in related areas.

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November 15, 1970

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Lynchburg, Virginia

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1000-MWe LMFBR Accident Analysis
and Safety System Design Study

Effects of Irradiation-Induced Metal
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B. J. Goulding, M. W. Croft

Key Words: Irradiation Swelling, LMFBR

ABSTRACT

This report describes the work performed to ascertain the effects of irradiation-induced metal growth on core components subjected to operating characteristics as defined by the B&W 1000-MWe LMFBR Follow-On Study reference design. Four metal growth correlations were reviewed and compared using the B&W design. Several possible solutions were evaluated in separate studies, and the results from these studies formed the basis for defining a modified design. This modified design, which utilizes a core clamp device, resulted in a reduction of peak volumetric growth from 35 to 18%. The economic penalty associated with this reduction is less than 0.4 mill/kWh.

CONTENTS

	Page
1. INTRODUCTION	1-1
1.1. Background	1-1
1.2. Objective.	1-3
1.3. Reference Design	1-3
2. SUMMARY.	2-1
3. DESCRIPTION OF WORK	3-1
3.1. Recommended Design	3-1
3.1.1. General	3-1
3.1.2. Description.	3-4
3.1.3. Physics	3-6
3.1.4. Thermal Hydraulics and Growth Evaluation . . .	3-8
3.1.5. Stress and Bowing Analysis	3-10
3.1.6. Safety Aspects.	3-12
3.1.7. Related Design Aspects.	3-23
3.1.8. Economics	3-24
3.2. Effect on the Accident Analysis Study.	3-56
3.3. Review of Experimental Data and Growth Correlations	3-58
3.3.1. General	3-58
3.3.2. Growth Correlations.	3-60
3.3.3. Mechanical Properties	3-63
3.4. Identification and Evaluation of Potential Solution. . . .	3-76
3.4.1. General	3-76
3.4.2. Evaluation of Reference Design	3-76
3.4.3. Physics Study	3-81
3.4.4. Plant Temperature Study.	3-87
3.4.5. Stress Study	3-93
3.4.6. Mechanical Study	3-101
3.4.7. Economic Study.	3-108
3.4.8. Identification and Evaluation of Potential Solutions	3-110
4. CONCLUSIONS AND RECOMMENDATIONS.	4-1
APPENDIXES	
A. Computer Codes for Modifying the Fuel Assembly Wrapper Can	A-1
B. Flux and Temperature Distribution for the Reference Core	B-1

CONTENTS (Cont'd)

	Page
C. Thermal-Hydraulic Analysis of the Double-Walled Can	C-1
D. 1500-psi Steam Cycles	D-1
E. Novelty Concept	E-1
F. Stability Check — Axial Load on Fuel Element	F-1
G. Growth Effect of Axial-Zone Enriched Core	G-1

List of Tables

Table

3-1. Key Plant Parameters — Reference and Modified Designs	3-26
3-2. Selected Procedure for Peak Fluence Reduction	3-27
3-3. Description and Volume Fractions for Modified Core	3-27
3-4. Peak Fluences in the Modified Reference Core	3-28
3-5. Peaking Factors, Power Fractions, and Average Power Densities	3-28
3-6. Breeding Ratios	3-29
3-7. Reactivity Coefficients, Delayed Neutron Fraction, and Prompt Neutron Lifetime at EOC	3-29
3-8. Axial Temperature Distribution in the Modified Reference Design	3-30
3-9. Flux Distribution	3-31
3-10. Coefficients for ANL Growth Equation	3-67
3-11. Locations of Axial Increments	3-117
3-12. Regional Flow Changes	3-118
3-13. Methods for Reducing Fluence Levels and Gradients in the Core	3-118
3-14. Effects of Reduced Fluence	3-119
3-15. Core Description — Reference and Modified Designs	3-120
3-16. Annular Cores Vs Reference Core	3-121
3-17. Reflector Volume Fractions	3-121
3-18. Reflected Cores — Beginning of Life	3-122
3-19. Relative Zone Enrichment Variations	3-123
3-20. Current Plant Designs	3-123
3-21. Reference Design Compared With 1800 psi/900/900 Cycles	3-124
3-22. Reference Design Compared With 1500 psi Cycles	3-124
3-23. Reference Design Compared With 1000 psi Saturated Cycles	3-125
3-24. Fuel Pin Diameters	3-125
B-1. Fuel Assembly Average Temperature Distribution	B-3
B-2. Flux Plot — Total	B-4
B-3. Flux Plot — Above 0.112 MeV	B-6
D-1. Sargent & Lundy Turbine Island Data	D-4
D-2. Modified Gross Efficiencies and Cost	D-4

Tables (Cont'd)

Table	Page
D-3. Calculated Net Efficiencies and Cost	D-5
D-4. Capital Costs for Fixed Items.	D-5
D-5. Capital Costs.	D-5

List of Figures

Figure	
3-1.	Solution for Reduction of Swelling 3-32
3-2.	Power Generation Cost Vs Metal Swelling 3-33
3-3.	Modified Recommended Design 3-34
3-4.	1000-MWe LMFBR — Reference Concept Core and Internals Arrangement. 3-35
3-5.	Core Clamp Assembly 3-36
3-6.	Radial Loading Sequence 3-37
3-7.	Reference and Modified Fuel and Blanket Assemblies . . . 3-38
3-8.	Temperature-Enthalpy Diagram for 1800 psi/900/900F Cycle 3-39
3-9.	Modified Reference Core Radial Flux Distribution 3-40
3-10.	Normalized Axial Power Shape of Modified Reference Design (Including Axial Blanket) 3-41
3- 1.	Normalized Radial Power Shape of Modified Reference Design 3-42
3-12.	Volumetric Growth — Modified Reference Design 3-43
3-13.	Radial Volumetric Growth Profile at Core Midplane 3-44
3-14.	Axial Temperature Distribution for Modified Reference Fuel Assembly No. 12 3-45
3-15.	Axial Flux Distribution for Modified Reference Fuel Assembly No. 12. 3-46
3-16.	History of Maximum Strain for Modified and Reference Fuel Assemblies. 3-47
3-17.	History of Maximum Deflection for Modified and Reference Fuel Assemblies 3-48
3-18.	History of Deflection at Core Midplane for Modified and Reference Fuel Assemblies. 3-49
3-19.	Axial Distribution of Deflection for Modified and Reference Fuel Assemblies 3-50
3-20.	Time History of Restraint Load at Intermediate Support for Modified and Reference Fuel Assemblies . . . 3-51
3-21.	Time History of Maximum Stress for Modified and Reference Fuel Assemblies 3-52
3-22.	Axial Stress Distribution for Modified and Reference Fuel Assemblies. 3-53
3-23.	Maximum Coolant and Fuel Temperature Vs Time — Flow Transient. 3-54
3-24.	Energy Generation Vs Doppler Coefficient 3-55

Figures (Cont'd)

Figure	Page
3-25. Irradiation-Induced Swelling in Austenitic Stainless Steels	3-68
3-26. PNL Growth Correlation for Solution-Treated Types 304 and 316 Stainless Steel	3-69
3-27. GE Growth Correlation for Type 304 Stainless Steel	3-70
3-28. PNL Growth Correlation for 20% Cold-Worked Type 316 Stainless Steel.	3-71
3-29. ANL Growth Data	3-72
3-30. Minimum Thermal Creep Rate for Type 316 Stainless Steel.	3-73
3-31. Neutron-Induced Creep Rate — PNL, March 1970.	3-74
3-32. Time to Rupture in Fast Reactor Environment at Fluences Above 10^{22} nvt — Types 304 and 316 Stainless Steel.	3-75
3-33. Metal Swelling Vs Burnup at Various Temperatures for Reference Design — PNL Data	3-126
3-34. Core Arrangement — Reference Design	3-127
3-35. Axial Linear Growth — PNL Correlation.	3-128
3-36. Volumetric Growth for Radial Zone 1.	3-129
3-37. Radial Linear Growth — PNL Correlation.	3-130
3-38. Axial Linear Growth — ANL Data	3-131
3-39. Radial Linear Growth — ANL Data.	3-132
3-40. PNL and ANL Axial Growth Data	3-133
3-41. PNL and ANL Radial Growth Data	3-134
3-42. Total Core Flow Vs Pressure Drop	3-135
3-43. Peak Fluence Vs Burnup, Reference Core.	3-136
3-44. Power Generation Cost Vs Burnup, Reference Core	3-137
3-45. Power Generation Cost Vs Reduction in Fluence >0.111 MeV	3-138
3-46. Power Generation Cost Vs Reduction in Total Fluence.	3-139
3-47. Peak Fluence Above 0.1 MeV Near the Core-Blanket Interface	3-140
3-48. Peak Total Fluence Near the Core-Blanket Interface.	3-141
3-49. Effects of Reflectors on BOL Flux Distribution Above 0.1 MeV — Midplane	3-142
3-50. Effects of Reflectors on BOL Total Flux Distribution — Midplane	3-143
3-51. Flux Shift Due to Axial Zone Loading — Flux >0.1 MeV, Midplane	3-144
3-52. Flux Shift Due to Axial Zone Loading — Total Flux, Midplane	3-145
3-53. Midplane Core Temperature Vs Metal Swelling	3-146
3-54. Power Generation Costs Vs Midplane Core Temperature	3-147
3-55. Power Generation Cost Vs Volumetric Growth.	3-148
3-56. Reference Plant — Schematic Diagram.	3-149
3-57. Temperature-Enthalpy Diagram for 2400 psi/900F Cycle Without Reheat.	3-150
3-58. Temperature-Enthalpy Diagram for 2400 psi/900F / 900F Cycle With Normal Reheat	3-151

Figures (Cont'd)

Figure		Page
3-59.	Constraint With Stress-Free Operation	3-152
3-60.	Constraints With Positive Reactivity	3-153
3-61.	Comparison of Three Fuel Assembly Models	3-154
3-62.	Temperature Distribution in the Reference Core	3-155
3-63.	Neutron Flux Distribution in the Reference Core	3-156
3-64.	Maximum Stress in Various Reference Fuel Assemblies	3-157
3-65.	Maximum Strain in Various Reference Fuel Assemblies	3-158
3-66.	Maximum Deflection in Various Reference Fuel Assemblies	3-159
3-67.	Restraint Load at Intermediate Support in Various Reference Fuel Assemblies	3-160
3-68.	Fuel Assembly Length and Relocation of Intermediate Support Vs Maximum Stress	3-161
3-69.	Fuel Assembly Length and Relocation of Intermediate Support Vs Maximum Deflection	3-162
3-70.	Fuel Assembly Length and Relocation of Intermediate Support Vs Restraint Load at Intermediate Support . . .	3-163
3-71.	Maximum Stress and Restraint Load at Intermediate Supports Vs Moment of Inertia of Wrapper Can	3-164
3-72.	Maximum Stress, Deflection, and Intermediate Restraint Load Vs Inlet Temperature	3-165
3-73.	Maximum Deflection Vs Thickness of Wrapper Can Above Intermediate Support	3-166
3-74.	Approaches for Accommodating Fuel Swelling	3-167
3-75.	Fuel Assembly Movement	3-168
3-76.	Power Generation Cost Vs Volumetric Swelling — Two- and Three-Year Cycles	3-169
3-77.	Power Generation Cost Vs Burnup — Two- and Three-Year Cycles	3-170
3-78.	Gap Between Fuel Assembly Cans Vs Power Generation Cost	3-171
A-1.	Simplified Model for Fuel Assembly	A-5
C-1.	Thermal-Hydraulics for Double-Walled Can	C-3
D-1.	1500 psi/796F Steam Cycle — Temperature Vs Enthalpy	D-6
D-2.	1500 psi/596F Steam Cycle — Temperature Vs Enthalpy	D-7
E-1.	Temperature-Enthalpy Diagram for 600/1500 psi Dual Cycle	E-4
E-2.	Temperature-Enthalpy Diagram for Binary Biphenyl Steam Cycle Achieving a 740F Core Midplane Temperature	E-5
F-1.	Axially Loaded Beam	F-2
G-1.	Central Fuel Assembly Volumetric Growth — ANL Correlation	G-3
G-2.	Central Fuel Assembly Volumetric Growth — PNL Correlation	G-4

1. INTRODUCTION

1.1. Background

Babcock & Wilcox (B&W) is conducting the Accident Analysis and Safety System (A²S²) Design Study to develop a better understanding of the influence of safety requirements on the design of large LMFBRs. The work is being carried out on a cost-sharing basis under Argonne National Laboratory Contract No. 31-109-38-2339; utility companies are participating under separate agreements with B&W.

Although the Follow-On Study provided first-step information on industry-favored LMFBR conceptual designs, the scope of the study did not provide for in-depth resolution of many safety-oriented problems or for detailed guidance of specific research and development requirements. To achieve this objective, B&W is analyzing the accidents that determine the design bases for certain protective systems and safety features and preparing conceptual designs for these systems and safety features.

The contract scope of work for our current effort specifies that the study should be based on the B&W 1000-MWe LMFBR Follow-On Study reference plant design. However, this conceptual design was developed during 1967 and 1968, before the swelling of metal structures was recognized as a matter of concern. Consequently, since the magnitude of this swelling phenomenon—and in fact the phenomenon itself—was not known at that time, the reference design included no provision for accommodating the structural metal swelling phenomenon. As more experimental data have been made available during the past two years, this swelling has become a matter of increasing concern, and it was apparent that provisions for accommodating the swelling in the reference design should be investigated.

As recently as two years ago, the recognized problems associated with irradiation in a fast flux environment were low-temperature hardening induced by atomic displacement and high-temperature embrittlement

(above 650F) attributed to helium segregation at the grain boundaries. The severe loss of ductility in the core construction material (austenitic stainless steel) was regarded as the greatest concern. The position was being reached that, although these phenomena required precautionary constraints on fuel pin and reactor structure design and operation, they presented no insurmountable obstacles to fast reactor economics.

Late in 1967 the UKAEA reported that gross swelling of austenitic stainless steels was being observed in cladding on mixed oxide fuel elements irradiated in the Dounreay fast reactor. This less familiar consequence of swelling caused by void formation now seems potentially more formidable and appears to dominate present day assessments of fuel pin and structural material behavior in LMFBR designs. Based on present-day predictions and correlations, it appears that core lifetime in a commercial LMFBR will not be limited by the burnup capability of the fuel (which may even be improved by the stainless steel swelling) but rather by the reactor designer's ability to economically accommodate the volumetric swelling of the non-fuel material in the core, whose neutronic behavior must be both predictable and safe.

The available data for non-fuel material in the core is derived from studies of austenitic stainless steel subjected to fast neutron fluxes, but void generation in aluminum, nickel, tungsten, molybdenum, and vanadium has also been reported. In stainless steel the voids are spherical or polyhedral in form, varying in size from 100 to 2000 Å diameter and up to $10^{15}/\text{cm}^3$ in population density; they are distributed randomly within grains but appear to avoid the vicinity of grain boundaries and are believed to be virtually gas-free.

The precise mechanism by which voids are formed is open to speculation, especially regarding the importance of helium and hydrogen, which is evidently a result of transmutation. In such reactor evaluations it is virtually impossible to isolate the individual effects of irradiation damage and those of helium and hydrogen production. It is conceivable that an equilibrium situation or an alloy having nuclear-resistant properties might be achieved, but too many factors are involved to allow prediction.

It is important to note that metal swelling has not led to operational or design difficulties in any fast reactor built or operated to date. In LMFBRs projected for the next decade, however, fluxes will be increased and atom displacement in the core structure could reach levels more than one order of magnitude greater than those experienced to date.

1.2. Objective

The objective of this activity is to conduct an evaluation study of sufficient depth to better define the problem of metal swelling. Potential solutions will be identified and their merits compared to select the most promising candidates. The major perturbations that these solutions might have on B&W's reference design and its safety characteristics will be considered.

1.3. Reference Design

The nuclear and turbine island complex produces a minimum of 1000 MWe. An integral sodium-cooled reactor unit produces heat in the core and transfers it to the nonradioactive sodium of the secondary system, which is circulated through the six intermediate heat exchangers (IHXs). This secondary sodium flows to three once-through steam generators, where steam at 2400 psig and 950F is generated and transmitted to a tandem-compound, six-flow turbine. Three sodium reheaters raise the temperature of the exhaust steam from the high-pressure turbine to 950F before the steam enters the intermediate low-pressure turbine.

The integral or pot-type primary system has an under-the-plug refueling scheme. The fuel for the system is contained in hexagonal fuel cans which make up the core. The fuel element cans are located between a lower grid plate attached to the inside of the core vessel and an upper support plate attached to the pressure vessel cover structure. The core vessel is surrounded by thermal and neutron shielding (combined). The shielding and the core vessel are held in place by a support grid suspended from the top reactor closure plug. All of these components are located in a large sodium pool within the 52-foot-diameter reactor vessel. In the annulus between the outside of the neutron shielding and the inside of the reactor vessel are six primary sodium pumps, six IHXs, two rotating fuel decay storage drums, cold traps, and an emergency cooling unit. The top of the reactor vessel is covered by an

annular cover plug 10 feet thick. Three eccentrically mounted plugs are placed in the cover structure above the core vessel. Each of these plugs is approximately 10 feet thick and is supported by heavy-duty bearings to facilitate indexing. Refueling is carried out under the plug by a fuel transfer machine mounted on the inner rotating plug, and irradiated fuel from the decay storage drums is transferred through the containment building using a shielded, gas-cooled cask mounted on a gantry. The primary system is housed in a cylindrical containment vessel 100 feet in diameter. This arrangement provides a compact building in which all of the equipment can be serviced by the main overhead rotary crane.

The active core has 288 identical fuel assemblies and 25 control rod assemblies which are evenly spaced among the fuel assemblies to accomplish the normal power regulating functions. Since the fuel pins are arranged on a triangular pitch, both fuel and regulating assemblies have hexagonal cross sections. The fuel assemblies are 6.450 inches across the flats and placed on a 6.500-inch triangular pitch with bearing pads located along their length. Each fuel assembly contains 331 pins 0.280 inch in outside diameter located on a 0.337-inch triangular pitch. The pins are held together by top and bottom assembly fixtures and intermediate spacers. The fuel assemblies are arranged in a cylindrical pattern; 138 radial blanket assemblies (also pin bundles having hexagonal cross sections) are located around the fuel assemblies. All fuel and blanket assemblies are located between the top and bottom fuel element holddown structures and are preloaded upward to prevent hydraulic lifting during operation. The walls of the fuel element assembly cans are solid, which enables individual orificing to each fuel assembly.

The stainless-steel-clad fuel pins are of the vented-to-sodium design with an active fuel length of 34.7 inches of vibratory compacted $\text{PuO}_2\text{-UO}_2$. Approximately 14 inches of depleted UO_2 material is assembled above and below the active fuel length.

The blanket pins are similar to the fuel pins but slightly larger in diameter. Each blanket pin, containing about 45 inches of vibratory compacted UO_2 material, is vented to the sodium. Venting the fission gases out of the fuel pins drastically reduces the cladding pressure, thus enabling lower fuel costs and increased breeding ratios.

The poison reactor material in the control rod assemblies is tantalum. These assemblies operate freely in a fixed can similar to the fuel element can. Each regulating rod has a mechanical drive assembly mounted on the upper surface of the reactor cover plug to provide finite rod positioning. An extension rod and a positive coupling connect the drive to the control assemblies. Coupling and uncoupling for refueling procedures are accomplished by a separate drive assembly. In the event of a reactor scram, both the control assembly and the extension rod are uncoupled from the actuator mechanism and allowed to fall freely.

2. SUMMARY

The study was directed primarily toward ascertaining the effects of irradiation-induced metal growth on core components subjected to the operating characteristics defined by B&W's 1000-MWe LMFBR Follow-On Study reference design. Four metal-growth correlations were reviewed and compared using the B&W reference design. Two metal-growth correlations were used in the final evaluation; one was developed by PNL and WARD, and the other was supplied by ANL. The PNL correlation is based on the fluence above 0.1 MeV, and the ANL correlation is based on the total fluence.

To understand the interaction of the metal swelling phenomenon on B&W's LMFBR system, it was considered essential to evaluate several possible solutions. These solutions were evaluated as separate studies, utilizing the reference design as the basic point of departure. Having established the growth effects by separate studies specifically related to temperature and fluence, the results from the studies provided the basis for defining a modified reference design. The objective of the modified design characteristics was to achieve a volumetric metal swelling of approximately 15%.

The results of the studies indicate that either fluence or temperature can be reduced to reduce the anticipated metal swelling to currently acceptable levels; however, the economic penalty would be less if both fluence and temperature were reduced to some extent. To accommodate an acceptable level of volumetric growth, various devices and design changes were evaluated, and the core clamping mechanism was selected as the proposed method. Any acceptable level of metal swelling is expected to require design devices to accommodate the residual growth.

The fuel burnup for the proposed modified design was reduced from 100,000 to 80,000 MWd/T, and the fuel residence time was reduced from

three to two years. The core sodium inlet temperature was reduced from 800 to 700F, and the core ΔT was increased from 300 to 350F. The core midplane temperatures were therefore reduced 75 degrees, whereas the outlet temperature was only reduced 50 degrees. The material selected for the core components is 20% cold-worked AISI type 316 stainless steel. The cold-worked material was selected to exploit the resistance of the material to irradiation-induced swelling. The steam cycle was modified from 2400 psi/950F/950F to 1800 psi/900F/900F. The selected modified reference design was then analyzed to ascertain that it met the requirements of the objective.

The proposed modified design reduced the peak volumetric growth to 18%. The desired objective of 15% was not quite achieved with the first iteration. In addition, the core clamp design conditions have been aggravated by the increase in the flux gradient at the outer fuel assembly stations and by the increase in the core ΔT . The associated maximum volumetric metal swelling data are summarized below.

Concept	Maximum volumetric growth, %			
	PNL/WARD, types 316 and 304 SS	GE, type 304 SS	PNL/WARD, 20% CW type 316 SS	ANL data
Reference design	80	100	35	27
Modified reference design	--	--	18	18

The estimated economic penalty involved in reducing volumetric swelling to 18% is slightly less than 0.4 mill/kWh. It is anticipated, however, that this estimated penalty could be substantially reduced with optimization.

The reference design can be successfully modified to accommodate the predicted metal swelling, but the conceptual changes defined in the evaluation would impose some limit on overall plant safety and economic performance characteristics. Except for the radial expansion characteristics, most of the changes appear to produce minor differences in the overall probability of accident or system response. The radial expansion

characteristics result in the insertion of positive reactivity during core life, and a prompt positive feedback may be obtained in certain transients. In other transients, it is conceivable that prompt negative but delayed positive feedback could occur.

The magnitude and time dependence of the transient bow and core radial expansion are extremely complex. Therefore, it is recommended that the problem be considered further on a high-priority basis.

The effects of these potential changes in the reference design have also been assessed to determine how the design study should be changed to account for the effects of metal swelling.

3. DESCRIPTION OF WORK

3.1. Recommended Design

3.1.1. General

It is apparent from experimental data and growth correlations (refer to section 3.3) and preliminary evaluations of separate study items to identify and evaluate potential solutions, using the follow-on study reference design (section 3.4) as a common point of departure, that metal swelling can be reduced to acceptable levels by various methods. The effect of these results on the A²S² study, reducing the metal swelling to an acceptable level, is identified in section 3.2.

The results of the study show that either fluence or temperature could be employed to reduce the anticipated metal swelling to acceptable levels, but the economic penalty is higher than that associated with a combination of fluence and temperature reduction.

At this time it is anticipated that any acceptable level of metal swelling will still require design devices to accommodate this level. The recommended solution for the B&W design requires the use of fluence, temperature, and design; it is illustrated in Figure 3-1. The end result will obviously require optimization and, hence, a redesign of both the reactor and the turbine island.

Of all the methods considered, the core clamp appeared to be the most reasonable approach to minimize the bow of the duct toward the core. In addition, ducts can be located positively with this method. The clamp could be used with either the top-mounted duct arrangement, the bottom-mounted duct arrangement, or the B&W reference design. The use of a core clamp in conjunction with the B&W reference design presents the fewest perturbations. Consequently, this approach was selected.

The maximum acceptable level of volumetric metal swelling was established as 15% (4.9 linear), using engineering judgment based

on the consideration that metals with densities of less than 85% lack mechanical strength characteristics. Also, United Kingdom investigations have found as much as 6.8% volume change in type 316 stainless steel material irradiated to 7.8×10^{22} n/cm² total fluence, and a value of twice this change for a plant design using 1980 technology does not appear unreasonable. In addition, it is anticipated that the current metal development programs to improve the austenitic stainless steels suitable for the requirements of LMFBR will be successful, enabling core structural material to adequately withstand the operating and design characteristics of the revised core.

The selected material for the redesigned core components is 20% cold-worked AISI type 316 stainless steel (316 SS). The cold-worked material was selected to exploit the resistance of the material to irradiation-induced swelling. The current evaluation of irradiation-induced swelling indicates that both cold-working the material during fabrication in the range of 10 to 80% and stress are important variables in determining the amount of swelling, but no reliable data are currently available regarding the stress variable.

It is understood that the effects of cold-working the material during fabrication may be annealed out during cycle life, but any delaying mechanism resisting metal growth can only be benefited by increasing the time to reach any acceptable growth level. However, the rupture life of austenitic stainless steels has been found to vary inversely with the degree of cold-work, and the irradiation creep rates are greater for the cold-worked materials.

During the evaluation it became apparent that the twisted-ribbon spacer for the fuel assembly was not compatible with the additional growth and relaxation data currently available. It was also considered that, in the transition period, the B&W design should more closely reflect the design that will eventually be proven in the FFTF. In essence, the knowledge gained in the national facility will be directly utilized in generating the B&W fuel assembly design. The fuel assembly helical wire spacer (which is the current FFTF design) was therefore selected as the B&W reference design.

The information obtained from the studies of temperature and burnup versus swelling were combined in an attempt to estimate the

desired parameters of the modified reference design (Figure 3-2). The results indicate that a reduction in metal swelling can be obtained more economically from the singular reduction of fluence than from temperature affects. However, by combining the 2-year fuel cycle with the temperature curve to obtain minimum slope characteristics, it would appear that a 3% volumetric reduction of metal swelling (approximately 30F) can be accomplished economically by temperature alone. Below the 28% volumetric growth value, a combination of fluence and temperature reduction results in better economics.

Reducing the volumetric swelling growth from 28 to 16%, utilizing a two-year fuel cycle, requires a reduction from the 100,000 MWd/T burnup to approximately 80,000 MWd/T. Reducing the volumetric growth 1% further requires a reduction of 10F in the core midplane temperature. Since the maximum reference design core growth is approximately 4% greater than that for the normalized design, the core midplane temperature must be further reduced by 40F. Thus, the modified recommended design will apparently require a midplane core temperature no higher than 875F. In an attempt to improve the economics, the core ΔT was increased from 300 to 350F. The summary of the recommended design solution to achieve a 15% volumetric swelling is shown in Figure 3-3. This figure illustrates the growth relationship at the mid-core plane condition. In actuality, the maximum growth occurring above the mid-core plane was estimated to be slightly above 15% with the modified design. The growth analysis for the modified reference design (see section 3.1.4) resulted in a maximum volumetric growth of 18% based on EOL flux conditions, which are higher than average conditions.

The modified design described in section 3.1.2 resulted in a reduction of peak volumetric growth from 37 to 18% (PNL correlation) and from 28 to 18.5% (ANL correlation). The desired objective of 15% volumetric growth was not quite achieved with the first iteration. In addition, the core clamp design conditions have been aggravated by an increase of flux gradient at the outer fuel assembly stations and by the increase in the core ΔT .

3.1.2. Description

The modified design is fundamentally similar to the reference design. The differences lie primarily with the reduction of burnup, fuel residence time, and core inlet and outlet temperatures, together with the incorporation of a core clamp. The burnup and fuel residence time modifications, however, reflect on the pin diameter, requiring the core and thus the plant to be reoptimized.

