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MASTER

AN EVALUATION
OF
STEAM-COOLED
FAST BREEDER REACTORS

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Prepared for
DIVISION OF REACTOR DEVELOPMENT & TECHNOLOGY
U.S. ATOMIC ENERGY COMMISSION

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AN EVALUATION OF STEAM-COOLED FAST BREEDER REACTORS

Prepared by
OAK RIDGE NATIONAL LABORATORY
Under the Direction of
Division of Reactor Development & Technology
U.S. Atomic Energy Commission

With the Assistance of
Babcock & Wilcox
Gulf General Atomic
Westinghouse Electric Corporation
Pacific Northwest Laboratory
Argonne National Laboratory
Los Alamos Scientific Laboratory
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FOREWORD

In implementing the civilian nuclear power research and development program, specific fast breeder concepts have been assessed in detail. This report, "An Evaluation of Steam-Cooled Fast Breeder Reactors," was prepared by an Oak Ridge National Laboratory working group under the direction of the Division of Reactor Development and Technology, U.S. Atomic Energy Commission, and is part of an overall assessment of the Civilian Nuclear Power Program being made in response to a request in 1966 by the Joint Committee on Atomic Energy. As outlined in the 1967 Supplement of the 1962 Report to the President on Civilian Nuclear Power, changes since 1962 in the technical, economic and resources picture have necessitated further study of the AEC program. This report evaluates one family of reactors in the reactor development program of the USAEC, and provides a basis for their consideration in the assessment of the overall program.

The overall assessment will include consideration of the technical status and economic potential of advanced converters and breeders, the role of thorium, various reactor fuel cycles, and **systems analyses** of the future nuclear-electric power complex. Although all phases of the assessment effort are based on one set of ground rules to achieve a common basis for comparison, it is inevitable that when the reports are published, many changes now taking place will not be reflected. The difference will be due to efforts to consolidate and strengthen the reactor development programs, and to the rapid expansion of the nuclear power industry. These matters are discussed in more detail in the 1967 Supplement to the 1962 Report to the President on Civilian Nuclear Power.

This evaluation was prepared by the Oak Ridge National Laboratory and reviewed by the "Alternate Coolant Task Force" whose members include representatives of Babcock & Wilcox (B&W), Gulf General Atomic (GGA), General Electric Company (GE), Westinghouse Electric Corporation (WAPD), Oak Ridge National Laboratory (ORNL), Argonne National Laboratory (ANL), Pacific Northwest Laboratory (PNL), Los Alamos Scientific Laboratory (LASL), and the Division of Reactor Development and Technology (RDT) of the U.S. Atomic Energy Commission. The evaluation is based largely upon designs and information provided by Karlsruhe Nuclear Research Center

and B&W, and includes consideration of coolant steam pressures of 1250, 2680, and 3700 psia. This evaluation is based on reactors fueled with plutonium from PWR discharge fuel; further, it takes into account improvements in the state of knowledge existing at the end of 1967. An additional objective of this study was to evaluate the technical development program which would be needed to achieve commercial application of the concept.

The procedure followed in the study and preparation of this report was:

1. presentation or refinement of designs and data by the concept sponsors, B&W and Karlsruhe,
2. technical and economic assessment of the concept and preparation of draft report by the ORNL working group,
3. review of draft report by design sponsors, task force members, and the Division of Reactor Development and Technology staff,
4. preparation of the final report by the ORNL working group, taking into consideration comments of reviewers,
5. approval of final report by Advanced Converter Task Force, and
6. review of the final report by the Commission.

During this process, wherever possible, data applicable to the systems analysis study were extracted. Finally, before publishing, the report was reviewed by selected representatives of the reactor industry, national laboratories, and the U.S. Atomic Energy Commission. There has been general acceptance of the content of the report. All comments received were carefully considered, and those accepted as valid and pertinent were incorporated into the body of the report. Comments that were not considered valid or that could not be logically incorporated are summarized in the section entitled, "Comments by Reviewers."

As discussed in the 1967 Supplement to the 1962 Report to the President on Civilian Nuclear Power, a large effort has been required to develop the light water reactors -- starting with naval reactors and followed by the early civilian power experiments and demonstrations--which culminated in major engineering efforts to construct large central station plants. The widespread acceptance of the light water reactor is an established

fact. The large industrial commitments and improvements in technology should result in further improvements in performance. These factors will make difficult the introduction in the United States of any new system even though a potential economic gain is indicated. Further, the continued improvement in the industrial posture of the light water reactors, the possible extension of some of this technology to steam cooling, plus the urgent need to introduce breeder reactors at the earliest date possible, could provide an incentive to introduce the steam-cooled fast breeder if it appears economically attractive. The possible future role of such reactors in the U.S. nuclear power economy is, therefore, not clear.

Based on the degree of continued success of current light water systems, as well as the availability of low cost uranium ore, and the progress of breeder reactor development, which is being assessed as part of the Commission's ongoing civilian power analysis program, it was determined early in 1967 to permit readjustment of the advanced converter program. Alternate coolants to sodium for fast breeders include steam and gas. Early studies indicated low breeding ratios for fast reactors cooled by supercritical steam, but study of use of lower pressure steam was not complete. A research and development program on alternate coolant fast breeders involving a modest expenditure of U.S. resources is planned. This program, coupled with active international technical liaison provides an economical means for the U.S. to maintain the option to exploit the gas- and steam-cooled fast reactors in the future.

If the assessment of the steam-cooled fast reactors and other alternate coolant fast reactors indicates that a specific reactor concept appears to have an attractive economic potential, such a conclusion could provide a basis for further development. The basis for development, however, would depend upon additional factors, such as the interest of U.S. utilities in these reactors, the availability of funds over and above those necessary to meet the present heavy commitment to develop other breeders, particularly the LMFBR, as soon as possible, and the ability of the U.S. Government, industry and utilities to support parallel reactor development efforts.

In large measure, the designs evaluated in this report are based on information provided by the developers of the systems and, therefore, generally reflect their viewpoint. It is recognized that the inclusion of members on the task force from industrial organizations engaged in the development of steam-cooled fast reactors has resulted in a report that reflects their enthusiasm. As fossil fuel was a moving target for the light water reactors, so now light water reactors and advanced converters, as well as fossil fuel, will be the moving targets for fast breeders such as the steam-cooled reactor. This report presents reactor technology that the proponents of steam-cooled reactors believe can be achieved.

Milton Shaw, Director
Division of Reactor Development
and Technology

PREFACE

Although most of the development work on fast breeder reactors has been concerned with liquid metal-cooled systems, in the early 1960's attention was focused on the use of alternate coolants as means for avoiding some of the problems associated with liquid metals. Numerous coolants were considered; however, most of these were eliminated, either because of chemical and metallurgical considerations, or because of heat transfer considerations. Argon has problems of neutron activation, and neon is very expensive. Nitrogen has a high neutron absorption cross section and may cause nitriding at high temperatures. Supercritical SO_2 cooling has been studied and appears to be feasible; however, experience with this system at the required operating conditions is very limited. Thus, the list of alternate coolants of interest for fast breeders was reduced to carbon dioxide, helium, and steam. Carbon dioxide and helium appear to be very similar from thermal and cost standpoints, but helium has been chosen by the proponents of gas-cooled reactors in this country because of its inert chemical nature and its desirable nuclear properties. Thus, present designs of alternate coolant fast breeder reactors are based on the use of helium and steam cooling. This report covers an evaluation of three steam-cooled fast breeder reactor concepts that have inlet steam pressures of 1250, 2680, and 3700 psi. The gas-cooled fast breeder reactor concepts are evaluated in Report WASH 1089 of this series, and an overall comparison of the alternate coolant fast breeder reactors is covered in Report WASH 1090, which also summarizes the steam-cooled reactor evaluation results given in this report.

In order to supplement the available background information on alternate coolant breeders, it was believed that further design optimizations should be carried out for both the helium-cooled and the low pressure steam-cooled fast breeder reactors. Thus, early in 1967, subcontracts were negotiated with Gulf General Atomic and Babcock & Wilcox, respectively, to do this work. These new designs were used as the basis of the evaluations.

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1. INTRODUCTION

1.1 Designs Studied

A steam-cooled fast breeder reactor designed to generate 300 Mw of electricity was described by Nuclear Development Corporation of America in 1961.¹ In that study the use of a Loeffler-type steam cycle was proposed, and by using an inward radial shift refueling program, it was estimated that breeding ratios of about 1.5 could be attained. An economic analysis of the reactor was reported in November 1961.²

In September 1962, the Babcock & Wilcox Company (B&W) and the American Electric Power Service Corporation initiated a study of a 1000-Mw(e) breeder reactor with 3600-psia steam as a working fluid in a direct cycle. The preliminary results,³ reported in August 1964, indicated that average core power densities of 350 kw/liter could be achieved that would result in relatively favorable economics; however, the estimated average breeding ratio was quite low (1.09). Further work on the design was reported in September 1965.⁴ The final results indicated low capital and fuel cycle costs, but the breeding ratio dropped to 1.03. A condensed description of the breeder reactor cooled with 3600-psi steam is available⁵ that summarizes these results. Following work on the 3600-psi system, B&W independently investigated the possibilities of cooling a direct cycle fast reactor with 1200-psi steam. Preliminary results⁶ were reported in October 1965, which indicated that, for a thermal power of 2500 Mw and burnup of 80,000 Mwd/MT, an integrated overall breeding ratio of 1.40 could be attained with a doubling time of nine years. The total power generation cost was estimated to be 2.5 mills/kwhr, based on modifying the estimates for the supercritical steam-cooled plant to allow for the lower steam pressure, and incorporating changes that include incorporation of the complete primary system in a single vessel with the Loeffler boiler in the bottom and steam circulators inserted through openings in the vessel wall. A more complete study of this concept was later completed,⁷ and a brief description is available.⁸

A study was made for the AEC by Hanford Atomic Products Operation in 1963 of a 300-Mw(e) supercritical pressure fast power reactor. The

Hanford study⁹ resulted in a breeding ratio of 1.14, as compared with the value of 1.03 determined by the B&W supercritical study made for the American Electric Power Service Corporation. The Hanford study was based on a core design in which the coolant flowed through tubes surrounded by web-type oxide fuel. With this configuration, the coolant fraction in the core ~~was~~ only 0.31, as compared with 0.44 in the B&W 3600-psi design.

In addition, the Karlsruhe group has been investigating steam-cooled fast breeders since 1965. The results of their work are published in a German report.¹⁰ A summary report on the Karlsruhe study is also included in this report as Appendix A. The reference reactor ~~was~~ designed to deliver steam at 2500 psi and 1000°F, and had an estimated breeding ratio of 1.15 and a doubling time of about 37 years. After the design report was prepared in 1966, the Allgemeine Elektrizitäts Gesellschaft (AEG), in cooperation with the Karlsruhe Laboratory, started the design of a 300-Mw(e) prototype of a 2280-psi steam-cooled breeder that is expected to be in operation about 1974. The core of this prototype will be tested prior to 1974 in the superheat reactor being constructed at Grosswetzheim.

Besides the studies mentioned above, more recent studies have been made by General Electric Company,¹¹ Euratom-Belgium Fast Reactor Association,¹² and by ASEA and AB Atomenergi in Sweden.¹³

The three designs evaluated in this report are the 3600-psi design by Babcock & Wilcox described in Refs. 4 and 5, the 2650-psi design by Karlsruhe described in Appendix A and Ref. 10, and the 1250-psi design by B&W described in Refs. 7 and 8. These designs were selected because they covered a broad range of design pressures. In this report the 3600-psi design is referred to as the high pressure design, the 2650-psi design is termed the intermediate pressure design, and the 1250-psi design is called the low pressure design.

1.2 Method of Evaluating Concepts

Starting with the reference designs provided by the proponents, the Oak Ridge National Laboratory evaluated the various concepts by first performing detailed nuclear physics and thermal-hydraulic analyses to determine whether the proposed operating conditions are technically

feasible.* The performance requirements of individual components and the power generation system as a whole were also checked. Fuel performance parameters (e.g., burnup, mass balance, power peaking factors, and reactivity coefficients) were analyzed.

Once the technical feasibility of the concept was established, the capital cost of the plant was estimated and compared with the vendor's estimated costs. Revisions were made where necessary.

The performance of the fuel from the standpoint of allowable burnups, temperature conditions, and other factors was also evaluated. Unit fuel fabrication, processing, and shipping costs were determined for each type of fuel, and these were used to determine fuel cycle costs over the life of any given plant. All cost data were finally combined to obtain a comparison of overall power costs.

One part of the evaluation made by the Oak Ridge National Laboratory involved preparation of data to be used in the linear programming calculations performed by Pacific Northwest Laboratory for the Systems Analyses Task Force. These data included: reactor material balance histories; fabrication, shipping, and reprocessing costs; fuel cycle data; block flow diagrams; normalized capital costs; and normalized operating costs. With respect to capital and operating costs, ORNL had the responsibility for insuring that costs for all of the reactors being considered by the Systems Analyses Task Force were on a consistent basis.

As a supplement to the evaluation of the technical feasibility and economics of each concept, ORNL also analyzed the research and development programs necessary to bring the concepts to commercial realization. Three alternate research and development programs were proposed (see Chap. 8) to provide the technological basis for commercially competitive steam-cooled fast breeder reactors. The purpose of these programs was to develop the low pressure (1250-psi) 1000-Mw(e) reactor, the high pressure (3600-psi) 1000-Mw(e) reactor, or both. Each of these programs included a 50-Mw(e) experimental reactor, a 500-Mw(e) demonstration unit, and a 1000-Mw(e) commercial plant. The time requirement was from 10 to 12 years for any of the three alternatives, and the costs to the AEC were

*The ground rules are given in Appendix B.

\$225 million, \$210 million, and \$425 million, respectively. These costs **included** research and development and certain first-of-a-kind costs associated with the large plants.

It should be pointed out that this study did not include support for a major planning effort. No attempt was made to lay out in great detail the research and development required to bring this concept to commercial fruition. Consequently, the research and development program plans were less well defined than for the other concepts for which substantial support was provided for planning.

2. DESCRIPTION OF REFERENCE DESIGNS

The three concepts evaluated are described in this chapter by first discussing the fuel, and then working outward: through the core composition and construction; the blanket; the coolant flow diagrams; and the major components of the system -- the reactor vessels, steam generators, circulators, containment system, and the shutdown and emergency core-cooling systems. The principal design characteristics for the three systems are given in Table 2.1. The design values that were modified by ORNL calculations are so indicated.

2.1 Fuel

Some of the features of the fuel proposed for the three concepts of the steam-cooled fast breeder reactor at the beginning of its operational lifetime are given in Table 2.1. The fuel material proposed for all three designs is a vibration compacted mixture of plutonium and uranium oxides, and the cladding materials are Inconel 625 and stainless steel. The fuel rods are pressurized to reduce the collapsing tendency of the cladding at the beginning of life. The proposed maximum ratings vary from about 11 kw/ft in the German design to about 18 kw/ft for the 1250-psi B&W design. Fuel elements for the three concepts are shown in Figs. 2.1, 2.2, and 2.3.

2.2 Core Composition and Construction

Some of the principal design parameters of the cores in the concepts of the 1000-Mw(e) fast breeder reactor are given in Table 2.1. The incentive to maintain low water content in the steam-cooled systems is reflected in the low coolant fraction in these three designs. The average power densities range from 286 to 447 kw/liter, and the values of average specific power range from 660 kw/kg for the German design to 940 kw/kg for the high pressure steam-cooled reactor; the low pressure system average specific power is 740 kw/kg. The data shown are for the beginning of the operational lifetime of the reactor, and they do not include power generated by the blanket. Horizontal views of the three core and blanket arrangements are shown in Figs. 2.4, 2.5, and 2.6, and the vertical zoning

Table 2.1. General Data for Steam-Cooled Fast Reactors

	Low-Pressure System	Intermediate-Pressure System	High-Pressure System
Reactor coolant pressure at inlet to core, psi	1,250	2,680	3,700
Net electric power, Mw	1,012	1,000	980
Reactor thermal power, Mw	2,900	2,519	2,326
Net station thermal efficiency, %	34.9	39.7	42.2
Core volume, liters	7,434	8,190	4,160
Core height-to-diameter ratio	0.206	0.574	0.604 annular
Axial blanket thickness, ft	1.5	1.18	1.5
Radial blanket thickness, ft	1.2	1.25	1.56/1.42
Core volume fractions			
Fuel	0.54	0.47	0.42
Structure	0.13	0.20	0.18
Coolant	0.33	0.33	0.40
Fuel type	UO ₂ -PuO ₂	UO ₂ -PuO ₂	UO ₂ -PuO ₂
Fuel configuration, rods per bundle	391	469	504
Cladding material	Inconel 625	Inconel 625	19-9DL stainless steel
Moderator	None	None	None
Coolant	H ₂ O steam	H ₂ O steam	H ₂ O steam
Coolant conditions to turbine, psig/°F/°F	1,050/925	2,350/1004/950	3,300/994/950
Coolant temperature in core, inlet/outlet, °F	575/925	685/1005	750/1000
Coolant pressure in core, inlet/outlet, psi	1,250/1,154	2,680/2,534	3,700/3,445
Coolant flow rate, lb/hr	36.9 × 10 ⁶	25.15 × 10 ⁶	25.51 × 10 ⁶
Average core power density, ^a kw/liter	353	286	447
Average specific power, ^a kw/kg fuel			
Core U + Pu	79	74	128
Core fissile Pu	740	660	940
Maximum linear heat rating, ^a nominal, kw/ft	16.5	11.1	15.1
Maximum linear heat rating, ^a hot spot, kw/ft	18.1	12.2	16.6
Maximum $\int k \, d\theta$, ^a w/cm	43.0	29.0	39.4
Pumping power, Mw	101	67	46
Fuel-pin diameter, in.	0.350	0.276	0.230
Length of fuel, in.	29	59.4	57
Cladding thickness, in.	0.012	0.015	0.010
Pin spacing, triangular pitch, in.	0.400	0.346	0.288
Average amount of steam in core volume, lb/ft ³	0.66 ^b	1.36	2.65

^aValues determined by ORNL.^bThe steam density used in ORNL physics calculations was 0.63 lb/ft³, as specified in the initial B&W design data. This value was revised to 0.66 lb/ft³ in later B&W thermal hydraulics studies.