The scope of this study did not permit reoptimization of the modified design. For convenience and continuity of design, the number of pins per assembly (331) and the core arrangement of 288 fuel assemblies, 25 control rod positions, and 138 radial blanket assemblies was unchanged. Likewise, the height of the core was left at the reference 34.7 inches. The pin diameter was selected at 0.250 inch with a ligament of 0.055 inch. The key parameters of the reference design and the proposed modified design are compared in Table 3-1.

Figure 3-4 (B&W drawing A31100E) shows the existing core and internals arrangement with regard to geometry and dimensions. Figure 3-5 shows a conceptual design for a core clamp and its relationship to the core holddown assembly and the lower radial neutron shield. Since this design was carried only to the point of establishing feasibility, further effort will be required to establish an optimum arrangement in regard to increases in core holddown assembly diameter, clamping pad locations on ducts, and the overall length of the fuel assembly. This core clamp arrangement would be used on each outboard radial blanket element (72 places). Figure 3-6 is a plan view of the radial pattern. Each beam will be radially loaded in the direction shown, so that the proper clamping load at the inboard elements could be attained.

The core clamp is attached to the core holddown assembly by a skirt (see Figure 3-5). Cams and plungers are mounted on the skirt for loading the beam springs, which are located in each outboard blanket element between the bearing pads. As the core holddown assembly is jacked down on the fuel assemblies, the core barrel actuates the plunger through upward motion of the cam. The plunger deflects the spring, which puts a load into the blanket element at two planes through spiders. The clamp is unlocked through the plunger release mechanism, which forces the cam to the "down" position during the lifting of the core

holddown assembly, thus withdrawing the plunger and removing the load from the beam spring. The plunger release mechanism, therefore, requires a downward and upward motion during the raising operation of the core holddown assembly. A trapezoidal plate section was chosen for the beam spring section as being the most desirable shape from the standpoint of maximizing the deflections for a limited stress level. The larger the deflections (within reasonable stress levels) the less effect system mechanical tolerances will have on load-producing capability.

The unloading sequence of the core clamp is the reverse of loading it. Fuel movement during these operations does not occur, as the clamp only operates during the axial loading of the fuel assembly compression spring.

As can be seen in Figure 3-5, the duct of the outer blanket element stops at the lower pad location. This was considered a desired engineering feature to simplify the design. Since the reactivity of the blanket element is about $1/5$ that of the fuel elements, it is assumed that some latitude in deflection of the blanket duct in the core region can be taken. If more detailed calculations show that the duct should be extended to the top pad location and flow separators should be incorporated for thermal effects, then the design changes would be accommodated. All inboard blanket elements and fuel elements have flow separators of the type shown in Figure 3-5, section X-X.

The lower pad location was chosen as 40 inches below the core holddown assembly. This position is far enough above the blanket in all elements so that internal structuring would be possible for accommodating the clamping pad loads. Lowering the pad location further into the core reduces the loads. The top pad was located 4 inches below the core holddown assembly to permit the location of element-lifting lugs on the casting to receive the handling grab tool. A ± 1.5 -inch misalignment can be tolerated in picking up either a blanket element or a fuel element. The possibility of yoking individual component parts together to reduce the number of individual parts should be considered (see Figures 3-6 and 3-7). In concept, the radial restraint system shown in Figure 3-4 is the preferred approach because of its apparent simplicity. However, with the present uncertainties in the swelling and creep rate, an adjustable compensating load system might be the preferred method for near-

term design plants. Figure 3-7 is a diagram of the reference and recommended blanket elements and the fuel element.

The 2400-psi non-reheat cycle appears to be on a par with the 1800 psi/900F/900F cycle in achieving minimum economic penalty for reducing the core midplane temperature to 875F. Although elimination of reheat to remain at 2400 psi may have some advantages, a final choice would again require work beyond the scope of this study.

Thus, for continuity of design consideration, the 1800 psi/900F/900F cycle was selected for the modified reference design. Figure 3-8 shows the system temperatures for a typical design condition wherein core sodium inlet and outlet temperatures have been reduced to 700 and 1050F, respectively.

3.1.3. Physics

3.1.3.1. General

It was decided that the peak fluence in the reference core would need to be reduced approximately 30% to provide the fluence reduction contribution to meeting the metal-swelling criteria of 15%. Table 3-2 summarizes the methods chosen to achieve this 30% peak fluence reduction. The starting point was the reference core, with 100,000 MWd/T average discharge burnup and a 3.4-year cycle (1050 effective full power days, 0.85 plant capacity). Cutting back to 80,000 MWd/T average discharge burnup with a proportional reduction of fuel residence time was expected to reduce the peak fluence by 20%. Holding the burnup constant of 80,000 MWd/T but further reducing the fuel residence time with a proportional reduction in fuel volume (accomplished by reducing fuel pin diameter) was expected to reduce the peak fluence by an additional 6%. Fissile material would then be moved to the outer core regions, away from the core centerline where the peak fluence occurs in the reference core, in order to reduce the peak fluence by another 4%. The resulting modified reference core was expected to show a reduction in peak fluence from the reference core of about 30%.

In order to accommodate the fuel assembly deflections expected as a result of metal swelling gradients, the sodium gap between fuel assembly cans was extended to 0.400 inch.

3.1.3.2. Modified Reference Core

The physical parameters and volume fractions of the modified reference core are listed in Table 3-3. Although the modified reference core design specifies a 0.400-inch sodium gap between fuel assembly cans, the physics calculations were made assuming a 0.200-inch gap. Changing from a 0.200- to a 0.400-inch sodium gap is not expected to significantly affect the flux, power levels, or distributions in the core because the amount and distribution of fuel and the average enrichment would remain nearly the same. Although the core would be somewhat larger with the 0.400-inch sodium gap, the only calculated physics parameter expected to show a significant change is the sodium void coefficient. This estimated change is reported with the reactivity coefficients.

All physics calculations were performed in twenty groups and one dimension using FARED. Alternate radial and axial criticality calculations were made at the beginning of life (BOL) to converge the transverse bucklings. Radial and axial depletion calculations were made. At the end of the cycle (EOC), the transverse bucklings were reconverged and reactivity coefficients were calculated. The methods used are described in detail in BAW-1328, Volume 2.²⁵ The fluxes above 0.111 MeV were obtained by summing the fluxes in the first eight groups. Peak fluences were obtained assuming a linear variation of flux with burnup at each mesh point in the radial depletion calculation and a linear variation of the axial peaking factors with burnup in the axial depletion calculation. Two-dimensional flux distributions for use in the metal-swelling calculations were synthesized from the one-dimensional distributions by assuming complete separability of the radial and axial components. The magnitudes of the fluxes were obtained from the radial calculation.

Table 3-4 shows the effectiveness of the design changes in reducing the peak fluence. The reductions in both the peak total fluence and the peak fluence above 0.1 MeV, 32.1 and 30.1%, respectively, are very close to the goal of 30%. The radial fluence distribution in the adjusted reference core is shown in Figure 3-9. Because the fissile fuel was overshifted to the outer core regions, the peak fluence occurs at the core zone 1-zone 2 interface. Shifting

some fissile fuel back towards the core center would move the peak fluence to the core centerline.

The power-related parameters for the modified reference core are tabulated in Table 3-5. The low radial peaking factors in core zone 1 were caused by the shift of fissile fuel to the outer core regions. Breeding ratios are tabulated in Table 3-6. The reactivity coefficients, the delayed neutron fraction, and the prompt neutron lifetime (all at EOC) are tabulated in Table 3-7. It is estimated that changing the sodium gap between fuel assembly cans from 0.2 to 0.4 inch will increase the sodium void coefficients about 10%.

3.1.4. Thermal Hydraulics and Growth Evaluation

Growth in the modified reference core has been analyzed in a manner similar to that used for the reference design (see section 3.3.4, Thermal Hydraulic Study). To accurately determine the temperature distribution, a complete core flow analysis must be made. This includes (1) the calculations to impose a maximum cladding temperature, (2) the flow distribution analysis, (3) the bypass leakage considerations, and (4) the thermal mixing analysis. The temperature analysis of the modified reference design could not be completed in this much detail in the time available.

Three assemblies in the modified reference core were examined:

1. The innermost assembly of radial core zone 1 (this corresponds to assembly No. 1 in the reference design).
2. The innermost assembly of radial core zone 2 (this corresponds to assembly No. 7 in the reference design).
3. The outermost assembly of radial core zone 3 (this corresponds to assembly No. 12 in the reference design).

The modified reference core thermal power is increased over the reference value (due to the reduction of net plant efficiency). Figure 3-10 shows the normalized axial power shape for the modified reference design, and Figure 3-11 shows the normalized radial power shape.

The assembly flow rate for each bundle was approximated as follows (using the center assembly as an example):

$$Q_{1R} = W_{1R} \Delta T_{1R}, \quad (1)$$

where

Q_{1R} = total heat generated in an average channel of center assembly of the reference design,

W_{1R} = average channel flow rate of the reference design,

ΔT_{1R} = channel temperature difference of the reference design.

A similar equation may be written for the same channel in the modified reference core:

$$Q_{1M} = W_{1M} \Delta T_{1M}. \quad (2)$$

Dividing equation 2 by equation 1 and solving for W_{1M} ,

$$W_{1M} = W_{1R} \frac{Q_{1M}}{Q_{1R}} \left(\frac{\Delta T_{1R}}{\Delta T_{1M}} \right). \quad (3)$$

By assuming that

$$\frac{\Delta T_{1R}}{\Delta T_{1M}} = \frac{\text{bulk core } \Delta T_R}{\text{bulk core } \Delta T_M} = \frac{300^\circ\text{F}}{350^\circ\text{F}}, \quad (4)$$

the flow (W_{1M}) can be determined. Inputting this flow in the CHESS code, the axial temperature distribution was calculated. Using this method to determine flows in each bundle of the modified reference, the core average temperature difference will be 350F. The resulting average axial temperature distribution and channel pressure drops for each bundle analyzed are shown in Table 3-8.

These temperature distributions and the flux calculations, which were made by the FARED code (see section 3.13), were then used in the calculations for growth. Table 3-9 lists flux values for both the

total flux and the flux with energies greater than 0.1 MeV for the three assemblies examined.

Growth calculations were made using both the ANL correlation and the PNL 20% cold-worked correlation. Figure 3-12 is a plot of the axial growth profile. Both correlations predict approximately the same peak growth in the innermost assembly of radial core zone 1 (18.5% for ANL and 18% for PNL). As in the reference design analysis, the ANL correlation predicts larger growths in the low-temperature, low-flux regions of the core. The radial growth profile at the core midplane is shown in Figure 3-13. The dotted straight lines are approximations for intermediate stations, since this analysis did not include an evaluation at these intermediate stations.

3.1.5. Stress and Bowing Analysis

As discussed in the stress study, section 3.4.5, the parametric evaluation was performed on assembly No. 12. Hence, the modified fuel assembly No. 12 was also evaluated to obtain a comparison of results. The characteristics of the modified core design, in particular the flux and flux gradients, are only approximate. Another difficulty arises from the fact that the dimensions of the reference and modified fuel assemblies are different; thus, it is difficult to compare equivalent design points.

Figures 3-14 and 3-15 show the temperature and flux distribution for the modified fuel assembly. For a comparison, the temperature and flux distribution of the reference fuel assembly are also shown. For this comparison the reference fuel assembly was assumed to be a unit in an open, spaced core with support pads removed from the midplane zone and clamping pads and core clamp located 85 and 100 inches above the grid plate. The temperature distribution for the modified fuel assembly is lower than that for the reference fuel assembly, but the neutron flux and associated radial flux gradient (not shown) are higher. With the fuel life cycle reduced from three to two years, the fluence at the end of life decreases. The overall effect of this appears to be a reduction in the average swelling but increased differential swelling for the modified fuel assembly.

Figures 3-16 through 3-22 show comparisons of stress, strain, deflection, and restraint load characteristics for the reference

and modified fuel assemblies. The time history of these quantities cannot be compared conveniently because the fuel life cycles for the two fuel assemblies are different. Hence, the maximum quantity for the reference fuel assembly at the end of a three-year fuel cycle should be compared with the one at the end of a two-year cycle for the modified assembly. The non-dimensional characteristics do not provide a true comparison in this respect.

With the primary goal of reducing the maximum core swelling established, the associated effect is the reduction in maximum strain, as shown in Figure 3-16, a plot of the maximum total strain with life. It includes the effects of irradiation growth and thermal expansion. The actual creep strain is approximately 0.8%. A significant reduction in the maximum deflection and the associated positive reactivity near the end of the fuel cycle is obvious from Figures 3-17, 3-18, and 3-19. The axial distribution of deflection of the reference and modified fuel assemblies shown in Figure 3-19 indicates that near the end of the fuel cycle the reference fuel assembly length below the intermediate support, where the entire active core and axial blankets are located, would bow inward. For the modified fuel assembly only about half of the active core would bow inward. The large reduction in the restraint loads at the intermediate support is obvious from Figure 3-20.

The increase in the stresses for the modified fuel assembly shown in Figures 3-17, 3-21, and 3-22 was a setback because of the side effects of neutron flux distribution in the modified core. In reducing the neutron fluence level near the core centerline, the actual neutron flux level and the flux gradients near the interface of the core and radial blanket are considerably increased. This is probably the reason for the increase in the maximum stress, as well as significant cutback in the reduction of maximum strain, deflection, and restraint loads, which were anticipated from the parametric study. Some modifications, such as rotation at half the fuel cycle, changing the section modulus of the wrapper can along the axial length, or raising the actual core from the reference base, may reduce the maximum stress significantly. The necessity of considering increasing the fuel assembly length is obvious from Figure 3-20, which shows a large reduction in the restraint load.

The limited results available from the 180-degree rotation study of the modified fuel assembly are difficult to compare quantitatively, primarily because of the different approaches used in the two computer codes (explained in Appendix A). The rotation results do show considerable improvement in the stress, strain, deflection, and restraint load characteristics. The results for the maximum stress, which occurs at the intermediate 85-inch support in both cases, is shown in Figure 3-21.

The results obtained for the modified fuel assembly show that, if we assume a pinned support instead of a fixed support at the bottom end of the fuel assembly, the maximum stress and restraint loads are reduced. The maximum strain remains unchanged because the swelling behavior does not change. The maximum outward deflection is increased, but the trend shows a constant negative reactivity effect because of this increased outward deflection.

3.1.6. Safety Aspects

3.1.6.1. General

The phenomenon of metal growth under high-temperature irradiation has presented several problems in fast breeder technology. Although the currently available data are quite limited, it appears that the magnitude of metal growth may be large enough to require some modification of the reactor. In addition, the nature of the problem is such that some departure from traditional design practices may be required. In particular, the core design, which has been modified to accommodate metal swelling, may no longer represent the optimum core from the points of view of economics and safety. Therefore, it is mandatory that the impact of core modification on reactor safety be carefully evaluated.

Changes in core design affect reactor safety in a number of ways:

1. New or modified features may introduce new accident-initiating mechanisms and/or increase the probability of certain previously recognized mechanisms becoming operable.

2. The reactivity feedback during transient operation will certainly be different in degree, and certain mechanisms may be different in kind.

3. The operating margins, especially with regard to temperature and burnup, may change in such a way as to significantly influence safety.

The modifications to the reactor to accommodate metal swelling are described elsewhere in this report. This chapter evaluates the safety implications of the new reference core. The task is exceedingly complex, and any in-depth evaluation of the effects of design implementation would require far more time than has been available for this study. Consequently, the evaluation presented here is more a survey than a definitive study. Only limited analyses were possible in the allotted time and, hence, some of the conclusions drawn here are, in part, supported by judgment.

3. 1. 6. 2. Design Characteristics

The modification of the core will obviously affect the design characteristics of the system in a number of ways. Principal among these effects are the effects on the operating margins and on the safety related parameters. These effects are discussed qualitatively in the following section.

1. Operating Margins — Two principal operating parameters will be affected by changes in the core design: temperatures and burnup. The reactor thermal power and linear heat rate have been increased and the pin size has been decreased. These changes combine to produce a net increase in the steady-state fuel temperatures. This implies that the operating margin to fuel melting has been decreased. On the other hand, the coolant outlet temperature has been dropped 50 degrees and the inlet temperature has been dropped 100 degrees. This means that the cladding temperatures will be lower in the steady state. The coolant temperatures are lower, so that the margin to coolant boiling has been increased.

The question of fuel pin failure states enters into all these considerations. The smaller pin and the increased heat

rate mean that, for a given energy generation (MW-s), a larger fraction of the pin radius will be molten. This effect is partially offset by the increase in the ratio of cladding thickness to pin diameter. Therefore, it is not clear to what extent (if any) the failure states will be affected. This area requires more effort for a definitive conclusion.

Burnup: If pin performance as a function of lifetime could be accurately predicted, we could design the pin to have a certain margin to failure at its effective end of life. Ideally, this margin would be the same for any burnup. However, calculations of the required accuracy cannot be performed at present. Therefore, if it is assumed that the reference pin had some margin to failure at 100,000 MWd/T average burnup, we must conclude that the smaller pin, with its greater cladding thickness-to-diameter ratio, will have an increased margin to failure at the reduced burnup of 80,000 MWd/T. This margin cannot be quantitatively defined at present for any but the simplest cases.

2. Safety Related Values — A number of parameters related to the overall safety of the reactor have been affected by core design changes. Among these parameters are the Doppler coefficient, the sodium density coefficient, and the plutonium inventory. These parameters are discussed individually in this section.

Doppler Coefficient: The Doppler coefficient of reactivity remains essentially unchanged by the design modifications. The magnitude of the Doppler coefficient is largely governed by the neutron spectrum. The addition of sodium in the modified core tends to soften the spectrum, which should increase the magnitude of the Doppler coefficient; however, this effect is offset by the higher enrichment and greater radial leakage in the modified core, both of which tend to harden the spectrum. As a result, the spectrum and, hence, the Doppler coefficient remain virtually unchanged. This means that there will be little difference in the response of the modified and reference cores in accidents that lead to rapid fuel heating, such as rapid reactivity insertions. For slower transients the excursion will be controlled by the slower feedback mechanisms, since the prompt feedback will be very similar.

Sodium Density Coefficients: The large volume of sodium in the modified core causes the coolant void coefficient to be somewhat larger than the reference void coefficient—0.0219 and 0.0183, respectively. Coupled with the fact that the Doppler coefficient remains relatively unchanged, this increase means that flow transients and slow reactivity transients will be somewhat more severe in the modified core.

Radial Expansion Coefficient: The core clamping scheme selected for the modified core has changed the nature of the radial expansion coefficient. The reference radial expansion coefficient was calculated on the basis of a uniform radial core expansion. The true expansion, however, is anything but uniform; it is a function of the transient type, power level, and power gradient (radial position). Hence, the bow is not only nonuniform axially, but the degree of bow may also be a function of radial position. The core clamp and bearing pad arrangement selected for the modified core produces a sinusoidal bow within the core. The midplane deflection, which is initially outward, changes with life so that the long-term net deflection at the end of life is positive. This effect is very slow and should cause only minimal problems. The transient bowing behavior of this arrangement, however, may have safety implications. The transient bowing phenomenon is a complex function of thermal hydraulic design and transient type. Since the magnitude of the deflection at any particular axial location is dependent on the thermal gradient at that location, the transient bow is strongly dependent on the time sequence of thermal events in the core. Thus, the response is likely to vary with transient type.

Outward bow is accomplished by establishing in the clamping area a thermal gradient of a magnitude similar to the core thermal gradient. In a reactivity transient there is a time lag between the establishment of the gradient in the core and in the assembly extensions. The momentarily larger gradient in the core will cause a transient inward deflection and, hence, a positive reactivity feedback. Approximately 0.25 to 0.5 second later, the same gradient will be established in the upper portions of the assembly and the member will begin to bow outward, leading to a net negative reactivity effect.

In flow-loss situations the effect is reversed. The loss of flow initially causes the outlet temperature to rise which

will momentarily increase the gradient in the upper portions of the assembly, causing an outward deflection. As the flow coasts down the peak coolant temperature shifts upstream, and as this peak enters the upper core regions the bow will be inward. Thus, the reactivity effect is likely to be prompt negative and delayed positive in flow transients.

The magnitude and time dependence of the transient bow cannot be accurately assessed at this time. The phenomenon is so complex that substantial effort would be required to produce a reliable model. This effect, however, is likely to be important in some transients, and consideration should be given to adequately incorporating it in transient calculations.

Another problem arises in connection with bowing. The clamped core will bow outward during steady-state operation, while the unrestrained control rod will bow inward. In a practical design allowance must be made for this behavior to ensure that the rod motion is not impeded.

Plutonium Inventory: Although the enrichment is higher in the redesigned core, the fuel fraction is reduced so that the plutonium inventory is reduced 13%. This reduction in plutonium inventory reduces the probability of a particular quantity of plutonium entering the containment in any given accident situation.

Core Protection: The effects of core changes on reactor protection were very briefly assessed. Most aspects of protection appear to be unaffected. Two protective functions seem to be influenced—one probably favorably and the other perhaps adversely.

The bulk outlet temperature scram protects against fuel pin cladding failure caused by excessive whole-core sodium temperatures. It provides primary or backup protection for virtually all whole-core accidents. Since the modified design provides for lower operation sodium temperatures, greater freedom is available for establishing a scram setpoint between normal and dangerous temperatures. It is noted that the discarded alternate of a double can wall would have affected the bulk outlet temperature scram adversely, since sodium flowing between the walls would represent a fuel bypass flow that would dilute the sensitivity of the scram instruments.

It is now considered necessary to protect against locally initiated accidents like single-assembly flow blockages. Placing thermocouples at the assembly outlets appears to be the most satisfactory method of sensing such accidents. In the modified fuel assembly, both the distance from the top of the fuel pins to the top of the assembly and the flow cross-sectional area above the pins are increased. These changes increase the sodium transit delay time from fuel to thermocouple and, thus, delay the scram by tenths of a second or more if the thermocouple remains above the assembly. Overcoming the added transit delay by inserting the thermocouples down into the assemblies would be mechanically complex. The importance of these effects is not yet clear, since the analysis of local accident protection has just begun.

3.1.6.3. Accident Analyses

Malfunctions and Faults

In Phase I of the 1000-MWe LMFBR Accident Analysis and Safety System Design Study, B&W prepared comprehensive malfunction catalogs and fault trees. A critical review of the fault and malfunction catalog has been performed in an effort to determine the effect of core design changes on the basic faults and malfunctions. Since the core modification was largely limited to the fuel assembly, the appropriate malfunction catalogs are as follows:

- 12 — Molten Fuel
- 13 — Power Transient Following Reactivity Insertion
- 14 — Overheating of a Single Assembly
- 15 — Non-Neutronic Void Propagation From a Single Assembly

Since catalogs 13, 14, and 15 are contained in 12, only 13, 14, and 15 are discussed. For convenience these catalogs are reproduced (in part), along with comments on the impact of core modification.

Catalog 13. Power Transient Following Reactivity Insertion

1. Possible Causes:

a. Fuel Movement to More Reactive Position

(1) Earthquake Shock — The core modification does not significantly affect the probability or consequence of this fault, since the shock is dependent on the whims of nature. See item 3 below per assembly response.

(2) Fuel Assembly(s) Bowing — The inclusion of a core clamp above the core midplane and a bearing pad at the upper axial blanket interface will definitely affect the mode of both transient and steady-state bowing. However, the possibility exists that in some transient situations the initial bow may be inward and the feedback consequently positive. In other words, for certain transients, the reactivity feedback from bowing may be prompt positive and delayed negative. At this time the available data are not sufficient to allow a realistic assessment of this phenomenon.

(3) Fuel Assembly(s) Movement — The structural response of the reference fuel assemblies to pressure pulses associated with sudden vapor formation are being analyzed in the A²S² study to determine gross damage or complete failure threshold. Several calculational runs were made to determine how varying the clearance between fuel assembly cans affected their displacements. An input force that varied linearly from 0.0 to a maximum of 30,000.00 pounds in 0.002 second was used for all runs, while the clearances (the same between all cans) were varied parametrically from 0.05 to 0.4 inch. The results showed increased displacements with increased clearances at sites near the force application point and decreased displacements at sites farther removed. This indicates that relatively large clearances between fuel assemblies may be beneficial from the standpoint of local high-pressure generating incidents in the core, i. e., the resulting damage, if any, would be more localized.

(4) Collapse of Ratcheted Fuel — This mechanism is deemed incredible for vipac fuel. The core modification does not affect this mechanism.

(5) Partial Unclamping of the Core — The introduction of a core clamp has introduced a significant new fault—partial unclamping. Since there are two bearing pads above the core, the potential exists for a net inward (positive feedback) bow if the pressure is released on the upper bearing pad and maintained on the lower one. However, the clamp design is such that partial unclamping is virtually impossible.

b. Control Rod Withdrawal

(1) Single CR withdrawal at 1800 in. /minute due to secondary motor failure.

(2) Withdrawal of up to four CRs at 15 in. /minute due to control system or operator error.

These faults are not significantly affected by core design changes, since these changes do not affect the rod drives.

Catalog 14. Overheating of a Single Assembly

1. Possible Causes:

a. Misloaded Fuel Assembly — No significant effect.

b. Inadequate Primary Coolant Flow Through Affected Assembly

(1) Debris or Fission Gas From Failed Fuel Pin(s) — The effect is expected to be very small. However, the reduction in pin pitch will retard crossflow to some extent.

(2) Fuel Pin(s) Swelling or Bowing — The decreased burnup, lower cladding temperatures, and the greater ratio of cladding thickness to fuel diameter should contribute to the reduction of swelling problems. Since the wire-wrapped spacer has been introduced, the gap between the outer row of pins and the wrapper can has been increased and hence, the hydraulic passage between pins and wrapper is larger than the hydraulic passage between pins. In the event of a flow transient, such as a coastdown, the flow may be redistributed preferentially to the larger outer passage, and thus the outer portion of the assembly will be overcooled and the inner region will be undercooled. Conceivably, this could lead to inward pin bow. The effect has not been evaluated, but the net effect is expected to be extremely small.

(3) Flow Blockage to Assembly — This fault is not significantly affected by the design changes since the changes do not modify the assembly concept.

(4) Can Wall Failure — The thicker can wall and the wider gap (which provides a larger sink) should significantly reduce both the probability and the consequences of this failure.

(5) Fuel Pin(s) Spacer Failure — The operating characteristics of the spiral ribbon and wire-wrapped spacers are not known well enough to permit an evaluation.

(6) Entrained Cover Gas Bubbles in Inlet Coolant — The major effect of redesign on this malfunction will not appreciably change the velocity of the bubbles through the core. The smaller hydraulic passages are compensated by the reduced total core flow; the net effect is expected to be within 5% of the reference.

Catalog 15. Non-Neutronic Void Propagation From a Single Assembly

1. Possible Causes:

a. Mechanical Propagation

(1) Pressure Pulse From Flashing of Superheated Na — An accurate evaluation of this event cannot be made at this time for either the reference or the modified core. However, the effect of adding 10% more sodium to the core is expected to be small with respect to wave propagation.

(2) Pressure Pulse Generated by Molten Fuel-¹²Na Vapor Explosions — This fault falls into the same category as the previous one; therefore, it requires further evaluation.

b. Thermal Void Propagation

(1) Radial Heat Transfer Causing Boiling in Adjacent Assembly(s) — The probability of this event is significantly reduced by widening the gap between assemblies.

(2) Molten Fuel Penetrates Assembly Wrapper Can — The probability of this event should be somewhat reduced by the use of a thicker wrapper can.

c. Na Vapor Bubble Exiting From Bottom of Fuel Assembly and Entering Adjacent Assembly

The core modification does not change the assembly concept; thus, it will not influence the probability of this event.

Initiating Conditions

In Phase II of the study, accident-initiating conditions that arise from definable faults and malfunctions were investigated. In general, the accident-initiating conditions tend to fall into two broad categories: (1) those arising from flow abnormalities and (2) those arising from reactivity insertions. Each of these categories will be investigated with respect to the modified core.

Flow Abnormalities — Because the Doppler coefficient remains essentially unchanged and the sodium density coefficient is increased, one would expect an increased vulnerability to flow-induced transients. This, indeed, is the case. In order to compare the response to the modified and reference cores, calculations were performed to simulate a flow ramp-down. The radial expansion coefficient was not included in the calculations. The temperature traces for these studies are presented in Figure 3-23. It will be seen that slightly higher coolant temperatures are incurred in the modified core; however, the difference is less than 1%.

Reactivity Insertions — The response to reactivity-induced transients is as follows:

1. For slow reactivity transients in which there is some significant heat transfer from the fuel, the behavior of the modified core is similar to its behavior during flow transients—it displays a slight vulnerability to very slow transients.
2. Since the Doppler feedback controls rapid reactivity-induced transients, there is no significant difference in the response of the reference and modified cores.
3. It is not known how redesigning the core will affect damage propagation. The effect is expected to be small, however.

The decrease in pin pitch at a constant pitch-to-diameter ratio will decrease the cross flow for a given pressure gradient. This may lower propagation thresholds somewhat. The thicker can walls and larger assembly gaps should act to inhibit assembly-to-assembly propagation.

Disassembly

The magnitude of the energy generation can be estimated from previous sensitivity studies. Figure 3-24 shows the energy generation as a function of Doppler coefficient. This curve is representative of the reference core with a density coefficient of 0.0258. The initiating accident was a flow coastdown from power. The effective ramp rate at prompt criticality was \$37/s; this rate was governed by the rate of sodium expulsion from the core. The lower value of the sodium density coefficient in the modified core (0.0219 versus 0.0258) will reduce the ramp rate at prompt critical and, hence, the energy generation.

The curve in Figure 3-24 indicates that for a sodium density coefficient of 0.0258 and a Doppler coefficient of -0.0070 , the energy generation will be 3.3×10^4 MW-s. Therefore, for $\frac{dk}{dp} = 0.0219$ and $T \frac{dk}{dt} = -0.0070$, we can predict with confidence that the energy generation will be less than 3.3×10^4 MW-s.

3.1.6.4. Summary

On the basis of the foregoing survey, one can reasonably conclude that:

1. Except for the radial expansion coefficient, the safety characteristics of the modified core are not significantly different from those of the reference core.
2. No new accident types are encountered, although some small changes in accident probabilities are incurred.
3. The radial expansion of a clamped core is very complex, and significant effort will be required to define an appropriate coefficient. This coefficient, however, could be very important in certain types of transients.

3. 1. 7. Related Design Aspects

Changes in the core design to reduce radiation-induced metal swelling will affect other design aspects. Changes in the operating temperature and fluence of the core will affect reactor and turbine island equipment; changes in the design will also affect, to a greater degree, the fuel assembly, reactor internals, pressure vessel, instrumentation, control rod drive, and fuel handling.

The changes proposed severally alter the core design, and, because of the short period of evaluation, the recommended modified design only illustrates a trend and is definitely not an optimized design. The fuel assembly is slightly smaller across the flats but taller to accommodate the clamping device, although the core height remains unchanged. The complete design interaction between the clamping device, fuel assembly, and the reactor operating characteristics requires an extremely detailed evaluation.

With the reduction of the fuel assembly cross section, the core diameter will be approximately 2 inches smaller; the core clamp, however, will add about 6 inches to the diameter of the upper support structure. Reoptimization of the IHX diameter and the diametral clearance of the internal structure around it should enable the additional 4-inch requirement to be readily accommodated without changing the diameter of the pressure vessel.

A longer fuel assembly is required; this may necessitate lowering the core if decay heat cannot be removed by radial conduction across the fuel assembly during fuel handling operation. The reference design uses natural circulation for cooling during initial fuel handling. If the core must be lowered, then the additional height available for the active tube length of the IHX could be utilized to reduce the diameter of the pressure vessel. The additional length of the fuel assemblies, required to accept a core clamp design, reduces the economic incentive for vented fuel pins.

The reactor internals will require re-evaluation with the lowering of the core temperatures; the increase in the core ΔT obviously aggravates the design of the reactor components and pipework. However, at this time it would appear that the characteristics of the clamping

device and the associated fuel assembly top fixture would be substantially improved by a reduction in the core ΔT .

The control rod drive centers are correspondingly reduced with a reduction of fuel assembly cross section, but the gap between the assemblies has increased. This small reduction feature should not cause additional complications to the drives or the penetrations in the rotating plug of the pressure vessel cover structure.

The coolant flow through the assembly will increase with the radial growth of the fuel assembly during life; this will result in a smaller core outlet life temperature variation for the central fuel zone but a greater variation of the outer fuel assemblies. The bulk core outlet instrumentation requirement is not expected to be basically different from the reference design. The safety thermocouple instrumentation requirement will require evaluation, since the extra fuel can length has increased the transit time for sensing flow blockage conditions.

The in-vessel storage facility for fuel handling will not be affected by the proposed modification. Even assuming that half of all the fuel, blanket, or control assemblies are replaced every year, 240 storage positions are provided by normal procedures (in the outer two rows of both storage drums) to accept 226 assemblies.

The upper portion of the fuel assembly will require modification to the reference design for the fuel assembly grab, but eccentricity of 3 inches (diametral) during fuel handling operation can be readily accommodated with the proposed redesign.

3.1.8. Economics

The evaluation study to determine the effects of metal swelling for the recommended modified reference design was conducted using the NAPS computer code. During the identification and evaluation period it was apparent that the twisted-ribbon spacer should be replaced by the helical wirewound spacer. In addition, the plant capacity factor was reduced to a figure nearer the reference design figure of 0.86. Thus, a direct comparison of the NAPS reference design and the recommended modified reference design places an apparent penalty on the solution of metal swelling above the estimated actual solution cost.

The key parameters for both reference and modified designs are identified in Table 3-1. The associated fuel cost for the

reference design is 0.744 mill/kWh and the total power generation cost is 3.474 mills/kWh. The associated costs for the modified design, having a plant capacity factor of 0.85 (compared with the reference design of 0.86, thus allowing for the increased annual refueling operations), are 0.986 mill/kWh for fuel and 3.804 mills/kWh for the total power generation cost. These costs do not include the cost of the core clamp device. Assuming a cost of about \$300,000 for this item, the revised power generation cost would be increased by 0.007 mill/kWh.

Summarizing, the estimated additional total power generation cost for reducing the volumetric metal-swelling growth to 18% with the B&W reference design is 0.337 mill/kWh. The cost of changing from a twisted-ribbon spacer to a wirewound type would add approximately 0.130 mill/kWh to the total power generation cost. It is anticipated that the estimated costs for the proposed modified design could be substantially decreased with optimization; thus, we reduce the apparent cost differential between the reference core and a core that will accommodate 15% volumetric irradiation-induced metal swelling. If, however, we could not achieve the 100,000 MWd/T burnup with the reference design through future R&D, then the penalty associated with modifying the design to accommodate metal swelling would be further reduced. In addition, the reduction of both temperature and fuel burnup resolves itself to a design that is closer to present-day technology, thus reducing total R&D expenditure.

Table 3-1. Key Plant Parameters — Reference and Modified Designs

Basic Parameters		
	Reference design	Modified design
Reactor power, MWt	2450	2580
Avg discharge burnup, MWd/T	100,000	80,000
Fuel cycle life, yr	3	2
Flux > 0.1 MeV, n/cm ² -s (EOL)	3.78×10^{15}	4.36×10^{15}
Flux, total, n/cm ² -s (EOL)	6.80×10^{15}	7.77×10^{15}
Fluence > 0.1 MeV, n/cm ²	3.5×10^{23}	2.3×10^{23}
Bulk core inlet temp, F	800	700
Bulk core outlet temp, F	1100	1050
Steam cycle, psig/F/F	2400/950/950	1800/900/900
Plant capacity factor	0.86	0.85
Core		
Height, in.	34.7	34.7
Pin OD, in.	0.280	0.250
Pin pitch, in.	0.337	0.305
Cladding material	SS 304	SS 316
Spacer type	Tw ribbon	Wirewound
No. of pins per S/A	331	331
No. of fuel S/A	288	288
No. of control S/A	25	25
Pitch of S/A, in.	6.500	6.323
Can material	SS 304	SS 316
Can thickness, in.	0.128	0.140
Volume fractions (1/3 rods in core)		
Fuel	0.442	0.390
Steel	0.165	0.181
Sodium	0.381	0.418
Control	0.012	0.012
Fissile loading, kg	2770	2411
Avg enrichment, %	11.6	12.9
Core avg linear heat rate, kW/ft (BOL/EOL)	8.32/7.75	8.94/8.36
Nuclear power peaking (BOL/EOL)	1.48/1.50	1.52/1.39
Breeding Ratio, EOC	1.35	1.28
ΔK void (EOL), $\Delta K/\Delta\rho$ (core + axial blanket)	0.0183	0.0219
Doppler effect, $-T(dK/dt)$	0.0075	0.0076

Table 3-2. Selected Procedure for Peak Fluence Reduction

	Core	Radial blanket	Axial blanket	Radial refl'r	Axial refl'r
Outer diameter, in.	113.7	136.5	113.7	160.2	113.7
Height, in.	34.7	42.7	14.0	42.7	11.8
Volume, liters	5784	3139	2331	3857	1967
No. of fuel assemblies	288	138	288	--	--
Zone 1	96	--	--	--	--
Zone 2	84	--	--	--	--
Zone 3	108	--	--	--	--
Fuel pin OD, in.	0.250	0.551	0.250	--	--
Fuel pin pitch, in.	0.305	0.581	0.305	--	--
Cladding thickness, in.	0.010	0.017	0.010	--	--
Assembly pitch, in.	6.323	6.323	6.323	--	--
Can thickness, in.	0.140	0.140	0.140	--	--
Sodium gap, in.	0.200	0.200	0.200	--	--
Fuel density, % TD	85	90	85	--	--
Fissile loading, kg Pu	2411	--	--	--	--
Initial enrichment					
Zone 1	0.1112	--	--	--	--
Zone 2	0.1233	--	--	--	--
Zone 3	0.1498	--	--	--	--
Avg	0.1292	--	--	--	--
Fuel residence time, EFPD	620	--	--	--	--
Avg disch burnup, MWd/T	80,000	--	--	--	--
Power, MWt	2580	--	--	--	--

Volume Fractions

	Fuel	Sodium	Steel	Tantalum
Core zone 1	0.3731	0.4320	0.1774	0.0175
Core zone 2	0.3954	0.4130	0.1819	0.0098
Core zone 3	0.4013	0.4079	0.1831	0.0077
Radial blanket	0.5884	0.2433	0.1683	--
Axial blanket	0.3898	0.3900	0.1850	0.0352
Radial refl'r	--	0.5000	0.5000	--
Axial refl'r	--	0.3560	0.1798	--

Table 3-3. Description and Volume Fractions for Modified Core

Procedure	Reduction of fluence, %
1. Reduce fuel residence time: Cycle time — 3.4 to 2.7 yr Burnup — 100,000 to 80,000 MWd/T	20
2. Reduce fuel residence time and volume: Cycle time — 2.7 to 2.0 yr Pin OD — 0.280 to 0.250 in. Burnup — constant at 80,000 MWd/T	6
3. Optimize zone enrichment ratios: Subject to constraint on peak/average power	4
Total	30

Table 3-4. Peak Fluences in the Modified Reference Core

	<u>Fluence, $n/cm^2 \times 10^{23}$</u>	
	<u>>0.1 MeV</u>	<u>Total</u>
Adjusted reference core	2.28	4.02
Reference core	3.26	5.92
Reduction, %	30.1	32.1

Table 3-5. Peaking Factors, Power Fractions, and Average Power Densities

	<u>Time in life^(a)</u>		
	<u>BOL</u>	<u>MOC</u>	<u>EOC</u>
<u>Peaking factors</u>			
Radial core zone 1	1.007	1.052	1.096
Radial core zone 2	1.160	1.165	1.165
Radial core zone 3	1.225	1.155	1.124
Axial	1.241	1.211	1.196
<u>Power fractions</u>			
Core	0.956	0.914	0.894
Radial blanket	0.018	0.031	0.037
Axial blanket	0.026	0.055	0.069
<u>Avg power density, kW/l</u>			
Core	426.2	407.7	398.8
Radial blanket	18.5	31.8	37.9
Axial blanket	28.9	60.5	75.9

Total power = 2580 MWt

(a) BOL: beginning of life; MOC: middle of cycle;
EOC: end of cycle.

Table 3-6. Breeding Ratios

Integrated — EOC, avg burnup 60,000 MWd/T 1.29

Instantaneous

	<u>Radial</u>	<u>Axial</u>	<u>Average</u>
BOL			
Core	0.911	0.859	0.885
Radial blanket	0.184	0.174	0.179
Axial blanket	0.291	0.274	0.283
Overall	1.386	1.307	1.347
MOC			
Core	0.847	0.817	0.832
Radial blanket	0.179	0.173	0.176
Axial blanket	0.292	0.282	0.287
Overall	1.318	1.272	1.295
EOC			
Core	0.819	0.797	0.808
Radial blanket	0.178	0.173	0.176
Axial blanket	0.295	0.287	0.291
Overall	1.292	1.257	1.275

Table 3-7. Reactivity Coefficients, Delayed Neutron Fraction,
and Prompt Neutron Lifetime at EOC

<u>1-D calculation direction</u>	<u>Radial</u>	<u>Axial</u>	<u>Average</u>
Doppler coefficient			
Full dense, $-T(dk/dT)$	0.0077	0.0075	0.0076
Sodium void coefficient (0.2 in. gap)			
Core only, $\rho(\Delta K/\Delta\rho)$	0.0212	0.0226	0.0219
Delayed neutron fraction, β_{eff}	0.00324	--	--
Prompt neutron lifetime, l^* (μs)	0.377	--	--

Table 3-8. Axial Temperature Distribution in the Modified Reference Design

Distance up channel, in.	$\Delta P = 31.1$ psi, inner assy of core I, F	$\Delta P = 34.3$ psi, inner assy of core II, F	$\Delta P = 17.9$ psi, inner assy of core III, F
0.0000	699.6	699.6	699.6
0.5000	699.6	699.6	699.6
4.5000	701.3	701.2	700.9
8.5000	703.9	703.9	702.9
10.5000	706.2	706.1	704.7
12.5000	709.6	709.5	707.3
13.5000	711.6	711.5	708.8
14.5000	715.9	715.8	712.2
15.4840	722.8	722.5	717.4
16.4680	730.1	729.8	723.1
18.4370	746.5	746.0	735.6
20.4060	764.8	764.2	749.7
24.2260	805.3	804.3	780.8
28.0460	850.6	849.1	815.6
31.8660	898.1	896.1	852.1
35.6860	945.6	943.2	888.6
39.5070	990.9	988.0	923.4
43.3270	1031.4	1028.1	954.5
45.2960	1049.7	1046.2	968.5
47.2650	1066.1	1062.4	981.1
48.2490	1073.5	1069.7	986.8
49.2330	1080.3	1076.5	992.0
50.2330	1084.6	1080.8	995.4
51.2330	1086.6	1082.8	996.9
53.2330	1090.0	1086.1	999.5
55.2330	1092.3	1088.4	1001.3
59.2330	1095.0	1091.0	1003.3
63.2330	1096.6	1092.6	1004.5
66.0000	1096.6	1092.6	1004.5

Table 3-9. Flux Distribution

Distance along channel, in.	Inner assay of core I $\times 10^{15}$		Inner assay of core II $\times 10^{15}$		Inner assay of core III $\times 10^{15}$	
	ϕ_{total}	$\phi > 0.1 \text{ MeV}$	ϕ_{total}	$\phi > 0.1 \text{ MeV}$	ϕ_{total}	$\phi > 0.1 \text{ MeV}$
0	0.387	0.043	0.369	0.042	0.177	0.020
0.5	0.580	0.172	0.554	0.167	0.265	0.079
4.5	1.122	0.387	1.071	0.377	0.513	0.178
8.5	1.973	0.773	1.884	0.754	0.902	0.355
10.5	2.515	1.074	2.401	1.047	1.149	0.493
12.5	3.134	1.460	2.992	1.423	1.432	0.671
13.5	3.483	1.675	3.325	1.633	1.592	0.769
14.5	3.831	1.933	3.657	1.884	1.751	0.888
15.484	4.140	2.191	3.953	2.135	1.892	1.006
16.468	4.489	2.405	4.285	2.344	2.051	1.104
18.437	5.224	2.835	4.987	2.763	2.387	1.302
20.406	5.882	3.222	5.165	3.140	2.688	1.479
24.226	6.927	3.810	6.612	3.714	3.165	1.749
28.046	7.507	4.167	7.167	4.061	3.431	1.913
31.866 MP	7.739	4.296	7.388	4.187	3.537	1.972
35.686	7.507	4.167	7.167	4.061	3.431	1.913
39.507	6.927	3.810	6.612	3.714	3.165	1.749
43.327	5.882	3.222	5.615	3.140	2.688	1.479
45.296	5.224	2.835	4.987	2.763	2.387	1.302
47.265	4.489	2.405	4.285	2.344	2.051	1.104
48.249	4.140	2.191	3.953	2.135	1.892	1.006
49.233	3.831	1.933	3.657	1.884	1.751	0.888
50.233	3.483	1.675	3.325	1.633	1.592	0.769
51.233	3.134	1.460	2.992	1.423	1.432	0.671
53.233	2.515	1.074	2.401	1.047	1.149	0.493
55.233	1.973	0.773	1.884	0.754	0.902	0.355
59.233	1.122	0.387	1.071	0.377	0.513	0.178
63.233	0.580	0.172	0.554	0.167	0.265	0.079
66.000	0.387	0.043	0.369	0.042	0.177	0.020

MP: Midplane.

Figure 3-1. Solution for Reduction of Swelling

Figure 3-2. Power Generation Cost Vs Metal Swelling

Figure 3-3. Modified Recommended Design

Figure 3-4. 1000-MWe LMFBR — Reference
Concept Core and Internals
Arrangement (A31100E)

Figure 3-5. Core Clamp Assembly (163340E)

Figure 3-6. Radial Loading Sequence

LEGEND:

- FUEL ASSEMBLY - 288 REQ'D.
- CONTROL ROD - 25 REQ'D.
(S - SAFETY ROD POSITION)
- BLANKET ASSEMBLY - 138 REQ'D.

Figure 3-7. Reference and Modified Fuel and Blanket Assemblies

B = Blanket

C = Core

Figure 3-8. Temperature-Enthalpy Diagram for 1800
psi/900/900F Cycle

Figure 3-9. Modified Reference Core Radial Flux Distribution

Figure 3-10. Normalized Axial Power Shape of Modified Reference Design
(Including Axial Blanket)

Figure 3-11. Normalized Radial Power Shape of Modified Reference Design

Figure 3-12. Volumetric Growth — Modified Reference Design

Figure 3-13. Radial Volumetric Growth Profile at Core Midplane

Figure 3-14. Axial Temperature Distribution for Modified Reference Fuel Assembly No. 12

Figure 3-15. Axial Flux Distribution for Modified Reference Fuel Assembly No. 12

Figure 3-16. History of Maximum Strain for Modified and Reference Fuel Assemblies

Figure 3-17. History of Maximum Deflection for Modified and Reference Fuel Assemblies

Figure 3-18. History of Deflection at Core Midplane for Modified and Reference Fuel Assemblies

Figure 3-19. Axial Distribution of Deflection for Modified and Reference Fuel Assemblies

Figure 3-20. Time History of Restraint Load at Intermediate Support for Modified and Reference Fuel Assemblies

Figure 3-21. Time History of Maximum Stress for Modified and Reference Fuel Assemblies

Figure 3-22. Axial Stress Distribution for Modified and Reference Fuel Assemblies

Figure 3-23. Maximum Coolant and Fuel Temperature Vs Time — Flow Transient

Figure 3-24. Energy Generation Vs Doppler Coefficient

3.2. Effect on the Accident Analysis Study

In general, this study indicates that the reference design could be successfully modified to accommodate the metal swelling currently predicted by available correlations, but the conceptual changes defined in the evaluation would impose some limitations on overall plant safety and economic performance characteristics. If the degree of swelling calculated from the current correlation is confirmed, several fairly significant changes will be required in both plant parameters and design features to reduce the growth of the structural materials and safely accommodate the residual dilation and deflection of the fuel assemblies, control rod guide tubes, and other related structures.

Furthermore, even if the degree of swelling currently predicted is reduced considerably (as expected when experimental data are obtained at conditions closer to those in the reference design, or when structural materials with improved resistance to irradiation-induced swelling are developed), an open fuel assembly core arrangement and a core restraint system similar to the one defined in this evaluation would probably be required. However, should a significant reduction in swelling be achieved, the plant parameters reflected in the composite solution could probably be improved to approach or equal the optimum values in the reference design.

The study evaluation also indicates that the composite solution affects the safety characteristics of the plant in a number of ways, but so far most of the changes appear to produce relatively minor differences in the overall probability of accidents or in system response. Consequently, except for the radial expansion characteristics of the core, the safety characteristics of the modified design are not expected to differ significantly from the reference design. However, the radial expansion characteristics of the core have changed to the extent that positive reactivity is inserted during life, and a prompt positive reactivity feedback may be obtained in certain transients because of the requirement for an open core arrangement and its response with the selected core restraint system. It is conceivable that prompt negative but delayed positive feedback could occur in other transients. In either case, the response could drastically alter the course and consequences of potential accident situations.

Unfortunately, the magnitude and time-dependence of transient bowing and core radial expansion in an open core arrangement are so complex that the phenomena cannot be accurately assessed at this time. Furthermore, substantial effort would be required to produce reliable models to handle the coupled neutronics and thermal and structural phenomena and then apply them to evaluate the transients from the time of the initiating condition to the point at which the accident is either controlled or terminated. However, since open-type cores may be essential to accommodate metal swelling in view of the implication and the importance of potentially positive radial expansion feedback coefficients on the safety characteristics of LMFBR plants, we believe that this problem should be given additional consideration on a high-priority basis. Accordingly, B&W will propose additional work (1) to evaluate the radial expansion characteristics of the modified design, including the development of the necessary analytical procedures or models; (2) to perform design sensitivity studies in an effort to minimize or eliminate adverse radial expansion characteristics; and (3) to reflect the final coefficient(s) in the accident analysis activities in the A²S² Design Study.

3.3. Review of Experimental Data and Growth Correlations

3.3.1. General

The irradiation behavior of reactor materials was reviewed with particular emphasis on potential LMFBR fuel cladding materials. The leading material candidates for the fuel cladding and core structural components for the LMFBRs are the austenitic stainless steels. Knowledge of their physical and mechanical properties during irradiation and exposure to high temperatures is important to the design and the safety evaluation of LMFBRs.

The review revealed that there is no basic general understanding of radiation effects in material and that it is not possible to predict superior irradiation performance for any given type of metal. The technology has not advanced to the point at which the application of theory and established principles can guide a materials specialist to the development of a "radiation-proof" alloy, nor are material data available to enable reactor designers to compare different incore materials for economic reactor environment parametric evaluations.

The effects of fast neutron irradiation on the microstructure and mechanical properties of stainless steels is being evaluated both in this country and in Europe. At present the only operating fast reactor in this country is the Experimental Fast Breeder Reactor II (EBR II). Since this facility is not fully instrumented to measure neutron flux and temperature, e.g., in some cases to within only $\pm 50^\circ\text{C}$, quantitative studies are difficult. In addition, its characteristic neutron energy spectrum is considered to be "harder" than that for LMFBR design spectra. Also, the long exposure times—greater than three years—which are necessary to accommodate representative fast fluence levels of $2 \times 10^{23} \text{ n/cm}^2$ can only come gradually over the next several years to ensure a reasonably reliable choice of LMFBR core materials.

Void formation in face- and body-centered cubic structural materials subjected to fast neutron ($E > 1 \text{ MeV}$) bombardment was first reported by Cawthorne and Fulton¹ in austenitic stainless steels. It has since been discovered in aluminum² and nickel³ after they have been exposed to fast neutron fluxes. Cawthorne and Fulton noted in their initial observation that the cavities, too numerous to be explained

in terms of gas-filled bubbles, were probably the result of vacancy agglomeration. This conclusion has since been borne out by annealing studies.⁴

The most recent examination of neutron damage to high-purity aluminum² has revealed the presence of voids in specimens exposed to 3×10^{19} n/cm² at 50C and allowed the identification of loops of both vacancy and interstitial character.

Voids in molybdenum⁵ were observed when specimens were irradiated at 700C to a neutron fluence of approximately 1.4×10^{20} n/cm², $E > 0.1$ MeV. Voids may possibly be nucleated at lower temperatures, but this was not observed. The irradiation of tungsten appears to follow a pattern similar to that for molybdenum. Masters and Brimhall³ found voids in pure nickel foil after irradiation in a fast fluence of 4×10^{19} n/cm² ($E > 1$ MeV) at a temperature of 425C. These voids were not associated with either surface but lay within the foil.

Since the early days of reactor technology, ceramic materials have been known to exhibit bulk dimensional changes when subjected to relatively low fluence and temperature.

Voids have been reported in stainless steel after fluences in excess of 10^{22} n/cm² have been attained. This difference of several orders of magnitude before the formation of voids is encouraging in that it offers a potential for selecting, modifying, or developing new materials to satisfy the requirements for fast reactor core components that will be subjected to fast neutron fluences far in excess of those previously encountered in thermal reactors.

A number of irradiated type 304 stainless steel samples were examined by transmission electron microscopy; three general characteristics were revealed. First, after irradiation between 371 and 650C (700 to 1200F) the microstructure exhibited a large number density ($\sim 10^{15}$ /cm³) of polyhedral and Frank dislocation loops. Second, the microstructure contained no defects at the grain boundary; this behavior contrasts with the irradiation of austenitic stainless steels in thermal reactors, where a large number of gas-filled cavities are formed at the grain boundaries. Third, the size distribution of voids in the irradiated microstructure was narrow. The standard deviation of the size distribution curve was from 0.3 to 0.4 of the average void size.

The effect of temperature on void formation is striking. The phenomenon appears to be limited to a relatively narrow temperature range of 300 to 650C (650 to 1200F). The effect of neutron exposure on void volume appears to be exponential. Several features still remain uncertain: the effect of applied stress during irradiation and the possible similarity of axial and radial growth factors in thin-walled sections.

A recent compilation of irradiation-induced swelling in types 304 and 316 stainless steel is shown in Figure 3-25. The temperature dependency curves shown for solution-treated and 20% cold-worked material are from empirical equations developed by PNL and WARD, normalizing to 5×10^{22} n/cm² ($E > 0.1$ MeV). Through agreement urged by the AEC on a correlation of available swelling data, these laboratories have fitted the data by a multiple least-squares regression analysis of the following form:

$$\Delta V/V = A(\phi)^n \exp(-Q/RT)$$

where

V = volume,

ϕ = fluence (n/cm², $E > 0.1$ MeV),

T = irradiation temperature (K),

A , n , Q = other constants.

As can be seen from Figure 3-25, the scatter of result points is significantly large, while at the same time the scarcity of fully instrumented experimental data dictates a simple approach.

There are indications that swelling due to neutron irradiation may be a function of the neutron energy and flux. If this is an important variable in irradiation swelling, then all data correlations based on experience with the hard spectra of EBR II and DFR might be unduly pessimistic for softer spectrum cores like the oxide core proposed for B&W's reference core. The experience with the French reactor Rhapsodie is an example in which much less swelling has been observed.

3.3.2. Growth Correlations

Analysis of the irradiation-induced swelling in the B&W reference design began with the review of four swelling correlations:

1. PNL, Westinghouse—equation for solution-treated types 316 and 304 stainless steel.
2. General Electric—correlation for type 304 stainless steel.
3. PNL, Westinghouse—equation for 20% cold-worked type 316 stainless steel.
4. ANL data.

These correlations are described in detail below.