Table 2.1 (continued)

	Low-Pressure System	Intermediate-Pressure System	High-Pressure System
Blanket-pin diameter, in.	0.475	0.492	0.455
Blanket-pin cladding material	Type 348 stainless steel	Incoloy 800	19-9DL stainless steel
Number of blanket pins	26,586		33,462
Axial blanket length, in.			
Top	18	13.8	18
Bottom	18	13.8	0
Axial blanket-pin diameter, in.	0.325	0.276	0.230
Reactor pressure vessels			
Design pressure, psi	1,400	2,950 ^a	4,100
Design temperature, °F	925	1004	1000
Material	SA-302, grade B, steel	SA-302, grade B, steel	SA-302, grade B, steel
Outside diameter, ft	20.8	18.26 ^a	17.2
Height, ft	51	39.37	34.3
Wall thickness, in.	7	15.1 ^a	20
Thermal shield thickness, in.	6.75	21.2	6.25
Weight, excluding thermal shields and internals, lb	1,000,000	1,442,000	1,194,000
Coolant circulators			
Number of circulators	4	6	2
Type	Single-flow radial	Multistage axial	Single-flow radial
Drive	Turbine	Turbine	Turbine
Capacity, each, lb/hr	10 × 10 ⁶	3.73 × 10 ⁶	12.8 × 10 ⁶
Suction pressure/discharge pressure, psi	1,140/1,250	2,425/2,690	3,400/3,700
Inlet temperature/outlet temperature, °F	553/585	658/685	731/750
Shaft speed, rpm	10,000	Unknown	3,600
Shaft horsepower, each	35,000	15,000	30,500
Efficiency, %	85		85
Impeller diameter, in.	24.8		31

Legend for Fig. 2.2

1. Fuel	11. Fuel
2. Fertile material	12. Fertile material
3. Fission-gas space	13. Supporting sheet
4. Three circumferential fins; pitch, 250 mm	14. Section A-B
5. Three circumferential fins; pitch, 350 mm	15. Detail E
6. Fertile material	16. Section C-D
7. Fuel	17. Pin type:
8. Fertile material	336 pins of type I
9. Fission-gas space	133 pins of type II
10. Fertile material	469 Total
	18. Supporting sheet
	19. Detail F

Fig. 2.2. Fuel Element for Intermediate-Pressure Steam-Cooled Breeder Reactor.

FULL SIZE VIEW

SECTION-Z-Z
SCALE 1/4"=1'-0"

PARTIAL
TOP PLAN VIEW
SCALE 1/4"=1'-0"

Fig. 2.3. Fuel Assembly for High Pressure Steam-Cooled Breeder Reactor.

Fig. 2.4. Core Arrangement of Low Pressure Steam-Cooled Breeder Reactor.

SCALE 1:10

1 INSIDE SHIELD	4 INSIDE FISSION ZONE
2 COMPOSITE CONTAINER	5 OUTSIDE FISSION ZONE
3 CONTROL RODS	6 RADIAL BREEDING ZONE

Fig. 2.5. Core Arrangement of Intermediate Pressure Steam-Cooled Fast Breeder Reactor.

Fig. 2.6. Core Arrangement of High Pressure Steam-Cooled Breeder Reactor.

arrangement of the intermediate pressure core is shown in Fig. 2.7. Vertical sections of the low and high pressure cores are shown in Chapter 6 as Figs. 6.1 and 6.4.

- a - INSIDE FISSION ZONE
- b - OUTSIDE FISSION ZONE
- c - RADIAL BREEDING ZONE
- d - UPPER AXIAL BREEDING ZONE
- e - LOWER AXIAL BREEDING ZONE
- f - FISSION GAS ACCUMULATOR

Fig. 2.7. Zone Classification of Intermediate Pressure Reactor.

2.3 Blanket

In each of the three concepts, the radial blankets are approximately 15 to 18 in. thick, and the axial blankets consist of extensions of the fuel rods approximately 18 in. above the fuel section in all concepts, and below it in all except the high pressure design. The annular core in the design for the high pressure steam-cooled system contains a central

blanket and an annular blanket. The design data for the blankets in the three concepts for the 1000-Mw(e) reactor are given in Table 2.1.

2.4 Coolant Flow Diagrams

All these concepts operate on a direct-cycle Loeffler type boiler principle in which a portion of the superheated steam from the outlet of the reactor is sent to the turbine generators to produce power; the remainder of the steam is mixed with feedwater to produce saturated steam, which is circulated to the inlet of the reactor. The schematic flow diagram for the low pressure steam-cooled breeder reactor (SCBR), shown in Fig. 2.8, illustrates a so-called "integral" design in which steam is recirculated inside the primary reactor vessel. The direct contact boiler is located at the bottom of the vessel, where feedwater is sprayed into direct contact with the superheated steam from the bottom of the core. In the other designs, the boiler and the circulators are located external to the reactor vessel, as shown in Figs. 2.9 and 2.10. For these designs, more piping is required to convey the large volume of recirculated steam; however, the boiler and the circulator are more accessible for maintenance. In the design illustrated in Fig. 2.8, the only steam

Fig. 2.8. Simplified Flow Diagram for Low Pressure Steam-Cooled Breeder Reactor.

Fig. 2.9. Simplified Flow Diagram and Containment System for Intermediate Pressure Steam-Cooled Breeder Reactor.

Fig. 2.10. Simplified Flow Diagram for High Pressure Steam-Cooled Fast Breeder Reactor.

leaving the primary vessel is that required to operate the turbines that drive the electric generator and the circulators.

The steam-cooled breeder reactor resembles the boiling water reactor in that it employs a direct cycle, with the steam from the reactor being used to drive the turbines. When reheat is necessary, steam-to-steam surface heat exchangers are used, as shown in Figs. 2.9 and 2.10.

2.5 Major Components

The major components of the concepts for the 1000-Mw(e) fast breeder reactors are the reactor vessels, the steam generators, the circulators, the containment vessel, and the shutdown and emergency core cooling systems.

2.5.1 Reactor Vessels

Some data on the reactor vessels proposed for all three concepts are given in Table 2.1. The steel vessels used in these concepts are fabricated of materials equivalent to SA 302, grade B, steel. The vessels proposed for all three steam-cooled reactors are described more fully in Section 6.1. The design for the low pressure steam-cooled system requires a somewhat larger vessel than is required for the systems with higher pressures. This is caused partially by the pancake shape of the core, and partially by the incorporation of the steam generator and circulators within the vessel in the low pressure steam-cooled system. The German design differs from the American designs in having a removable head that permits access for complete removal of all components inside the reactor vessel.

The vessels for the low and high pressure systems are designed for fuel handling machines that operate through ports in the head or wall of the vessels. It is intended that this practice will reduce the cost of construction, even though it may be necessary at some future time to cut the heads off the vessels to remove major components.

The German steam-cooled reactor has a conventional control rod drive system that requires penetrations through the head of the vessel. The low and high pressure systems designed by B&W contain internal control rod drive mechanisms built into approximately half of the fuel element assemblies. These mechanisms are actuated by hydraulic pressure supplied from an external source through individual command lines penetrating the bottom of the vessel in a manifold penetration. This is a novel means for driving control rods that will require some development. Lacking adequate development or success in developing the design for these mechanisms, it is reasonable to assume that the more conventional approach adopted in the German design is also applicable in the other steam-cooled systems.

2.5.2 Steam Generators

The steam for cooling each of the reactors is generated by what is known as a Loeffler boiler in which steam from the reactor is mixed with

the feedwater to produce equilibrium saturated steam, which is then fed to the suction of the steam circulator. In the intermediate and high pressure systems, this boiler is outside the reactor vessel. The high pressure boiler consists of only a mixing tee because no change of phase is required in the supercritical region. In the intermediate pressure system, the boiler consists of a separate vessel outside the reactor vessel in which the feedwater is sprayed into the entering steam at the top of a vessel, the mixture falls downward, reverses direction, and leaves at an exit near the top; excess water is left in the bottom of the vessel. Steam from the boiler is then pumped back to the reactor vessel to cool the reactor.

At a lower pressure, the latent heat of vaporization is higher, and a greater amount of superheated steam is required in the boiler for the low pressure steam-cooled system. This makes it advantageous to circulate the heating steam within the vessel rather than through pipes outside the vessel because the size of the pipes required to carry the necessary volume of steam would be very large. Therefore, the steam generators for the low pressure reactor are located in the bottom of the reactor vessel, and the circulators are inserted through penetrations in the wall of the vessel. Thus, all the recirculated steam remains inside the vessel, and only that required for driving the turbines is carried outside the vessel through pipes.

2.5.3 Circulators

Some of the data on the main coolant circulators used in the three concepts of a 1000-Mw(e) fast breeder reactor are given in Table 2.1 and in Table 6.6 of Chapter 6. Centrifugal circulators are used in the low and high pressure designs, and a multistage axial flow machine is used for the intermediate pressure system. All circulators are turbine driven, with the drive turbines exhausting either to feedwater heaters, condensers, or the low pressure cylinder of the main turbines. In most cases, throttle steam for these drive turbines is taken directly from the main steam leaving the reactor core.

2.5.4 Containment System

Containment philosophy for the steam-cooled breeder reactors varies according to the designer and his current thinking. In Fig. 6.4 (Chapter 6), the cross sectional view of the reactor vessel for the low pressure reactor, the secondary containment vessel is shown as being closely fitted around the primary reactor vessel. This vessel is designed so that a rupture of the primary system within its confines would result in only a partial reduction of pressure. As long as the external piping remained intact and the cooling system remained functional, it would be possible to continue cooling the reactor by pumping feedwater into the boiler section of the vessel, generating steam, and circulating it through the core. If the coolant system were not functional, the pressure resulting from this type of failure would still be high enough for some convective cooling, and for adequate cooling by use of the emergency and shutdown cooling systems. A third vessel designed for a low pressure surrounds the entire system to contain the fluids released in event of rupture of the secondary vessel shown in the figure.

The intermediate pressure reactor is contained in a secondary gas-tight container designed for a pressure of approximately 37 psig. This container is cast into the foundation of the reactor building. The reactor building is also gastight, and both the building and the containment vessel are entered only through air locks. The containment systems for all three concepts are described more completely in Section 6.5.

2.5.5 Shutdown and Emergency Core Cooling Systems

Diagrams of the shutdown and emergency core cooling system proposed are shown in Figs. 2.11, 2.12, and 2.13. The low pressure system provides for operation of the circulators under shutdown conditions by diverting the drive turbine exhaust steam to an emergency condenser so that normal flow of the coolant within the reactor can be maintained. As a last resort, the core spray system is provided to supply water from the feedwater storage tank to sprays above the reactor core. The design for the intermediate pressure reactor proposed by the Germans includes a secondary steam system and a special shutdown cooling system that are in

Fig. 2.11. Shutdown and Emergency Core Cooling Systems for Low Pressure Reactor.

Fig. 2.12. Shutdown and Emergency Core Cooling Systems for Intermediate Pressure Reactor.

Fig. 2.13. Shutdown and Emergency Core Cooling Systems for High Pressure Reactor.

parallel with each other and with the main systems for operation during shutdown, as well as an emergency core spray system for use as backup. The design for the high pressure reactor did not have an adequate safety system in the original proposal and, consequently, a typical head tank, suppression chamber, spray system, and associated equipment were added by ORNL.

2.6 Summary

In summary, the points of similarity between these three systems are that they all use the same kind of fuel, they all require power driven circulators, and they all are subject to similar problems in the loss-of-coolant accident. They all have Loeffler steam generating systems-

and a direct cycle for generation of power. The maximum heat rating of the fuel rods varies in the designs studied between 11 and 18 kw/ft.

The major differences between the systems are external to the fuel and consist of different types of control rod drives; some differences in vessel designs, containment systems, and shutdown and emergency core cooling provisions; and variations in refueling methods.

The designs are based on fairly well established technology, with the exception of the innovation of self-contained control rod drives. These may require some development or revision to external drives.

3. SUMMARY AND DISCUSSION

The three reactors selected for this evaluation do not comprise a set of comparable concepts representing optimum designs for each set of conditions. The two B&W designs are not comparable with the Karlsruhe design because of the more conservative approach taken at Karlsruhe. Further, the B&W designs are different from each other because of use of different cladding materials and different core compositions and plant arrangements. Because of these differences, it is not possible to determine a trend that is purely an effect of the operating pressure. The evaluation work followed the ground rules stated in Appendix B in that all concepts were reviewed and analyzed on the basis of the best available data on physics, thermal-hydraulic analysis, fabrication and engineering practices, and cost data. Attempts were made also to incorporate equivalent engineered safeguards where they were lacking so that all three concepts would be essentially equal in that respect. Even if each design had been optimized by its proponent, application of the ground rules in this evaluation would probably have shifted the optimum in each case. Therefore, it is improbable that any design concept could be optimum for the conditions of this evaluation.

This chapter contains summaries of the various parts of the evaluation made at ORNL, a statement of the conclusions agreed upon by the evaluation group, and some general discussion of the steam-cooled fast breeder reactor.

3.1 Physics Evaluation

The physics evaluation of the concepts was devoted to determining power distributions, Doppler and density coefficients, and the complete mass balances throughout the life of the reactors, with the resultant breeding ratios and doubling times. As was expected, the presence of water in the core and its softening effect on the neutron spectrum has a tendency to reduce breeding ratios, as compared with those of other fast reactors in which water is not present. A summary of the physics characteristics of the three reactors is given in Table 3.1.

Table 3.1. Physics Characteristics of Steam-Cooled Fast Reactors

	Low-Pressure System	Intermediate-Pressure System	High-Pressure System
Fuel-cycle data			
Fuel management			
Core and axial blanket	Batch refueling	One-third scatter refueling	Batch refueling
Radial blanket	Batch refueling	One-third each alternate cycle	One-third each cycle; two-thirds each alternate cycle
Core heavy-metal inventory, kg	33,012	31,800	14,492
Axial blanket heavy-metal inventory, kg	34,900	14,162	4,600
Radial blanket heavy-metal inventory, kg	20,860	38,595	36,200
Total heavy-metal inventory, kg	88,772	84,556	55,292
Average core fissile inventory, kg	3,554	3,530	1,980
Average axial blanket fissile inventory, kg	~322	~60	~19
Average radial blanket fissile inventory, kg	~207	~247	~327
Average out-of-reactor fissile inventory, kg	1,354	1,320	1,472
Total fissile inventory, kg	5,437	5,157	3,798
Fissile specific power, kw(th)/kg total	533	488	612
Median fission energy, kev	160	120	30
Core conversion ratio	0.98	1.01 ^a	0.63
Total breeding ratio	1.38	1.14	1.11
Fuel yield, % per year	5.90	1.84	1.70
Doubling time, years	12	38	41
Maximum fuel exposure, Mwd/MT	~100,000	~100,000	~100,000
Average heavy-metal burnup, Mwd/MT			
Core	~70,000	~57,500	~58,000
Axial blanket	~5,800	~4,100	~5,400
Radial blanket	~2,000	~4,000	~7,600
Core life, years at 0.8 power factor	3.02	2.65	1.58
Out-of-reactor Pu holdup period, years	1.0	1.0	1.0
Power-peaking factors			
Axial			
Beginning of life	1.21	1.33	1.72
End of life	1.16	1.33	1.39
Radial			
Beginning of life	1.21	1.44	1.11
End of life	1.48	1.44	1.09
Reactivity coefficients			
Doppler, $\Delta k/^\circ K$			
Normal	-8.6×10^{-6}	-1.6×10^{-5}	-7.2×10^{-6}
Core void	-7.6×10^{-6}		
Density effects, Δk			
Voiding to zero density			
Beginning of life	0.0094	0.0209	0.0050
End of equilibrium cycle	0.0073	0.0188	-0.0384
Flooding at density of 62.4 lb/ft ³			
Beginning of life	0.1664		
End of equilibrium cycle	0.0691	0.0115	0.0438

^aIncludes core and axial blanket.

3.1.1 Reactivity Effects of Coolant Density

Effective multiplication factors were calculated for conditions ranging from complete loss of coolant to complete flooding with room temperature water. In these calculations, the control rods were assumed to remain in their operating positions while the change in coolant density took place. For the intermediate and low pressure systems, the normal steam density was considered to be at or near the density for minimum reactivity. For the high pressure system, changes in steam density might increase or decrease reactivity, depending on the amount, direction, and time of the change. Large increases or decreases in density result in a more reactive system. Loss of coolant at beginning of life results in increases in effective multiplication factors of 0.009, 0.021, and 0.005 for the low, intermediate, and high pressure systems, respectively. Loss of coolant at end of life results in corresponding changes of 0.007, 0.019, and 0.034. Flooding at end of life results in an increase of 0.044 for the high pressure system, 0.011 for the intermediate pressure system, and 0.069 for the high pressure system. The maximum effect of flooding occurs at beginning of life in the low pressure system when the change in effective multiplication factor is +0.166. If flooding is determined to be a credible situation, the reactivity worth of the control system will have to be increased to insure the control of this condition in the low pressure system.

3.1.2 Breeding Ratios

The breeding ratios calculated were about 1.38 for the low pressure system, 1.14 for the intermediate pressure system, and 1.11 for the high pressure system.

Although these conversion ratios are low in comparison with those for other fast breeder reactors, the steam-cooled fast reactor will compare favorably with advanced converters because of its higher conversion ratio and relatively good economy. Inclusion of this reactor in the systems analysis should indicate its relative importance in the overall program in comparison with all types studied.

Figures 3.1 and 3.2 (taken from Ref. 14) indicate, for 8000- and 6000-liter cores, the limits of coolant volume fraction and water density of the core that must be maintained in order for breeding ratios greater than 1.0 to be achieved. The areas below the curves represent the conditions for breeding; those above the curves represent lower conversion ratios. The breeding ratio for the high pressure system cannot be meaningfully plotted on either Fig. 3.1 or 3.2. The data in these figures are for Inconel-clad fuel, whereas the high pressure system has 19-9DL stainless steel-clad fuel. Estimates show that Inconel cladding will lower the breeding ratio for the high pressure system to below 1.0. In addition, the data of Fig. 3.2 were developed for a core volume of 6000 liters, which is 1.5 times the volume of the high pressure core. However, the figures are of interest because they indicate the approximate ranges of combinations of coolant volume fraction and water density that can be

Fig. 3.1. Allowed Combinations of Steam-Density and Volume Fraction for Breeding Ratios of 1.0 to 1.3 in 8000-liter Steam-Cooled Fast Reactor Cores. (Taken from Ref. 14)

Fig. 3.2. Allowed Combinations of Steam-Density and Volume Fraction for Breeding Ratios of 1.0 to 1.3 in 6000-liter Steam-Cooled Fast Reactor Cores. (Taken from Ref. 14)

used in steam-cooled fast reactors to yield breeding ratios higher than 1.0. Curves are given for breeding ratios of 1.0, 1.1, 1.2, and 1.3, and it may be seen that the locations of the low pressure and intermediate pressure reactor designs on Fig. 3.1 are in fairly good agreement with the curves.