1. PNL, Westinghouse — Equation for Solution-Treated Types 316 and 304 SS: This correlation⁶ is based on the results of density measurements of solution-treated type 304 SS components from EBR II and type 316 SS tensile specimens. The equation is shown below.

$$100 \frac{\Delta V}{V} = 4.9 \times 10^{-49} (\phi t)^{1.71} 10 \left(\frac{1.55 \times 10^4}{T} - \frac{5.99 \times 10^6}{T^2} \right),$$

where

$$100 \frac{\Delta V}{V} = \text{volume change (\%)},$$

ϕt = fluence, n/cm^2 ($E > 0.1$ MeV),

T = temperature (K).

Figure 3-26 shows volume change (percent) plotted as a function of temperature for a given fluence. The correlation predicts peak growths between 925 and 950F for any given fluence. In the B&W reference design, the peak fluence ($E > 0.1$ MeV) in the core (3.2×10^{23} n/cm^2) occurs at the midplane, where the temperature is approximately 950F. This "worst" combination of temperature and fluence results in a peak growth of 80%. Because of the extremely high growths predicted, compared to the predicted growth for cold-worked material, this correlation was not used for design considerations.

2. General Electric — Correlation for Type 304 SS: This correlation² is also based on EBR II data. The equation is shown below.

$$100 \frac{\Delta V}{V} = 7.53 \times 10^{-5} (\phi t \times 10^{-22})^{1.62} (T - 360)^{0.898} (775 - T)^{0.7111},$$

where

$$100 \frac{\Delta V}{V} = \text{volume change (\%)},$$

$$\phi t = \text{fluence (n/cm}^2, E > 0.1 \text{ MeV)},$$

$$T = \text{temperature (C)}.$$

Figure 3-27 is a plot of volume change (percent) versus temperature for a given fluence. Peak growths are predicted at 1100F. In terms of the reference design ($\phi t = 3.2 \times 10^{23}$ n/cm² and $T = 950\text{F}$), the peak growth is 100%. As with the PNL-Westinghouse solution-treated equation, this equation predicts growths substantially larger than for cold-worked material. Therefore, no further work was performed with this correlation.

3. PNL, Westinghouse — Equation for 20% Cold-Worked

Type 316 SS: This correlation¹ is also based on data from EBR II materials. The equation is as follows:

$$100 \frac{\Delta V}{V} = 1.01 \times 10^{-36} (\phi t)^{1.69} \left(\exp - \frac{7800}{RT} - 5480 \exp - \frac{25,300}{RT} \right),$$

where

$$100 \frac{\Delta V}{V} = \text{volume change (\%)},$$

$$\phi t = \text{fluence (n/cm}^2, E > 0.1 \text{ MeV)},$$

$$T = \text{temperature (K)}.$$

Figure 3-28 is a plot of volume change (percent) as a function of temperature for a given fluence. This correlation predicts peak growths at 1150F for any given fluence. Therefore, the peak fluence (3.2×10^{23} n/cm², $E > 0.1$ MeV) results in a volumetric growth of 35%. This growth is much less than that predicted by either the PNL solution-treated equation or the GE correlation. Therefore, the cold-worked material was chosen for the core components of the modified B&W design, and this correlation was selected for use in this adjunct study.

4. ANL Data: ANL transmitted data to B&W on the swelling of stainless steel structures exposed to high-energy neutrons.^{3,4} These data represent the Argonne-supplied "swelling correlation." The data were plotted on log-log paper as straight lines in the form of volumetric growth versus fluence at constant temperature. The fluence is based on

total flux. An equation of the form

$$\text{Volumetric growth} = C_1 (\text{fluence})^{C_2}$$

was determined at each temperature for use in computer calculations. The coefficients of these equations are listed in Table 3-10. A linear interpolation method was used to determine growths at temperatures between the given values. To calculate growths above 600C (1112F), the 600C equation was used. Similarly, below 400C (752F), and 400C equation was used. Figure 3-29 is a plot of volumetric change (percent) versus temperature at constant fluence for the ANL data. Unlike the three previous correlations, the maximum growth predicted for a given fluence varies with temperature. At midplane conditions ($\phi t \approx 6 \times 10^{23}$ n/cm² and 950F) the volumetric growth was approximately 27%. Both the ANL data and the PNL 20% cold-worked correlation were used in this study.

3.3.3. Mechanical Properties

Measurements at 450C (840F) indicate that the true yield strength of the irradiated material increases rapidly and does not appear to saturate with increasing neutron exposure.¹⁰ This effect, however, decreased with increasing test temperature and appeared to vanish at 750C (1380F). Lower irradiation temperatures have resulted in higher yield strength for specimens exposed to the same neutron fluence.

The effect of irradiation on tensile strength is much less pronounced. An effect of neutron exposure on the tensile strengths was found only at the highest fluence levels (4.8×10^{22} n/cm²) at a temperature of 450C. The uniform and total strains were about the same for both irradiated and unirradiated material. The strain decreased with increased test temperature for both irradiated and unirradiated material.

Report GEAP-10062 deals with the analysis of published information and suggests the use of mechanical properties for current designs.¹⁰ The available data are primarily from irradiation in EBR II and the DFR; all are either at low temperature during irradiation, at relatively low fluence, or both. Because no data are available at fluences greater than 10^{23} n/cm², extrapolation to LMFBR design conditions must be considered tenuous. However, it appears reasonable to assume that

the minimum property levels at fluences less than 10^{23} n/cm² will not be improved with continuing exposure. On the basis of these data the following property limits have been recommended for first-round design analysis for irradiated type 304 and 316 stainless steel at total fluences above 10^{22} nvt:

<u>Yield stress at 0.2% offset, psi</u>		<u>Ultimate stress, psi</u>
44,000 at 500C		58,000 at 500C
31,000 at 600C		46,000 at 600C
20,000 at 700C		30,000 at 700C
Uniform elongation	<1%	
Total elongation	<1%	
Maximum creep rate	Little or no increase due to irradiation	
Time to rupture	Reduced significantly	
Creep strain to fracture	As low as 1%	

However, in addition to satisfying the requirements for high fluence rating, incore components must also meet the unirradiated design criteria for BOL, which can control design stress¹¹ limitation as follows:

<u>Yield stress at 0.2% offset (type 304/316), psi</u>	<u>Ultimate stress (type 304/316), psi</u>
21,000/32,000 at 500C	69,000/69,000 at 500C
21,000/29,000 at 600C	54,000/57,000 at 600C
15,000/24,900 at 700C	36,000/41,000 at 700C

3.3.3.1. Cold Worked Selection

The results to date indicate that cold-working reduces the amount of radiation swelling primarily by decreasing the concentration of voids. The use of cold-working as a technique for controlling the amount of swelling in LMFBR components will depend on the deviation distribution of swelling data in cold-worked and annealed materials at fluence and temperature.

Since 10% cold-worked material annealed at 482 and 700C before irradiation swelled essentially the same amount as

annealed material,¹² the use of cold-worked materials at irradiation temperatures above 700C appears to be questionable.

3.3.3.2. Creep Rupture Behavior

The study of reactor creep behavior presents large and costly experimental problems since it requires long-term remote application of loads and compact but stable instrumentation at elevated temperatures. Few experiments have been successful to date, and consequently, the amount of reliable data obtained is relatively small. Although several investigators have tried to obtain creep rate information, the numerous possible materials and conditions make it unlikely that the required information to exactly fit the design condition will be found. The extrapolation of fairly short-term creep data for long-term applications requires an empirical correlation, several of which have been proposed. No attempt is made here to analyze in detail the published data on the creep behavior of irradiated and unirradiated stainless steel (types 304 and 316); detailed survey data can be obtained from references 10, 13, and 14. For the present study, PNL data have been selected for simplification and future continuity of the work.

Creep data for the nuclear applications were developed in two different stages. The more commonly known thermal creep behavior can be found in various forms. For long-term application within the elastic limits, the primary creep rate (decreasing under constant stress) is generally neglected. The secondary creep rate (usually taken as thermal creep rate) is usually plotted against stress for various temperatures. For type 316 stainless steel this is shown in Figure 3-30. For a long time it was assumed that the radiation did not have any significant effect on the creep rate. Initially, this assumption was based on the data from thermal reactors; however, there appear to be some conflicting views on the effect of fast reactor environment. Recently, more and more evidence indicates that the creep rates are increased in the presence of fast neutron bombardment. This neutron bombardment appears to upset the structure of materials by introducing interstitials, vacancies which collapse to form dislocation loops, and foreign elements, thus tending to accelerate the creep rate.

The effects of fast neutron irradiation and environment on the creep and rupture behavior of candidate vessel and

core structural materials for the LMFBR are being studied at ANL and PNL. The quantitative correlation suggested by PNL shows that the neutron-induced creep can be considered the sum of a transient strain component that saturates at low fluence plus a steady-state component. The most recent available correlation equations used by PNL (as of March 1970) are as follows:

Neutron-Induced Creep in FTR

Type 304 or 316 solution-treated SS:

$$\bar{\epsilon}/6\bar{\sigma} = 0.18 (1 - \exp - \phi t / 1.465 \times 10^{21}) 10^{-8} + 1.87 \times 10^{-30} \alpha(\phi t);$$

Type 316 cold-worked SS:

$$\bar{\epsilon}/6\bar{\sigma} = 0.782 (1 - \exp - \phi t / 6.37 \times 10^{20}) 10^{-8} + 1.87 \times 10^{-30} \alpha(\phi t);$$

where

$$\alpha = \exp (1.405 - 0.0027T),$$

$$\bar{\epsilon} = \text{effective strain, psi,}$$

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

$$\phi t = \text{neutron fluence, } E > 10^{-10} \text{ MeV, n/cm}^2,$$

$$T = \text{metal temperature, K.}$$

The variation of thermal creep rate with stress is shown in Figure 3-30 for various temperatures. The constant value at a particular stress of 15,000 psi is also shown in Figure 3-31 for comparison with the neutron-induced creep rate proposed earlier. The steady-state component of neutron-induced creep rate is also shown in Figure 3-30 for a similar comparison. All these results were obtained from the published results of PNL for the FFTF reactor design materials data.^{11, 15, 16} Whether or not these data predict lower creep rates than other published data is questionable. The main problem stems from the fact that extrapolation from the numerous possible materials and conditions may scatter the data considerably. The present selection of data for this study is based on the selection for simplification in the computer code CRASIB input.

The importance of creep in the present study can be strikingly explained by the example given in the present FFTF report by BNWL.¹⁹ An unrestrained FFTF fuel assembly duct at goal burnup is predicted to bow outward more than an inch as a result of the radial swelling gradient if creep is not considered. However, if neutron-induced creep is considered (thermal creep is negligible because of low temperatures), the unrestrained duct is predicted to bow inward about 1/4 inch. This unrestrained deflection is also very sensitive to the temperature gradient across the duct, since the neutron-induced creep causes the thermal stress in the duct to decrease considerably from its initial value at BOL. Under most conditions in the FFTF, neutron-induced creep is predicted to keep the bending stress below its initial thermal value, even though the swelling gradient is active enough to increase the bending stress. Thus, this would suggest that a conservative approach would be to select the lower creep rates. There is some evidence that, in the present metal-swelling study, increasing and decreasing the creep rates by factors of 2 and 1/2, respectively, does not change the results significantly.

To complete the creep behavior survey for the present study, the rupture life behavior is also included here. Figure 3-32 shows the published survey by Comprelli and coworkers.¹⁰ The combined data on the time before rupture for type 304 and 316 stainless steel were obtained from the DFR and EBR II reactors. The recent published data by PNL¹⁵ also shows good agreement with these results.

Table 3-10. Coefficients for ANL Growth Equation

<u>Temperature, C</u>	<u>C₁</u>	<u>C₂</u>
400	1.06467×10^{-22}	0.97688
450	1.42326×10^{-23}	1.01913
500	5.57175×10^{-27}	1.16567
525	3.64843×10^{-30}	1.30001
550	6.44568×10^{-35}	1.50056
575	4.62410×10^{-36}	1.54396
600	3.28210×10^{-37}	1.58970

Figure 3-25. Irradiation-Induced Swelling in Austenitic Stainless Steels

Figure 3-26. PNL Growth Correlation for Solution-Treated Types 304 and 316 Stainless Steel

Figure 3-27. GE Growth Correlation for Type 304 Stainless Steel

Figure 3-28. PNL Growth Correlation for 20% Cold-Worked Type 316 Stainless Steel

Figure 3-29. ANL Growth Data

Figure 3-30. Minimum Thermal Creep Rate for Type 316 Stainless Steel

Figure 3-31. Neutron-Induced Creep Rate — PNL, March 1970

Figure 3-32. Time to Rupture in Fast Reactor Environment at Fluences Above 10^{22} nvt — Types 304 and 316 Stainless Steel

3.4. Identification and Evaluation of Potential Solution

3.4.1. General

To gain an understanding of the interaction of the metal swelling phenomena on the B&W LMFBR system in order to select a recommended design (identified in section 3.1), it was considered essential to evaluate several possible solutions. These solutions were evaluated as separate studies, using the reference design as a point of departure. The reference core was analyzed using both the ANL data and PNL's 20% cold-worked correlation. In perturbing the design from the reference point, the number of design changes was minimized for each study to have a better understanding of the singular effects of the item being studied. However, the investigation of the main study item required the variation of other design parameters.

The main subjects of investigation were the flux and temperature variations and mechanical devices required to accommodate the reduced growth. In the evaluation of radial core restraint, the bowing characteristics were analyzed in reasonable detail. The economic effects of the growth evaluation were also evaluated. To compare the separate studies meaningfully, the NAPS code was used for the economic evaluation, and the core midplane conditions were used to expedite the evaluation of the effects on growth. It should be noted that the core midplane conditions do not give maximum growth. Assuming a 3.73×10^{15} n/cm²-s flux, three years of life, and a load factor of approximately 0.9, the resulting fluence is 3.15×10^{23} . The relationship between metal swelling and burnup, assuming a linear fluence relationship at various temperatures for the reference design (based on PNL data), is shown in Figure 3-33. This graph was used to evaluate the effects of temperature, fuel burnup, and cycle time. It shows a 32.5% growth at a core midplane temperature of 950F and 100,000 MWd/T burnup; the actual maximum growth for the reference design is 37.8%.

3.4.2. Evaluation of the Reference Design

3.4.2.1. Core Design

The active core for this design consists of hexagonal fuel assemblies, blanket assemblies, and control rods arranged in a modified hexagonal configuration within the core vessel. In

Figure 3-34 the assemblies are numbered in radial zones for identification, and the core is divided into 28 axial increments (see Table 3-11 for the location of the axial zones).

A wrapper can is located around each fuel blanket and control assembly to assist in the following:

1. To keep the coolant from bypassing the high flow resistance of the orificed fuel assembly.
2. To provide a barrier against local accident propagation.
3. To provide a structural matrix throughout the core to support the fuel pins.
4. To form a convenient unit for fuel handling operations.

These cans, because of their exposure to high fluxes and fluences in the core region, present problems associated with irradiation-induced swelling caused by void formation. Although the rate of swelling is most rapid in structures near the center of the core, the most severe problem is encountered at the core periphery, where sharp temperature and flux gradients occur. Since swelling is a function of both temperature and flux, differences in swelling behavior occurring across a duct can produce distortions and bowing.

Fuel assembly axial growth due to high temperatures or metal swelling can be readily accommodated in the design of fuel pins and cans. Radial movement is far more difficult to accommodate and is therefore the major problem area. The three major aspects affecting radial growth are as follows:

1. Thermal temperature gradients across the fuel assembly.
2. Bulk metal irradiation-induced growth across the assembly and the core.
3. Differential metal irradiation growth across a fuel assembly resulting in bow.

Thermal gradients can produce dimensional changes that could result in adverse fast reactivity changes; this is not desirable. Bulk differential metal growth alone will not cause fast reactivity changes, but if not given adequate consideration, its lifetime effect may be faster reactivity responses when thermal gradients act.

The CHESS code was used to determine the axial temperature distribution in each bundle (numbered in Figure 3-34) for use in the growth correlations. End-of-cycle power profiles were used so that temperatures would be consistent with the available flux values. Average channel conditions were calculated and assumed to be representative of average bundle values. No cell-to-cell fluid mixing is accounted for in the CHESS code.

Two flux distributions were required because the ANL data are based on a total flux, whereas PNL's 20% cold-worked equation is based on a flux with neutron energies greater than 0.1 MeV. These flux values were calculated using the TODB code at the end of cycle. EOC flux values are higher than mid-cycle values; therefore, the resulting calculated growths are considered to be maximum values.

Appendix B identifies the average temperature of the axial bundle for each assembly, along with the total flux and flux ($E > 0.1$ MeV) for the reference design at EOC.

3.4.2.2. Growth Calculation

The volumetric change (percent) was calculated in an open-shop computer code, GROW, where temperature and flux values are input and the program calculates the fluence after one, two, and three years. The combination of temperature and corresponding fluence is used in the growth correlation. Linear growth is calculated from the volumetric growth by the following equation:

$$\% \text{ linear growth} = \left[\left(1.0 + \frac{\text{vol } \% \text{ change}}{100.0} \right)^{1/3} - 1.0 \right] 100;$$

this assumes that growth is isotropic.

Figure 3-35 is a plot of linear growth (percent) versus distance along the channel for the PNL 20% cold-worked equation. These linear growths are representative of assemblies that have been exposed for three years. Radial zone 1 is the innermost core assembly and radial zone 12 is the outermost core assembly. Zone 13 is typical of the blanket assemblies. The peak growth occurs just above the core midplane and is therefore a compromise between fluence and temperature.

A 30% reduction in fluence in zone 1 will result in a 45% reduction in growth. This illustrates the incentive for fluence reduction as a means of reducing growth.

Lowering the operating temperature range will also reduce volumetric growth. Figure 3-36 compares volumetric growth (predicted by PNL's 20% cold-worked equation) for two core inlet temperatures—700 and 800F; all other input parameters are constant. Lowering the core inlet temperature 100 degrees results in a 22% reduction in peak volumetric growth.

Figure 3-37 shows linear growth (percent, after three years of exposure) as a function of core radius for the PNL 20% cold-worked equation. Layer 1 is just above the core midplane, and layers 7 and -7 are the upper and lower edges of the active core. As in Figure 3-35, the advantage of reducing flux (fluence) can readily be seen. Figures 3-38 and 3-39 show the growth results for the reference design using the ANL growth data.

Figures 3-40 and 3-41 are graphic comparisons of results for the PNL 20% cold-worked correlation and the ANL data. The PNL equation predicts a larger peak growth in the core. However, in the low-temperature, low-flux regions of the core, the ANL data predict higher growths. This effect is due to the neutron energy spectrum of the reference design and also to the different forms of the correlations. ANL data are based on the total neutron flux, while the PNL correlation is based on a flux with neutron energies greater than 0.1 MeV. A design change that alters the energy spectrum will alter the relation between these two correlations. For this reason, both swelling correlations have been considered when examining further design changes.

3.4.2.3. Flow Redistribution

The preceding five sections have dealt with the growth problem in terms of the reference design. The growth will result in a change in flow resistance (and thus pressure drop) in each bundle. To establish a constant pressure drop across the core, the flow in each bundle will redistribute. This creates the possibility that a relatively new bundle may receive less flow than that required to meet cladding temperature limits. However, it has been shown that the change

in core flow resistance from BOL to the end of cycle III alters the average core pressure drop only 3% (assuming constant flow over life). The method of analysis is discussed below.

As shown previously in Figure 3-34, the core is divided into three enrichment zones (numbered 1, 2, and 3). A "typical" bundle from each zone was analyzed to determine its growth at BOL and after one, two, and three years. The CHESS code was then used to determine the pressure drop-flow characteristic at each stage of growth for each bundle. For control rod assemblies (CRA) the flow characteristic was assumed to be of the form

$$W = K(\Delta P)^{1/2},$$

where

W = flow rate, lb/h,

ΔP = pressure drop (including the orifice), psi,

K = constant.

Blanket assemblies were assumed to have unchanging W - ΔP characteristics with life. The low temperatures and low fluxes associated with the radial blankets result in very low growths. From the cycle refueling scheme described in Volume 2 of the 1000-MWe LMFBR Follow-On Study,²⁵ the number of assemblies exhibiting each W - ΔP characteristic was determined. Because the majority of growth occurs during the second and third year of an assembly's exposure, the beginning and end of the third year cycle (BOC III and EOC III, respectively) were assumed to be the worst cases for design consideration. Taking the W - ΔP characteristics and the number of assemblies at each characteristic, a plot was made of total core flow versus core pressure drop at BOL, BOC III, and EOC III (see Figure 3-42). Assuming that the total core flow is constant over life ($W_{\text{tot}} = 92.7 \times 10^6$ lb/h, from Volume 2 of the 1000-MWe LMFBR Follow-On Study), the core pressure drop will change by only 1 psi. The overall pressure drop (including elevational changes) of the reference design is 43.4 psi. Therefore, 1.0 psi represents only a 3% change.

The fuel management scheme is currently a "salt and pepper" type in-place refueling. This means that assemblies

that have been exposed for three years (assemblies with maximum growth) are distributed throughout the core. As a result, significant regional flow changes do not take place. Table 3-3 indicates the change in flow (percent) in each core region.

The differences in flow and fluid temperature were determined for two average channels—one that reflects growth and one that does not. An average channel from the innermost core bundle (where maximum growth occurs) was examined at EOC. The growth accounted for a 6.5% flow increase, which for this channel resulted in a 23-degree temperature drop. This local analysis has not accounted for the interaction of two adjacent channels (i.e., thermal mixing or fluid cross flow).

In summary, the growth phenomenon creates neither flow distribution nor cladding temperature problems. As channels grow, flow increases in core regions 1 and 2. Channel temperatures will decrease, which will act to retard further growth (but this is only a small effect). Temperatures should increase slightly in the outer bundles (core region 3) because flow is decreasing. However, if local flow changes in the center of the core are only about 6.5% (growth is maximum), then local effects should be even less in regions of less growth.

With these results the advantages of the double-walled fuel can diminished and the thermal hydraulic analysis study of the double-wall can was terminated. The results of that investigation are shown in Appendix C.

3.4.3. Physics Study

3.4.3.1. General

The physics work had two objectives: first, to reduce the neutron fluence in the core to minimize bulk metal swelling. Two options are plainly visible—a reduction in the flux level and a reduction in the fuel residence time. A third option, the softening of the neutron spectrum, applies to the high-energy fluence. This option was not emphasized in the limited time available because previous experience has shown that softening the neutron spectrum, for example with BeO, has a very adverse effect on breeding. The second objective was to flatten flux and fluence distributions in the core to reduce the deflections

caused by differential swelling. These deflections are particularly severe near the core edge where the flux gradient is large.

The methods investigated to decrease fluence levels and gradients in the core are summarized in Table 3-13. Since the fluence distribution is relatively flat over much of the core, the bulk metal swelling was investigated from the standpoint of reducing peak fluence. Several correlations between fluence and metal swelling are in use; they require different data. The PNL correlation is based on the fluence above 0.1 MeV, and the ANL correlation is based on the total fluence. For this reason, we investigated the effects of the modifications summarized in the table on both the fluence above 0.1 MeV and the total fluence.

3.4.3.2. Methods

Except for the axially zone-loaded core analyses, all physics calculations were performed in one dimension using FARED, a fast reactor physics design and analysis code. Alternate radial and axial criticality calculations were made at BOL to converge the transverse bucklings. Depletion calculations were made in the radial direction only. The average power over life in the core plus radial blanket was assumed to be the same as that for the reference core. Axial power and flux peaking factors were obtained at BOL from the axial calculations. At other times during life they were assumed to vary with burnup in proportion to their variation with burnup in the reference core. All calculations were made in 20 groups. The fluxes in the first eight groups were summed to obtain the flux above 0.111 MeV. One-dimensional calculations were made with FARED for the reference core to serve as a basis for comparison.

Two-dimensional calculations were made with TODB for the axially zone-loaded core. Cross sections were obtained from FARED. A similar calculation was made for the reference core for comparison.

Fluences were obtained assuming a linear variation of flux with burnup at each mesh point in the radial depletion calculations. The average flux values over life were multiplied by the fuel residence time to obtain radial fluence distributions.

The power generation costs were obtained with the NAPS (sodium parameter study) code. It is considered that NAPS gives reasonably accurate indications of the relative power generation costs for different design modifications, although the absolute values for the power generation costs may not be correct.

1. Reduction of Fuel Residence Time — The cycle time for the reference core was reduced from 3.4 to 2.1 years with an accompanying proportional reduction in average discharge burnup from 100,000 to 62,000 MWd/T. The results are summarized in Table 3-14. The resulting reduction in peak fluence was essentially proportional to the reduction in fuel residence time, 38%, for both the total fluence and the fluence above 0.1 MeV. Figure 3-43 shows peak fluence as a function of burnup in the reference core.

Because of the decreased burnup, the initial average enrichment was lowered from 0.1162 to 0.1133. The lower enrichment results in a slightly larger breeding ratio. Power generation costs increased from 3.38 mills/kWh for the reference core to 3.70 mills/kWh for the 2.1-year cycle. Power generation costs as a function of burnup are shown in Figure 3-44.

2. Reduction of Fuel Residence Time and Fuel Volume (burnup held constant) — The reference core was modified by decreasing the fuel pin OD from 0.280 to 0.225 inch and increasing the active fuel height from 34.7 to 41.0 inches. The pin size was determined by keeping the same total length of pin as the reference core and requiring 100,000 MWd/T burnup with approximately a 2-year cycle. The core height was increased to optimize the economics. The physical parameters of the modified design are compared with those of the reference design in Table 3-15. The results are summarized in Table 3-14. The peak total fluence and the peak fluence above 0.1 MeV were reduced 11.2 and 12.1%, respectively, from those in the reference core. The peak fluences occur on the core centerline for both cores. The reduced fuel volume decreases the fissile loading from 2769 to 2054 kg plutonium but increases the average initial enrichment from 0.1162 to 0.1375. The increased enrichment lowers the core breeding ratio significantly. The power generation costs are increased from 3.38 mills/kWh in the reference core to 3.45 mills/kWh.

From Figures 3-43 and 3-44 it can be seen that the peak fluences and power generation costs that were obtained for this modified design could have been obtained by reducing the burnup in the reference core to approximately 88,000 MWd/T by reducing only the fuel residence time. This implies that the power generation cost penalty as a function of reduction in fluence (percent) is approximately the same for either method of fluence reduction. Hence, to achieve a given percent of reduction in fluence, the two methods of fluence reduction may be combined in varying relative amounts to adjust burnup and cycle time without affecting the associated economic penalty.

3. Annular Core Design — To investigate the annular core concept and its effects on fluence, we retained the reference core composition, 100,000 MWd/T average discharge burnup, and the same total length of pin as the reference core. Two annular core designs, one 29.5 inches thick with a 36.2-inch active fuel height, and one 17.7 inches thick with a 61.7-inch active fuel height, were examined; the results are summarized in Table 3-16.

As the annulus becomes thin, large reductions in fluence are possible—more than 30% in peak total fluence and 23% in peak fluence above 0.111 MeV for the 17.7-inch-thick annulus. The difference in the reductions in total fluence and fluence above 0.1 MeV is the result of the harder spectrum that results from increased leakage in the annulus. However, the penalty in increased power generation costs is high, principally because of the large fissile loading required—over 4000 kg of fissile plutonium in the 17.7-inch-thick annulus. As shown in Figures 3-45 and 3-46, the penalty in power generation cost for a given percent reduction in fluence is considerably greater than the penalty incurred by simply reducing the fuel residence time in the reference core. In addition, the internal breeding ratio of the annular core is significantly lower than that for the reference core because of the higher enrichment. Although increased breeding in the radial blankets compensates for this so that the overall breeding ratio remains about the same or even increases, the low internal breeding ratio means that the annular core requires a large excess reactivity at BOL.

The flux gradient at the core-radial blanket interface is not significantly affected by the thickness of the annular core; this is illustrated in Figures 3-47 and 3-48. However, the flux levels are higher than those in the reference core. Because of the exponential dependence of metal swelling on fluence, the same gradient results in more severe gradients in metal swelling at a higher flux level. On the other hand, the thinner the annulus, the more rapidly the flux gradient decreases with distance into the core. This means that fewer fuel assemblies or portions of fuel assemblies will be exposed to the steep flux gradient.