3.1.3 Doubling Time

The doubling times calculated were 12 years for the low pressure system, 38 years for the intermediate pressure system, and 41 years for the high pressure system. Recently reported measurements of the capture-to-fission ratio for ^{239}Pu indicate a probable increase in this quantity, which will result in decreased breeding ratios and increased doubling times. Additional experimental and analytical work is needed in this area.

3.1.4 Power Distributions

The power distributions obtained by ORNL are slightly less favorable than those presented by the proponents of the intermediate and low pressure systems. These distributions are very sensitive to small changes in relative enrichments of fuel zones. The distributions quoted by ORNL can be improved by small adjustments in the relative enrichments. The improvement may be possible only at one time during a cycle, and therefore, the power density must be evaluated carefully as a function of time. In these calculations of power distributions, the poison in the control rods was assumed to be uniformly distributed and, thus, any effect the control rods might have on power distribution was not considered.

B&W appears to have been successful in shifting the axial peak in the high pressure design toward the end of the core near the low pressure steam inlet. The maximum radial peak-to-average power distributions are 1.48, 1.44, and 1.11 for the low, intermediate, and high pressure cores, respectively.

3.2 Thermal-Hydraulic Analysis

Calculations based on the Heineman correlation and engineering factors to account for variations in flow, dimensions, and fuel composition show that the surface temperature in the low pressure design is 1332°F, and that in the intermediate pressure reactor it is 1140°F. Both values are below the selected limitation of 1350°F. Based on the Westinghouse correlation for heat transfer to supercritical steam, the high pressure design has a maximum surface temperature of 1351°F, which is acceptable.

The melting point of the irradiated mixed uranium and plutonium oxides was assumed to be about 4900°F, and calculations show that only the intermediate pressure design meets the criterion of no center melting. It was estimated that some center melting would occur in the other two designs, for which the linear heat ratings reach maximum values of 15 to 16 kw/ft. For a center-line temperature of 4900°F, the heat that can be removed from the fuel per unit length of fuel rod is about 12 kw/ft. Of the two fuel forms considered, sintered pellets and vibration packed oxides, the pellets will yield a higher linear rating without center-line

melting for the given fuel surface temperature, and both will yield higher fuel ratings if the surface temperature is reduced.

3.3 Linear Heat Ratings and Surface Temperatures for No Fuel Melting

The maximum linear heat removal rate with no center melting was calculated as a function of the outer cladding surface temperature. The maximum center-line temperature was taken as 4900°F, and the types of fuel considered were: (1) vibration-packed mixed oxides of uranium and plutonium with a theoretical density of 85%, as proposed for the steam-cooled reactors in this study; and (2) sintered pellets of the mixed oxides with a theoretical density of 95%.

Where the cladding surface temperature is 1350°F, the results indicate linear heat ratings of about 17 kw/ft for pelletized fuel and 12 kw/ft for vibration compacted fuel. It should be kept in mind that these linear heat ratings are those which occur at the same position on the rod as the specified surface temperature. The peak cladding temperature and the maximum heat generation rate do not necessarily occur at the same point in the reactor.

3.4 Core Materials

In steam-cooled fast reactors, fuel cladding materials are exposed to high coolant temperatures and pressures and the high fast neutron exposures associated with the high burnups required to achieve economical power. No prototype fuel elements have been tested for any of these reactors, but steam-cooled superheater elements have been tested at comparable temperatures and pressures in thermal reactors to burnups of a few thousand megawatt days per ton, and sodium-cooled rods have been tested in fast reactors at lower temperatures to burnups in excess of 50,000 Mwd/ton. Thus, the fuel element designs are based on considerable extrapolations of existing technology, and extensive testing and development will be required to define the materials limitations and to optimize fuel element designs to operate successfully to the burnups proposed.

Type 19-9DL stainless steel cladding might suffer stress corrosion cracking and would also be much less resistant to oxidation; it, therefore, would probably not be satisfactory. Coolant-cladding compatibility appears satisfactory for Inconel 625 and/or Incoloy 800; however, a potential compatibility problem might develop between the cladding and the mixed oxide fuel at temperatures above about 1300°F. Surface reactions have been observed that are believed to result from excess oxygen released as the fuel fissions. Such reactions may reduce cladding strength.

It appears that fuel and coolant compatibility will be satisfactory if no excess oxygen is present. A possible area of concern may be oxidation and potential swelling of the $\text{UO}_2\text{-PuO}_2$ fuel in a defected or failed fuel element if sufficient free oxygen is present in the steam.

Radiation effects on cladding are recognized as a potential problem in that reduced ductility would reduce stress rupture life at the low strain rates involved. On this basis, a limiting fuel element life was selected to be that at which the increasing cladding stress from fission-gas pressure would develop 1% cladding strain. Maximum burnup levels based on this criterion are approximately 100,000, 53,000, and 62,000 Mwd/MT for the low, intermediate, and high pressure designs, respectively. These values may be increased if more plenum volume is provided. Both the high **and** intermediate pressure concepts were designed for 50,000-Mwd/MT burnup and would require additional void plenum volume to achieve 100,000 Mwd/MT. The low pressure concept was designed for 100,000-Mwd/MT burnup, and the design appears satisfactory from the aspect of fission gas pressures.

Fuel swelling would also contribute to cladding stress. Cladding restraint must be sufficient to force the fuel to flow and accommodate swelling within the fuel porosity. Successful operation to 50,000 Mwd/MT was demonstrated with sodium-cooled pins with about comparable restraint, but an increasing resistance to accommodation developed at higher burnup. Considerable testing will be required to demonstrate that swelling can be contained without excessive cladding strain at design burnup.

All the steam-cooled reactors are to use prepressurization of the fuel rods to prevent collapse under initial operating conditions. ORNL calculations indicate that prepressurization should be increased for both

the high and low pressure designs. For the low pressure design, this is a relatively minor problem. However, there are insufficient data available on 19-9DL stainless steel for prediction of collapse life under creep conditions at the very high external pressure that would exist in the high pressure design. Increased pressurization would be required if the stronger Inconel 625 could be used as cladding for the high pressure design. Some increase in gas plenum volume would also be required.

There is some incentive to develop cladding and structural materials with better nuclear properties than those of Inconel 625 and better mechanical and corrosion-resistant properties than those of stainless steel. The high molybdenum content of Inconel 625 is undesirable because of its neutron absorption in the neutron energy range of these reactors. It was found that if Inconel 625 is used to replace 19-9DL stainless steel in the high pressure core, the number of absorptions in structural material in the core will increase from 0.0137 to 0.0404 in the neutron balance. This would reduce the core conversion ratio appreciably.

3.5 Reactor Safety

Reactor safety is realized if the potential for the release of fission products is minimized and it is assured that if fission products are released from the core, they will be successfully controlled and contained. The safety problems of all fast reactors differ from those of thermal reactors in that: (1) the power density is higher; (2) the larger quantity of fissile material present in the core of each fast reactor provides a potential of several critical assemblies in a thermal neutron environment; (3) the negative reactivity response to change in temperature and power is less; and (4) there is a positive reactivity effect of voiding of the coolant from the core, either locally or totally. The steam-cooled fast reactor has all these problems except local voiding, and because of its low density coolant, it has a low heat capacity that causes rapid temperature changes with loss-of-coolant flow or reactivity insertions.

The types of accidents considered in this evaluation included reactivity accidents, thermal accidents, plant startup and control accidents, and destructive nuclear bursts. Burst containment capability is discussed in this summary in Section 3.6.

3.5.1 Reactivity Accidents

Except for the power-limiting effect of the Doppler coefficient associated with the fuel, the SCBR does not have an inherent shutdown mechanism; therefore, an applied reactivity shutdown will be required to limit the fuel temperature due to a reactivity transient. A general review was made of possible causes of reactivity transients in the SCBR. Based on this review, it appears that for realistic reactivity insertion rates, a well-designed fast safety system and shutdown mechanism will be able to terminate an accident before the fuel was damaged.

A highly reliable applied shutdown requires the use of two complete and independent sets of safety systems and fast scram mechanisms. The reference systems do not include any form of reserve shutdown system, so the addition of a second automatic safety system and fast scram mechanism is recommended. In order to minimize common failure loads (systematic failures), the two systems must be of different design and construction, and both must have adequate time response.

Although flooding of the core may not be feasible in the loss-of-coolant type of accident, it is possible that under some conditions the core can be flooded. In fact, two of the designs provide for flooding the reactor for normal shutdown cooling. The calculations indicate that (1) a large reactivity increase must be counteracted if the rods remain in the operating position when the core is flooded, and (2) the rods do not have sufficient worth to keep the core subcritical when it is flooded. This situation will require the provision of some form of poison injection system that can add sufficient negative reactivity to make the core subcritical regardless of the source of flooding water.

Further study of the kinetics of the steam-cooled fast reactor cores will be necessary to provide information on which to base adequate safety system designs.

3.5.2 Thermal Accidents

The fuel in these reactors will heat up at a rate four to five times as fast as that in water-cooled reactors if it is not cooled. Thus the time margin for starting emergency cooling will be much shorter. The

evaluation indicates that the cladding temperature will reach the failure point within 1 to 5 sec after a piping failure, if emergency cooling had not been started, and that fuel melting will occur within 1 to 4 min without emergency cooling. Since cladding failure may allow the fuel to move into a more reactive configuration or obstruct cooling, cladding failure will probably be more significant than fuel melting in determining the allowable time margins.

The SCFBR reference designs do not provide the degree of redundancy in emergency core cooling systems that is employed in current light water reactor systems, in which both spray and flooding systems are provided, or where multiple systems are used. The SCFBR primary vessel designs, because of their bottom connections, prevent the use of rapid flooding systems for emergency cooling following a failure in one of those bottom lines.

Direct core spraying is the main form of low pressure emergency cooling provided in these designs. The spray systems provided in the SCFBR designs probably have the required spray cooling capacity, provided it can be demonstrated that the flow is distributed properly and the reliability of the system is high enough. In general, the designs provide one spray system with redundant pumps. Since these are essential systems, it is recommended that, as a minimum, two independent full capacity spray systems be provided, each with a separate spray ring and source of water. The auxiliary boilers may be used to inject steam for fog cooling to serve as another form of low pressure emergency cooling.

It will probably be necessary to add some form of high pressure emergency cooling for cases such as loss of forced circulation or slow depressurizations. In the intermediate pressure design, the Loeffler boilers in the secondary steam systems partially serve this purpose. In the high pressure design, the pressure of the pressurized head tank can be increased to partially aid in emergency cooling. Current BWR designs have steam driven pumps, and PWR designs have pressurized storage tanks for injection of water for high pressure emergency cooling. These designs can probably be adapted for application to the steam-cooled fast reactor.

The SCBR designs incorporate several features that are intended to minimize the effects of a primary system rupture. Valves and automatic

isolation systems are provided to isolate a broken line or coolant loop. In most instances, multiple lines and coolant loops are used to minimize the loss of coolant from a failure in an individual line. The low pressure design has an integral reactor vessel arrangement that reduces the number and size of possible rupture points. The high and low pressure designs include high pressure containment vessels that will reduce the effects of a break in the main reactor vessel or nozzles. The intermediate and low pressure designs have sources of water in the primary system that can flash and provide some makeup steam in the event of a system rupture.

3.5.3 Plant Startup and Control Accidents

The plant startup procedure has not been completely analyzed in this evaluation; however, three incidents have been considered that may be associated with the startup procedure. They are: (1) inadvertent rod withdrawal during startup; (2) inadvertent flooding of the core; and (3) sudden changes in flow of steam from the reactor. A reasonably fast scram can handle reactivity insertion rates of the order of \$2 to \$20/sec; so it will be necessary to limit the number of rods withdrawn at one time or the rate of withdrawal. The effect of flooding and some means of dealing with it were discussed above.

Preliminary dynamics studies indicate that these plants will have load following characteristics in which an increase in turbine steam flow will cause an increase in reactor power (by means of the density-reactivity effect). In general, this is a desirable characteristic; however, any large rapid increase in steam flow may produce power increases that will require a reactor scram. These studies also indicate that these plants will be stable for flux perturbations (with the control rods in a fixed position).

The conclusion of the safety evaluation is that more study of safety problems is required, including emergency cooling, reactor kinetics under both normal and accident conditions, coping with an excursion, and containing the debris after a core meltdown. The problems of inadvertent rod withdrawal, core flooding, and steam density changes during startup

can be solved satisfactorily to provide a degree of safety in these reactors equivalent to that now provided in thermal reactors.

3.6 Engineering Review of Plant Designs

In comparing the three plants, it was apparent that differences in design philosophy accounted for as much or more difference in the three **design as the difference in** steam conditions. The intermediate pressure design is more conservative than the other two in thermal performance of the reactor and in the design of the external heat transfer and steam circulation system. The high pressure design has a more compact layout than either of the other two. Further differences among the three concepts are in the containment system designs, the emergency core cooling system designs, and the turbine-generator selections.

3.6.1 Reactor Vessel Evaluation

The proposed steam-cooled reactor vessels are considered feasible to build in present-day manufacturing facilities, but the construction of the two-layer vessels for the intermediate **and** high pressure designs may require some development. (This item is included in the component development program proposed in Chapter 8.) The welded-on heads for the two B&W designs will require assembly and postweld heat treatment in the field to avoid damage to vessel internals during shipment. Elimination of bolted flanges reduces the cost of the vessels, but complicates the initial assembly and later access into the vessel if replacement of internals becomes necessary.

The maximum integrated neutron doses ($E > 1$ Mev) to the three reactor vessels are calculated to be 1.4×10^{20} neutrons/cm² for the high pressure design, 2.5×10^{18} neutrons/cm² for the intermediate pressure design, and 2.6×10^{20} neutrons/cm² for the low pressure design for a 30-year life of each of the vessels. Present design practice for SA-302, grade B, steel is to limit the exposure of the vessel to below 2×10^{19} to 4×10^{19} neutrons/cm² ($E > 1$ Mev) over the life of the vessel. To meet this criterion, additional thermal shield thickness will be required in the vessels for the high **and** low pressure reactors.

3.6.2 Destructive Excursion and Meltdown Containment Capability

The maximum hypothetical accident for the SCBR could involve a nuclear excursion with a sudden release of energy. The magnitude of this energy release was not completely analyzed for these concepts, but potential energy releases of the order of 1000 to 1600 Mwsec (500 to 800 lb of TNT) were postulated. Calculations performed by Nuclear Technology Corporation as part of this evaluation indicate that for an ultimate strain of approximately 7% or more, the vessels proposed for the three reactors can withstand energy releases equivalent to more than 1000 lb of TNT in the cases of the two higher pressure designs, and about 600 lb of TNT in the case of the low pressure design. These estimates are based on the vessels containing a compressible fluid at the time of the excursion. It is estimated that if they were full of water, the magnitude of the energy release they could contain will be reduced by a factor of 2. The criterion of 7% ultimate strain is based on the assumption that the SA-302, grade B, material will remain ductile with an ultimate strain in excess of that amount throughout its life. The high pressure containment vessels included in the high and low pressure designs would increase the capabilities of containing a destructive excursion, but the effects of these vessels were not considered in these calculations. The designs do not include provision for cooling the debris after a destructive excursion. Further study is required to establish the credibility of a destructive excursion as well as the upper limit of the energy release, and to develop reliable means for containing and cooling the fuel after a core meltdown or a destructive excursion.

3.6.3 Components

The principal components of these systems that differ from those in other water-cooled reactors are the steam generators and steam circulators. Special test facilities will be required to develop and proof test these and other SCFR components.

3.6.4 Steam Generators

The steam generators are a mixing type in which superheated steam from the reactor is mixed with the feedwater to generate steam to cool

the core. In general the direct contact steam generator proposed for all three systems appears to be satisfactory. The means provided for moisture separation are widely used. Spray nozzles are also quite common and should present no problems; however, data on spray distribution, droplet size, etc., will be required, and the performance of each steam generator concept should be demonstrated. Since there are few data on supercritical fluid heat transfer, the ability of the mixing tees proposed in the high pressure design to produce a homogeneous fluid should be investigated.

Little is known of the possibility of entrainment of moisture and dissolved solids in the steam leaving the steam generators. This and the erosion problems in these units are proposed for investigation in the development program (see Chap. 8).

3.6.5 Circulators

The circulator designs proposed for circulating the steam all represent an extension of present technology. Although gases at these pressures and in these volumes are being handled today, there are few, if any, situations in which these combinations exist. The highest pressure used to date in a gas-cooled reactor is of the order of 1000 psi. The shaft seals and bearings are perhaps the most significant problem areas, and the majority of the development work and proof-testing will be needed on these components.

3.7 Economic Evaluation

3.7.1 Costs

A summary of the economic evaluation of the three steam-cooled fast breeder reactors is given in Table 3.2. The estimates of direct costs included in this table are based on adjustments of the designer's cost estimates, or on new estimates made wherever they appeared necessary for cost normalization. These costs include some estimates determined by analogy with systems of other reactor plants for those parts of the systems that are not clearly defined in the design reports. Indirect costs were

Table 3.2. Cost Evaluation of Investor-Owned 1000-Mw(e) SCFBR Power Plants

	Low Pressure System	Intermediate Pressure System	High Pressure System
Direct construction costs			
Land and land rights	\$ 360,000	\$ 360,000	\$ 360,000
Structures and improvements	19,003,000	15,391,000	15,200,000
Reactor plant equipment	34,909,000	47,230,000	39,240,000
Turbine-generator units	32,853,000	25,213,000	24,307,000
Accessory electric equipment	4,254,000	4,000,000	3,800,000
Miscellaneous power plant equipment	1,250,000	1,250,000	1,250,000
Total direct construction cost (without land)	\$ 92,269,000	\$ 93,084,000	\$ 83,797,000
Indirect construction costs	\$ 37,993,000	\$ 38,316,000	\$ 35,067,000
Total capital cost	\$130,262,000	\$131,400,000	\$118,864,000
Net electric power output, Mw	1,012	1,000	970
Unit capital cost, normalized to 1000 Mw(e), \$/kw(e)	129.1	131.4	121.7
Power-generation cost, mills/kwhr			
Capital cost	2.53	2.57	2.38
Operating cost	0.28	0.28	0.28
Fuel-cycle cost	1.36	1.52	1.26
Total	4.2	4.4	3.9

determined by methods outlined in Ref. 15, except that percentages applied in determining these costs were changed.