4. Reflectors — All of the radial blanket fuel assemblies in the inner row were considered removed and replaced with reflector assemblies. Beginning-of-life enrichment searches were made to keep the k_{eff} and the relative zone enrichment ratios equal to those of the reference core. The necessary enrichment is decreased in a reflected core because of the decreased leakage. Beginning-of-life criticality calculations were made on the resulting compositions to determine the effect of the reflectors on the flux distribution. Five reflector materials were examined; the volume fractions are tabulated in Table 3-17. The PFR-type reflector consists of carbon clad with nickel. All the reflectors contained some structural stainless steel and some sodium coolant. Table 3-18 summarizes the results of the calculations.

Because the peak fluence in the reference core occurs on the centerline, the flux level at the center of the core at BOL was taken as a measure of the peak fluence. With respect to the reduction of peak fluence, the beryllium reflector appears best. However, it produces an unacceptably high power peak of approximately 1.7 at the core-reflector interface because of thermalization of the flux spectrum near the reflector. The PFR-type and nickel reflectors are about equally effective; each reduces the peak flux to approximately 10% of its value in the unreflected reference core. The overall breeding ratios are significantly decreased, primarily because of the removal of the inner radial blanket. The internal breeding ratios are slightly decreased because of a softening of the spectrum.

Figures 3-49 and 3-50 show the BOL flux distributions in the nickel and the beryllium reflected cores as well as

the unreflected reference core. The stainless steel reflector produced a flux distribution close to that produced by the nickel reflector, but with somewhat less flux reduction in the center of the core. The flux distribution produced by the sodium reflector was about midway between the distribution in the reference core and that produced by the nickel reflector. The PFR-type reflector produced a flux distribution between those of the reference core and the beryllium reflector, with about the same flux reduction in the center of the core as was obtained with the nickel reflector. All the reflectors were ineffective in reducing the flux gradients at the core edge. Only the beryllium and PFR-type reflectors reduced the total flux gradient noticeably, but they also increased the gradient in flux above 0.1 MeV because of the thermalization of the spectrum near the reflector. All the reflectors tended to increase the flux level near the edge of the core. Because of the exponential dependence of metal swelling on fluence, the same gradient at a higher flux level results in more severe gradients in metal swelling.

5. Variation of Relative Zone Enrichments — The effect of varying the relative zone enrichments was investigated for the modified version of the reference core described in Table 3-15. The enrichment ratios investigated were constrained so that the maximum radial peak-to-average power ratio over life would not exceed that of the reference core. This constraint restricted the relative zone enrichments to a fairly narrow range. However, a significant effect on the peak fluence was still found. The decrease in peak fluence from the reference core varied from 8.7 to 14.3%. In all cases, the peak fluence occurred on the core centerlines. For the range of enrichment ratios considered, the effect on the breeding ratios is negligible. The results are summarized in Table 3-19.

From the standpoint of peak fluence reduction, as long as the peak fluence occurs on the core centerline, as much fuel as possible should be shifted to the outer core regions. However, if too much fuel is shifted, the peak-to-average power ratio in the outer core zone will become unacceptably high or the peak fluence will move off the core centerline.

6. Axial Zone Loading — Beginning-of-life two-dimensional R-Z calculations were made to determine the effects of axial

zone loading on the flux and power distributions. Radially, the relative zone enrichments were not changed from those of the reference core. Calculations were made for the reference core uniformly loaded axially and with two axial zone-loading schemes; the enrichments were increased 10 to 15% in the lower third of the core and decreased 10 and 15% in the upper third of the core. The peak-to-average power ratios obtained for the reference core uniformly loaded axially and for the axial zone-loading schemes with 10 and 15% in enrichments were 1.48, 1.58, and 1.68, respectively. The peak power in the core uniformly loaded axially occurred on the midplane, whereas the peak power in the axially zone-loaded cores occurred on the central zone-lower zone interface one-third of the way up the core.

It was found that, because of the symmetry involved, when the enrichment in the lower third of the core is increased by the same percentage as the enrichment in the upper third of the core is decreased, the fissile loading required to maintain the same k_{eff} as obtained with uniform axial loading does not change. The first calculation was for the axial zone-loading scheme with a 15% change in enrichment but with an unnecessary increase in fissile loading over the reference core. Thus, the flux levels obtained were lower than they would be with the proper fissile loading, although the spatial distributions remain essentially correct. The peak flux occurs lower in the core than with uniform axial loading, near the central zone-lower zone interface. A second calculation was made for the axial loading scheme with a 10% change in enrichment, keeping the same fissile loading as in the uniformly axially loaded reference core. The axial distributions are compared with those of the reference core in Figures 3-51 and 3-52. The peak flux with this axial loading scheme also occurs near the central zone-lower interface. The magnitude of the flux at its peak is essentially the same as that in the reference core.

3.4.4. Plant Temperature Study

3.4.4.1. General

This section describes a preliminary survey of what might be expected from the standpoint of plant design and

economics if reduced core coolant temperatures are employed to control radiation-induced growth in the core structure.

The temperature levels of B&W's reference plant design and other contractor designs are shown in Table 3-20.¹⁸ It is noted that core inlet and outlet temperatures are within a narrow range for all designs. Furthermore, steam cycle pressures and temperatures are surprisingly similar, and reheat is used in four out of five designs. These similarities appear to be the result of important economic and design constraints.

It is similarly implied that these constraints tend to oppose changing the relationship of temperature levels existing in the primary system, the secondary system, and the steam cycle. For example, in a pool plant arrangement, reducing the primary sodium temperature by reducing the LMTD of the IHX causes a disproportionate increase in component size and cost and complicates plant arrangement problems. From Table 3-20 it is noted that an LMTD approximating 100F is used in both pool plants.

The uniformity of these temperature relationships also suggests that it may not be desirable to make core design changes to reduce the core coolant temperature rise below the range shown in Table 3-20. It is also implied that changes like a reduced core H/D ratio and a higher coolant volume fraction may produce economic penalties that are unacceptably large compared to the temperature reductions achieved. Therefore, it was concluded that this study should be directed toward lowering the core midplane temperature by reducing the steam pressure and temperature while minimizing changes to the core ΔT and to LMTD of the IHX. Thus, a number of alternate steam cycles would appear to be of interest; these are as follows:

1. 2400 psi/900F/900F
2. 2400 psi/900F/no reheat
3. 1800 psi/900F/900F
4. 1500 psi/800F/no reheat
5. 1500 psi/600F/no reheat
6. 1000 psi saturated

An average core sodium temperature of 850 to 900F was considered to be a desirable target value if satisfactory control of swelling was to be realized through a concurrent reduction of fluence. The first three steam cycles listed above are potentially compatible with this required temperature reduction.

The sixth cycle, 1000 psi saturated, is typical of the cycle that might be used to control core structural swelling by temperature reduction alone. A small economic gain over the estimated conditions might be expected through minor adjustments of this cycle, such as the addition of 100 degrees of superheat. Nevertheless, the results obtained through analysis of this cycle are believed to be generally representative. The fourth and fifth cycles provide data between the two levels described above. They are useful in helping to define the relationship of core midplane swelling to power generation cost over the current range of interest.

As previously stated, this analysis assumes that the reference core burnup and fuel cycle are not changed. This and additional ground rules are listed below:

1. Reference core and fuel cycle are used.
2. Maximum fluence occurs at core midplane.
3. Maximum fluence is 3.2×10^{23} nvt > 0.1 MeV at 100,000 MWd/T.
4. Swelling is related to fluence using average core midplane sodium temperature.
5. Swelling data are for cold-worked type 316 SS.

The relationship of the average core midplane temperature to swelling on the basis of these ground rules is shown in Figure 3-53. It is noted that the current average primary coolant temperature of 950F will result in predicted swelling of 33.0%. In contrast, the target of 15% requires a reduction to 740F.

The objective of the steam cycle studies was to determine in a preliminary fashion the economic penalties associated with the degradation of temperatures throughout the plant as required to reduce core swelling.

The NAPS computer code, in conjunction with hand calculations supplemented by data from reference 19, was used to

analyze power generation cost trends as a function of primary, secondary, and steam system temperatures and core growth. Because the NAPS code was restricted to the steam cycle generated by United Engineers and Constructors, it was not possible to evaluate all of the potentially attractive steam cycles quantitatively. A qualitative discussion is given for the 2400 psi cycles, while a quantitative evaluation is given for the remaining steam cycles.

The results presented indicate that some reduction in core growth can be achieved by reducing the core midplane temperatures without an excessive economic penalty. Because the work presented here is preliminary, it is important to realize that the essence of the study is to show potential for solving the core growth problem. The general trends are shown in Figure 3-54, where power generation cost is plotted against midplane temperature. It appears that growth can be reduced by half the amount from 33 to 15% (i.e., 24%) with a relatively small economic penalty by lowering plant temperatures. In Figure 3-55 calculated power generation cost is plotted against core midplane swelling. A reduction of midplane swelling from a current value of 33% in the reference design to a target value of 15% can apparently be achieved through the use of a 1000 psi saturated steam cycle at an economic penalty of about 0.5 mill/kWh.

Intermediate reductions of swelling can be accommodated by other steam cycles depending on the desired core midplane temperature. Further work will be required to assess the economic advantages of any selected cycle. For an average core sodium temperature in the range of 850 to 900F, the 1800 psi/900F/900F cycle appears to provide an economically attractive choice. Conclusions on the use of temperature reduction to reduce swelling in the core structure are stated and discussed in section 3.4.5.

3.4.4.2. 2400 psi Cycles

As a matter of review, a schematic diagram of the reference nuclear island showing the principal design temperatures is given in Figure 3-56. The reduction in the steam generator outlet temperature of 50 degrees below the IHX inlet temperature resulted from optimizing the power generation costs as a function of the sum of

steam generator and reheater capital costs. The concept of steam generator-reheater optimization is applied to the 2400 psi cycles discussed below.

Figure 3-57 is the temperature diagram corresponding to typical design conditions wherein the core sodium temperature rise is 350F and the steam cycle is 2400 psi/900F/no reheat. This diagram illustrates the basic compatibility of this cycle with the desired core midplane temperature of 875F. If reheat to 900F is added, a reduction in the temperature of the sodium leaving the steam generator (as normally required to avoid greater reheater size and cost) will now reduce the steam generator pinch point ΔT to the vanishing point (see Figure 3-58). Some relief of this condition is possible by reducing the core and IHX ΔT s. However, the hot-end temperature difference on the steam generator tends to become too small before a reasonable boiler pinch point ΔT is realized.

Based on these facts, it appears that the non-reheat cycle is more suitable for achieving a core midplane temperature of 875F. In support of these conclusions, we cite the Westinghouse 1000-MWe Follow-On Study (WARD-2000-96), which describes a design with a 2400 psi/900F/non-reheat cycle and 885F core midplane temperature.

3.4.4.3. 1800 psi Cycles

Many combinations of core inlet and outlet temperatures were investigated in conjunction with this cycle using the NAPS code. The two most attractive cases are shown in Table 3-21. From Table 3-21 it is concluded that about 860F is the lowest practical average core temperature that can be attained with an 1800 psi/900F/900F reheat cycle.

3.4.4.4. 1500 psi Cycles

These cycles were investigated to determine the economic penalties associated with depressing the core midplane temperatures to below 860F. Two cases were investigated—a 1500 psi/796F cycle with a core midplane temperature of 833F and a 1500 psi saturated steam cycle with a core midplane temperature of 817F.

As stated previously, the NAPS computer code is not capable of analyzing steam cycles other than those generated by the United Engineers' study. Therefore, it was necessary to combine information from a number of sources¹⁹ and use hand calculating methods to generate costs for the IHXs, steam generators, and pumps.

Fuel costs for this study, estimated as approximately 0.8 mill/kWh, are based on experience with NAPS runs for similar steam cycles. This value is probably on the high side. The approach used assumed that the gross efficiency for the 1450 psia/1000F cycle, as calculated by NAPS was the same as that for the 1500 psia/996F cycle given by Sargent & Lundy¹⁹ when the 1500 psi cycle was corrected to a feedwater temperature of 434F (refer to Appendix D for detailed calculation). Subsequently, all points, e.g., the decreasing superheats, were adjusted to yield the same net electrical output. The costs of the turbine islands were then recalculated on the basis used by the AEC cost account code.

The cost of the nuclear islands was calculated by assuming that all items remain substantially constant except the steam generators, the intermediate heat exchangers, and the primary and intermediate sodium pumps. A number of NAPS runs were examined to determine an average value for those items remaining constant. The remaining items (SG, IHX, pumps) were calculated by hand. The total capital cost included a factor of 0.4 for indirect costs.

Table 3-22 compares the reference plant with the 1500 psi cycles. As expected, the power generation costs for the 1500 psi cycle were higher than for the reference design (approximately 0.2 for the 1500 psia/796F cycle and 0.3 for the 1500 psia/saturated cycle). These values are slightly higher than the power generation costs predicted by NAPS for the 1800 psia/900F cycle.

3.4.4.5. 1000 psi Saturated Cycle

The saturated 1000 psi steam cycle will permit average core coolant temperatures of 740F, giving the target 15% swelling value. Table 3-23 compares the economics of three possible sets of core temperatures with the reference 2400 psi/950F/950F plant. From this table it is concluded that case 3 is the most economical and

that the target of 15% growth in 100,000 MWd/T can be achieved with about 0.5 mill/kWh penalty by reducing plant temperatures alone.

3.4.5. Stress Study

3.4.5.1. General

Bowing can occur in any reactor that has a temperature or flux gradient in the radial direction. The bowing and stability problem and the possible solutions for large LMFBRs are fundamentally similar to the fast reactors in the present generation, but the problem is aggravated by swelling and differential swelling, which was not studied for the previous B&W fast reactor design. This preliminary study is an attempt to improve the radial core constraint system of the B&W design to accommodate swelling.

The detailed study of thermal bowing and its effects is presented by Jakub and Sutherland²¹ with the application to the Fast Test Reactors Conceptual Design Study. The effects of radiation-induced swelling were not known at that time, but the principles covered are applicable for the modifications in the LMFBR core restraint system study. The following brief review describes this study.

Figures 3-59a and 3-59b illustrate the only acceptable ways to achieve correct motion of the fuel assembly without core radial supports, while still maintaining zero stress level at all times. However, the resulting deflections are generally large and may interfere with control rods or fuel handling operations.

Figure 3-60 shows a fuel assembly held firmly at the top and bottom of the core. The radial temperature and flux gradient will cause a differential expansion, which results in forces tending to bow the fuel assemblies toward the center of the core, increasing the reactivity. This support arrangement at the upper and lower grid structure although having positive reactivity, offers the maximum support stability to the fuel assembly.

The logical solution during early development of fast reactors was to provide a spacer pad for each fuel assembly and avoid any radial clearances between the fuel assemblies. This solution, providing maximum rigidity, was generally accepted until swelling problems nullified the use of small clearances between fuel assemblies.

Figure 3-61 compares the fuel assembly models for Fermi, EBR II, and FTR. The fuel assemblies in the Fermi reactor were supported axially and radially at both ends—at the top by holddown fingers and at the bottom by core support plates. To prevent positive reactivity, spacer pads were welded to the wrapper can a little above the core midplane. The position of the holddown fingers was offset toward the core centerline to ensure permanent contact of the pads when the holddown device was lowered. Therefore, the radial temperature gradient caused bowing of the fuel assembly into S-shaped curves, and the pad location was selected to equalize the reactivity effect of inward and outward fuel movement. The Fermi study did show an improvement in negative reactivity with the top-of-core location for pads, but it was rejected because of higher stresses in the higher-temperature region.

The final EBR II radial constraint system contains several especially provided gaps. The lowest tubesheet has some clearance at the bottom before it can change the end condition of the fuel assembly from pinned to fixed joint. The gaps at the top of the fuel assembly and the support plane are generated by adjacent fuel assemblies. The fuel assembly motion occurs as follows: In the initial motion the top end of each assembly bows outward freely until it is stopped by the adjacent fuel assembly pad. The inward bowing continues until the core support dimples interact with enough force to hold the inward movement.

The conceptual FTR approach to radial constraint is stress-free or is spring loaded outwards. The critical location of the intermediate support for the desired negative reactivity effect is in the transition zone from hexagonal to round tubes one foot above the top of the core.

For this study the radial restraint system, which provides a load at startup and still possesses a minimum loading condition at shutdown, was the preferred approach due to its apparent simplicity. However, the critical location of the intermediate support for the desired negative reactivity effect was obtained by accommodating the PNL and ANL growth relations.

Several assumptions made in the past study are accepted as valid for the present study. The small fuel pin motions within

the fuel pin bundle and the fuel pin bundle motions within the wrapped can may cause significant positive or negative reactivity effects within a given assembly, but, considering the entire core, the overall reactivity effect of this motion is considered to be negligible. The fuel pin bundle's moment of inertia is much smaller than that of fuel wrapper can. Hence, for the present bowing study, it can be assumed that the fuel pin bundle will follow the bowing pattern of the wrapper can with no significant interference.

Another important assumption for the proper functioning of the radial restraint system is that no flow mixing occurs near or above the top of the core (upward flow). This is essential in the inward bowing of the fuel assembly portion between the two upper radial supports. Consequently, this inward motion moves the fuel portion of the fuel assembly, which is located below the intermediate restraint, away from the centerline of the core, thus introducing negative reactivity with an increase in bulk temperature.

To study the complex distortion of the fuel assembly involving the dynamic behavior of temperature, temperature gradient, neutron flux, neutron flux gradient, and the effects of all these parameters on creep and swelling, several computer codes have recently been developed. Two codes, CRASIB (Creep Analysis of Statically Indeterminate Beams) and DEFLECT, are explained in Appendix A. A typical model fuel assembly and creep regions are also explained. All the qualitative and quantitative results discussed below are obtained from these two codes. The present swelling study was performed in two different stages:

1. The selection of the reference fuel assembly experiencing the worst conditions for bowing. This fuel assembly was used for the subsequent parametric study.
2. The preliminary evaluation of the effects of various parameters on the fuel assembly selected earlier.

3.4.5.2. Selection of Worst-Condition Fuel Assembly

The fuel assembly arrangement in the core and radial blanket is shown in Figure 3-34. From the standpoint of the nuclear

physics and thermal hydraulics associated with each assembly, the entire core and blanket assemblies can be separated into 15 different groups. Some of these groups may be similar as far as bowing is concerned, but for consistency they are numbered in 15 different groups as shown in Figure 3-34.

Figures 3-62 and 3-63 show the axial temperature and flux distribution for the various fuel assemblies in the reference design core. To calculate temperature and flux gradients, it is assumed that the radial peaking factors did not change along the entire length of the core. As explained earlier, it is further assumed that, once the maximum temperature is established at the end of the core, the temperature gradient does not change along the remainder of the fuel assembly.

The axial flux distribution for the region outside the active core is extrapolated from the available flux distribution inside the core. The flux and flux values outside the core region are comparatively small; hence, no significant error will be involved.

The bowing characteristics obtained from the computer code CRASIB are time-dependent. The complete time history is difficult to discuss here, but the beginning of life (BOL at $t = 0$ hours), and end of life (EOL at $t = 15,000$ hours) results are identified and evaluated. A two-year ($t = 15,000$ hours) study was selected for a three-year fuel assembly cycle because initial studies showed meaningless results at the end of three years ($t = 22,500$ hours), probably because of considerable extrapolation in swelling and creep behavior.

Maximum stress, maximum strain, maximum deflection, and restraint loads at the intermediate supports for various fuel assemblies are shown in Figures 3-64, through 3-68, respectively. The result shows three different radial zones. The zone near the center of the core having maximum temperature and flux in the core is associated with negligible temperature and flux gradients. Hence, this region experiences negligible bowing, and the associated restraint load, stress, and strain are also very low. But the swelling and resulting linear swelling strain is very high in this region. In fact, the entire strain shown in the Figure 3-65 for this zone may be assumed to be caused by swelling.

The second zone, at the radial end of the core and the beginning of radial blanket, is associated with average temperature and flux but with significant temperature and flux gradients. The results obtained for this zone show the combined effects of swelling and bowing.

The third zone is the radial blanket. It is associated with comparatively low temperatures and fluxes but with high temperature gradients. The effect of swelling appears to be negligible here, and the entire bowing characteristics may be attributed to the effects of temperature gradients.

The selection of the fuel assembly experiencing the worst conditions may be based on several different considerations. The maximum swelling and creep and the associated linear strain in the region near the center of the core appear to be quite high. But this is not the loading strain, and hence, the maximum allowable limit in such cases is not adequately available. However, there is a recent indication²⁴ that there is no ductility limit associated with the neutron-induced creep. The conclusion indicates that irradiation creep provides a strain mechanism that will not give rise to local necking and an eventual failure. Hence, a thermal creep ductility limit must be considered. At present, on the basis of the low deflections, stresses, and restraint loads, it may be concluded that the region near the center of the core is probably not experiencing the worst conditions for the present study.

In the radial blanket region, the available data from the reference fuel assembly design are incomplete and uncertain. It is also expected that, unless a detailed study is performed, the modified fuel assembly data in the blanket region will be uncertain. Hence, the finite results for the blanket region are not available, but the results for several different assemblies for the blanket region do show that they are not as severe as the effects for the region near the interface of the core and radial blanket.

The choice finally comes to fuel assemblies 11 and 12, shown in Figure 3-34. Both are located in the active core but at the interface of the core and blanket. The obvious choice for the worst-condition fuel assembly should have been fuel assembly No. 11,

but the limited time available for the present study required the parametric study to be performed at the end of early limited results. Based on the limited results available at that time, fuel assembly No. 12 was selected for the parametric bowing study. However, the results do not show large significant differences between these two fuel assemblies.

3.4.5.3. Parametric Study of Radial Core Constraint

The parametric study for the radial core constraint system was conducted in several successive stages. It should be noted that the parametric study was based on the reference fuel assembly; hence, only the parameters that had some significance with the reference fuel assembly were considered.

The effect of the fundamental parameters was evaluated, and good features obtained from this evaluation were applied to the reference assembly for modification. At this stage some optimization for this modified assembly was assessed. With the time limitation involved in the study, a careful evaluation for the possible thermal-hydraulic and nuclear physics changes associated with the change in parameters was not possible. Another drawback resulting from time limitations is that when a good feature at a particular stage was evaluated, it was applied to the modified assembly at that stage. Thus, some of the possible changes cannot be quantitatively established when referred to the original reference fuel assembly. Basically, the effects of the following parameters were studied:

1. Length of fuel assembly and relocation of intermediate support for various lengths.
2. Size of fuel assembly wrapper can.
3. Inlet temperature variation.
4. Section modulus change along the length.
5. Mass flow rate.
6. Combined effects of good features from these parameters, in successive stages.

The following results were investigated:

1. Maximum stress.
2. Maximum strain.

3. Maximum restrained deflection.
4. Restraint loads at all supports.
5. Reactivity change at the end of cycle.
6. Maximum unrestrained free deflection at the end of cycle.

Figures 3-68, 3-69 and 3-70 show the effect of fuel assembly length variation and relocation of the intermediate support on maximum stress, deflection, and restraint load at the intermediate support, respectively. For simplicity, only results at 0 and 8000 hours' fuel life are presented. It is obvious that the axial length added above the top of the core will have high radial temperature gradients, and the axial length added below the core would have no radial temperature gradient. Hence, axial length added above the top of the core increases inward bowing compared to reference case, whereas the axial length added below the core would resist the inward bowing.

Referring to the notations used in Figures 3-68 through 3-70, the following conclusions can be derived: Case B shows considerable deflection movement. Case C, having additional axial length below the core, shows considerable improvement in maximum stress, but the associated maximum deflections are very high. The reduction in the restraint load at the intermediate support for case C is much lower than for case A. Thus, if the high negative reactivity introduced with case C is acceptable, then the optimization would need trade-off studies for maximum stress and restraint load. Since the maximum stresses obtained with case A are within the elastic limit range, and since the greater concern is for the restraint load, it can be concluded here that the modification of the reference fuel assembly should be based on case A, where the additional axial length is added between the intermediate support and the top end of the fuel assembly. The maximum strain does not seem to vary significantly with the fuel assembly length.

The variation of maximum stress and the restraint load with different can size is shown in Figure 3-71. The result definitely shows considerable reduction in maximum stress and restraint load for a smaller flexible can size. The maximum deflection and strain do not seem to have significant effects. The application of a smaller flexible can is limited for the present modification study because this

requires the relocation of control rods and thus a detailed analysis of the core.

The variation of maximum stress, deflection, and restraint load with inlet temperature is shown in Figure 3-72. It appears that some optimization may be achieved with inlet temperature variation. The typical characteristics of reduction in stress, deflection, and possibly restraint load at higher and lower inlet temperature are probably due to a similar change in swelling pattern. The considerable reduction in maximum strain is also achieved with a lower inlet temperature, but with uncertainty in creep behavior and the present limitations in computer code creep data input (without modification), a smooth characteristic is not possible. For the present study the selection of inlet temperature would be based on the overall system requirements.

Along with the negative reactivity effect for the core, the change in section modules along the length of the fuel assembly can be achieved by increasing the thickness of the assembly above the intermediate restraint. This increased thickness does not seem to have a significant effect on maximum stress, strain, and restraint load. The variation in maximum deflection is shown in Figure 3-73. The result shows that the maximum deflection away from the centerline of the core (outward) increases with the increase in thickness. This itself is not a desirable feature, but it seems to reduce the possibility of positive reactivity in the fuel assembly at increased time. This may be a possible replacement for 180-degree rotation of the fuel assembly.

The quantitative results for mass flow rate variation are difficult to correlate at the present time because of the need for a precise axial temperature distribution and radial temperature gradient for input to the computer code. However, some quick estimation of these values shows that the increased mass flow rate decreases the radial temperature gradient and lowers the average temperature along the axial length of the fuel assembly. This two-fold effect significantly reduces the maximum stress, strain, deflection, and restraint loads with the increase in mass flow rate.

As a result of the study discussed above, several good features were introduced in the successive modification of the fuel assembly. Again, the quantitative results are quite difficult to assess,

because of the trial-and-error approach with several extrapolations, as far as thermal-hydraulic and nuclear physics aspects are concerned. With the time limitation in the present study, this probably was the only alternative to determine some trends. The axial spring compressive load on the fuel assembly has no significant effect on the results (refer to Appendix F).

3.4.6. Mechanical Study

3.4.6.1. General

A review of the experimental data²⁰⁻²² conducted against the background of the existing reference design revealed that the following approaches for accommodating fuel swelling should be considered (refer to Figure 3-74):

1. Top-Mounted Can With Top-Mounted Drives — In the top-mounted arrangement, cans cantilevered off the upper grid plate with allowance for lateral can movement would reduce stress levels. Irradiation growth and thermal gradient bowing would always be away from the core, and the cans would be free to bow in the lower region without complicating the control rod drive alignment problem, since the top entry location would be fixed.

2. Bottom-Mounted Can With Bottom-Mounted Drives — In the bottom-mounted scheme, the design approach would be similar to the top-mounted scheme except that the support of the assemblies would be greatly simplified.

3. Duct Temperature Control — The currently predicted metal swelling data indicate that irradiation growth in stainless steels is more severe in the temperature range from 600 to 1200F. If the can could be held at or below 600F, or above 1200F at the core midplane, the metal swelling problem could be eliminated or readily accommodated. Reducing the bulk core midplane temperature below 600F would result in a severe economic penalty for the LMFBR. Increasing the reference temperature above 1200F would also be unacceptable from a near-term technology approach.

The lower-temperature design requirement can theoretically be approached to minimize growth by can bypass control

(double ducting). The double-ducting arrangement consists of an inner duct containing the fuel pins and an outer can surrounding this array. The outside can serves as the structural member and the inner duct simply keeps the fuel coolant away. The basic objective is to keep the outer wall at a lower temperature to minimize growth and deflection while allowing the inner wall to expand and accommodate the growth that occurs.