The table includes total construction costs, net electrical output and the corresponding unit costs, the capital contribution to power production costs, and the total power generation costs for investor-owned nuclear power plants. The unit costs were adjusted to a normalized base for 1000-Mw(e) output by dividing the total construction costs by

$$10^6(\text{rating}/1000)^{0.75}$$

Although there are differences in reactor plant equipment costs, accessory electric equipment costs, and costs of structures and improvements, they tend to offset each other, so that the difference in the total capital costs of the high and low pressure systems is largely accounted for in the difference between their costs for turbine-generator units; the low pressure design suffers a penalty because of the considerably larger size of the turbine required. The intermediate pressure plant has the highest capital costs for reactor plant equipment of the three designs considered. This considerably higher cost for reactor plant equipment reflects the difference in philosophy of design followed by the Germans from that followed by B&W in their low and high pressure designs.

3.7.2 Capital Cost Uncertainties

While part of the difference between the capital costs of the high pressure design and the other two designs can be attributed to design features associated with the particular steam conditions, another part is due to the preference of different designers in layout, design of components, and safety features of the plant. Further development work and analyses are required to determine a design of a safety system that will best satisfy the requirements of the steam-cooled fast reactor plant; and when all three concepts are designed for equivalent safety systems, some of the cost differences may tend to be eliminated. Optimizing plant layouts and standardizing the design of reactor vessels, reactor internal structures, and thermal shields would also be expected to affect the cost differences. Although it is beyond the scope of this study to perform the necessary

design analyses and optimization studies to identify relative costs solely attributable to steam conditions, some of the cost trends can be indicated.

As an example, at the suggestion of B&W, rough estimates were made of the incremental costs of adding safety features to the high pressure design to make it equivalent to the low pressure design, and these incremental costs were included in the total capital costs for that concept. The modifications to the design included altering the reactor vessel to accept circulators and mixers, increasing the size of 1000-psi secondary containment vessel, eliminating the recirculating loop and reheat circulators, adding tertiary containment, and adding to the isolation condenser system. A lower efficiency resulted from these changes that added \$1.5 million to the cost. No modifications and cost adjustments such as those made for the high pressure concept were made in establishing the cost of the intermediate pressure design.

These adjustments are only a few of those that could be expected from a design normalization study of the three reactor plants. Although a small amount of design normalization (i.e., using single tandem compound turbine-generator units and turbine drives for the steam circulators) was performed, major revisions of the design were not attempted in the evaluation study, except those described above for the high pressure design. Therefore the cost estimates reported in Table 3.2 were obtained basically by evaluating the designs submitted by the sponsors.

3.8 Discussion

The following summary statements are based partly on this evaluation study and partly on general understanding of the use of steam for cooling fast reactors:

1. Steam has been used in power-generation systems over many years, and is the basic working medium not only for nearly all large conventional power plants, but also for all large-scale commercial nuclear power plants proposed at this time. It is assumed, therefore, that any fast reactor in a large-scale power-generating plant will use steam as the working medium.

2. The presence of steam in the core softens the neutron spectrum and, hence, affects reactivity. The fact that the density of the steam is

dependent on temperature and pressure requires a thorough study of the effects of these parameters on the kinetic characteristics of the core. These effects on reactivity and the kinetics do not appear to be appreciably stronger at higher pressures than at lower pressures, except in the extremes. The effect of voiding is greater for high pressure systems than for low pressure systems, and the effect of flooding is greater for low pressure systems than it is for high pressure systems.

3. A relatively large fissile inventory is a common requirement of all fast breeder reactors, and this leads to an incentive to design for high specific power in the fuel, which in turn leads to a need for long lifetimes or high burnup to avoid frequent refueling. Since linear heat ratings of fuel rods are independent of rod diameter, the high specific power requirement necessitates the use of very small diameter fuel rods. Support and control of the spacing of these small rods require the use of extensive structural material in the bundle. The presence of this structural material introduces flow resistance that is increasingly important as its ratio to the coolant volume increases. Because of its degrading effect on the neutron flux, the volume fraction of steam used as a coolant is kept as low as possible; as a consequence, the added resistance to flow by the structural material must be kept low. Thus, the fuel element design problem for steam-cooled fast breeder reactors is to design and develop fuel bundles with a large exposed surface per unit volume of fuel, a very small volume for coolant, and a high degree of structural stability. Various investigators have studied means of accomplishing these objectives; for example, Hoskins at ORNL,¹⁶ McNeely at General Electric,¹⁷ and Peterson and Stewart at Hanford.¹⁸

4. The cost of the turbine-generator units for the low pressure reactor is so much higher than for the units for other concepts that there is a cost incentive to study a design somewhere between the low the intermediate pressure designs. Based on the parametric study made by Pacific Northwest Laboratory, 14 a combination of coolant volume fraction and steam density could be selected that would produce a reactor design with a favorable breeding ratio, and the design study could start from that point. The turbine cost could be reduced appreciably if the steam conditions at the throttle were increased to about 1800 psia and 900°F.

This would probably require a core inlet pressure of slightly over 2000 psia.

5. Safety considerations for fast reactor cores with high specific power levels and the low heat capacities that exist in the cores of both steam-cooled and gas-cooled concepts lead to the need to give serious consideration to designing for an ultimate meltdown of an appreciable fraction of the core. Meltdowns that have occurred in fast reactor cores cooled by sodium have generally terminated with all of the fuel remaining in the core. When this occurs, the resulting effect is the fusing together of several fuel bundles, which imposes the requirement that facilities be provided for removal of several bundles as an integrated unit. This should be required in all fast reactor designs. For the steam-cooled and gas-cooled reactors, there is possibility of a meltdown in which part of the fuel actually flows through the bottom of the core and drops into the bottom of the reactor vessel; therefore, provisions should be made for quenching the molten fuel and for removal of the heat that would continue to be generated by decay fission products in the fuel. In the low pressure design proposed by B&W, a pool of feedwater is located immediately below the core. Studies should be made of the effects of molten fuel dropping into this pool, and the possible remedies for problems that might develop.

In a reactor with no water present in the bottom of the vessel, some provision should be made for catching, dispensing, and cooling molten fuel that might drop out of the core. None of the designs studied in this evaluation provide for this contingency.

3.9 Conclusions

The intermediate pressure design produced at Karlsruhe appears more conservative than is necessary to prevent center-line melting of the fuel, as contrasted with the two designs by B&W which, because of higher heat rating of the fuel rods, would probably have melting in some parts of the fuel. The conservatism of the lower fuel rating, however, provides a margin of safety in the thermal-hydraulic analysis and, thus, allows the designer more freedom in allocating the flow distribution through the

core and blanket of the reactor. This safety margin also provides some reserve heat capacity against the incident or incidents that might result in a fuel meltdown and, thus, provides more time for operation of the safety systems in preventing the meltdown.

The areas of uncertainty that require further study and developmental effort with appropriate test facilities are: (1) thermal conductivity of the mixed oxides as a function of heat generation, temperature, exposure to reactor operating conditions, and burnup; (2) the effect of center-line melting of the fuel on core physics and fuel element performance; (3) the compatibility of both the fuel and the coolant with the cladding at the temperatures, pressures, and burnup anticipated for these reactors; (4) the kinetics of steam-cooled fast reactors, including startup and shutdown conditions, loss-of-coolant effects, and the effect of changes in load; (5) fuel element and bundle design for optimum performance and reliability at high specific power and for high burnup; (6) control systems capable of meeting the exacting requirements of all possible circumstances from complete flooding to complete voiding; (7) carryover of radioactivity from the reactor to the turbine, which might complicate turbine maintenance; (8) steam generators; (9) steam circulators; (10) vibration problems in all components; (11) containment and control of core meltdown; (12) emergency core cooling; and (13) probability of destructive nuclear excursions, including maximum effects and means for coping with them.

It may also be necessary to develop special emergency cooling systems capable of adequately cooling the compact cores with very short response times. These capabilities should be verified experimentally.

Design and construction of the reactor vessels, piping, valves, and auxiliary systems for the steam-cooled fast breeder reactors are not greatly different from those of water-cooled reactor plants. Circulators may present some unusual problems, although there is some technology available from which to start.

Many of the fuel and physics problems that require study and development for the steam-cooled breeder reactor are common to all fast reactors; some were under investigation as part of the nuclear superheat development program, but the presence of steam in fast reactor cores introduces some

special problems of reactivity control and materials compatibility that differ from those encountered in other fast reactors, or in the development of nuclear superheat for thermal reactors.

The economics of these reactors do not seem to be strongly dependent on the pressure of the steam used for cooling, but the high pressure concept appears to be better economically than the other two, while its conversion ratio is the lowest. It is possible that this result is due to a lack of complete development of the design data for that concept, and the fact that it is not truly comparable with either of the other designs.

Finally, it is concluded that no major technological breakthroughs would be required to solve the problems of making a steam-cooled fast reactor operate successfully and economically. Breeding ratios from 1.0 to 1.4 can be attained, with the actual value dependent on the operating conditions, materials of construction, and core arrangements chosen.

Recent values of the capture-to-fission cross section ratio for ^{239}Pu , if confirmed, would strongly affect the results of this evaluation. It would be necessary for the designers to revise their designs to take the higher values into account, and they would probably revise the core designs to yield different results. New designs and a new evaluation would be required to accommodate the new data.

4. PHYSICS ANALYSES

The system properties used in the physics analyses are summarized in Table 4.1. Some of the listed parameters (such as average fuel temperatures) were not stated explicitly by the designers and, therefore, were estimated on the basis of other data given.

All the systems are fueled with mixed uranium-plutonium oxide pins arranged in hexagonal bundles. The intermediate and low-pressure systems have cylindrical cores that differ in their height-to-diameter ratios. The core of the high pressure system is annular, with an inside radius of 2.36 ft. The intermediate and low pressure systems have blankets in the radial direction and both axial directions, while the high pressure system has inside and outside radial blankets and an axial blanket at the upper end only.

The values stated as average steam densities were used throughout the core or blanket region. This was, of course, an approximation to the continuously varying coolant densities that would actually occur during passage of steam through the system.

4.1 Scope of Physics Analyses

The calculations involved in the physics analyses of these systems are of two basic types. The first is a depletion analysis concerned with production of the fuel requirements of the system for its entire operating history. To obtain the desired information, the system is "operated" analytically for its proposed lifetime. The masses put in and taken out during this "operation" constitute complete fuel requirements for the system.

In the other type of calculation, the instantaneous behavior of the system is analyzed. The following factors are evaluated in these analyses.

4.1.1 Power Distribution

Evaluation of the cooling and the structural integrity of the core must take into consideration local variations. Therefore, information

Table 4.1. System Properties Used in Physics Analyses

	Low-Pressure System	Intermediate-Pressure System	High-Pressure System
Net electric power, Mw	1,012	1,000	980
Thermal power, Mw	2,900	2,519	2,326
Thermal efficiency, %	34.9	39.7	42.2
Core height, ft	2.42	4.95	4.75
Core radius, ft	5.88	4.31	2.36-3.93
Core volume, liters	7,434	8,190	4,160
Axial blanket thickness, ft	1.5	1.18	1.5
Radial blanket thickness, ft	1.2	1.25	1.56/1.42
Core volume fractions			
Fuel	0.54	0.47	0.42
Structure	0.13	0.20	0.18
Coolant	0.33	0.33	0.40
Fuel material	UO ₂ -PuO ₂	UO ₂ -PuO ₂	UO ₂ -PuO ₂
Core fuel density, % of theoretical	85	87	85
Fertile fuel density, % of theoretical	90	87	90
Core fuel cladding material	Inconel 625	Inconel 625	19-9DL stainless steel
Fertile fuel cladding material	Type 348 stainless steel	Inconel 800	19-9DL stainless steel
Core fuel pin outside diameter, in.	0.35	0.276	0.23
Core fuel cladding thickness, in.	0.012	0.015	0.010
Number of fuel pins per core assembly	391	469	504
Average blanket steam density, lb/ft ³	1.92	7.07	11.8
Average core steam density, lb/ft ³	1.92	4.11	6.62
Average core fuel temperature, °K	1644	1673	1693
Average blanket fuel temperature, °K	1089	773	800
Core heavy-metal inventory, kg	33,012	31,800	14,492
Blanket heavy-metal inventory, kg	55,760	52,757	40,800
Fertile pin outside diameter, in.	0.475	0.492	0.455
Fertile pin cladding thickness, in.	0.016	0.022	0.015
Number of pins per fertile assembly	211	169	162
Number of fertile assemblies	126	120	198

is needed concerning the spatial distributions of fissions in the radial and axial directions and over the volume of the core. The data obtained give the power distribution.

4.1.2 Density Coefficients

In order to evaluate the safety requirements of the core, the effects of changes in coolant density on reactivity, power level, and other dynamic properties must be known. To obtain this information the steam density of the reactor is varied while the other parameters (temperature, power level, etc.) are held constant. The effect of this variation on the effective multiplication is then calculated. This information, used alone, describes the effect of coolant loss or flooding and may be used in conjunction with Doppler coefficients to describe the reactivity effects of changes in power level.

4.1.3 Doppler Coefficients

In order to evaluate the stability of the core, its intrinsic tendency to oppose a change in power level must be known. A measure of this tendency is the reactivity effect of a change in fuel temperature. The Doppler coefficient is a quantitative statement of this temperature effect.

4.1.4 Exposure of the Pressure Vessel to Fast Neutrons

Fast neutrons affect the properties of the material of the pressure vessel. The level of exposure to fast neutrons must, therefore, be known so that it can be kept within established limits.

4.2 Analytical Techniques

4.2.1 Cross Sections

The cross section data used were derived from the GAM-II library.¹⁹ These data were supplied by the originators of the GAM-II code. This library is continually updated to keep it in agreement with the latest experimental information. Values for several nuclides were revised in preparation for the steam-cooled fast reactor studies.

The GAM-II code is a consistent B_n code for the calculation of fast neutron spectra and associated multigroup constants. The fine-group information contained in the GAM-II library is averaged over the spectra calculated to obtain a suitable number of broad groups.

For most cases the systems were considered to be homogeneous in the resonance region. However, for systems with high-density coolant and for flooded systems this simplification was not adequate, and a Dancoff factor was derived from tables of reactor physics constants.²⁰

For all flooded systems and for the normal condition in the high pressure system a significant number of neutrons are thermalized and a thermal group is required. For these systems a thermal group was derived by using the code THERMOS.²¹ One thermal group was used to describe the entire energy range below 0.414 ev.

4.2.2 Burnup Calculations

Burnup calculations were performed by using the broad-group cross sections in the ASSAULT code.²² This code consists of a coupling of the eigenvalue calculations from the code EXTERMINATOR²³ with a depletion analysis. EXTERMINATOR is a two-dimensional multigroup diffusion code. It was used to solve the diffusion equations for an eigenvalue and fluxes. The fluxes thus produced were used in the calculation of reactions for a specified time. At the end of this time the eigenvalue problem was repeated and the resulting fluxes were again used in calculation of reaction rates. This procedure was repeated until a maximum time or a minimum k_{eff} was reached. The system was then refueled and the operation repeated. In each of the eigenvalue problems, a search was performed for the poison concentration necessary to reduce k_{eff} to 1.004. This procedure approximated the actual operation of a critical reactor.

In the depletion analysis of the low and intermediate pressure systems, the fission products were described by two pseudo-nuclides: one for uranium fissions and one for plutonium fissions. Each fission was assumed to give rise to two of the appropriate nuclides.

The increased importance of thermal neutrons in the high pressure system, due to the greater steam density, required a more detailed analysis.

For depletion calculations in this system, a thermal group was included, and the explicit fission product treatment recommended by Bennett²⁴ was used.

The decision was made to run all calculations to a maximum exposure of 100,000 Mwd/MT of heavy metal. The fissile concentration or the initial k_{eff} that would result in a given fuel exposure can be found only by a time-consuming iterative process. The maximum exposures were, therefore, not exactly 100,000 Mwd/MT.

4.2.3 Density Coefficients

To obtain the reactivity effect of reduced steam density, the broad-group cross sections were averaged over the spectrum corresponding to the reduced steam density. A k_{eff} was then calculated by using these cross sections and the appropriate atomic densities, and this k_{eff} was compared with the normal condition k_{eff} to obtain the effect of reduced steam density.

The reactivity effect of increased steam density is more difficult to obtain because the fission product treatment used for the normal condition becomes inadequate as the system becomes more thermal. This results from the increased importance of the thermal group, where the effect of the fission products is most important. In the density coefficient analysis of the low and intermediate pressure systems, a point depletion calculation was run by using an explicit fission product treatment.

The fission product concentrations thus obtained for the end of life were used in conjunction with the end-of-life fuel compositions obtained from the ASSAULT analysis. The cross sections were reaveraged for the increased density condition, the eigenvalue was recalculated, and the normal and increased density eigenvalues were compared to obtain the effect of increased steam density on the effective multiplication factor. For the high-pressure system, the fission product concentrations were produced directly by the depletion analysis.

At the time the conditions for flooding calculations were chosen, no information was available as to what flooding accidents might be credible, the relative time constants for flooding and control rod

insertion, or the credibility of partial or complete control rod failure. Therefore, the calculation was done for flooding at room temperature with full density water and the control rods in the operating position. It is recognized that a detailed safety analysis of the reactor might show that this case is not credible and did not need to be considered.

4.2.4 Doppler Coefficient

The reactivity effect of a change in fuel temperature was found in a manner similar to that used to obtain the density effects. The cross sections were reaveraged for changes in fuel temperature, and a k_{eff} was calculated with the new cross sections and compared with the normal conditions. For example, if

D. C. = Doppler coefficient,

k_N = normal condition k_{eff} ,

k_D = k_{eff} corresponding to the nonnormal temperature,

T_N = normal temperature,

T_D = nonnormal temperature,

then

$$D. C. = (k_N - k_D) / (T_N - T_D) .$$

4.2.5 Power Distributions

The power distributions derived for the systems consist of the ratio of point values to the average along a given traverse. For example, if

P. D. = power distribution,

P_i = power at point i ,

V_i = volume associated with point i ,

P_{av} = average power along a given radial or axial traverse,

then

$$P. D. = \frac{P_i}{P_{av}} = \frac{P_i}{\sum_i P_i V_i} \frac{1}{\sum_i V_i} .$$

4.2.6 Pressure-Vessel Exposure

In order to determine the maximum exposure of the pressure vessel, an ASSAULT eigenvalue calculation was run in which the geometric description included all the structure out to the pressure vessel. In order to fit such a calculation into the available machine capacity, some simplifications in the representation of the shield and core were necessary. The exposure of the pressure vessel was found from the point flux values for a 30-year history and a 0.8 plant factor. Neutrons of greater than 1 Mev energy were considered in this calculation.

4.3 Results

4.3.1 Depletion Calculations

The mass balances obtained from the ASSAULT depletion calculations are given in Appendix C. The refueling schemes used in the systems are described in the following paragraphs.

In the high pressure system, the core, the axial blanket, and the portions of the radial blankets nearest the core were refueled at each cycle. The remainder of the radial blanket was in the system for two cycles. The equilibrium condition therefore consisted of two cycles that differed by whether part or all of the radial blanket was refueled.

The core of the intermediate pressure system was scatter-refueled, one-third at a time. Therefore, each core fuel element remained in the system through three cycles. One-third of the blanket was refueled at each alternate cycle, so all portions of the blanket remained in the system through six cycles. The equilibrium data presented for the intermediate pressure system consisted of effective cycles arrived at by forming separate averages over the odd- and even-numbered cycles. At the end of each odd-numbered cycle at equilibrium, one-third of the core was refueled, and at the end of the each even-numbered cycle, one-third of the core and one-third of the blanket were refueled.

The entire low pressure system was refueled after each cycle. Recycle material was delayed for one cycle in all cases before being returned to the system.

The startup fuel used was that available from the operation of PWR systems.²⁵ The plutonium composition used was

^{239}Pu	56.03%
^{240}Pu	23.74
^{241}Pu	14.93
^{242}Pu	5.30%

The mass balances given in Tables C.1 through C.8 of Appendix C were used for initial and equilibrium cycles of all the systems analyzed with PWR plutonium startup fuel. In addition, a depletion analysis was performed for the low pressure system based on startup with ^{235}U fuel. The reactor was shifted to plutonium as plutonium was bred within the system. At startup the ^{235}U -fueled system had a conversion ratio of about 0.9. This conversion ratio increased as the fuel was changed to plutonium. About 17 calendar years was required for the system to become fully plutonium fueled. The use of ^{235}U required a high fissile inventory. The initial operation of the system required 5100 kg of ^{235}U , as compared with about 3300 kg of fissile plutonium. Thus, while startup with ^{235}U would be possible, the penalties on nuclear performance would be quite severe.

A summary of the depletion calculations is presented in Table 4.2. In this summary the start-of-life values are shown for fertile material loadings, while the fissile loadings shown are the average over a cycle. The total fissile inventory shown includes an out-of-core inventory based on an assumed one-year delay time. This total was used as the total committed to a given reactor for calculating the fraction of annual increase for a reactor operating at a 0.8 plant factor. The doubling times shown were calculated from

$$\text{Doubling time} = \frac{0.693}{\text{Fraction of annual increase}} .$$

The breeding ratios represent fertile absorptions divided by fissile plutonium absorptions. The values quoted are averages over the equilibrium cycle.

The calculated breeding ratios do not disagree seriously with those calculated by the system proponents. The disagreements are summarized in Table 4.3.

Neutron balances for the three systems are given in Tables C.7 through C.9 of Appendix C, in which the results are normalized to one neutron lost.

Table 4.2. Summary of Depletion Calculations for Equilibrium Cycle

	Low Pressure System	Intermediate Pressure System	High Pressure System
Core heavy metal inventory, kg	33,012	31,800	14,492
Blanket heavy metal inventory, kg	55,760	52,757	40,800
Average core enrichment, %	10.76	11.1	13.66
Average core fissile inventory, kg	3,554	3,530	1,980
Average blanket fissile inventory, kg	529	307	346
Out-of-core inventory, kg	1,354	1,320	1,472
Total fissile inventory, kg	5,437	5,157	3,798
Fissile specific power, kw(th)/kg total	533	488	612
Median fission energy, kev	160	120	30
Total breeding ratio	1.38	1.14	1.11
Core conversion ratio	0.98	1.01	0.63
Fuel yield, % per year	5.9	1.84	1.70
Doubling time, years	12	38	41
Net gain in fissile Pu, kg/year	318	95	65
Fuel life at 0.8 plant factor, years	3.02	2.65	1.58
Average exposures, Mwd/MT			
Core	70,000	57,500	58,000
Axial blanket	5,800	4,100	5,400
Radial blanket	2,000	4,000	
Inner			9,200
Outer			5,900
Average power fractions			
Core	0.90	0.93	0.81
Axial blanket	0.08	0.03	0.02
Radial blanket	0.02	0.04	0.17

Table 4.3. Summary of Some Differences in ORNL
and Designers' Calculations

	Difference* in Maximum of Ratio of Peak to Average Power Density (%)		Difference* in Breeding Ratio (%)
	Radial	Axial	
Low pressure system	+5.4	-1.7	-6.8
Intermediate pressure system	+8.0	+0.7	-2.7
High pressure system	-1.8	-4.1	-3.0

$$*\text{Difference } (\%) = \frac{(\text{ORNL Value} - \text{Proponent's Value}) \times 100}{\text{ORNL Value}} .$$

Recently reported measurements²⁶ of the capture-to-fission ratio for ^{239}Pu in the energy range from about 100 ev to 30 kev indicate that $\alpha(E)$ for ^{239}Pu may be higher than previously assumed in this energy range. These recent Harwell experiments have not been thoroughly analyzed, and the preliminary nature of the data does not allow determination of reliable values for $\alpha(E)$. However, the most probable values of these data seem to be systematically higher than the $\alpha(E)$ data recommended for the ENDF/B files.²⁷

Although it was not possible to obtain revised values of $\alpha(E)$ for inclusion in the present evaluations, it was estimated that an increase in the capture-to-fission ratio for ^{239}Pu would result in a decrease in breeding ratio and in an increase in doubling time for these systems. Further consideration of steam-cooled breeder reactors should include an accurate determination of this effect.

4.3.2 Density Coefficients

Table 4.4 summarizes the reactivity effects of changes in steam density for these systems. The calculated behaviors of the low and intermediate pressure systems under flooding and voiding conditions are similar in that reactivity changes are positive in those systems for either

Table 4.4. Reactivity Effects of Various Steam Conditions

Ratio of Nonnormal Steam Density to Normal Steam Density	Reactivity Effect ^a		
	Low Pressure System	Intermediate Pressure System	High Pressure System
Beginning of Startup Cycle			
0	1.00943	1.02095	1.00503
0.05		1.01370	
0.5			0.99536
0.90		1.00223	
1.0	1.00000	1.00000	1.00000
2.0	1.00031		
Flooded	1.16637		
End of Equilibrium Cycle			
0	1.00734	1.01880	0.96156
0.5	1.00172	1.00229	0.97954
0.95			0.99815
1.0	1.00000	1.00000 ^b	1.00000
2.0	1.00097		1.02700
4.0	1.00728		
5.0			1.04670
7.0	1.01993	1.00754	
10.0	1.03027		
15.0	1.04208		
20.0	1.06152		
Flooded	1.06913	1.01145	1.04381

^aThe quantity tabulated is the ratio of k_{eff} at nonnormal steam density to k_{eff} at normal steam density.

^bThis number was calculated by using six fast groups. When a thermal group was included 1.0001 was calculated.

flooding or voiding. The reactivity change in the high pressure system may be either positive or negative under reduced steam density, depending on the amount of density change and the time at which the change takes place. The results quoted in Table 4.4 are based on the changes in coolant density taking place without the control rods being moved from the operating positions. If the control rods were inserted during the change, the results would, of course, be quite different.

Although no detailed analysis of the control system was performed, it appears that the increase in effective multiplication factor under flooding in the low pressure reactor would be significantly greater than the worth of the control. If flooding is a credible situation in this system, revisions in the controls will be essential. (The analysis of changes in steam density in the low pressure system was performed by The Nuclear Technology Corporation.)

4.3.3 Doppler Coefficients

Results of the Doppler coefficient studies are summarized in Table 4.5. The agreement between the evaluation and the proponents' results for Doppler coefficients seems quite good. All the evaluation results were obtained by reducing the average fuel temperature from the normal condition for beginning-of-life fuel. This reduction was necessary because of difficulties with the cross section technique in the neighborhood of 2000°K. A reasonable increase in temperature from the normal conditions would result in temperatures in this vicinity. (The Doppler coefficients for the low pressure system were evaluated by The Nuclear Technology Corporation.)

For a 10% change in fuel temperature accompanied by a 10% change in coolant temperature, the Doppler effect was found to be larger than the density effect in the low pressure system and, thus, the power coefficient was negative. Since fuel temperatures can change more rapidly

Table 4.5. Doppler Coefficients

	Doppler Coefficient ($\Delta k/^\circ K$)	
	Proponents' Reported Results	Evaluation Results
Low pressure system		
Normal		-8.6×10^{-6}
Core void		-7.6×10^{-6}
Intermediate pressure system	-1.75×10^{-5}	-1.6×10^{-5}
High pressure system	-9.0×10^{-6}	-7.2×10^{-6}

than coolant temperatures, this is believed to be a conservative estimate of the effect of small changes in density or temperature.

4.3.4 Power Distributions

The results of the power distribution calculations are given in Figs. 4.1 through 4.8. The discontinuities shown in the distributions occur at the interface between zones of different fissile content. The zone loadings used in these calculations are summarized in Table 4.6. The results for the axial traverse in the high pressure system are shown in Figs. 4.1

Fig. 4.1. Axial Power Peaking Factors at Beginning of Life of High Pressure System.

Fig. 4.2. Axial Power Peaking Factors at End of Life of High Pressure System.

Fig. 4.3. Radial Power Peaking Factors at Beginning of Life of High Pressure System.

Fig. 4.4. Radial Power Peaking Factors at End of Life of High Pressure System.

Fig. 4.5. Axial Power Distribution of Intermediate Pressure System.

Fig. 4.6. Radial Power Distribution of Intermediate Pressure System.

Fig. 4.7. Radial Power Distribution of Low Pressure System.

Table 4.6. Zone Loadings of the Reactors

	Radial				Axial*			
	Number of Active Zones	Zone Number	Fraction of Core Volume	Fraction of Fissile Material	Number of Active Zones	Zone Number	Fraction of Core Volume	Fraction of Fissile Material
Low pressure system	3	1	0.3358	0.3001	1	1	1.0	1.0
		2	0.2878	0.2833				
		3	0.3764	0.4166				
Intermediate pressure system	2	1	0.4860	0.4474	1	1	1.0	1.0
		2	0.5140	0.5526				
High pressure system	1	1	1.0	1.0	4	1	0.25	0.3000
						2	0.25	0.2600
						3	0.25	0.2400
						4	0.25	0.2000

*Zones are numbered top to bottom over the entire core.

Fig. 4.8. Axial Power Distribution of Low Pressure System.

and 4.2 and compared with the corresponding calculations by B&W. This comparison shows acceptable agreement. The chief characteristic of the axial power distribution is the reflection of the four enrichment zones. This zoning arrangement was provided to place the power peak near the low temperature steam inlet. Figures 4.3 and 4.4 show radial power distributions for the high pressure system. B&W did not report results that can be compared directly with these. They did, however, give the ratio of the power in each element to the average element power. The maximum they obtained for this number was about 1.12, which compares quite well with the maximums of the curves shown in Figs. 4.3 and 4.4.

Figures 4.5 and 4.6 show radial and axial power distributions for the intermediate pressure system and a comparison with values calculated by the proponent of the design. While the axial results are in good agreement, the radial results are not. The ORNL results show a significantly less favorable power distribution. While no explanation is known for the disagreement, power distributions are very sensitive to relative enrichments of the core zones, and small adjustments in enrichment could produce large improvements in power distribution. Only beginning-of-life results are shown for this system because the power distribution changes very little over the short cycle used.

Figures 4.7 and 4.8 show radial and axial power distributions for the low pressure system. The chief characteristic of this curve is the large change from a minimum to a maximum that occurs in the central core region as a function of burnup. This behavior agrees qualitatively with that forecast by B&W. However, the maximum value for the curve is higher than the B&W forecast by about 1.4 to 1.48. The earlier remarks about the sensitivity of power distributions to zone enrichments apply to the present three radial zone core.

The most important disagreements are summarized in Table 4.3. For the intermediate and low pressure systems, the power distributions calculated by ORNL result in less conservative hot channel conditions. The power distributions calculated by ORNL could, however, be improved by adjustments in the relative enrichments in the core zones. In cases where the maximum in the power distribution moves radially during the cycle, it would probably not be possible to lower both beginning- and end-of-life values. The effect on the radial power distribution would have to be carefully evaluated as a function of time.

4.3.5 Maximum Pressure-Vessel Exposures

The maximum exposures of the pressure vessels to neutrons of greater than 1-Mev energy are listed below:

	Maximum Exposure (neutrons/cm ²)
High pressure system	1.4×10^{20}
Intermediate pressure system	2.45×10^{18}
Low pressure system	2.6×10^{20}

The value for the high pressure design compares quite well with the value of about 1×10^{20} neutrons/cm² quoted by B&W. This value is, however, higher than usually allowed for such systems. The value for the intermediate pressure system shows a conservative design in this respect. The low pressure system, like the high pressure system, has a value higher than is usually allowed for such exposure. No proponent value is available for comparison at the present time. (The maximum tolerable fast neutron exposure is discussed further in Chapter 6.)

4.4 Summary and Conclusions

The breeding ratios calculated for the steam-cooled fast breeders are about 1.38 for the low pressure system, 1.14 for the intermediate pressure system, and 1.11 for the high pressure system. The disagreements between these values and those calculated by the system proponents are not large relative to the uncertainties involved in calculating this quantity.

The doubling times calculated are 12 years for the low pressure system, 38 years for the intermediate pressure system, and 41 years for the high pressure system.

Effective multiplication factors were calculated for conditions ranging from complete loss of coolant to complete flooding with room temperature water. Loss of coolant at beginning of life results in increases in effective multiplication factor of 0.009, 0.021, and 0.005 for the low, intermediate, and high pressure systems, respectively. Loss of coolant at end of life results in changes very similar to those at start of life, except in the high pressure system, where a decreased density results in decreased reactivity. Flooding at end of life results in an increase of 0.069 for the low pressure system, 0.011 for the intermediate pressure system, and 0.04 for the high pressure system. The maximum effect of flooding occurs at beginning of life in the low pressure system. The change in effective multiplication factor is 0.166. If flooding is determined to be a credible situation, the reactivity worth of the control system will have to be increased to insure the control of this condition in the low pressure system.

The power distributions obtained by ORNL are slightly less favorable than those presented by the proponent of the intermediate and low pressure systems. These distributions are very sensitive to small changes in relative enrichments of fuel zones. The distributions quoted by ORNL could, however, be improved by small adjustments in these relative enrichments. Since the improvement might be possible only at one time during a cycle, the power density would have to be evaluated carefully as a function of time.

5. ENGINEERING EVALUATION OF CORE

5.1 Thermal and Hydraulic Analyses

Analyses of the flow, pressure, and temperature distributions in the three proposed designs for the steam-cooled fast breeder reactor were made. The pertinent values used in these analyses, as given by the proponents of the three concepts, are summarized in Table 5.1.

A computer program,²⁸ called HYDRATHERM I, was written for analyzing the high pressure design; the code made stepwise calculations of the temperatures and pressures developed in the hot bundle over the axial length of the core. For each increment of length, the code computed the pressure loss and the head added to the coolant. The new coolant temperature was determined from the enthalpy change and the pressure change. The code then calculated the inner and outer cladding surface temperatures and the center-line temperatures of the fuel. Two modifications of this code, HYDRATHERM II and III, were used to perform similar calculations for the intermediate and low pressure designs.

Engineering factors were used to predict the hot channel conditions from the nominal hot bundle conditions. A significant component of these factors for the walled tube bundle proposed by B&W was a redistribution effect caused by the frictional drag of the walls. A modification of the dimensions given in Fig. 5.1 that was proposed for the low pressure system eliminated the undesirable characteristics of this effect. A similar modification would produce a reduction in the redistribution factor in the high pressure system; however, the resultant reduction in maximum cladding temperature would not be as large due to the relatively larger heat transfer coefficient that already exists for this design.

Based on these calculations, the cladding temperatures for both the high and low pressure systems exceed the specified maximum value of 1300°F at 100% power with the inclusion of engineering factors; however, the calculated maximum cladding temperatures are within the limitation of 1350°F that was allowed in this study. The maximum cladding temperature calculated for the intermediate pressure system on a similar basis is 1140°F.

Fig. 5.1. Spacer and Rod Arrangement for Low Pressure System.

Table 5.1. Thermal-Hydraulic Values as Given by Designers

	Low-Pressure System ^a	Intermediate- Pressure System	High-Pressure System
Net power to coolant, Mw(th)	2900	2519	2326
Total coolant flow, lb/hr	36.9×10^6	25.15×10^6	25.513×10^6
Coolant inlet temperature, °F	575	685	750
Coolant outlet temperature, ^b °F	980/895	1004	1000/950
Core average coolant velocity, fps	147.9		
Velocity at midplane, ^b fps			82/76
Velocity in hot bundle, fps		99 to 158	
Pressure drop over core, psi	96	116.4	255
Inlet pressure (active lattice), psi	1250	2680	3700
Number of fuel assemblies	271	181	108
Number of rods per bundle (normal)	391	469	504
Total active fuel length, in.	29	59.4	57
Total rod length, in.	88.15	96.84	88.16
Maximum cladding surface temperature, °F	1300	1389	1335
Maximum center-line temperature, °F	4440	3938	4900
Maximum linear heat rating, kw/ft	18	9.94	16.39

^aEnd-of-life conditions.

^bValues given for two regions of core.

The maximum center-line fuel temperatures at 100% power, with the inclusion of engineering factors and with the most probable value that can be estimated at present for the thermal conductivity of the vibration compacted mixed-oxide fuel, were found to be above the melting point of this fuel for both the high and low pressure systems. Present experimental evidence indicates, however, that it would be feasible to operate these reactors with center-line melting of the proposed fuel over a limited fraction of the core. For the intermediate pressure design, the calculated center-line temperatures are well below the melting point of the mixed oxides.

The center-line temperatures were based on an estimate of the thermal conductivity of the fuel that allows for partial sintering of all fuel above 1300°C, and complete sintering of all fuel above 1500°C. This estimated thermal conductivity, which was expressed as a function of fuel temperature, was lower than the value used by B&W. Experimental data for this physical property are rather limited, and it is possible that further experiments will indicate a larger value for it. On the other hand, there is an indication that irradiation and consequent deposition of fission

products in the crystal lattice may tend to lower this physical property; however, this has not been well substantiated.²⁹ In any event, there is the possibility of the use of sintered oxide pellets, which would operate at the design heat ratings without center melting; however, this modification might require redesign of the fuel pins to eliminate as many of the changes in pin diameter as possible, and a further evaluation would have to be made of cladding strain due to fuel swelling.

From the lower cladding and fuel temperatures, it is obvious that the intermediate pressure design is based on a more conservative estimation of fuel performance characteristics. This fact is reflected in the lower nominal heat rating of this design, which is about 9.6 kw/ft compared with maximum nominal values of 15 to 16 kw/ft for the other two designs.