If long-term technology permitted the higher-temperature approach, at least two solutions are available; both require a higher bulk outlet temperature. In one possible solution the fluid is recirculated back to a location above the lower support grid, where the hot fluid is allowed to flow vertically upward. This, while not changing the bulk reactor outlet temperature, results in a high can temperature specifically in a high-stress area at the bottom of the fuel can. Another solution would be to place a mechanical seal at the upper grid structure to extract outlet temperature sodium in the vicinity of the upper axial blanket, forcing the hot outlet sodium to flow downward and radially outward across the core, where it would flow upward again in an annulus between the flow baffle and the core support skirt. Such a solution could require a minor flow restrictor at the top outlet of the fuel assembly and a thermal baffle on the upper face of the lower support structure.

4. Core Clamp Arrangement — The core clamp arrangement consists of a radially directed load applied to the outside blanket elements. The load would be applied in two places above the core region on each blanket element and would be transmitted to inboard assemblies through pads machined on the outside of all cans.

The basic and overriding requirement in the design and use of a core restraint device is that, under operating conditions including all transients, it must minimize subsequent component motion that would tend to provide positive feedback effects. Therefore, the design must consider differential thermal expansion, irradiation metal swelling, thermal creep, and irradiation creep. Under steady-state and transient conditions the differential thermal expansion results in a first-order effect on reactivity feedback. The other items are affected by temperature and fluence and, being primarily dependent on the residence time in the reactor, these items have a long-term effect on reactivity control.

3.4.6.2. Evaluation of Solutions

In combination with the preliminary results from the stress study, potential mechanical devices were evaluated to assess the effects of metal swelling. The results enabled a qualitative evaluation using engineering judgment. Axial thermal or material growths in the fuel assembly can be readily accommodated by the inherent design of fuel pin and fuel can compression springs.

Therefore, the radial movement of the fuel is the major problem area and requires the greatest work effort to obtain an acceptable design solution. The three major aspects affecting the radial growth are as follows:

1. Thermal temperature camber across the fuel assemblies.
2. Bulk metal growth affecting interaction on adjacent fuel assemblies.
3. Differential metal growth across a fuel assembly resulting in fuel assembly bowing.

The thermal temperature camber can result in a fast reactivity change which must be designed to give a negative reactivity feedback to the core. The bulk differential metal growth will not result in a fast reactivity change, but its lifetime effect may induce faster reactivity response due to the thermal camber effects if not given adequate consideration or correctly designed.

Due to the limited time available, approaches other than those previously mentioned could not be investigated apart from some novelty concepts identified in appendix E. Some examples of these uninvestigated approaches would be flexible cans or ducts of dissimilar material.

The thermal radial gradient of the core will cause the fuel can surface nearest to the axial centerline of the core to expand at a faster rate than the other surfaces. A core structure having gaps between the fuel assemblies and locators at their top and bottom ends will produce a positive reactor feedback if subjected to an increase in the thermal radial gradient. This condition will prevail if midcore bearing pads are not in contact and to a lesser extent if the bearing pads

of the fuel assemblies in the core zones are contact but those of the inner fuel assemblies are not. Under this condition the cascading stiffness effect will reduce the inward movement of the core.

If the fuel assemblies were axially located but free to move radially at their ends, being clamped at the core midplane by a core clamping device, then any thermal gradient across the fuel can would result in a negative reactivity feedback, since only the outer ends of the fuel assemblies would be subjected to radial movement. The radial movement at the top of the fuel assembly could affect the operation of the control drive and the movement at the bottom of the fuel assembly could have a detrimental effect on the orifice and core seal. The orifices can be located in the fuel assembly, but it is difficult to conceive an efficient seal using only the core clamping station as the core sealing medium. With batch refueling the seal problem becomes acute because the irradiated fuel assemblies, which have been subjected to metal swelling, are replaced by unirradiated assemblies, resulting in can cross sections that are appreciably smaller than the hole profiles generated by the neighboring irradiated fuel assemblies.

To reduce the seal bypass flow characteristics, the core seal must be located at either the bottom or the top of the core. With a core clamp device mounted at the midplane and an upward-motion refueling operation, the core seal can logically be located only at the bottom of the core. Actually, any change from this approach would severely complicate B&W's reference design concept.

1. Top- or Bottom-Mounted Fuel Assembly Cans — A preliminary look at the top- or bottom-mounted can scheme shows that the disadvantages outweigh the advantages although there are benefits to these schemes. The disadvantages are as follows:

- a. The cans must be hung from the upper grid plate in the top-mounted scheme.
- b. Either arrangement would be a major change from the reference design.
- c. In both arrangements, early thermal gradient movement could produce adverse reactivity feedback as a result of pads contacting adjacent ducts.

- d. In the top-mounted arrangement, a bow away from the centerline of the core could be counteracted by a gravity swing toward the centerline. This would increase reactivity.
- e. The major disadvantage in both schemes, however, is the lack of positive location of the cans at their free ends.

2. Can Temperature Control — Double-Ducting — Based on engineering judgements, the double-ducting scheme was discarded for the following reasons:

- a. Swelling of the outside can can never be eliminated.
- b. Transient gradients can never be completely removed.
- c. Breeding capability is reduced due to additional in-core material.
- d. Fabrication costs are high.
- e. A higher enrichment is required for the extra can wall and coolant annulus.
- f. From the pressure-balancing and structural standpoint, it is difficult to tie together two structures that swell at different rates.

3. Recirculation — This is a long-term technology approach. No immediate conclusions can be drawn at this time; a separate study evaluation would be required.

3.4.6.3. Core Clamp

To ensure a predictable neutronic response to the thermal and seismic motions of the fuel assemblies and to facilitate fuel handling requirements, a radial restraint system is the preferred method for B&W's 1000-MWe LMFBR.

The core clamp complicates the internals arrangement slightly but appears to be an effective way to limit fuel assembly bowing toward the center of the core under excursion conditions. Midplane growth of the fuel assembly would be accommodated by opening the assembly pitch dimension.

The clamp acts in the following manner (see Figure 3-75): If the can was free to deflect at the top, swelling and thermal gradients would cause it to bow outward as shown in Figure 3-75(a).

A clamping load (A) applied at plane X would force the can to assume the shape shown in Figure 3-75(b). Inboard assemblies applying a load (B) through pads at plane Y would force the can to assume the shape shown in Figure 3-75(c). The stiffness of the duct, in conjunction with the inward thermal bowing between planes X and Y, forces the can in the core region to bow away from the core. Consequently, any further reactor transients will produce deflections only in this direction. To produce inward thermal bowing between X and Y, hardware to maintain the temperature gradient will be needed in the duct region above the blanket pins.

In addition, the existing scheme, which locates the top of the element in the core holddown assembly, must be replaced by a surface-to-surface interface to enable the clamping action to work.

After irradiation and after the clamping loads have been removed, permanent set of the can due to irradiation growth would cause the duct to assume the general shape shown in Figure 3-75 (d). To return the assembly to operating status, clamping loads A and C would be applied to the lower and upper pad locations.

Design Criteria —

1. The clamp must be capable of operating under all transient conditions of reactor operation.
2. The clamp hardware on the core holddown assembly should be capable of a 30-year life.
3. Preload motion must be slow and controlled, since it is a positive reactivity addition to the core. Under no circumstances may it be preloaded rapidly.
4. Every attempt must be made to use the beam and casting assembly in the blanket element for more than one cycle (one cycle = 4 years).
5. All stresses must be kept low enough so that stress relaxation (which is a function of stress levels, temperature, flux levels, and fluence) proceeds at an acceptable rate. Also, rupture stress levels must not be exceeded.

6. The clamp must accommodate tolerance build-up from the fuel assemblies, the beam spring/duct interface, and the actuating mechanism and its support structure.

7. Compensation for the steady-state, transient, and irradiation effects must be provided by the restraint mechanism to load the core into a tightly packed lattice at the bearing pads while allowing bulk growth of the fuel cans.

8. The clamp must accommodate radial swelling at the outer blanket lower pad location and growth at the upper pad location.

9. The clamping load must be applied before the core holddown assembly finally compresses the fuel assembly spring.

10. The clamp must be able to accommodate an unloaded EOL bow condition.

11. The overriding considerations in the design must be simplicity, safety, and reliable operation. The blanket element must have two-position indexing to ensure that the plunger on the core holddown assembly always contacts the beam spring.

12. The core may expand radially from thermal expansion, but the device must retain enough flexibility to allow this expansion without undue increases in preload forces.

3.4.6.4. Core Clamp Problems

Before a workable clamp can evolve, several areas will have to be examined very closely:

1. High bearing loads on components could cause fretting and galling; this must be investigated.

2. Stress relaxation of materials under irradiation must be investigated thoroughly.

3. A partial loss of the clamping load could cause an increase in reactivity; this can be seen in Figure 3-75(b). However, the recommended design described in section 3.1.2 minimized this effect by having one input load.

4. The amount of load to be applied at planes X and Y in Figure 3-75 would have to include the influence of deflections, transmission of force to inner ducts, duct radial temperature profiles, efficiency, friction, loads, and tolerance buildups.

5. The magnitude of metal swelling at planes X and Y would have to be known reasonably accurately, since this tends to put more load in the clamp.

6. To ensure contact between all ducts when the radial preload device acts, some pads may require machining to slightly different dimensions. If contact is not made at all ducts, transient thermal gradients could cause adverse reactivity changes.

3.4.6.5. Core Support Structures

The reference design top and bottom core support structures are located in an area that is basically of low neutron damage. For a 30-year life, the lower area of the core hold-down assembly and the upper area of the grid plate will show an irradiation growth of about 1.0 and 0.25%²³, respectively, based on calculated fluence and temperature. This will cause bowing and generate stresses in these structures. However, it is expected that this much growth can be accommodated.

3.4.7. Economic Study

3.4.7.1. Burnup/Growth Relationship

The reference design was evaluated to establish the relationship between burnup and volumetric growth. The objective of the study was to ascertain the effects on the reference design if fuel burnup was employed to reduce radiation growth in the core.

The reference design, identified by the data in Table 3-1, was used as the design point. The power generation cost of 3.715 mills/kWh was extracted from reference 25, and the 3.374 mills/kWh cost was obtained from the NAPS computer at 0.89 plant capacity factors. The difference between these figures can be primarily accounted for by the fact that the NAPS code was not upgraded to reflect the detailed cost estimate of 3.715 mills/kWh. Therefore, although the

code does establish trends, it does not establish accuracy; however, all economic evaluations (unless otherwise stated) should be compared with the 3.374 mills/kWh reference figure.

For this study it was assumed that the neutron flux was constant and that fluence was proportional to burnup. The investigation covered the range from 60,000 to 100,000 MWd/T, having cycle times of two and three years. The fuel pin diameters were adjusted, assuming a constant 0.010-inch cladding thickness, to accommodate the range of investigated burnup and cycle times as shown in Table 3-4. The height of the core was varied from 35 to 50 inches, and the pin ligament was also varied in the range from 0.040 to 0.070 inch. The lowest power generation cost for each series of computer runs was selected and plotted on Figure 3-76.

Utilizing the relationship between metal swelling and burnup shown in Figure 3-33 at a constant midplane temperature of 950F, Figure 3-77 was plotted. Figure 3-77 illustrates the advantages and cost incentive in favor of a two-year cycle over a three-year fuel cycle when considering the effects of irradiation-induced metal swelling. It was assumed for this study that the reduction of fluence between a three-year fuel cycle and a two-year cycle was equivalent to a 9% reduction, based on the results of a 3.4-year to 2.1-year fuel cycle, which resulted in a 12% fluence reduction (refer to section 3.1.3). This cost differential is not an absolute number because we did not evaluate fully the increased fuel handling times and possible increased capital equipment costs for handling a great number of fuel assemblies per fuel cycle. However, the results show no economic loss in selecting a two-year cycle, which is a preferred selection when considering metal swelling and core structure component life.

3.4.7.2. Fuel Can Gap

The irradiation growth induces dilation of the fuel assembly, which, with the B&W reference core and only a 1% maximum linear growth, results in a midplane core barreling effect of approximately 1 inch on the diameter. This type of deflection taken on the outer radial assemblies would induce an unacceptable stress condition; this illustrates the requirement for free-standing fuel assembly cans.

For the free-standing concept the gaps between the fuel assemblies must accommodate not only the maximum radial growth but also the manufacturing tolerance associated with geometric profile tolerance of the can, the tolerance of the fuel support structure, and dynamic movement tolerances associated with reactor operating and life conditions.

The reference design was originally conceived with a 0.050-inch gap between cans, and bearing pads were attached to the outer surfaces of the cans in the vicinity of the core. This design is not suitable for the acceptance of irradiation growth, which will require the reference design gap to be substantially increased. To ascertain the cost penalty associated with increasing the can gap the NAPS code was used, with the reference design as the base case. The results of this study are shown in Figure 3-78.

3.4.8. Identification and Evaluation of Possible Solutions

3.4.8.1. Identification

Since metal swelling is both temperature- and fluence-dependent, it will result in the dilation and distortion of fast reactor core components. If the bulk growth effects are reduced and the peak to average power and temperature ratios are maintained, it does not necessarily infer that the distortion problems are eased because the swelling gradient in the outer assemblies could be greater (use of radial reflectors).

The most significant effect of the swelling-gradient-induced distortion is the bowing of long slender core components, such as the fuel assemblies, control rods, safety rod guide tubes, and neutron shields. Some solutions to reduce the effects of both bulk and differential metal swelling-induced growth are identified in two groups below:

Group 1. Bulk Growth Reductions —

a. Reduction of Fuel Residence Time: Reducing the fuel residence time by shortening the cycle time reduces proportionately the time factor involved in the calculation of fluence. A potentially large reduction in fluence is possible by this method. The burnup is also

reduced in proportion to the fuel residence time. The method is constrained by the requirement for an annual or semiannual refueling scheme.

b. Reduction of Fuel Residence Time and Fuel Volume (burnup held constant): If both fuel residence time and fuel volume are decreased proportionately, the burnup remains constant. This leads to a smaller core with increased power density and flux level but with a shorter fuel residence time required to reach a given burnup. The smaller fuel volume necessitates increasing the enrichment. The increased enrichment and shortened fuel residence time tend to decrease the fluence, while the decreased fuel volume tends to increase the fluence. The net result is a decrease in fluence, but it is of smaller magnitude than could have been realized by the reduction of fuel residence time alone.

We have reduced fuel volume by decreasing the fuel pin diameter. Within the limitations on fuel pin size imposed by the manufacturing process, this method may be used in conjunction with the reduction of fuel residence time alone to obtain both the desired burnup and the desired cycle time.

c. Geometric Spoiling (annular core design): In an annular core the leakage is increased, requiring an increase in enrichment, which in turn decreases the flux level. Large reductions in fluence are possible in thin annular cores because of the high enrichment. We also investigated the possibility that a thin annular core might reduce the flux gradient at the core-radial blanket interface.

d. Reflectors: Since a reflector flattens the flux distribution, lower peak fluxes result for the same power output. Reflected cores were examined from the standpoints of reducing bulk fluence and of reducing the flux gradient at the core edge. We found that reflectors placed outside the radial blanket on the reference design produced no significant changes with respect to power or flux distributions, k_{eff} , or breeding ratios. In order for them to be effective, it is necessary to place the reflectors between the core and the radial blanket.

e. Variation of Relative Zone Enrichments: The peak fluence in the reference core occurs on the core centerline. Varying the relative zone enrichments at BOL to increase the average power

in the outer core zones will, therefore, reduce the peak fluence. The relative zone enrichments in the reference core were chosen in an effort to flatten the time-integrated power distribution in the core. This method of fluence reduction prescribes that the relative zone enrichments be chosen in an effort to flatten the fluence (time-integrated flux) distribution in the core. Because the core is zone-loaded with the higher enrichments in the outer zones, flattening the fluence distribution shifts the time-integrated power distribution toward the outer core zones.

f. Axial Zone Loading: The core is divided into axial as well as radial enrichment zones, with the highest enrichments in the lower sections of the core and the lowest enrichments in the upper sections. This axial enrichment distribution causes the peak fluence to shift towards the lower (low-temperature) region of the core, and reduces the fluence in the upper (high-temperature) region of the core. Since the peak fluence occurs in a lower temperature region than in the reference core, the metal swelling was expected to be less severe.

g. Core Moderator: A core moderator requires that fuel enrichment be increased and softens the flux spectrum. Therefore, it has a large effect on the PNL growth correlation, which has a high energy dependency factor.

h. Lower or Higher Temperature: This reduces the irradiation growth for a specific operating fluence, enabling optimization of the temperature-dependent growth variable.

Group 2. Differential Growth Reduction —

a. Reflector Material: Reduces the peak to average flux values.

b. Geometric Spoiling: Spoiling also reduces the peak-to-average effects, and in combination with the bulk reduction effects (Group 1e), swelling gradients may be reduced.

c. Periodic Rotation of Assemblies: The bowing effects are reduced to bulk growth effects only by rotating the incore assemblies periodically. The optimum solution is a continual "rotisserie" motion.

d. Articulate or Flexible Duct: Mechanical devices for obtaining continuity of core coolant flow or control rod guide tube guidance while accepting differential growth.

e. Clamping Device: This device imposes restraining loads on the fuel assemblies that correct the amount of bow and, when combined with the material creep effects, reduce the bowing characteristic.

f. Double Can: A double can enables the use of a bypass core coolant, thus suppressing the temperature and, hence, the differential growth of the fuel assembly structures.

In addition to these possible solutions to assist in the reduction of material growth caused by irradiation, the effects of the following also offer possible or potential solutions.

1. Flow Orificing — Our reference design uses an orificed core, each fuel assembly flow being adjusted until one fuel pin is subjected to the maximum design criteria requirements. This results in a greater temperature gradient across the fuel assemblies in the outer portion of the core. The economic benefits of fully orificing must be weighed against the more severe thermal camber effects of elements in the outer radial zones.

2. Fuel Density Power Flattening — If the fuel density in all fuel pins could be varied over a wide range, a core could theoretically have a uniform flow and a uniform radial temperature profile in every coolant channel. The local power generation would precisely match the local flux and the differential thermal gradient would be negligible. From a practical aspect, when specifically considering fuel fabrication and complicated fuel relocation programs and safety problems, the fuel density should be a constant value in a given pin. However, the fuel densities between each assembly must be evaluated to obtain the optimum number of core zones.

3. Flux Control — The neutron distribution in fast reactors can be varied somewhat for a given flux magnitude, as thermal neutrons will cause a higher power level due to their higher fission cross section. Thus, the addition of moderator to a core will cause a local power peak.

Likewise, the addition of neutron absorber poison to a core will cause a local flux reduction. An evaluation is necessary to assess this effect on the differential thermal gradient, as well as space and economic penalties.

4. Top and Bottom Grid Support — The location of the fuel assemblies between the top and bottom grid structures offers maximum stability for the core components. However, the interplay of the differential thermal radial gradient induces the fuel assemblies to bow into the core, resulting in a reactivity insertion. This method of support, in conjunction with a constraining mechanism, may require the deletion of one of these support members.

5. Variation of Top and Bottom Fuel Element Fixtures — The reference design uses a spigot-type of fixture at both ends of the fuel assembly; this type of fixture radially locates the fuel assemblies in space and, in conjunction with the can stiffness, tends to restrict the use of a core clamp device. To obtain flexibility at the lower and/or upper ends of the fuel assembly, a mechanical flexible joint (e.g., slip-face seal, dog-leg duct, or bellows) requires evaluation to meet the movement/growth criteria and inlet seal requirements.

6. Varying Axial Stiffness Along the Can — The desired motion of a can restraint system can be accommodated by varying the axial stiffness along the can. With further optimization, a basically "low-stress at high-temperature conditions" system may be obtained. A typical example is the restraint design for the dispersed conical core proposed for the earlier FFTF designs. This kind of approach requires further evaluation.

7. Radial Blanket and Fuel Assemblies — The reference design has different pin/pitch designs for the blanket and fuel assemblies, but the cans are of the same basic design. The metal swelling phenomenon reacts differently on these assemblies. The major growth takes place in the fuel assemblies, where thermal camber effects are of small order; the effect on the blanket assemblies is opposite, i.e., small growth but large differential effects. Due consideration should be given to different can designs.

8. Other Areas of Evaluation — Some other areas that might offer potential solutions for both metal growth and thermal radial gradients are as follows:

- a. Varying coolant flow, i. e., varying pitch of fuel pins.
- b. Varying the fuel pin-to-can wall dimension.
- c. Seam-welding the wrapper can with materials having different coefficients of expansion.
- d. Employing a grid structure with bi-metallic differential expansion.

3.4.8.2. Evaluation of Potential Solutions

The problems associated with metal swelling have been divided into two major groups and identified in section 3.4.8.1. Both the bulk and differential growth groups will result in a limitation of design or operating characteristics. The problems associated with both groups will generally be reduced with a smaller bulk growth effect of the material. However, design solutions to reduce the bulk growth effects may aggravate differential growth problems.

If they are symmetrical, the bulk growth effects on core components (e.g., dilation and length changes) can be readily accommodated with the B&W design. A general opening up of fuel assembly gaps to accommodate dilation effects and modification to the fuel element spring assembly to accommodate the axial effects of fuel assembly growth are required.

Fuel assembly dilation will preclude the use of spacers or heavy pads in the core zone because the dilation features will require that the fuel assembly centers be enlarged to accommodate EOL growth. Spacer pads would only tend to increase fuel assembly pitch, while serving no worthwhile purpose at BOL, and only override stops at EOL, which is in itself an undesirable feature. To attempt to compact the core at BOL and cause dilation of the whole core at EOL would result in unpermissible fuel can design conditions or would require highly flexible fuel assembly flow nozzles at the outer radial core stations.

Due to the increased fuel assembly can pitch, the uncanned fuel assembly or perforated cans cannot be considered, as the BOL bypass flow would be substantial.

The increased fuel assembly pitch results in reduced breeding capability and subsequently increases the power generation cost. Dilation features can complicate fuel handling operations because of the increased cross-sectional area of the fuel can. The natural circulation aspects for decay heat removal from the fuel assembly can, however, be improved.

The lengthening of the fuel assembly caused by volumetric growth will have a small effect on the refueling system, requiring extra handling and storage heights. Any change in fuel assembly length may, however, complicate the design of the clamp if axial movement occurs at the clamping interfaces. Such movement would be the result of clamping load, friction factors, and axial loading, which would vary throughout life because of the time-dependency factor of growth. Subsequent axial movement of the core might induce reactivity changes.

The differential growth of the assembly caused by temperature and flux gradients induces fuel assembly bowing. This induced bowing feature causes a major problem regarding irradiation growth. If uncorrected, it results in misalignment with the control rod drives and requires a flexible control assembly. If corrected, suddenly increased midcore temperatures will probably induce instantaneous positive reactivity insertion and may not induce a delayed negative reactivity insertion.

The bowing of the fuel assemblies can complicate the fuel handling system. In addition to handling a larger effective component diameter, the bowed assembly can be more difficult to locate and will also require a higher clamping load to locate the core correctly. An incorrectly located core could cause reactivity insertion with increase of core temperature.

The fuel bowing problem may result in more frequent fuel replacement or a rotation requirement for fuel assemblies, resulting in increased fuel handling times and subsequent reduced plant capacity and increased power generation costs.

The problem of radiation growth will also affect other core structures, which may require increased shielding and may necessitate periodic replacement during life or, in the case of the neutron shields, rotation consideration.

The potential solution of having an axial zone-enriched core has been analyzed—refer to section 3.4.3.2(5) for physics methods and to Appendix G for the growth effect. With the case analyzed, no reduction in peak growth was accomplished by using axial zone-loading for the core. Shifting the peak fluxes toward the channel inlet resulted in an increased channel temperature, thus defeating the purpose of the shift.

Table 3-11. Locations of Axial Increments

<u>Distance along channel, in.</u>	<u>Layer</u>	
0	-14	} Lower axial blanket
0.5	-13	
4.5	-12	
8.5	-11	
10.5	-10	
12.5	-9	
13.5	-8	
14.5	-7	} Core midplane
15.484	-6	
16.468	-5	
18.437	-4	
20.406	-3	
24.226	-2	
28.046	-1	
31.866	0	} Upper axial blanket
35.686	1	
39.507	2	
43.327	3	
45.296	4	
47.265	5	
48.249	6	
49.233	7	} Upper axial blanket
50.233	8	
51.233	9	
53.233	10	
55.233	11	
59.233	12	
63.233	13	
66.000	14	

Table 3-12. Regional Flow Changes

<u>Core zone</u>	<u>BOL (ref), %</u>	<u>BOC (III), %</u>	<u>EOC (III), %</u>
1	--	0.8	2.0
2	--	0.3	0.8
3	--	-0.7	-1.5

Note: The increased flow in zone 3 is a result of the greater growth in zones 1 and 2.

Table 3-13. Methods for Reducing Fluence Levels and Gradients in the Core

Bulk Metal Swelling

1. Reduce fuel residence time
2. Reduce fuel residence time and fuel volume (burnup held constant)
3. Annular core concept
4. Reflectors
5. Vary relative enrichments of radial zones
6. Use axial zone loading for core

Metal Swelling Gradients

1. Reflectors
2. Thin annular core

Table 3-14. Effects of Reduced Fluence

	<u>Reference core</u>	<u>Reduced fuel residence time</u>	<u>Reduced fuel residence time and fuel volume^(a)</u>
Pin OD, in.	0.280	0.280	0.225
Avg disch burnup, MWd/T	100,000	62,000	100,000
Fuel residence time, EFPD	1050	656	656
Fissile loading, kg Pu	2769	2698	2054
Avg initial enrichment	0.1162	0.1133	0.1375
Integrated breeding ratios ^(b)			
Core	0.94	0.98	0.83
Radial blanket	0.24	0.24	0.27
Core + axial blanket	1.18	1.22	1.10
Peak fluence, n/cm ² × 10 ²³			
>0.111 MeV	3.26	2.00	2.86
Total	5.92	3.67	5.26
Decrease in peak fluence from reference core, %			
>0.111 MeV	--	38.5	12.1
Total	--	38.0	11.2
Total operating cost, mills/kWh	3.38	3.70	3.45

(a) Constant burnup.