5.1.1 Calculational Methods

It was necessary to follow the changes in coolant temperature and pressure through the core by using a stepwise computer calculation because of the large change in steam properties over the length of the flow channel and the nonideal behavior of steam (particularly near the saturation conditions). The FORTRAN/360 (locally written) compiler was used to write a code, called HYDRATHERM I, for the IBM 360 computer to make the calculations for the high pressure design. The difference in property values of the steam and the different geometrical configurations of the flow channel necessitated two major modifications of this code for the intermediate and low pressure designs. These modifications were called HYDRATHERM II and III, respectively. Detailed descriptions of these codes, together with listings of each and the detailed output, are given in Ref. 29, and the input data for these calculations are summarized in Appendix D. All the calculations were performed for the nominal channel in the hot bundle, and it was necessary to analyze the hot channel in the hot bundle through the use of engineering factors that related the conditions in these two channels. The calculations that provided the basis for these factors were performed with a program written for the CEIR remote computing facility.

Tabulated values for the enthalpy and specific volume of steam were obtained from Keenan and Keyes' steam tables³⁰ and entered into the computer calculations. Similar values from the British NEL Steam Tables - 1964 (Ref. 31) were also used in the calculations to check the enthalpy balances. In general it was found that enthalpy balances based on these two sets of steam tables differed about 3% over the range of temperatures and pressures found in the SCFBR designs.

In both the high and low pressure designs, the fuel-rod bundles were housed in walls that were formed from pentagonal tubes, with three sides in the form of a rectangle and the other two sides as two sides of a regular hexagon. A corner of this hexagonal walled tube bundle for the low pressure design is shown in Fig. 5.1. In the cooling of such a bundle, three types of flow channel were considered: (1) the pentagonal flow channel around the fuel rod in the wall tube; (2) the hexagonal flow channel around a rod in the interior of the bundle, whose only flow restrictions are the frictional drag of the surface of the rod and the form loss caused by the intermittent spacers; and (3) the hexagonal flow channel around the rods, which are in the bundle and adjacent to the hexagonal sides of the wall tubes. These latter flow channels have continuous walls on two of the six sides of the hexagonal channel. These walls produce additional frictional drag, which restricts the coolant flow and causes it to redistribute its flow within the bundle to the interior flow channels. There is an additional hexagonal flow channel like the last type in each of the six corners of the bundle that has three continuous walls rather than two. This imposes a slightly different problem, but since there are only six of these channels, the solution can be determined by altering the fuel rod diameter or the fuel enrichment.

The problem of flow redistribution in the low pressure design was avoided by altering the dimensions of the hexagons adjacent to the wall tubes to provide more coolant flow area between the rods in these channels and the tube walls. This same adjustment could have been made for the high pressure design, but the net reduction in cladding temperature would have been less than for the low pressure design.

The effect of this flow redistribution was determined from a calculation made with the local remote computing facility. The flow to the

hot bundle was specified, and the hydraulic characteristics of the various channels were entered. The computer code then iterated to determine the relative flow distribution among the channels. The ratio of relative flow for the nominal channel and the channel adjacent to the wall tube was used as a redistribution component of the engineering factors.

Since the intermediate pressure design has flat bundle walls, the peripheral rods have a slightly larger coolant flow channel at the bundle wall, and redistribution does not present a problem.

5.1.2 Correlations Used in the Calculations

The heat transfer characteristics of supercritical water were developed by Westinghouse³² under AEC contract. The correlation for the heat transfer coefficient was obtained from that study, which related the Nusselt number to the Reynolds number to the 0.9 power and the Prandtl number to the 0.66 power (see Appendix D). For these dimensionless groups, all physical properties were evaluated at the bulk coolant temperature. A correction term that consisted of the ratio of the density of the coolant at the wall temperature to the density at the bulk conditions was used to correct for the varying steam properties over the film; this ratio was raised to the 0.43 power. A term involving the ratio of the heated length to the equivalent flow channel diameter was omitted in the calculation of the coefficients for the high pressure design.

In selecting the heat transfer correlation for the intermediate and low pressure designs, a study was made of presently available data, which included two rather recent correlations presented by Sutherland³³ and Heineman.³⁴ After a review of the experimental conditions involved in the investigations, the Heineman correlation was selected as the one that best represented the conditions that occurred at the midplane of the reactor core where the maximum cladding and fuel temperatures occurred. Heineman presented two correlations, one for conditions near an abrupt entrance and one for fully developed flow. The correlation for fully developed flow was selected as the basis for the calculations, since the maximum temperatures found in the SCFBR's were far removed from the entrance and the spacers did not appear to be effective turbulence promoters over the distance between spacers. This latter conclusion was based on experimental

studies performed at ORNL to determine the effectiveness of the spacers in the EGCR in promoting downstream heat transfer enhancement.

The form of the Heineman equation used presented the Nusselt number as a function of the Reynolds number to the 0.84 power and the Prandtl number to the 1/3 power. All physical properties were evaluated at the theoretical film temperature.

The friction factor correlation for the high pressure design was obtained from a study by Pratt and Whitney.³⁵ The friction factor was correlated graphically as $f(D_e G)^{0.3}$ as a function of mean fluid temperature, where D_e is the equivalent hydraulic diameter and G is the mass velocity in lb/hr.ft². For use in the ORNL calculations, the graphical function was fitted to the following third-degree polynomial:

$$f(D_e G)^{0.3} = 0.10031 + 3.96796 \times 10^{-7} T + 8.33338 \times 10^{-9} T^2 - 2.77778 \times 10^{-11} T^3 ,$$

where T is in °F and the standard deviation for the fit was 7.18×10^{-3} .

The friction-factor correlation for the intermediate and low pressure designs was obtained from an article by Waggener.³⁶ The equation was

$$f = 0.0055 \left[1 + \left(20,000 \frac{e}{D_e} + \frac{10^6}{Re} \right)^{1/3} \right] ,$$

where e was the surface roughness (assumed to be 6.25×10^{-6} ft) and Re represents the Reynolds number. This expression was established by Moody in 1947 as an approximation to an equation derived by Colebrook in 1939. The above expression agrees with the well-known Moody diagram within ±5% for values of Re from 4×10^3 to 10^7 and for values of f up to 0.05.

To estimate the pressure losses due to changes in flow area at the entrance, exit, and spacers, the equations used were those recommended by Kays.³⁷ These equations consist of a coefficient, either expansion or contraction, multiplied by a velocity head based on the velocity in the more restricted flow area. The expansion and contraction coefficients were determined from graphical representations in Kays' article.

For the high and low pressure designs, the form loss due to the honeycomb spacers was evaluated as recommended by de Strodeur.³⁸ This correlation is based on a drag coefficient, which is presented graphically

in the paper,³⁸ plus the velocity under the spacer. When this correlation was used, no additional friction drag was allowed for higher velocity under the spacer.

5.1.1.3 Physical Property Data

The viscosity and thermal conductivity for both steam and supercritical water were obtained from a publication by Fritsch and Grosh.³⁹ Both these properties were correlated by giving values as a function of temperature at 1.0-atm pressure, and then giving a correction term for the pressure effect as a function of density.

The enthalpy values used were obtained from the Keenan and Keyes' steam tables,³⁰ as previously noted. The NEL steam tables³¹ were considered to represent the most accurate data presently available; however, the variances between enthalpy values based on the two tables were, at most, 3% over the range of conditions encountered in the SCFBR designs. On the other hand, the heat transfer correlations cited were based on the Keenan and Keyes' tables and, therefore, it was necessary to use these tables in applying the correlations.

The specific heat at a given temperature and pressure was determined as the slope of the line relating enthalpy to temperature. This procedure was used for all three designs.

A rather intensive review⁴⁰ was made of all information concerning the thermal conductivity of the vibration compacted mixed uranium and plutonium oxides that existed at the time of this study. As a result, it was possible to develop an expression that presented the thermal conductivity as a function of temperature, and could be analytically integrated between the limits of the surface temperature and the center-line temperature (see Appendix D). This development allowed for the self-sintering of the oxides, which is complete at temperatures above 1800°C and is directly proportional to temperature between the limits of 1400 and 1800°C. Below 1400°C, a constant thermal conductivity of 0.0125 w/cm·°C (0.723 Btu/hr.ft.°F) was used; this value represents present experimental data for similar vibration compacted beds with backfill pressures that are at most 2 atm. (A detailed discussion of the relation of linear heat rating to surface temperature is given in Appendix D.)

Use of the analytical expression given above for the integral of $k(T) dT$ permitted the calculation of fuel temperatures above the melting point, based on the design heat ratings and calculated coolant, cladding, and fuel surface temperatures. Such values are fictitious, since the thermal conductivity of the solid has no meaning at temperatures above the melting point. They have been reported, however, when they occurred, and since they represent an extrapolation of the integral, the value of the fictitious temperature that is reported represents essentially the degree to which melting has been surpassed.

Combination of these considerations resulted in the following relationship for the integral of $k(T) dT$ from the surface temperature of T_s to the center-line temperature of T_m ; the relationship is

$$\int_{T_s}^{T_m} k(T) dT = 27.28 - 0.0125 T_s + 32.62 \ln \frac{T_m + 129.4}{2202.4} + 1.0213 \times 10^{-13} T_m^4 ,$$

where the temperatures are in °K. The development of this equation is given in Appendix D.

In performing these calculations, it was necessary to estimate a contact coefficient between the outer fuel surface and the interior of the cladding. A value of 2000 Btu/hr·ft²·°F was used by Lyons and his associates⁴¹ in the reduction of data to obtain the correlation used for the thermal conductivity of the sintered oxide as a function of temperature. There appeared to be no reason to expect the contact coefficient to be different for the vibration compacted powder; in fact, there appeared to be some indication to the contrary from a study of the data. Therefore this value was used in the calculations.

5.1.4 Power and Flow Distributions

The total reactor power and total reactor flow were taken directly from design specifications. The power distribution between the core and the various blankets and the variation of the power distribution with core lifetime were obtained from physics calculations performed at ORNL.

The axial power distributions were also obtained from the physics calculations performed at ORNL. However, it was found that for the high and intermediate pressure designs, the basic radial power distributions calculated at ORNL were significantly different from the values given by the sponsor. In these two cases, the radial power distribution given by the sponsor could be obtained by relatively minor adjustments in fuel enrichments, or by including additional radial zones of enrichment in the case of the intermediate pressure design. Therefore, the basic radial factors given by the sponsors were used in the calculations.

For the low pressure design, relatively minor differences exist between the basic peaking factors produced by B&W and ORNL physics calculations. These factors cannot be compared directly at end of life because the ORNL calculations were based on a maximum exposure of 100,000 Mwd/T, while those of B&W were based on an average exposure of 100,000 Mwd/T. According to the ORNL calculations, the maximum power density in the core and, therefore, the most severe thermal conditions occur at end of life, while the B&W values indicate that the most severe conditions occur at beginning of life. The most influential factor is the basic radial peaking factor, which is given by ORNL at end of life as 1.48, and by B&W as 1.408. While the factor of 1.48 can easily be changed to 1.408 by relatively minor changes in enrichment of the core, any decrease in the end-of-life radial peaking factor must be accompanied by an increase (which is not presently determined) in the radial factor at beginning of life. Since these beginning-of-life conditions also appear to be limiting, arbitrary adjustment of the end-of-life factor did not appear to be reasonable without further investigation. Since the end-of-life calculations presented by B&W were for a different exposure than was used in the remainder of the evaluation, and since the B&W results were not completely presented, the ORNL distribution factors were used at end of life.

The major significance of the basic radial flux ratio is that if the coolant flow is orificed to this pattern, the development of the coolant temperature will be the same in each bundle, and will thereby give the same outlet temperature from each bundle. In this manner, higher temperature steam will not be degraded by mixing with cooler steam. The basic

radial flux ratio will also indicate the maximum flux encountered in the hot bundle.

It appears that orificing to the basic radial flux pattern is not desirable because of the nature of the steam coolant. The high pressure design actually proposes two effluent streams at different temperatures. In this case, the calculations treated the reactor as two cores with the specified flow and power for each. For the intermediate pressure design, the assumption of orificing to the basic radial flux pattern was used.

For the low pressure design, it was found that the basic radial maximum-to-average power ratio at the beginning of life is 1.2, and at the end of life it is 1.48, with the peak shifting from the inner radius of the outer enrichment zone to the center of the central zone. This necessitates shifting coolant flow over the lifetime of the core. It was proposed by B&W that variable orifices be installed in the top of the bundles, and that these orifices be adjusted during the annual downtime for maintenance. For the periods between reorificing, the flow would be adjusted to meet the worst conditions encountered in any bundle.

In addition, the coolant flow specified for the low pressure design is to be divided by a baffle so that cooler steam is recirculated, and hotter steam is withdrawn to the turbine. Since the computer code did not allow the treatment of this variable, two calculations were made: one with the flow orificed to the basic radial flux pattern, and one with the stipulated flow of 159,700 lb/hr to the nominal hot bundle.

5.1.5 Engineering Factors

Only those engineering factors that would affect the operation of the reactor were considered. Uncertainties in the prediction of the power distribution, physical property values, and heat transfer correlations were not included, since the designs did not represent first-of-a-kind plants. In addition, the contributions of each of these components to the coolant enthalpy rise, the temperature difference between the bulk coolant and the cladding surface, and the heat flux were estimated. Since the most probable values were selected and their effects were essentially statistically independent (or at least their simultaneous occurrence was

not precluded), they were combined multiplicatively rather than statistically.

The engineering factors for the three designs are given in Tables 5.2, 5.3, and 5.4. Flow maldistribution represents the inability to orifice the coolant flow to the basic radial flux pattern. It is estimated to be 5%, and its effect on the enthalpy rise of the coolant is a factor of 1.05. Its contribution to the temperature difference across the coolant heat transfer film is a factor of 1.05 to the 0.8 power, or about 1.04. It does not affect the heat flux.

Variations in mechanical tolerances and in enrichment contribute a 10% uncertainty to the calculation of the thermal flux and of the film temperature difference. The variation may not exist over the entire

Table 5.2. Engineering Factors for the Low Pressure Design

Component	Engineering Factor		
	Coolant Enthalpy Rise	Temperature Drop	Heat Flux
Flow maldistribution (5%)	1.05	1.04	1.00
Tolerances (mechanical, enrichment, and others, 10%)	1.05	1.10	1.10
Flow redistribution	0.93	1.10	1.00
Total (product)	1.03	1.26	1.10

Table 5.3. Engineering Factors for the Intermediate Pressure Design

Factor	Total (product)
Coolant enthalpy rise	1.24
Temperature drop	1.20
Heat flux	1.10

Table 5.4. Engineering Factors for the High Pressure Design

Component	Engineering Factor		
	Coolant Enthalpy Rise	Temperature Drop	Heat Flux
Flow maldistribution (5%)	1.05	1.04	1.00
Tolerances (mechanical, enrichment, and others, 10%)	1.05	1.10	1.10
Flow redistribution	1.12	1.30	1.00
Total (product)	1.23	1.48	1.10

length of the subchannel, and intersubchannel mixing would smear the effect on enthalpy rise, so the effect on the coolant enthalpy rise was reduced to 5%.

The engineering factors for the high and low pressure designs also include an effect of redistribution of flow in the bundle due to the drag effect of the bundle walls. This drag effect results in flow reduction in the coolant subchannel between the wall tubes (which constitute the bundle wall) and the rod that is a neighbor to the bundle wall. This factor does not apply to the rod in the wall tube, since that is a closed channel and its geometry can be designed to provide adequate cooling; it is not subject to flow redistribution. B&W adjusted the diameter of this pin to provide adequate coolant flow.

The flow redistribution within the hot bundle for the intermediate pressure design was not included because the abnormal channels had better cooling characteristics than the nominal channels. However, for the high and low pressure designs, the flow redistribution in the bundles was estimated. These redistribution factors were included in the engineering hot channel factors. For the high pressure design, it was indicated by the program that the difference in flow between the subchannel at the bundle wall and the flow in the less restricted channels in the middle of the bundle is about 36%.

The flow redistribution has a direct effect on the coolant enthalpy rise. This effect will, however, be moderated by the mixing of coolant between adjacent subchannels. The effect of mixing was estimated for the intermediate pressure design to be 33%. For the high pressure design the effect should be about this value or less because of the difference in rod support. Without further analysis, 33% mixing was assumed and the coolant enthalpy rise contribution was taken as one-third of 36%, or 12%. The effect on the film temperature drop should be 1.36 to the 0.8 power, or about 1.30.

For the low pressure design, the wall tube dimensions were reduced so that the hexagonal cell adjacent to the wall tube was elongated. In this manner the mass coolant flow past the rod adjacent to the bundle wall was increased, although the coolant velocity was still reduced by the drag effect of the wall. For this design, the calculation indicated a decrease in velocity past this rod of about 17% from the velocity in the nominal channel, but the total mass coolant flow was increased about 13%. In this case, intersubchannel mixing is detrimental to the development of the hot spot temperature, since the coolant temperature development around the hot rod is less than in the nominal channel. From these factors, a redistribution component for the low pressure design was estimated to be 0.935 on the coolant enthalpy rise and $(1.19)^{0.8}$, or 1.136, on the temperature difference between the cladding surface and the bulk coolant.

An additional factor was included to account for the fact that the equivalent hydraulic diameter of the hot subchannel was not the same as that used as the equivalent hydraulic diameter for the bundle in performing the heat transfer calculations. Since the coefficient is inversely proportional to the diameter to the 0.2 power, the coefficient was increased by the ratio $(0.1321/0.1130)^{0.2}$, or 1.031. The redistribution factor is, thus, $1.136/1.031$, or 1.10. The remaining components were the same as those for the high pressure design.

The total engineering factors for designs are obtained by direct multiplication of the component factors. In the evaluation of the factors for the intermediate pressure design, values were obtained from the design report that corresponded to the components listed above. Again, uncertainty predictions of properties and coefficients were omitted, and the

factors were combined directly rather than statistically. These factors did, however, contain the "burnup" factors that result from the partial refueling. These factors indicated the relative "overpower" produced when a new bundle replaces an old bundle in a partially burned lattice. The total factors for the designs are given in Tables 5.2, 5.3, and 5.4.

5.1.6 Results of the Calculations

The results of the calculations of temperatures and pressure losses for the three concepts are presented in Table 5.5. Values are presented at 100% power with and without the inclusion of engineering factors, and for operation at 110% power with the inclusion of engineering factors.

5.1.6.1 Results of the Hydraulic Analysis. The pressure drop over the core, as given in Table 5.5, includes the friction losses in the upper and lower axial blankets and the core, the form losses due to the spacers in the high and low pressure designs, and an allowance for entrance and exit losses to the bundles of 0.5 and 1.0 velocity head, respectively.