(b)
$$\frac{\text{fissile nuclides produced in the region}}{\text{fissile nuclides destroyed in core + radial blanket}}$$

Table 3-15. Core Description — Reference and Modified Designs

	Reference		Modified	
	Core	Blanket	Core	Blanket
Pin pitch, in.	0.337	0.638	0.285	0.503
Pin OD, in.	0.280	0.608	0.225	0.483
Cladding thickness, in.	0.010	0.017	0.010	0.017
Assembly pitch, in.	6.50	6.50	5.59	5.59
Can thickness, in.	0.128	0.128	0.153	0.153
Sodium gap, in.	0.050	0.050	0.050	0.050
Active fuel height, in.	34.7	42.7	41.0	49.0
No. of fuel assemblies	288	138	246	180
Pins per assembly	331	91	331	91
No. of control assemblies	25	--	25	--
Fissile loading, kg Pu	2769	--	2054	--
Avg initial enrich., %	11.62	--	13.75	--
Fuel residence time, EFPD	1050	--	656	--
Avg disch burnup, MWd/T	100,000	--	100,000	--

Table 3-16. Annular Cores Vs Reference Core

	<u>Reference core</u>	<u>Thick annulus</u>	<u>Thin annulus</u>
Thickness of annulus, in.	--	29.5	17.7
ID/OD	0.0	0.60	0.74
Active fuel height, in.	34.7	36.2	61.7
Fissile loading, kg Pu	2769	3434	4015
Avg initial enrich.	0.1162	0.1440	0.1682
Integrated breeding ratios ^(a)			
Core	0.94	0.72	0.60
Radial blankets	0.24	0.62	0.64
Core + blankets	1.18	1.34	1.24
Peak fluence, n/cm ² × 10 ²³			
>0.111 MeV	3.26	2.90	2.50
Total	5.92	4.90	4.09
Decrease in peak fluence from reference core, %			
>0.111 MeV	--	11.1	23.2
Total	--	17.2	30.9
Total operating cost mills/kWh	3.38	3.62	3.91

(a) fissile nuclides produced in the region
fissile nuclides destroyed in core + radial blanket

Table 3-17. Reflector Volume Fractions

<u>Reflector type</u>	<u>Volume fractions</u>				
	<u>Nickel</u>	<u>Steel</u>	<u>Sodium</u>	<u>Carbon</u>	<u>Beryllium</u>
Nickel	0.671	0.134	0.195	--	--
Stainless steel	--	0.805	0.195	--	--
PFR type	0.115	0.134	0.195	0.556	--
Beryllium	--	0.134	0.195	--	0.671
Sodium	--	0.100	0.900	--	--

Table 3-18. Reflected Cores — Beginning of Life

	Type of reflector					
	Reference (none)	PFR	Ni	SS	Na	Be
Fissile loading, kg Pu	2769	2720	2719	2729	2750	2709
Avg enrichment	0.1162	0.1142	0.1141	0.1146	0.1154	0.1137
Breeding ratios ^(a)						
Core	0.99	0.97	0.98	0.98	0.98	0.96
Radial blanket	0.24	0.14	0.09	0.12	0.17	0.13
Core + blanket	1.23	1.11	1.07	1.10	1.15	1.08
Flux at core ctr, n/cm ² -s × 10 ¹⁵						
>0.111 MeV	3.27	2.93	2.95	3.02	3.14	2.83
Total	6.04	5.43	5.46	5.58	5.81	5.24
Flux decrease at core ctr from ref core, %						
>0.111 MeV	--	10.4	9.9	7.9	4.0	13.6
Total	--	10.1	9.6	7.6	3.9	13.3

(a) $\frac{\text{fissile nuclides produced in the region}}{\text{fissile nuclides destroyed in core + radial blanket}}$

Table 3-19. Relative Zone Enrichment Variations
(100,000 MWd/T, 656 EFPD)

Initial enrichments relative to zone 1			
Zone 1	1.0	1.0	1.0
Zone 2	1.094	1.105	1.109
Zone 3	1.327	1.366	1.387
Avg initial enrichment	0.1365	0.1375	0.1391
Fissile loading, kg Pu	2039	2054	2078
Integrated breeding ratio ^(a)			
Core	0.83	0.83	0.82
Radial blanket	0.27	0.27	0.28
Core + blanket	1.10	1.10	1.09
Peak fluence, n/cm ² × 10 ²³			
>0.111 MeV	2.98	2.86	2.79
Total	5.45	5.26	5.13
Decrease in peak fluence from: reference core, %			
>0.111 MeV	8.7	12.1	14.3
Total	8.0	11.2	13.3

(a) $\frac{\text{fissile nuclides produced in the region}}{\text{fissile nuclides destroyed in core + radial blanket}}$

Table 3-20. Current Plant Designs

	Company				
	AI	B&W	CE	GE	W
Power, MWe	1002	1040	1000	1008	1044
Configuration	L	P	L	P	L
Pri temp, F	780/1140	800/1100	797/1095	810/1150	770/1000
Sec temp, F	670/1070	700/1000	--	646/1071	658/967
Steam press., F	2400	2400	2400	2400	2400
Steam temp, F	1000/1000	950/950	900/900	950/950	900

Table 3-21. Reference Design Compared With
1800 psi/900/900 Cycles

	<u>Reference</u>	<u>Case 1</u>	<u>Case 2</u>
Core inlet/outlet temp, F	800/1100	700/1050	700/1025
Core avg temp, F	950	875	863
Net thermal efficiency, %	42.4	40.8	40.7
Fuel cost, mills/kWh	0.73	0.731	0.738
Fixed cost, mills/kWh	2.38	2.435	2.482
Total power generation cost, mills/kWh	3.38	3.432	3.483
Core growth, %	33.0	25.6	24.5

Table 3-22. Reference Design Compared With
1500 psi Cycles

	<u>Reference</u>	<u>1500/796/NRH</u>	<u>1500/596/NRH</u>
Core inlet/outlet temp, F	800/1100	683/983	667/967
Core avg temp, F	950	833	817
Net thermal efficiency, %	42.4	36.7	34.3
Fuel cost, mills/kWh	0.73	0.8	0.8
Fixed cost, mills/kWh	2.38	2.51	2.61
Total power generation cost, mills/kWh	3.38	3.58	3.68
Core growth, %	33.0	22.0	20.0

Table 3-23. Reference Design Compared With
1000 psi Saturated Cycles

	<u>Reference</u>	<u>Case 1</u>	<u>Case 2</u>	<u>Case 3</u>
Core inlet/outlet temp, F	800/1100	640/840	625/855	610/870
Core avg temp, F	950	740	740	740
Net thermal efficiency, %	42.4	34.0	34.0	34.0
Fuel cost, mills/kWh	0.73	0.903	0.876	0.856
Fixed cost, mills/kWh	2.38	2.785	2.761	2.764
Power generation cost, mills/kWh	3.38	3.951	3.889	3.880
Metal growth, % $\Delta V/V$	33.0	15.0	15.0	15.0

Table 3-24. Fuel Pin Diameters

<u>Fuel burnup, MWd/T</u>	<u>Fuel pin OD, in.</u>	
	<u>2-yr cycle</u>	<u>3-yr cycle</u>
100,000	0.213	0.280
80,000	0.257	0.311
70,000	0.274	0.331
60,000	0.294	0.356

Figure 3-33. Metal Swelling Vs Burnup at Various Temperatures for Reference Design - PNL Data

Figure 3-34. Core Arrangement — Reference Design

LEGEND

- FUEL ASSEMBLY — 288 REQ'D.
- CONTROL ROD — 25 REQ'D.
(S — SAFETY ROD POSITION)
- BLANKET ASSEMBLY — 138 REQ'D.

Figure 3-35. Axial Linear Growth – PNL Correlation

Figure 3-36. Volumetric Growth for Radial Zone 1

Figure 3-37. Radial Linear Growth — PNL Correlation

Figure 3-38. Axial Linear Growth — ANL Data

Figure 3-39. Radial Linear Growth — ANL Data

Figure 3-40. PNL and ANL Axial Growth Data

Figure 3-41. PNL and ANL Radial Growth Data

Figure 3-42. Total Core Flow Vs Pressure Drop

Figure 3-43. Peak Fluence Vs Burnup, Reference Core

Figure 3-44. Power Generation Cost Vs Burnup, Reference Core

Figure 3-45. Power Generation Cost Vs Reduction in Fluence >0.111 MeV

Figure 3-46. Power Generation Cost Vs Reduction in Total Fluence

Figure 3-47. Peak Fluence Above 0.1 MeV Near the Core-Blanket Interface

Figure 3-48. Peak Total Fluence Near the Core-Blanket Interface

Figure 3-49. Effects of Reflectors on BOL Flux Distribution
Above 0.1 MeV — Midplane

Figure 3-50. Effects of Reflectors on BOL Total Flux Distribution — Midplane

Figure 3-51. Flux Shift Due to Axial Zone Loading — Flux >0.1 MeV, Midplane

Figure 3-52. Flux Shift Due to Axial Zone Loading — Total Flux, Midplane

Figure 3-53. Midplane Core Temperature Vs Metal Swelling

Figure 3-54. Power Generation Costs Vs Midplane Core Temperature

Figure 3-55. Power Generation Cost Vs Volumetric Growth

Figure 3-56. Reference Plant — Schematic Diagram

Figure 3-57. Temperature-Enthalpy Diagram for 2400 psi/900F Cycle Without Reheat

Figure 3-58. Temperature-Enthalpy Diagram for 2400 psi/900F/900F Cycle With Normal Reheat

Figure 3-59. Constraint With Stress-Free Operation

Figure 3-60. Constraints With Positive Reactivity

Note: Top and bottom supports
in the grid structure.

Figure 3-61. Comparison of Three Fuel Assembly Models

Figure 3-62. Temperature Distribution in the Reference Core

Figure 3-63. Neutron Flux Distribution in the Reference Core

Figure 3-64. Maximum Stress in Various Reference Fuel Assemblies

Figure 3-65. Maximum Strain in Various Reference Fuel Assemblies

Figure 3-66. Maximum Deflection in Various Reference Fuel Assemblies

Figure 3-67. Restraint Load at Intermediate Support in Various Reference Fuel Assemblies

Figure 3-68. Fuel Assembly Length and Relocation of Intermediate Support Vs Maximum Stress

Figure 3-69. Fuel Assembly Length and Relocation of Intermediate Support Vs Maximum Deflection

Figure 3-70. Fuel Assembly Length and Relocation of Intermediate Support Vs Restraint Load at Intermediate Support

Figure 3-71. Maximum Stress and Restraint Load at Intermediate Supports Vs Moment of inertia of Wrapper Can

Figure 3-72. Maximum Stress, Deflection, and Intermediate Restraint Load Vs Inlet Temperature

Figure 3-73. Maximum Deflection Vs Thickness of Wrapper Can Above Intermediate Support

Figure 3-74. Approaches for Accommodating Fuel Swelling

Figure 3-75. Fuel Assembly Movement

Figure 3-76. Power Generation Cost Vs Volumetric Swelling —
Two- and Three-Year Cycles

Figure 3-77. Power Generation Cost Vs Burnup — Two- and Three-Year Cycles

Figure 3-78. Gap Between Fuel Assembly Cans Vs Power Generation Cost

4. CONCLUSIONS AND RECOMMENDATIONS

1. The results indicate that either fluence or temperature reductions could reduce the currently anticipated metal swelling to acceptable levels, but the economic penalty is less when a combination of fluence and temperature reductions is used.

2. It is concluded that core restraint systems or other design devices will be required to accommodate the residual growth currently anticipated in the structural materials.

3. The core clamp appears to be the most reasonable approach to limit fuel assembly deflections during normal or transient conditions.

4. The material for the components in the redesigned core should be 20% cold-worked AISI type-316 stainless steel to minimize the effects of growth.

5. The reference design can be successfully modified to accommodate the predicted metal swelling, but the conceptual changes defined in the evaluation would affect overall plant safety and economic performance characteristics.

6. Except for the radial expansion characteristics, most of the changes appear to cause minor differences in the overall probability of accident or system response.

7. The radial expansion characteristics result in the insertion of positive reactivity during life, and a prompt positive reactivity feedback may be obtained in certain transients. In other transients, prompt negative but delayed positive feedback could conceivably occur.

8. The magnitude and time-dependence of the transient bow and core radial expansion are extremely complex. Therefore, a high priority should be granted for further study of the problem.

9. Possible approaches to reduced swelling and loss of ductility during irradiation through modification or control of composition, structure, and/or processing variables on the basic structural austenitic stainless steels (types 305 and 316) require additional study. The effects of additives (such as titanium), cold work, and carbide agglomeration—which have shown promise in achieving this goal—should be fully explored. Also, the effects of thermomechanical treatments and unique structural and processing variations should be examined. When significant reductions in swelling or ductility loss are realized, studies should be undertaken to utilize the results of the mechanisms responsible for the improvement.

APPENDIX A
Computer Codes for Modifying the
Fuel Assembly Wrapper Can

The study of fuel assembly distortion and the resulting reactivity changes is a complex problem involving the dynamic behavior of temperature, temperature gradient, neutron flux, neutron flux gradient, and creep. To study the effect of these possible sources of reactivity change on various types of fuel assemblies and structural supports, several computer codes have recently been developed. Two different codes are employed for the present study.

The computer code CRASIB (Creep Analysis of Statically Indeterminate Beams),²⁶ was developed by PNL primarily for the design of FFTF core restraint systems. The severe environmental conditions—high temperature and irradiation levels—expected during the operation of fast reactors make it necessary to consider primary and secondary thermal creep. These mechanisms for stress relaxation are expected to reduce the restraint loads at the support points and thereby reduce the radial deformation of the fuel assembly when the restraint mechanism is released. CRASIB can perform the creep analysis of any statically indeterminate beam of arbitrary cross section using the same structural idealization as used for any conventional beam theory. There is a provision in the formulation to consider the time-dependent loading condition that results from material swelling and time-varying mechanical and thermal loads on each segment. Several incremental functional representations for the creep law are accommodated. Other arbitrary creep laws can be readily accommodated.

The end result of CRASIB is the complete time history of creep and static response such as displacements, strains, stresses, moments, shear forces, and reactions for any multiple-supported beam with arbitrary cross section. There is a provision for obtaining these results under restrained and unrestrained (free) conditions when the fuel assembly is cooled to a uniform temperature at the end of fuel cycle or at other specified times.

The computer code DEFLECT,²⁷ which is a modification of the ELBOW code²⁸ developed for HTFR, uses the superposition technique, in which thermal and fast flux deflections are first determined. The forces at the restraint point are then determined; this restores the element to the proper deflections at these points. Time-dependent neutron flux-induced swelling is incorporated. The option is available for the

180-degree rotation of a fuel assembly at various time intervals during the reactor core lifetime.

The end results of DEFLECT are the displacements, restraint forces, and stress distribution at various time intervals. There is a provision for obtaining the restrained and unrestrained (free) deflections for the shutdown condition (uniform core temperature) at various time intervals.

Test cases were run on CRASIB to check the results obtained from DEFLECT. Both computer results were in good agreement. The CRASIB code is under constant revision to include the latest available experimental data and good features explored in the present fast reactor core design. Hence, it was decided that CRASIB should be used for most of the present swelling study. The main disadvantage of the code CRASIB is that the code lacks the feature of 180-degree rotation of the fuel can at this time. For the final analysis of a selected case, if rotation is required, the analysis may be performed on both these codes. In the near future, CRASIB is expected to include 180-degree rotation analysis; thereafter, it will be used exclusively.

Figure A-1 shows a simplified model for the current fuel assembly configuration. The assembly is divided into 20 or more axial elements, each less than 5 inches long, and 20 radial divisions. For the present analysis, the fuel wrapper can is assumed to be fixed at the lower support (grid plate). Although CRASIB can accommodate pinned connections, the parallel study with DEFLECT, which assumes a fixed lower support, suggested similar conditions for use in both the codes. The location of two upper restraint points along the length of the fuel assembly were varied for the parametric study.

CRASIB accommodates the precise variation of neutron-induced creep behavior along the length of the fuel assembly. However, at present, only one average value of thermal creep can be accommodated. For the present simplification, the fuel assembly is provided with two different creep rates along its length. In the low-flux, high-temperature region (flow exit), thermal creep is of primary importance. In the present study, high flux near the midplane of the core assembly is associated with low temperatures (less than 1000F). Hence, in the high-flux region, only neutron-induced creep rate is used. The error introduced here

appears to be negligible, and the results will be conservative. In the low-flux, low temperature (flow inlet) region, thermal creep is absent and only negligible neutron-induced creep rate is used. These regions are shown in Figure A-1.

From time to time during the parametric study, various computer runs were plotted with the maximum strain versus nodal point along the fuel element. In the plotting process, a peculiar phenomenon was encountered wherein there was a jump between the two node points separating the axial elements. For a continuous beam under the temperature and creep load, this type of discontinuity of strain distribution would not be likely in the actual situation. By reviewing the mathematic derivation in CRASIB, it was found that a finite element method was used in the analysis procedure. The beam is initially cut into a finite number of sectors; at each sector, it is assumed that a linear distribution of bending moment is applied. Although this assumption simplifies the problem, it also implies a constant shearing force acting on the beam. During the joining process of two consecutive elements we apply the continuity of displacement and slope at the common node point. This process introduces the discontinuity (on jumps) of the shearing forces. This is a normal phenomenon for the finite element method and should not cause any alarm. When the situation became worse, we simply increased the number of elements. It was recommended that the plot should be based on the element instead of the node point.

Another point of concern during the parametric study was the abrupt change of strains between the fast-flux creep zone and the thermal creep zone. This was caused by two completely different creep laws used at two zones. The fuel element is hypothetically subjected to one type of creep influence along its axis and is suddenly changed to a different kind of creep at the next sector. From a physical viewpoint this would be unlikely. It was considered that the code should be modified to incorporate a transition zone wherein the fast-flux creep and thermal creep both possess the same level of influence. When starting at the lower portion of fuel element, the temperature is low and the fluence is dominated by the fast flux. At the upper portion of the element the temperature is high and the thermal creep effect is dominated. However, between the lower and the upper portion there exists a middle portion, the

influences of which are shifted from fast flux to high temperature. For this purpose, B&W has now modified the CRASIB code to incorporate the transition zone.

Figure A-1. Simplified Model for Fuel Assembly

APPENDIX B
Flux and Temperature Distribution
for the Reference Core

The analysis of the reference core design began after a short review of all considered swelling correlations. The reference design²⁵ was used as a basis for this work. Table B-1 is a listing of the axial bundle average temperatures for each assembly in the reference design. An average channel in each bundle was analyzed, and the resulting temperatures were assumed to be bundle average values. A small discrepancy occurs in the blanket temperature calculation; this is a result of inaccuracies involved in selecting the peaking factor and assembly flow from the reference design.

Tables B-2 and B-3 are maps of total flux and flux ($E > 0.1$ MeV), respectively, for the reference design at the end of the cycle (EOC). The two-dimensional flux distribution was calculated in 20 groups with TODB following a two-dimensional six-group depletion calculation, also with TODB. These calculations are described more fully in reference 29. The fluxes in the first eight of the 20-group structure were summed to obtain the flux above 0.111 MeV, which was used to calculate fluences for use with the PNL metal-swelling correlation. Because of differences in physics and thermal-hydraulics methods, the radial zones numbered in Table B-1 do not agree with those in Tables B-2 and B-3.

Table B-1. Fuel Assembly Average Temperature Distribution

Assembly No.: Dist from core $\bar{Q}$, in.: Distance along channel, in.	Temperature, F												Core → Blanket		
	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15
	<u>6.5</u>	<u>13.0</u>	<u>19.5</u>	<u>26.0</u>	<u>32.5</u>	<u>--</u>	<u>39.0</u>	<u>45.5</u>	<u>--</u>	<u>52.0</u>	<u>58.5</u>	<u>--</u>	<u>65.0</u>	<u>71.5</u>	<u>--</u>
0.0	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7
0.5	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7	800.7
4.5	801.9	801.9	801.9	801.9	801.9	801.9	801.9	801.8	801.8	801.7	801.6	801.6	800.7	800.7	800.7
8.5	804.5	804.5	804.5	804.4	804.4	804.4	804.4	804.3	804.1	803.9	803.6	803.6	800.7	800.7	800.7
10.5	806.6	806.6	806.6	806.6	806.5	806.5	806.6	806.3	806.1	805.8	805.2	805.3	800.7	800.7	800.7
12.5	809.6	809.6	809.6	809.5	809.4	809.4	809.5	809.1	808.7	808.3	807.5	807.5	803.4	803.0	803.4
13.5	811.3	811.4	811.4	811.3	811.1	811.2	811.2	810.8	810.4	809.9	808.8	808.9	805.1	804.4	805.2
14.5	815.1	815.1	815.1	815.0	814.8	814.9	814.9	814.3	813.7	813.1	811.7	811.8	807.1	806.2	807.3
15.484	820.9	821.0	821.0	820.8	820.5	820.6	820.7	819.9	819.1	818.1	816.1	816.3	809.4	808.1	809.6
16.468	827.4	827.5	827.5	827.2	826.8	827.0	827.2	826.0	824.9	823.7	821.1	821.2	811.9	810.2	812.2
18.437	841.9	842.1	842.1	841.7	841.0	841.3	841.5	839.7	838.1	836.2	832.1	832.4	818.4	815.8	819.0
20.406	858.3	858.5	858.5	857.9	857.0	857.3	857.7	855.2	852.9	850.3	844.6	844.9	826.8	822.9	827.5
24.226	893.8	894.1	894.1	893.2	891.7	892.3	892.9	888.8	885.1	880.9	871.7	872.2	846.8	840.0	848.2
28.046	932.8	933.3	933.2	932.0	929.8	930.7	931.5	925.8	920.5	914.5	901.4	902.2	870.6	860.2	872.7
31.866	973.8	974.4	974.3	972.6	969.8	970.9	972.1	964.5	957.6	949.7	932.6	933.6	896.0	881.8	898.9
35.686	1014.7	1015.5	1015.4	1013.3	1009.8	1011.2	1012.6	1003.3	994.7	985.0	963.6	965.1	921.4	903.4	925.0
39.507	1053.8	1054.7	1054.5	1052.1	1048.0	1049.6	1051.3	1040.2	1030.1	1018.6	993.6	995.1	945.2	923.7	949.6
43.327	1089.3	1090.3	1090.1	1087.3	1082.7	1084.5	1086.4	1073.8	1062.3	1049.1	1020.6	1022.4	965.3	940.8	970.3
45.296	1105.6	1106.8	1106.5	1103.6	1098.7	1100.6	1102.6	1089.3	1077.1	1063.2	1033.1	1034.9	973.6	947.8	978.8
47.265	1120.1	1121.3	1121.1	1118.0	1112.9	1114.9	1117.0	1103.0	1090.2	1075.7	1044.2	1046.1	980.2	953.4	985.6
48.249	1126.6	1127.8	1127.6	1124.4	1119.2	1121.3	1123.4	1109.2	1096.1	1081.3	1049.1	1051.0	982.7	955.6	988.2
49.233	1132.5	1133.7	1133.5	1130.2	1124.9	1127.0	1129.2	1114.7	1101.4	1086.4	1053.6	1055.5	984.9	957.5	990.5
50.233	1136.2	1137.5	1137.2	1134.0	1128.6	1130.7	1132.9	1118.3	1104.8	1089.6	1056.4	1058.4	986.9	959.2	992.5
51.233	1138.0	1139.2	1139.0	1135.7	1130.3	1132.4	1134.6	1119.9	1106.4	1091.1	1057.8	1059.8	988.7	960.6	994.3
53.233	1140.9	1142.2	1142.0	1138.6	1133.2	1135.3	1137.6	1122.7	1109.1	1093.6	1060.0	1062.0	991.3	962.9	997.1
55.233	1143.1	1144.4	1144.1	1140.8	1135.3	1137.5	1139.7	1124.8	1111.1	1095.5	1061.7	1063.7	991.3	962.9	997.1
59.233	1145.7	1146.9	1146.7	1143.3	1137.8	1140.0	1142.2	1127.2	1113.4	1097.7	1063.6	1065.7	991.3	962.9	997.1
63.233	1146.9	1148.1	1147.9	1144.5	1139.0	1141.1	1143.4	1128.3	1114.5	1098.7	1064.5	1066.6	991.3	962.9	997.1
66.000	1146.9	1148.1	1147.9	1144.5	1139.0	1141.1	1143.4	1128.3	1114.5	1098.7	1064.5	1066.6	991.3	962.9	997.1

Table B-2. Flux Plot — Total

		Total flux, $n/cm^2-s \times 10^{13}$														
Radial zone No.:		1	2	3	4	5	6	7	8	9	10	11	12	13	14	15
Radial dist from G_c , cm:		10.373	20.746	31.119	41.492	51.865	62.238	72.6117	82.984	87.984	90.484	92.984	97.484	106.373	115.262	119.762
Layer No.	Dist above core MP, cm															
15	109.671	26.581	--	--	--	--	--	--	--	--	--	--	--	--	--	--
14	89.790	51.625	--	--	--	--	--	--	--	--	--	--	--	--	--	--
13	79.670	80.731	--	--	--	--	--	--	--	--	--	--	--	--	--	--
12	69.550	143.59	--	--	--	--	--	--	--	--	--	--	--	--	--	--
11	59.430	212.78	--	--	--	--	--	--	--	--	--	--	--	--	--	--
10	54.270	267.40	266.36	264.28	261.11	256.80	251.24	244.24	235.42	227.02	222.34	219.02	214.13	203.94	187.72	173.00
9	49.110	312.85	--	--	--	--	--	--	--	--	--	--	--	--	--	--
8	46.610	343.67	--	--	--	--	--	--	--	--	--	--	--	--	--	--
7	44.110	374.69	373.27	370.41	366.10	360.27	352.84	343.62	332.29	321.78	315.90	311.56	305.23	292.01	270.47	250.87
6	41.610	405.18	--	--	--	--	--	--	--	--	--	--	--	--	--	--
5	39.110	450.22	--	--	--	--	--	--	--	--	--	--	--	--	--	--
4	34.110	505.73	--	--	--	--	--	--	--	--	--	--	--	--	--	--
3	29.110	578.80	--	--	--	--	--	--	--	--	--	--	--	--	--	--
2	19.407	645.66	--	--	--	--	--	--	--	--	--	--	--	--	--	--
1	9.703	680.25	677.78	672.84	665.44	655.58	643.30	628.59	611.39	596.44	588.42	582.65	573.62	553.27	517.81	484.87

Table B-2. (Cont'd)

		Total flux, n/cm ² -s × 10 ¹³														
Radial zone No. :		16	17	18	19	20	21	22	23	24	25	26	27	28	29	30
Radial dist from Q, cm:		122.262	124.762	129.262	136.548	143.834	148.334	150.834	153.334	155.834	158.334	163.334	173.696	184.058	194.421	214.059
Layer No.	Dist above core MP, cm															
15	109.671	--	--	--	--	--	9.7795	--	8.8162	8.4350	8.0612	7.5069	63.806	--	--	--
14	89.790	--	--	--	--	--	18.509	--	16.833	16.301	15.726	14.793	12.637	--	--	--
13	79.670	--	--	--	--	--	26.553	--	23.064	22.373	21.716	20.444	17.151	--	--	--
12	69.550	--	--	--	--	--	45.104	--	35.316	32.675	30.904	28.033	21.947	--	--	--
11	59.430	--	--	--	--	--	68.626	--	50.155	43.589	39.815	34.386	24.743	--	--	--
10	54.270	164.56	158.23	148.99	132.34	109.61	89.805	77.732	69.117	59.588	51.445	41.542	26.733	15.667	11.061	4.6640
9	49.110	--	--	--	--	--	108.54	--	84.992	74.422	64.124	50.814	30.607	--	--	--
8	46.610	--	--	--	--	--	121.49	--	95.663	84.046	72.407	57.056	33.454	--	--	--
7	44.110	239.64	231.17	218.59	195.39	163.06	135.05	118.34	106.59	93.730	80.731	63.408	36.519	18.427	12.541	5.0354
6	41.610	--	--	--	--	--	148.78	--	117.60	103.43	89.052	69.800	39.725	--	--	--
5	39.110	--	--	--	--	--	169.10	--	133.88	117.80	101.40	79.347	44.645	--	--	--
4	34.110	--	--	--	--	--	194.24	--	154.16	135.84	117.01	91.552	51.163	--	--	--
3	29.110	--	--	--	--	--	227.36	--	180.89	159.60	137.63	107.80	60.089	--	--	--
2	19.407	--	--	--	--	--	257.77	--	205.73	181.87	157.09	123.32	68.866	--	--	--
1	9.703	465.97	451.64	429.69	387.66	326.97	273.46	241.26	218.49	193.26	167.04	131.25	73.408	33.934	20.435	7.2642