The pressure loss over the core, as specified by B&W for the high pressure design, was 250 psi at 105% flow, and the calculated value was 224 psi at nominal flow. If the B&W value is proportioned by the square of the flow to estimate the value at 100% flow, it is 226 psi, which is in very good agreement with the ORNL calculated value.

For the intermediate pressure design, the value given for the core pressure drop by Karlsruhe was 116.4 psi for the same components as those included in the ORNL calculation. Nuclear Technology Corporation also reviewed this reactor design and indicated a pressure drop of 162.1 psi. The major difference between the Nuclear Technology Corporation calculations and the Karlsruhe calculations was in the estimation of the pressure loss over the lower axial blanket. The ORNL calculation of flow areas and hydraulic diameter for this design agreed more closely with the sponsor's values; ORNL calculated a pressure loss of 100 psi.

For the low pressure design, the pressure drop over the core was originally given by B&W as 103 psi, but their final value was in the range 86 to 96 psi from beginning-of-life to end-of-life conditions. The total coolant flows were given as 38.73×10^6 and 36.9×10^6 lb/hr, respectively, for the two times, and the corresponding flows through the reactor core

Table 5.5. Results of the Thermal-Hydraulic Analyses

	Low Pressure System			Intermediate Pressure System			High Pressure System		
Power rating, %	100	100	110	100	100	110	100	100	110
Engineering factors included in calculations	No	Yes	Yes	No	Yes	Yes	No	Yes	Yes
Maximum center-line temperature, °F	(a)	(a)	(a)	3827	4200	4562	(a)	(a)	(a)
Maximum surface temperature, °F	1237 ^b	1332 ^b	1407 ^b	1019	1140	1210	1116	1351	1440
Outlet coolant temperature, °F	1004 ^b			968			1010		
Pressure drop over core, psi	90 ^b			100			224		

^aTemperature above the melting point of the mixed oxides.

^bValues are calculated for flow in the hot bundle as given in design specifications rather than for orificing to the basic radial flux pattern as in the other calculations.

(the radial blanket is in parallel with the core) were 36.77×10^6 and 34.22×10^6 lb/hr.

The ORNL calculations for the low pressure design **indicated** that if the given core flow at end of life **was** orificed to a factor equal to the basic radial flux factor of 1.4, the core pressure drop would be 108 psi. However, it **appeared** that the flow **was not** orificed to the basic radial flux ratio and, in fact, there **was less** than a proportional amount of flow passing through the hot bundle. The maximum hot bundle flow has been given as 158,120 lb/hr, and at this flow rate there would be a pressure drop of 90 psi, as shown.

5.1.6.2 Maximum Surface Temperatures. The maximum surface temperature for nominal operation with the inclusion of engineering factors was selected by B&W as 1300°F. This limitation is based on the long-term creep and yield characteristics of the cladding. As indicated in Section 5.2, it is believed that cladding temperatures up to 1350°F may be tolerated, although this limitation needs further evaluation on the basis of economics.

For a limitation of 1350°F, all three concepts appear to be acceptable. The high pressure design has a maximum surface temperature of 1351°F at 100% power with the inclusion of engineering factors. This value includes the rather large engineering factor on the temperature difference between the cladding and the coolant temperatures. A significant component of this factor is the redistribution component that could be significantly reduced by relatively minor variations in the bundle design.

The intermediate pressure design has a temperature well within the acceptable range, even at 110% power.

For the low pressure design, the ORNL-calculated maximum surface temperature is 1332°F at 100% power with the inclusion of engineering factors. The difference between this surface temperature and the 1300°F given by B&W appears to arise from the selection of the heat transfer coefficient correlation and the formulation of the engineering factors. The heat transfer correlation selected by B&W seems to be more optimistic than can be supported by current experimental data. The engineering

factors selected in this evaluation are smaller than those indicated by B&W, but they are consistent with those of other evaluations.

5.1.6.3 Maximum Center-Line Temperatures. As previously indicated, the melting point of the unirradiated mixed uranium and plutonium oxides is about 4960°F. This melting temperature would be lowered by exposure to irradiation. It is not considered, at this point in the evaluation, that operation with center-line temperatures below the melting point is necessary. However, the ability to operate with melting would have to be demonstrated by experiment. As discussed below, center melting, if it were encountered, would occur in only a small fraction of the fuel.

The designers indicated that all operations would be at conditions where center melting did not occur. According to this evaluation, only the intermediate pressure design met this criterion. This is obvious from the fact that the maximum nominal (i.e., without engineering factors) linear heat rating in this concept was about 9.6 kw/ft, while for the other two designs the nominal rating was in the range of 15 to 16 kw/ft. The differences between B&W's estimates of the center-line temperatures and the values obtained by ORNL were directly dependent on the value used for the thermal conductivity of the fuel. The development of the value used in this evaluation was discussed in Section 5.1.3. While it is very difficult to support any particular value based on the present data, the differences between the values for the center-line temperature indicate the degree of uncertainty of this physical property value, and the influence of this uncertainty on the operating characteristics of the reactor.

In order to indicate the relative effects of temperatures above melting, the fraction of the fuel cross-sectional area that would be molten and the diameter of the melted core are given in Table 5.6. As was indicated in Section 5.1.3, the use of sintered fuel pellets rather than vibration compacted powder would greatly enhance the value of the $\int k(T) dT$ and would allow operation at significantly higher linear heat ratings without encountering center melting of the fuel. The fabrication of pellet-loaded fuel rods of varying diameter would require further investigation.

5.1.6.4 Outlet Coolant Temperatures. For the high pressure design, two outlet temperatures were indicated for different parts of the core.

Table 5.6. Fuel Cross Sectional Area That Would Be Melted at the Point of Maximum Fuel Temperature

	Power (%)	Engineering Factors	Percentage of Cross Sectional Area Melted	Outside Diameter of Melted Zone (mils)
Low pressure system	100	None	9.6	100.9
	100	Included	22.1	153.4
	110	Included	27.9	172.3
High pressure system	100	None	8.6	64.6
	100	Included	12.8	78.9
	110	Included	19.6	97.5

ORNL calculations based on the indicated division of flow between these cores show an outlet temperature to the turbine of 1010°F, which is to be compared with the design value of 1000°F.

For the intermediate pressure design, the ORNL calculated outlet temperature from the core is 968°F, compared with the design value of 1004°F.

For the low pressure design, the outlet temperature from the hot bundle is 1004°F, which is similar to the exit temperatures from the adjacent bundles if the flow is properly orificed. The designer indicated that the coolant flow will be divided among the fuel bundles so that a cooler exit temperature is obtained around the outer zones; the cooler steam will be recycled, while the hotter steam from the center of the reactor will be taken to the turbine. Excess flow will be required in some of the cooler bundles because of a power density gradient across the bundle. When this gradient exists, it will be necessary to supply enough steam flow to cool the hotter side of the bundle, which will be a flow in excess of that required for the cooler side. This gradient presents the additional problem that the steam on the hotter side reaches higher temperatures, which produce lower densities and higher viscosities. The result of the nonuniform coolant properties will be to cause flow redistribution to the cooler side, and additional flow will be required to overcome this redistribution effect. Since the specified flow for the

hot bundle is considerably less than the amount that would result from orificing to the basic radial flux (158,120 lb/hr compared with 176,800 lb/hr), there appears to be sufficient flow for adequate cooling of the cooler bundles. Detailed calculations were not made for each bundle in the evaluation; however, it is reported that these effects were considered in the design.

5.1.7 Conclusions

The following conclusions are drawn from this evaluation:

1. The calculated pressure drop across the core agrees quite well with the values given by the designers for all three concepts when their specified flow rates are used.

2. With the exception of the intermediate pressure design, the ORNL evaluation indicates that melting of the oxide fuel will occur at the center of the fuel rods over a limited fraction of the core. The difference between the values given by B&W and those obtained by ORNL can be attributed to a difference in the evaluation of the thermal conductivity of the oxide fuel. There is no indication that operation with center fuel temperatures below the melting point must be a design criterion. It would be possible, however, through the use of sintered oxide pellets (if they are compatible with the fuel rod design) to meet the criterion of center temperatures below the melting point.

3. The surface cladding temperatures calculated by ORNL are higher than those calculated by B&W for both the high and low pressure designs. This difference, which can be attributed to the heat transfer coefficient correlations used in the two calculations, is, in part, offset by a lower estimation of the engineering factors by ORNL, particularly for the low pressure design.

5.2 Fuel Element Evaluation

The fuel elements proposed for the steam-cooled fast breeder reactors were analyzed to define areas that might limit the ability of the reactors to operate as designed. With any fuel element of the type proposed, fission gases released during operation create internal pressures,

and failure may occur if the stresses and strains of the cladding become excessive. Parameters investigated included fuel temperatures, fuel pin pressures, and cladding stresses. The data obtained, in conjunction with cladding mechanical property data, were used to estimate the burnups and exposure times that could reasonably be expected from the fuel pins.

Many other factors can contribute to fuel element failure. Capability of the fuel element materials and degradation of properties during long time exposure to the high temperature and radiation environment are considered briefly in this section. Other factors, such as thermal and vibrational stresses, **and the effects of fabrication variables and tolerances, are less easily evaluated.** In the final analysis, fuel element failure usually results from the improbable concurrence of several such factors. Extensive testing of fuel elements is therefore required to establish the burnup limits as a function of fuel element design and operating environment.

5.2.1 Fuel Element Description

The fuel elements for the three steam-cooled reactors are similar in that each is composed of many fuel pins. Each pin consists of an enriched core region, an upper and sometimes a lower blanket region, and a void plenum. The fuel is vibration compacted $\text{PuO}_2\text{-UO}_2$ in the cores, and depleted UO_2 in the blanket regions. All fuel pins are initially pressurized with helium to minimize the tendency of the fuel pin cladding to collapse under the external pressure of the coolant. The fuel elements are described in detail in Section 2.1. Fuel element parameters specified by the designers are listed in Table 5.7.

Fuel element temperatures were calculated as discussed in Section 5.1, and central temperatures in excess of the fuel melting point were obtained for both the low and high pressure designs (Table 5.8). Considerable uncertainty is associated with the calculation of central fuel temperatures for oxide fuels. For instance, initial melting may be avoided by a slow increase in power to allow time for fuel densification, which improves the oxide conductivity and fuel rod geometry. However, with increasing burnup, fuel swelling and decreased thermal conductivity can override these effects, and central melting may develop. While test rods have operated

Table 5.7. Core Fuel Element Parameters
Reported by Designers

	Low- Pressure System	Intermediate- Pressure System	High- Pressure System
Coolant pressure, psi	1250	2650	3700
Coolant inlet temperature, °F	575	685	750
Maximum cladding temperature at 100% power, °F	1300	1389	1335
Maximum thermal rating, kw/ft ^a	18.1	12.2	16.6
Maximum design burnup, Mwd/MT ^b	100,000	55,000	50,000
Time to design burnup, ^a days	1000	520	300
Axial power ratio, $P_{\max}/P_{\text{av}}$	1.21	1.33	1.72
Fuel density, % of theoretical	85	87	85
Cladding material	Inconel 625	Inconel 625	19-9DL stainless steel
Outer cladding diameter, in.	0.350	0.276	0.230
Cladding thickness, in.	0.012	0.0146	0.010
Fuel region length, in.	29	59.4	57
Top axial blanket length, in.	18	13.77	18
Top axial blanket cladding diameter, in.	0.325	0.276	0.230
Lower axial blanket length, in.	18	13.77 ^c	
Lower axial blanket cladding diameter, in.	0.325	0.492	
Void plenum length, in.	22.4	9.84	14.3
Void plenum cladding diameter, in.	0.325	0.276	0.230
Cladding neutron exposure ($E > 1$ Mev), neutrons/cm ²	10^{23}	6×10^{22}	10^{23}

^aFor hottest rod in reactor.^bAverage burnup of hottest rod.^cShort fuel elements have no lower blankets.

Table 5.8. Calculated Maximum Temperatures

	Low- Pressure System	Intermediate- Pressure System	High- Pressure System
Maximum central fuel temperature, °F	>4960	4200	>4960
Maximum cladding temperature, °F	1332	1140	1350
Temperature drop across cladding, °F	60	60	80
Temperature drop across fuel- cladding gap, °F	380	320	500
Void plenum temperature, °F	580	690	750

successfully with molten centers to burnups in excess of 10,000 Mwd/MT in water-cooled loops, no reactors have been designed for continuous operation with molten fuel. The vertical rods of the steam-cooled reactors could suffer fuel redistribution or slumping under such conditions that could develop local hot spots and excessive cladding strain on thermal cycling. Thus, heat ratings of 18 kw/ft may be too high for the low pressure design, and probably should be reduced to insure satisfactory performance at high burnup levels. The situation is less severe for the high pressure design, and the proposed 16.5 kw/ft may be feasible. The intermediate pressure design is much more conservative, and should operate successfully at the heat ratings proposed. Experimental testing will be required to establish maximum heat ratings that allow successful fuel performance to desired burnup levels.

5.2.2 Fuel Swelling

Mixed oxide fuels have been tested at high power ratings and burnups in excess of 100,000 Mwd/MT as potential fuels for sodium-cooled breeder reactors.^{42,43} At high burnup, a major fraction (80 to 90%) of the fission gas is released from the fuel. Fuel swelling as a result of solid fission product formation under such conditions is apparently about 1% or less per 10,000-Mwd/MT burnup.⁴² Swelling is somewhat greater in thermal reactor irradiations (1.7 to 2.0%), possibly because more fission gases are retained.⁴³

In the ORNL pressure analysis, it was assumed that fuel swelling could be accommodated by the internal void space in the low density fuel. Fissioning fuel is relatively plastic at temperatures above about 2500°F, and sufficient void volume is available in the fuel pin designs for these reactors. Some sintering and densification also occurs in the cooler outer annulus of fuel, and tests indicate that 50% of the pore volume of 94% dense pellets is eliminated by fuel swelling.⁴⁴ The effects of differential expansion, however, may not be accommodated unless temperature differentials are kept low during initial radiation. Thus, plastic strains may be developed in the fuel cladding. The effects of these factors have not yet been established.

Zebroski, Kittel, and Moss⁴² reported no diametral fuel swelling (<0.2 mil) but some axial expansion at burnups up to 127,000 Mwd/MT. Lawton⁴³ reported mean diameter changes ranging between 0.2 and 0.8% (on 0.20-in.-diam pins) for vibration compacted fuels at burnups ranging up to 60,000 Mwd/MT for fuel pins tested in the Dounreay Fast Reactor. Some of the differences may be associated with fission gas pressures or the low external restraint of the Dounreay Fast Reactor.

The development of cladding strain from these factors is, therefore, dependent on the creep strength of the cooler outer layer of fuel, which may be a function of stress, rate of stress application, character and amount of pore volume, fission rate, and external restraint. Thus, rapid thermal cycling might create plastic cladding strain through differential thermal expansion, whereas similar increases in fuel volume that developed more slowly would be accommodated by yielding of the fuel. This problem is common to all proposed fast reactor designs, and it is believed that continued study and development will yield satisfactory performance at design burnups for these fuel elements. Experimental tests will be required to evaluate effects of such fuel-cladding mechanical interactions on burnup limits as a function of the operating environment.

5.2.3 Pressure Calculations

Internal pressures were calculated for fuel pins operating at maximum temperatures. ORNL assumed 100% fission gas release for the core region and 10% release for the blanket regions. It was also assumed that all pore volume in the fuel and blanket regions was available to contain gas, but that it was reduced by fuel swelling and thermal expansion. Fuel swelling for the core region was taken as 1% per 10,000 Mwd/MT and that for the blanket as 1.7% per 10,000 Mwd/MT.

The calculated fuel element pressures and cladding stresses are listed in Table 5.9, and are shown graphically for the different fuel elements in Figs. 5.2, 5.3, and 5.4. Exposure times were calculated from the average burnup rates of the hottest rods; the burnup rates were assumed to be constant.

The primary irradiation effect on cladding mechanical properties will be a reduction in fracture ductility, as discussed below. ORNL, therefore,

Table 5.9. Calculated Pressures and Cladding Stresses
for Hot Fuel Pins

	Low Pressure System	Intermediate Pressure System	High Pressure System
Coolant pressure, psi	1,250	2,650	3,700
Initial internal helium pressure, psi	192	580	860
Initial operation			
Internal pressure, psi	441	1,740	2,460
Cladding stress, psi	-11,400	-8,200	-13,600
Design burnup operation			
Average burnup, Mwd/MT	100,000	50,000	50,000
Internal pressure, psi	1,477	3,850	3,930
Cladding stress, psi	3,210	14,000	2,600
Cladding stress if there is no coolant pressure, psi	20,900	37,100	43,600
Normalized burnup operation			
Average burnup in hot pin, Mwd/MT	93,200	92,200	66,600
Internal pressure, psi	1,370	5,800	4,470
Cladding stress, psi	1,680	30,500	8,460
Cladding stress if there is no coolant pressure, psi	19,300	52,500	49,150
Operating time, hr	21,200	20,500	11,100

estimated fuel cladding life to be that at which the increasing cladding stresses would develop 1% cladding strain according to the method described by Kennedy.⁴⁵ Time to 1% cladding strain at constant stress was obtained from stress-rupture data⁴⁶⁻⁴⁸, and was thus lower and more conservative than would be obtained from the more conventional creep data based on secondary creep rates. The effect of cladding volume increase as the result of irradiation-induced voids, also discussed below, was not applied to the fuel pin pressure calculations.

Fuel swelling will also contribute to cladding stresses. Total restraint must be sufficient to force the fuel to accommodate swelling within the fuel voids. Sodium-cooled test pins with about comparable restraint were operated successfully to 50,000 Mwd/MT, but an increasing resistance to fuel flow apparently caused failures at higher burnups.⁴³

Fig. 5.2. Pressures and Cladding Stresses During Life of a Hot Fuel Pin in Low Pressure System.

Fig. 5.3. Pressure and Cladding Stresses During Life of a Hot Fuel Pin in Intermediate Pressure System.

Fig. 5.4. Pressures and Cladding Stresses During Life of a Hot Fuel Pin in High Pressure System.

The higher fuel temperatures in the steam-cooled reactors may aid fuel plasticity. Testing will be required to establish burnup limits as a function of fuel pin design variables.