Table B-3. Flux Plot -- Above 0.112 MeV

		Flux >0.112 MeV, n/cm ² -s × 10 ¹³														
Radial zone No.:		1	2	3	4	5	6	7	8	9	10	11	12	13	14	15
Dist from core C ₂ , in.:		10.373	20.746	31.119	41.492	51.865	62.238	72.6117	82.984	87.984	90.484	92.984	97.484	106.373	115.262	119.762
Layer No.	Dist above core MP, cm															
15	109.671	8.4382	--	--	--	--	--	--	--	--	--	--	--	5.66	--	--
14	89.790	16.456	--	--	--	--	--	--	--	--	--	--	--	11.95	--	--
13	79.670	28.557	--	--	--	--	--	--	--	--	--	--	--	19.98	--	--
12	69.550	55.930	--	--	--	--	--	--	--	--	--	--	--	40.58	--	--
11	59.430	91.107	--	--	--	--	--	--	--	--	--	--	--	76.59	--	--
10	54.270	122.53	127.746	121.121	119.689	117.322	115.251	112.117	108.161	104.324	102.094	100.453	98.1411	93.5061	86.1853	79.372
9	49.110	150.72	--	--	--	--	--	--	--	--	--	--	--	116.77	--	--
8	46.610	170.86	--	--	--	--	--	--	--	--	--	--	--	133.59	--	--
7	44.110	192.43	191.71	190.26	188.06	185.10	181.35	176.74	171.19	166.20	163.49	161.51	158.54	152.16	141.39	131.59
6	41.610	212.39	--	--	--	--	--	--	--	--	--	--	--	169.90	--	--
5	39.110	250.57	--	--	--	--	--	--	--	--	--	--	--	195.24	--	--
4	34.110	276.17	--	--	--	--	--	--	--	--	--	--	--	224.99	--	--
3	29.110	319.83	--	--	--	--	--	--	--	--	--	--	--	262.78	--	--
2	19.407	358.63	--	--	--	--	--	--	--	--	--	--	--	296.18	--	--
1	9.703	378.34	376.95	374.25	370.16	364.72	360.95	349.93	341.03	333.85	330.22	327.63	323.50	313.18	294.32	276.94

Table B-3. (Cont'd)

		Flux >0.112 MeV, n/cm ² -s × 10 ¹³														
Radial zone No.:		16	17	18	19	20	21	22	23	24	25	26	27	28	29	30
Dist from core G _c , in.:		122.262	124.762	129.262	136.548	143.834	148.334	150.834	153.334	155.834	158.334	163.334	173.696	184.058	194.421	214.059
Layer No.	Dist above core MP, cm															
15	109.671	--	--	--	--	--	2.824	--	2.5205	2.4646	2.2854	2.1137	1.7680	--	--	--
14	89.790	--	--	--	--	--	5.281	--	4.7816	4.6236	4.4461	4.1521	3.4638	--	--	--
13	79.670	--	--	--	--	--	8.331	--	7.0208	6.7447	6.5058	6.04698	4.8839	--	--	--
12	69.550	--	--	--	--	--	15.957	--	11.871	10.767	10.004	8.9483	6.5980	--	--	--
11	59.430	--	--	--	--	--	27.563	--	18.835	15.692	14.043	11.699	7.7116	--	--	--
10	54.270	75.3186	72.1809	67.742	59.913	49.1082	39.392	33.9099	28.958	24.045	19.971	15.205	8.5576	--	--	--
9	49.110	--	--	--	--	--	50.992	--	38.137	32.231	26.647	19.736	10.139	--	--	--
8	46.610	--	--	--	--	--	59.568	--	44.682	37.756	31.057	23.977	11.298	--	--	--
7	44.110	126.00	121.83	115.41	103.19	85.610	69.298	59.242	52.138	42.728	35.674	25.948	12.549	5.3370	2.9416	1.0550
6	41.610	--	--	--	--	--	78.624	--	58.585	48.398	40.249	30.023	13.865	--	--	--
5	39.110	--	--	--	--	--	91.87	--	68.847	57.555	47.005	35.065	15.903	--	--	--
4	34.110	--	--	--	--	--	107.39	--	80.765	67.8020	55.522	41.619	18.677	--	--	--
3	29.110	--	--	--	--	--	127.14	--	95.957	80.7637	66.307	49.521	22.390	--	--	--
2	19.407	--	--	--	--	--	144.65	--	109.23	92.767	76.474	55.804	26.127	--	--	--
1	9.703	267.11	259.73	247.91	224.02	187.17	153.57	132.24	116.67	98.718	81.464	59.548	27.974	10.913	5.1181	1.7538

APPENDIX C
Thermal-Hydraulic Analysis
of the Double-Walled Can

The investigation was conducted to ascertain whether the additional flow required for the double can annulus would seriously degrade the core outlet temperature, and whether it would reduce the outlet temperature variations expected in the conventional bundle over the core life. Because of the scope of the problem and the time requirements, the analysis was limited to one assembly near the center of the core. Hand calculations have yielded Figure C-1, which illustrates the assembly's bulk outlet temperature and annulus flow rate as functions of the ratio of the annulus friction pressure drop to the assembly pressure drop. Since the friction pressure drop in the annulus is a fraction of the bundle pressure drop, the remaining fraction of the annulus pressure drop is produced by orifices at each end of the bundle. For purposes of discussion, a 0.2 ratio of annulus friction pressure drop to assembly pressure drop yields a 1043F bulk outlet temperature and an annulus flow rate of 7.6×10^4 lbm/h. The reference flow rate for this bundle is 31.1×10^4 lbm/h; therefore, the total bundle flow rate will be increased 24.4%. If the magnitude of the increase in flow rate is alike in many bundles, the required total reactor flow rate would be increased substantially. This increased flow rate would cause a reduction in plant efficiency and an increase in capital cost.

Bundle growth in the conventional bundle was initially thought to cause outlet temperature variations over the core lifetime, but it was speculated that the outlet temperature would be more constant with the double-walled can. A very elementary examination indicated that the redistribution of flow at EOL due to the growth of the double fuel can assembly causes the annulus flow rate to decrease, and the assembly bulk outlet temperature is maintained nearly the same as at BOL. However, additional work done on a conventional bundle (see section 3.4.2.3) demonstrated that the variation of the outlet temperature due to bundle growth was not a significant problem. Also, due to the complex engineering requirement, the investigation of the temperature variations with the double-walled can was discontinued.

Figure C-1. Thermal-Hydraulics for Double-Walled Can

APPENDIX D
1500-psi Steam Cycles

This appendix covers the detailed assumptions and calculations that were used to apply the 1500 psia steam cycles to the core in order to reduce the midplane temperatures. The data for these cycles were taken from Sargent & Lundy's Engineering Evaluation Studies of Heavy Water Moderated Power Reactor Plants, SL-1974-3, published in January 1963.

The objective of the investigation was to derive total power generation costs as a function of the steam cycle and core midplane temperatures for at least two throttle steam temperatures. Previous economic evaluations of steam cycles for the 1000-MWe Follow-On Study were performed with the aid of the NAPS computer code. The code's capability was limited to the six steam cycles generated by United Engineers & Constructors, and therefore it could not be used directly to evaluate the 1500 psia cycles. Consequently, it was necessary to base the evaluation on hand calculations, the results of which were combined with selected outputs from the NAPS code. Since the S&L data (Table D-1) were given in terms of gross efficiencies, the values were normalized to arrive at net efficiencies that appeared reasonable for a 1000-MWe plant.

The efficiency was calculated to compare these cycles with the 1450 psi/1000F cycle. This cycle has a net efficiency (as computed by NAPS) of 39% using a final feedwater temperature of 434F. The gross efficiencies based on 434F final feedwater temperature were calculated, as well as the new turbine island costs using a linear relationship. Table D-2 shows the results. Table D-2 shows that for the 1500 psia/996F cycle the gross efficiency is 39.9%. Since it is assumed that this cycle can be compared with a 1450 psia/1000F cycle whose net efficiency is 39%, then it would be reasonable to assume that by subtracting 0.9% from all values of efficiency in Table D-2, reasonable plant net efficiency could be obtained.

As modified in Table D-2, the turbine island costs do not comply with the AEC cost accounting system included in the B&W 1000-MWe Follow-On Study. These values were modified by a multiplying factor f_R as defined below:

$$f_R = \frac{43.44 (\$/MWe - 1450/1000) \text{ NAPS}}{48.83 (\$/MWe - 1500/996) \text{ Table D-2}} = 0.889.$$

The cost of the turbine island for each of the steam conditions in question was calculated by the following:

$$C_{TI} = Q_R (\text{MWt}) \times \text{eff, net} \times \text{cost (Table D-2)} \times f_R.$$

The net electrical output was held constant at 1035 MWe and the reactor output Q_R was allowed to vary. The results are shown in Table D-3.

Examination of the outputs for a large number of NAPS runs, indicate that some portions of the nuclear island capital costs remain fairly constant. Listed in Table D-4 are these items along with the costs used in this investigation. Also listed are the costs for these items as printed out by NAPS for the reference design ($T_{R1W} = 800$, $\Delta T_R = 300$) 2400 psi/950F/950F steam cycle. A further assumption was that the fuel costs were constant at 0.8 mill/kWh. The steam generators, IHXs, and sodium pumps were costed on an individual basis using steam temperatures of 796 and 596F.

Steam Generators — The temperature differences in the steam generator were maximized by selecting a ΔT of 50F in the IHXs. This selection was based on maximizing the potential of the steam cycle, even though it was recognized that the increased size of the IHX (over the reference design) would affect the size of the primary tank.

Figures D-1 and D-2 show the temperature enthalpy diagrams and the plant temperature schematics selected for both cycles. Steam and sodium flow rates were calculated on the basis of the reactor power, neglecting the heat input by the sodium pumps. The overall heat transfer coefficients for the different sections of the steam generator were assumed to be the same as those used in the NAPS program. The total cost was based on an average value of \$89/ft² of calculated heat transfer area.

Intermediate Heat Exchangers and Sodium Pumps — The capital costs of these two components were calculated by first establishing a base cost for the 1500 psia/996F cycle shown in Table D-3. This was achieved by running similar conditions, i.e., core ΔT , IHX temperature conditions, etc. on the NAPS code with a 1450 psia/1000F steam cycle. The costs were then ratioed according to the reactor power output for the cycles in question. The results are tabulated below:

Steam cycle, psia/F	IHX cost, \$ × 10 ⁻⁶	Cost of Na pumps, \$ × 10 ⁻⁶
1500/796	5.6	2.5
1500/596	6.0	3.1

Capital Costs — The direct costs were taken as the sum of the costs for the fixed items, the SG, the IHX, and the pump, together with a 40% factor for indirect costs. A constant value of \$700,000 for land was added to the direct costs. Table D-5 shows the total power generation costs.

Table D-1. Sargent & Lundy Turbine Island Data

Steam temp, F	Super- heat, F	Eff-6 hrs (FW = 450F)	Eff-5 hrs (FW = 390F)	Turbine island cost, \$ × 10 ⁻⁶	
				6 hrs	5 hrs
596	0	35.4	34.8	53.02	52.18
696	100	36.3	35.9	51.16	50.61
796	200	37.7	37.2	49.87	49.31
896	300	38.9	38.3	49.25	48.74
996	400	40.1	39.4	48.92	48.58

Table D-2. Modified Gross Efficiencies and Cost

Steam temp, F	Eff-6 → 5 hrs (FW = 434F)	Turbine island cost, \$ × 10 ⁻⁶
596	35.2	52.65
696	36.2	51.00
796	37.6	49.70
896	38.7	49.09
996	39.9	48.83

Table D-3. Calculated Net Efficiencies and Cost

Steam temp, F	Net efficiency, %	Q_R , MWe	Turbine island cost, $\$ \times 10^{-6}$
596	34.3	3020	48.49
696	35.3	2934	46.96
796	36.7	2822	45.75
896	37.8	2740	45.20
996	39.0	2656	44.96

Table D-4. Calculated Costs for Fixed Items

Item	Cost, $\$ \times 10^{-6}$	
	1450 psi/1000F	Reference
Core vessel	0.4	0.4
Reactor vessel	2.3	2.0
Internals	2.0	1.8
Rod drives	0.9	0.9
Biological shield	12.8	11.1
Na inventory	0.8	0.7
Piping	0.2	0.2
Valves	0.6	0.6
Other fixed capital	27.5	27.5
Total	47.5	45.2

Table D-5. Capital Costs

Cycle, psi/F	Cost, $\$ \times 10^{-6}$		Cost, mill/kWh		Total power generation cost, mills/kWh
	Fixed items	SG, IHX, pumps	Fuel	OM & I	
1500/796	47.5	18.2	0.8	0.27	3.58
1500/596	47.5	19.4	0.8	0.27	3.68

Figure D-1. 1500 psi/796F Steam Cycle — Temperature Vs Enthalpy

Figure D-2. 1500 psi/596F Steam Cycle — Temperature Vs Enthalpy

APPENDIX E
Novelty Concept

1. Bearing Pad Locking Device

Because of their long-term characteristics, neither irradiation material growth nor irradiation creep impose safety or reactivity control problems. From some engineering approaches one would prefer that both movements resolve themselves to negative feedback characteristics, i.e., their growth is outward, away from the core centerline, as continual inward growth could conceivably result in a locked-up fuel assembly bundle (statically welded or high refueling loads). This action could ultimately lead to the collapse of the can onto the fuel pins within. Providing that a large enough gap is left between the fuel assembly cans, the reactivity control system should be capable of controlling any slow rate of reactivity insertion, even if the creep and growth rates were of a positive feedback nature.

Thus, one of the major requirements is that a temperature ramp, step change condition, seismic loading, or accident condition does not manifest itself to a fast collapse of the core into a more critical array. If possible, any device within the core should result in a negative feedback condition. However, a positive feedback mode could still be completely acceptable if the time condition could be extended by incorporating a reliable displacement/time buffer mechanism. A simple application of this device is a hydraulic damper with a slow leakage control rate that comes close to the characteristics of a hydraulic lock.

This device would have contact with neighboring fuel assemblies and could readily account for the day-to-day creep or irradiation growth or the zero-to-full power condition (2- to 3-hour startup period). The effect of sudden movement, however (e.g., thermal ramp or seismic shock) would effectively geometrically freeze all fuel assemblies.

2. Low-Temperature Plant Design

During the analysis of low-temperature plant designs, two unorthodox but reasonably well-known design approaches came to mind: (a) a dual-pressure steam cycle and (b) a binary biphenyl-steam cycle.

The dual steam pressures employed in early BWR and GCR technology have since been abandoned. However, they offer a way to reduce core midplane sodium temperatures about 40 degrees, compared with a plant using a single steam pressure and having about the same net thermal efficiency.

Figure E-1 shows typical plant temperatures using dual steam cycles of 600 and 1500 psi, each with 100F superheat. Combined plant efficiency would be better than that with a 1000 psi saturated steam cycle. Core midplane sodium temperature could be reduced to 700F while maintaining an IHX LMTD of 100F. Steam generator surface requirements would rise because of reduced LMTDs. However, lower LMTDs would reduce thermal stresses in these units. The chief economic penalty would appear to be increased steam generator cost.

A binary-biphenyl-steam cycle could be applied in two ways. It could replace the dual cycle mentioned above to avoid sodium-to-steam heat exchange design, operation, maintenance, and safety problems. A more radical departure might also be considered—replacing the IHX with a biphenyl boiler, so that the biphenyl system actually becomes a kind of secondary heat exchange loop. In this respect, it would tend to comply with Task 2-2.6 of the LMFBR Program Plan.

An interesting example of the latter case would be to use a supercritical biphenyl cycle (200 psi) to further simplify heat exchange from the primary sodium. At this pressure there would be no phase change as the biphenyl is heated. Thus, the boiler would operate in a counter-flow heat exchange mode between two chemically compatible liquids. In combination with an 800 psi steam cycle having six feedwater heaters and 175F superheat, the gross plant efficiency would be between 40 and 42% of superheat, while achieving a reduction of core midplane sodium temperature to 740F. Figure E-2 is a temperature diagram for this plant.

A motor driven pump/turbine combination could be used to pressurize sodium in the biphenyl boilers to above 200 psi without disturbing pressures elsewhere in the primary systems. A gas-loaded sodium pressurizer in communication with a piston-operated biphenyl relief valve could be used to avoid the possibility of large quantities of biphenyl leaking into the primary sodium during an accident or occasional seepage through a leak during operating transients. In addition, any unforeseen accidental leakage to the primary system would vaporize during normal operation and pass to the cover gases, from which it could be removed by condensation. Radiolytic decomposition would be controlled

to a reasonable level by using a reduced sodium volume fraction in the biphenyl boilers and shielding them from the main body of radioactive primary sodium.

Figure E-1. Temperature-Enthalpy Diagram for 600/1500 psi Dual Cycle

Figure E-2. Temperature-Enthalpy Diagram for Binary Biphenyl Steam Cycle Achieving a 740F Core Midplane Temperature

APPENDIX F
Stability Check — Axial Load
on Fuel Element

A problem exists when axial loading is imposed on the fuel element. The problem can be defined in two buckling criteria: the structural buckling and the local buckling. The structural buckling is a column-type buckling that would cause excessive deflection or bowing of the fuel element. The local buckling is a wrinkle-type instability that would partially block the flow channel and cause local overheating on the fuel element.

1. Structural Buckling — The analysis for column-type buckling is based on Timoshenko's approach. The fuel element can be considered a continuous beam on multiple supports. It will be conservative to assume that the fuel element is simply supported. The following formulation can be obtained from reference 30:

Figure F-1. Axially Loaded Beam

Referring to Figure F-1, let us assume that the beam is compressed by the forces P applied at the ends. Based on the equilibrium conditions of the bending moments, we have

$$2M_2 \left[\psi(u_1) + \frac{L_2 I_1}{L_1 I_2} \psi(u_2) \right] = - \frac{6EI_1}{L_1} (\theta_{02} + \theta'_{02}), \quad (F-1)$$

where

I = area moment of inertia,
 θ = slope of the beam,
 $\psi(u)$ = Berry function of u .³¹

From equation F-1 the critical value of the compressive force is now obtained from the condition that M_2 becomes infinite, which means that

$$\psi(u_1) + \frac{L_2 I_1}{L_1 I_2} \psi(u_2) = 0. \quad (F-2)$$

For the lowest buckling load, we have

$$u_1 = \frac{K_1 L_1}{2} = \frac{L_1}{2} \frac{P}{EI_1}; \quad u_2 = \frac{K_2 L_2}{2} = \frac{L_2}{2} \frac{P}{EI_2}. \quad (F-3)$$

For the same cross section in both spans, equation F-3 reduces to

$$\frac{u_1}{u_2} = \frac{L_1}{L_2} \quad (F-4)$$

and equation F-2 becomes

$$\frac{\psi(u_1)}{\psi(u_1 L_2 / L_1)} = - \frac{L_2}{L_1}. \quad (F-5)$$

Equation F-5 can be solved using Table A-1 in Reference 31. For example, we have a typical fuel element with the following dimensions:

Flat to flat, in.	6.2
Length, in.	112
Moment of inertia (I), in. ⁴	15.64
Length (L ₁), in.	85
Length (L ₂), in.	27
Thickness (t), in.	0.146

From equations F-5 we have the following:

$$\frac{\psi(u_1)}{\psi(0.32 u_1)} = -0.32$$

and from reference 30 we obtain $u_1 = 1.05$. Using equation F-3, the critical loading equals

$$P_{cr} = \frac{4 u_1^2}{L_1^2} EI = \frac{4 \times 1.1}{7230} (25 \times 10^6)(15.64) = 23,800 \text{ lb.}$$

This column-type buckling depends very much on the overall size of the can rather than the thickness of the wall. Although the buckling load seems very high, it is a necessary design procedure to check the stability of every new geometry.

2. Local Buckling — Although the analysis of local buckling in a spatial stability of thin-walled beam can be applied to the fuel elements,³¹ a rough and simplified approach is selected.

Because of the scope of the work and the involvement in the mathematical formulation for an exact solution, it is assumed that hexagonal fuel can be treated in two extreme cases.

a. Thin-Walled, Circular Cylindrical Case: In this analysis the hexagonal can is replaced by a circular cylindrical shell under a uniform axial load. The buckling analysis of the thin-walled circular cylinder is well defined and illustrated in reference 31. The critical load can be calculated by

$$P_{cr} = 2\pi 0.605 Et^2. \quad (F-6)$$

For the case in which $E = 24 \times 10^6$ psi and $t = 0.146$ inch, the critical load is equal to 10,400 pounds. The buckling criterion should be checked when the fuel element is comparatively short.

b. Thin Flat Plate Case: The local buckling consideration is emphasized on a panel that is a side wall of the hexagonal can. In order to find the minimum buckling load, this panel is assumed to be simply supported along both sides. A detailed discussion is included in reference 32. Using the curve for the simply supported condition from Figure 2 (page 158) of that reference, we obtain

$$K_{min} = 4,$$

where

$$\begin{aligned} K_{min} &= \sigma_{cr} t b^2 / \pi^2 D, \\ t &= \text{thickness,} \\ b &= \text{width,} \\ D &= Et^3 / 12(1 - \nu). \end{aligned}$$

For the case in which $b = 6.25$ inches, $t = 0.146$ inch, $E = 24 \times 10^6$, and $\nu = 0.3$, we have

$$P_{cr} = \frac{4\pi^2 D}{6} = 6820 \text{ lb/edge.}$$

The total buckling load for the hexagonal can is $6,800 \times 6 = 41,000$ pounds.

APPENDIX G
Growth Effect of Axial
Zone-Enriched Core

This design change was analyzed to determine whether shifting the flux peak toward the inlet would reduce peak growth. BOL conditions were examined in the center core assembly (for the reference geometry). First, the nuclear analysis was performed to determine BOL fluxes (total and $E > 0.1$ MeV) for both the reference core and the axially zone-loaded core. (Refer to section 3.4.3—Physics Study, (6) Axial Zone-Loading.)

An axial normalized power profile was also determined from the nuclear analysis. Using the shifted profile and the profile of the reference design at BOL (which peaks at the center of the core), two cases were run using the thermal-hydraulics code CHESH to determine temperature distributions. Growth was calculated for both the ANL data and the PNL 20% cold-worked equation; the results are shown in Figures G-1 and G-2, respectively. No reduction in peak growth is accomplished by axially zone-loading the core. By shifting the peak fluxes toward the channel inlet, the power profile also shifts. The temperature increases faster, thus defeating the purpose of shifting the flux. The net result is a shifting of the peak growth toward the core coolant inlet.

Figure G-1. Central Fuel Assembly Volumetric Growth —
ANL Correlation

Figure G-2. Central Fuel Assembly Volumetric Growth —
PNL Correlation

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- ²⁸ "ELBOW — Fuel Element Bowing Code," Argonne Code Center Reference, Material Abstract 229, Argonne National Laboratory.
- ²⁹ 1000-MWe LMFBR Accident Analysis and Safety System Design Study, Quarterly Technical Report, October - December 1969, Babcock & Wilcox, BAW-1348.
- ³⁰ Timoshenko, S. and Gere, J., Theory of Elastic Stability, 2nd Ed.
- ³¹ Vlasov, V. A., Thin-Walled Elastic Beams, NSF, Washington D. C., pp 263-310.
- ³² Chilver, A. M., Thin-Walled Structures, John Wiley & Sons (1967), pp 153-207.

UNITED STATES
ATOMIC ENERGY COMMISSION
DIVISION OF TECHNICAL INFORMATION EXTENSION

Post Office Box 52
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37830

In Reply Refer To: TDD:PWR

October 20, 1970

Files

PROCESSING OF 1000-MWe LMFBR SAFETY STUDIES

We have been asked by L. W. Fromm, Manager, 1000-MWe Studies LMFBR Program Office, ANL, to print and distribute subject reports as a logical continuation of our involvement in the Follow-on Study Program. A total of 12 reports have been generated by BAW. There may also be a single report from each of 3 contractors, AI, GE and Westinghouse. Each report will be cleared for publication before being sent to DTIE.

Distribution is to be as follows:

UC-80	- 225 copies ✓
NTIS	- 25 extra cys. ✓
Stock	- 50 copies
1000 MWe Dist.	- 165 copies
	<u>465</u> copies

Phillip W. Rosser

cc: Dreyer
Masters (12)

ARGONNE NATIONAL LABORATORY

February 19, 1971

PRO:K:029

Mr. Robert L. Shannon, Director
Division of Technical Information Extension
U.S. Atomic Energy Commission
P.O. Box 62
Oak Ridge, Tennessee 27830

Subject: 1000-MWe LMFBR Safety Studies -
Publication of Babcock & Wilcox Topical Reports

Reference: Letter, L. W. Fromm to R. L. Shannon, "1000-MWe LMFBR
Safety Studies - Publication of Contractors' Phase and
Topical Reports," October 16, 1970

Dear Mr. Shannon:

In the reference letter I advised you that we would be transmitting to you for publication a total of twelve Babcock & Wilcox Company Phase and Topical Reports, and possibly three reports from other contractors, generated under the AEC-sponsored 1000-MWe LMFBR Safety Analysis Studies program. With that letter I enclosed one B&W report (BAW-1344, which you have since published), and advised that the remainder would be transmitted to you for publication when received and patent-cleared.

I am enclosing herewith one copy of each of the eight B&W Topical Reports listed below, all of which are now patent-cleared and ready for publication. The covers for these reports should be that used for the previously-issued BAW-1344, except for the changes noted in the table below. The letters heading the columns of the table are keyed to the markings on the attached xerox copy of the cover for BAW-1344.

OK
PWR
2-25-71

"A"

"B"

"C"

✓ BAW-1342	TOPICAL REPORT	Accident Analysis Methods
✓ BAW-1349	TOPICAL REPORT	Candidate Secondary Containment Support Systems
✓ BAW-1350	TOPICAL REPORT	Accident Initiating Conditions Part 1 - Flow Abnormalities
✓ BAW-1351	TOPICAL REPORT	Candidate Emergency Decay Heat Removal Systems
✓ BAW-1352	TOPICAL REPORT	Candidate Primary Containment Safety Features
✓ BAW-1354	TOPICAL REPORT	Candidate Protective Features
✓ BAW-1355	TOPICAL REPORT	Effects of Irradiation-Induced Metal Swelling on the Reference Design
BAW-1360	TOPICAL REPORT	Accident Initiating Conditions Part 2 - Reactivity Insertions

All other parts of the front covers for these reports should remain the same as the cover for BAW-1344.

Binding edge captions for the reports should read:

BAW-1342	1000-MWe LMFBR Safety Studies	B&W	Acc. Anal. Methods	USAEC
BAW-1349	1000-MWe LMFBR Safety Studies	B&W	Sec. Containment	USAEC
BAW-1350	1000-MWe LMFBR Safety Studies	B&W	Init. Cond. - 1. Flow	USAEC
BAW-1351	1000-MWe LMFBR Safety Studies	B&W	Decay Heat Removal	USAEC
BAW-1352	1000-MWe LMFBR Safety Studies	B&W	Pri Containment	USAEC
BAW-1354	1000-MWe LMFBR Safety Studies	B&W	Protective Features	USAEC
BAW-1355	1000-MWe LMFBR Safety Studies	B&W	Eff. of Metal Swelling	USAEC
BAW-1360	1000-MWe LMFBR Safety Studies	B&W	Init. Cond. - 2. Reactivity	USAEC

Mr. R. L. Shannon, Director
February 19, 1971

ma-t-e
4733

3

I note that for BAW-1344 you used a two-piece cover with staple binding, and the "binding edge caption" actually appeared on the back of the report. If this is to be the case with the reports enclosed, then the binding edge captions may be omitted. However, if any of the reports will actually have binding edges upon which printing can appear (and be visible with the reports on a library shelf), then the above captions should be used.

The distribution of all of these reports should be our "Distribution A" plus Category UC-80, Reactor Technology, as before. For your convenience I am enclosing another copy of the "Distribution A" list previously supplied to you.

In the reference letter I stated that there would be twelve B&W reports, and possibly three from other contractors. This has now been revised downward to eleven B&W reports and one report from Atomics International. The single remaining B&W report and the AI report will be transmitted to you when received and patent-cleared.

Thank you again for your excellent cooperation in publishing these reports. If there are any questions, please contact me on FTS extension 312/739-2971 or 312/739-4844.

Very truly yours,

L. W. Fromm, Manager
1000-MWe Studies
LMFBR Program Office

LWF:el
encls.

cc: (w/o encl.)

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Manager, AEC-CH
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1000 MWe

L M F B R

Liquid Metal Fast Breeder Reactor

SAFETY ANALYSIS STUDIES

BABCOCK & WILCOX

Accident Analysis and Safety System Design Study

PHASE I REPORT

"B"

**Fault Trees and Malfunction
Catalog**

"C"

Prepared for ARGONNE NATIONAL LABORATORY

Rev. per
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8/5/68

DISTRIBUTION "A"

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