5.2.4 Collapse of Fuel Cladding

All fuel pins are to be prepressurized with helium, but significant compressive stresses from the external coolant pressure will exist during initial operation, as shown in Figs. 5.2, 5.3, and 5.4. Calculations indicate that the void plenums will be free standing at the end plenum temperature. At the maximum operating cladding temperatures, the compressive stresses will cause creep collapse of the cladding against the fuel. Unpressurized superheater fuel rods with thin collapsed claddings, tested by General Electric Company,⁴⁹ developed ovality and decreased cladding

diameter at fuel temperatures and fission rates considerably lower than those proposed for the steam-cooled fast reactor designs. For fuel operating under steam-cooled fast reactor conditions, considerably less support can be expected.

The collapse of tubes under external pressure has been discussed by Kennedy and Venard,⁵⁰ and ORNL used their methods to evaluate collapse of the fuel tubes. Since isochronous stress-strain data are not available for the cladding materials, the time to 1% strain was taken from creep-rupture data for solution-annealed Inconel 625 to estimate the tangent modulus of the cladding as a function of stress. The results indicate that at 1400°F, perfectly round fuel tubes will be elastically stable, but creep collapse will occur. The period for development of instability by creep was calculated to be about 200 hr for initial operating compressive stresses for both B&W designs, and about 1200 hr for the intermediate pressure design. ORNL suggested that fuel pin internal helium pressures be increased to allow at least 1000 hr for development of instability, at which time increasing fission gas pressures may be able to avert collapse. For the B&W designs, the time for development of instability can be extended to 1000 hr by moderate increases in prepressurization (from 860 to approximately 1030 psi for the high pressure design and from 192 to 300 psi for the low pressure design). Similar data were not available for 19-9DL stainless steel cladding, which is not as strong as Inconel 625 and would require additional prepressurization, probably in the region of 1100 to 1200 psi, and would require additional void plenum volume to achieve satisfactory cladding stresses at the end of life.

For the B&W high pressure reactor, it is not known whether the void plenum volume can be increased sufficiently. With the design void plenum and initial pressurization of 1030 psi, the calculated stress at the normalized burnup at operating conditions is 14,400 psi, and 1% strain was calculated for Inconel 625 cladding at a burnup level of 60,000 Mwd/MT (90% of the normalized burnup), as shown in Fig. 5.4. For 19-9DL stainless steel, 1% strain will be reached at about 8000 psi and 48,000 Mwd/MT. However, cladding stress after a loss of coolant at these burnup levels will considerably exceed the yield strength of either material, so a larger end plenum will be required.

For the low pressure reactor, some increased pressure will probably be required, but the fuel pin, as designed, has sufficient void volume so that end-of-life tensile stresses will not be excessive, even if it is desirable to pressurize to an initial internal operating pressure equal to the coolant pressure.

The concept of preventing fuel cladding collapse through internal pressurization complicates fabrication and inspection processes because of the need to insure that all pins are pressurized correctly. The possibility exists that internal helium pressure might be lost through a leak too small to prevent satisfactory operation of a fuel pin under other conditions.

Kennedy's analysis indicates that ovality is a serious imperfection for tubes under external pressure, and that 1 or 2 mils initial deflection or ovality reduces the calculated critical pressure for collapse to about 50% of that for perfectly round tubes. Some experimental testing will be required to establish the prepressurization required to assure satisfactory fuel-rod performance.

5.2.5 Fuel-Cladding Compatibility

No problems of compatibility exist between stainless steels and UO_2 fuels. Successful performance of mixed oxide fuel pins at 50,000 Mwd/MT at temperatures up to 1100°F has been reported, and pins irradiated at 1300°F do not indicate a general problem, although some instances of cladding reaction have been observed.⁴³ From thermodynamic considerations, Anselin and Bailly⁵¹ concluded that irradiation of stoichiometric mixed oxide fuels to high burnup might release oxygen that would react with fuel claddings to cause chromium depletion and metallurgical changes that would affect cladding strength. Experimental testing will be required to establish the extent to which these factors may limit fuel element life.

5.2.6 Fuel-Coolant Compatibility

Satisfactory performance of vibration compacted fuels in pressurized and boiling water environments has been achieved to burnups in excess of 10,000 Mwd/MT. Tests with defected fuels or with fuels on which cladding

had failed indicate satisfactory coolant-fuel compatibility, and no excessive loss of fuel on failure.⁵² Similar results were obtained in the laboratory at steam temperatures up to 1300°F in superheater element tests, although if appreciable quantities of oxygen are present, swelling of the UO₂ may develop through formation of less dense oxides.⁴⁹ Additional data are needed in view of the more severe operating conditions required for fast reactors.⁵³

5.2.7 Coolant-Cladding Compatibility

Compositions of the three cladding materials of interest are listed in Table 5.10. Inconel 625 is the specified cladding material for the fuel elements of the low and medium pressure systems, while the high pressure system fuel elements are clad with 19-9DL austenitic stainless steel. Incoloy 800 is proposed as the blanket element cladding for the intermediate pressure reactor. Maximum fuel cladding temperatures range between 1100 and 1350°F. Fuel lifetimes range up to 22,000 hr, but probably no particular area of cladding will remain at the maximum temperature for more than about one-half its life.

Inconel 625 has shown excellent performance in steam environments.⁵⁴⁻⁵⁶ Linear corrosion rates calculated for isothermal exposure to superheated steam at 1050 or 1150°F are the lowest of the rates for any material tested.⁵⁵ The scale present after 5700 hr at 1150°F is not much thicker than that present after 1000 hr at 1050°F.

Table 5.10. Chemical Composition of Cladding Materials

	Chemical Analysis (wt %)								
	Ni	Cr	Fe	Cb or Ta	Mo	C	Mn	W	Al + Ti
Inconel 625	61	22	3	4	9	0.05	0.15		0.8
Incoloy 800	32	21	46			0.04	0.75		0.6
19-9DL stainless steel	10	20	65	0.50	1.75	0.30	1.0	1.4	0.3

Similar results were obtained at temperatures up to 1500°F in superheated steam containing 30 ppm O₂ and 2.5 ppm H₂ to simulate radiolytic decomposition effects typical of boiling water reactors. The data indicate linear corrosion rates of less than 1 mil in three years at 1300°F mean temperature, with a compositionally disturbed layer about 1/2 mil in depth.⁵⁶ There was no change in oxidation mechanism at higher temperatures. Data on oxidation during thermal cycling with Inconel 625 at maximum temperatures of 1800 and 2000°F have also shown superior performance.⁴⁶ Incoloy 800 has slightly lower corrosion resistance, but is still satisfactory at temperatures between 1100 and 1400°F.^{53,54}

Austenitic stainless steels, such as the 18-8 series, are marginally satisfactory as fuel element claddings in steam at proposed temperatures (under isothermal conditions up to 1150°F), and corrosion increases rapidly with increases in temperature.⁵⁵ In addition, austenitic stainless steels are subject to stress corrosion cracking in environments containing chloride ions.^{53,57} For this reason, the use of austenitic stainless steels has been abandoned in favor of higher nickel-containing alloys as potential superheater fuel element claddings.^{53,57} At temperatures up to 1600°F, type 19-9DL is reported to be more resistant to oxidation than other austenitic stainless steels. No information on performance of 19-9DL in steam environments has been located, but 19-9DL is also subject to carbide precipitation and intergranular corrosion if heated or operated in the temperature range of 800 to 1500°F.^{47,48} It is questionable whether the additional alloying elements will confer added resistance to stress corrosion cracking.

5.2.8 Mechanical Properties of Cladding Materials

Data on strength properties of Inconel 625 and 19-9DL stainless steel⁴⁶⁻⁴⁸ are given in Figs. 5.5 and 5.6. Of principal concern is the low strain rate stress-rupture data, since the life of the fuel rod will be limited by creep under the slowly increasing stresses developed as fuel pin pressures increase.

Irradiation test data have indicated that strength and creep rates of type 304 stainless steel are not altered by the radiation environment⁵⁸ at temperatures above about 950°F.

Fig. 5.5. Properties of Solution-Annealed Inconel 625.⁴⁶

Irradiation will decrease the ductility of the cladding alloys at temperatures in excess of about 1100°F. This is an effect of helium generation by transmutation of the cladding materials, and is common to all iron- and nickel-base fuel claddings. The problem of cladding development and/or fuel element design to achieve desired burnup levels in view of the limitations imposed by this effect is recognized as one of the most formidable in the achievement of successful fast breeder reactors.

Fig. 5.6. Properties of 19-9DL Stainless Steel.^{47,48}

In addition, some test data obtained by General Electric indicate that with Inconel 625, precipitation induced by irradiation **causes** over-aging with some loss in strength.⁵⁹ Additional investigation will be required before these factors can be evaluated.

Preliminary information also indicates that fast neutron damage may generate voids in fuel claddings that can cause volume increases. At

exposures of approximately 10^{22} neutrons/cm² ($E > 1$ Mev), the volume increase is approximately 0.1%, and data suggest that at 10^{23} neutrons/cm² ($E > 1$ Mev), the volume increase⁶⁰ may approach 10%. The combined effects of this expansion, the internal pressure of the gas, and the expansion of the fuel on fuel rod performance cannot be estimated at this time. Because of the limiting ductility due to radiation, it is concluded that fuel element life may be estimated as the time and stress at which 1% cladding strain is developed under the constantly increasing stress calculated from the time at which the fuel pin internal pressure equals the external coolant pressure.

5.2.9 Factors Not Evaluated

There are other factors that affect the performance of fuel elements that have not been evaluated in this study. Some of these are thermal stresses, coolant frictional drag forces, fabrication tolerances, and vibration effects. These effects should be evaluated in the development program, and alternate materials, shapes, and design characteristics should be studied.

5.2.10 Discussion and Summary

Results of this analysis of fuel element performance for each reactor are discussed briefly below.

5.2.10.1 High-Pressure Reactor. From corrosion considerations, it is believed that 19-9DL austenitic stainless steel may not be a satisfactory cladding material for this application. Its high carbon content leaves it susceptible to intergranular corrosion, and austenitic stainless steels are also subject to stress corrosion cracking in steam environments containing chlorides. The 19-9DL material also has inadequate strength for the design proposed. Inconel 625 may be a suitable substitute if the design can be changed to prevent creep collapse of the fuel pins.

The high pressure reactor fuel element was designed for an average burnup of 50,000 Mwd/MT which, with the 1.72 burnup peaking factor, may result in 86,000-Mwd/MT local maximum burnup. The cladding operating stress is low, but if loss of coolant pressure should occur while the fuel was at operating temperature, the resulting cladding stress of

45,000 psi would about equal the yield stress of the suggested Inconel 625 cladding at design burnup, and the yield stress would be exceeded for operation at the maximum burnup of 100,000 Mwd/MT (66,600 Mwd/MT average for the hottest fuel pin). A value of 1% cladding strain was calculated at an average burnup of 74,000 Mwd/MT.

5.2.10.2 Intermediate Pressure Reactor. The design burnup for the fuel element is 55,000-Mwd/MT average and 74,000-Mwd/MT peak. The maximum fuel temperatures will not exceed the fuel melting point, and from this aspect represents a conservative design. ORNL calculations indicate that 1% cladding strain will be achieved at the design burnup. Fuel pins without the lower blanket region will operate at slightly higher pressures, as shown in Fig. 5.3. Without coolant pressure, cladding stresses were calculated to be 39,000 psi, or about equivalent to the yield strength at 1400°F. It is believed that the fuel rods will operate satisfactorily as designed, and that the burnup limit for this fuel pin can be extended to 74,000 Mwd/MT (average) by inclusion of additional void space.

5.2.10.3 Low Pressure Reactor. The low pressure reactor was designed for an average burnup of 100,000 Mwd/MT, and has an average axial power peaking factor of 1.33. Calculated cladding stresses are low at design burnup because of the comparatively long void plenum. The maximum fuel temperature was calculated to be about 600°F above the fuel melting point, and this reactor is the least conservative from that aspect. Achievement of the design burnup also implies maximum local exposure levels of approximately 130,000 Mwd/MT, a region in which fuel swelling may contribute extensively to cladding strain, but at the maximum exposure of 100,000 Mwd/MT the fuel element will probably be satisfactory.

5.3 Reactor Safety

Any situation or condition that could lead to a failure of SCBR fuel cladding and to the subsequent release of fission products is a potential reactor safety problem. Assurance of reactor safety is assurance that the potential for the release of fission products is minimized, and that if fission products are released they may be successfully controlled and contained.

Failure of cladding would result in release of appreciable quantities of plutonium alpha activity, as well as fission products. If the failure occurred early in the life of the fuel, the alpha activity could be the major activity problem. While the activity was contained inside the primary system, it might complicate maintenance of steam system components by requiring decontamination prior to exposure to air. After accumulation of fission products, detection of beta and gamma activities is easier, and the measures taken to eliminate the fission products would also be adequate for removing the alpha activity. In either case, the presence of plutonium with its high alpha activity hazard does not increase the requirement for safety measures based on the presence of fission products, and is not considered separately in this evaluation.

The purpose of this discussion is to review mechanistically several of the more severe accidents that could lead to fuel failure and to identify and define the functional requirements of systems essential for accident prevention and control. Three classes of accidents are considered: reactivity accidents, thermal accidents, and plant startup and control accidents. Nuclear excursions and containment vessel melt-through are also discussed.

5.3.1 Reactivity Accidents

Reactivity insertions of sufficient magnitude to result in core damaging power excursions may arise from a variety of mechanisms. Safety analyses anticipate these mechanisms and define the functional requirements of systems to counteract the reactivity insertions and prevent core damage.

In the steam-cooled breeder reactors, there are a few mechanisms capable of changing core reactivity status rapidly enough to be of safety significance. These are control rod movement, changes in coolant density, changes in core material temperature, and changes in core geometry or specific fuel concentration such as might result from core material melting or sudden additions of fuel. The discussion here is limited to reactivity effects of control rod movement, changes in coolant density, and changes in material temperature.

Reactivity accidents through sudden fuel additions (that is, the fueling accident) are not considered separately because they involve special operations and specific procedural controls. Also, core meltdown, because it implies that an accident has already occurred, is not considered here, but it is considered in Section 5.3.4, Destructive Nuclear Excursion.

Except for the power limiting effect of the temperature coefficients associated with the fuel and structure, the SCBR does not have inherent shutdown mechanisms, so an applied reactivity shutdown will be required during a reactivity accident to prevent a core meltdown. Reactivity accident studies in steam- and helium-cooled fast reactors have given an indication of the reactivity insertion rates for which the power excursions can be terminated by a reasonably fast scram system without causing fuel damage. A Karlsruhe dynamics study⁶¹ of the intermediate pressure system indicates that reactivity insertion rates of \$16/sec at full power can be handled by a fast scram without fuel or cladding melting. A General Electric Company study⁶² of a 50-Mw(e) SCBR indicates that reactivity insertions of \$2/sec at full power can be handled easily by a somewhat slower scram. In an ORNL study⁶³ of a 1000-Mw(e) helium-cooled fast reactor, in which the extreme case of the maximum insertion rates that would bring the cladding to just the melting point or the fuel to the point (6500°F) of producing damaging vapor pressures was examined, it was found that the reactor can just tolerate insertion rates of \$33/sec. Reasonably fast scram times were used in these studies, with delay times of 50 to 170 msec and total rod insertion times of 250 to 600 msec. These scram times were shorter than those used in many power reactors; however, they were longer than those used in research reactors at ORNL, and it was thought that scram times of this order or shorter were feasible. Thus it appears that a reasonably fast scram system can handle reactivity insertion rates from \$2 to about \$20/sec. The allowable reactivity insertion rate could increase further by decreasing the response time of the fast scram system.

In general, fast reactors are more sensitive to reactivity accidents than thermal reactors, since they have a shorter prompt neutron lifetime, a smaller delayed neutron fraction, and a smaller Doppler reactivity

coefficient. Fast reactors will become prompt critical with a smaller amount of reactivity, and a prompt critical excursion will proceed on a far shorter period for a given amount of reactivity. It appears from the above studies of reactivity insertions, and from a general review⁶⁴ of fast reactor safety considerations, that, for realistic reactivity insertion rates in the presence of an adequate neutron source, a fast safety system and shutdown mechanism should be able to terminate an accident before the fuel is destroyed. For this reason, considerable effort should be given to making the response of the shutdown systems as rapid as possible.

5.3.1.1 Reactivity Insertion By Control Rods. Control rod reactivity insertion accidents are, in general, hypothesized on the basis of uncontrolled rod withdrawal, rod dropout, or rod ejection. The upper limit for an uncontrolled rod withdrawal accident is the runaway of all the rods. If the normal plant operational considerations require that reactivity rates from individual rods be such that the uncontrolled withdrawal of all the rods would give reactivity insertion rates that would exceed the capabilities of the safety system, it will be necessary to either improve the response time of the safety system, or impose limitations on the number of rods that can be withdrawn at one time. The controls used to limit the number of rods that can be withdrawn must become part of the safety system.

The two B&W reactors had shim safety rods with hydraulic drives that had only two positions: either fully inserted or fully withdrawn. B&W indicated that the total worth would be about $0.06 \Delta k$, so the large number of safety rods, 108 in the high pressure system and 124 in the low pressure system, assured that no single rod would be worth more than approximately 30¢ in reactivity. The low pressure system also had three regulating rods with hydraulic jack drives for incremental positioning. These would have about the same reactivity worth as the safety rods. It would be difficult to place a positive limit on the withdrawal speed of either type of hydraulic drive, so it would probably be necessary to make the safety system design such that not more than one or two safety or regulating rods could be withdrawn at one time.

The intermediate pressure design had only 18 rods for safety and control. The total worth of these rods was about $0.06 \Delta k$, so the average worth per rod was approximately $0.0033 \Delta k$ (or about \$1). The maximum worth rod was estimated to have a value of about \$1.33. This was a high worth per rod; however, the speed of the electric-motor drives could be positively limited to give reactivity rates that could be handled by the fast scram system.

Power excursions resulting from rod dropout appear to be highly unlikely in any of the three designs. In each system the rods are inserted from the top, and downward travel is limited by positive mechanical bottom stops. The high and low pressure-system control rods are integral with fuel assemblies, and downward movement is positively limited. In the intermediate pressure design the rods are inserted into special control elements that also provide positive bottom stops that would be effective in the event a control rod became detached from its drive.

The possibility of rod ejection is discussed in Section 5.3.4.1.

5.3.1.2 Reactivity Insertion By Coolant Density Changes. As may be seen from Table 4.4 of Chapter 4, SCBR core reactivity is significantly influenced by changes in coolant density. The actual value of a coolant density coefficient, or alternatively, a curve of k_{eff} versus coolant density, is difficult to calculate with low uncertainty. The results of calculations of coolant density reactivity effects were given in the design reports for each of the three SCBR concepts, and in the SCBR evaluations these calculations were repeated with newer methods and cross section data. Each new calculation yielded results very different in both magnitude and sign from the results of the preceding calculation. Table 4.4 presents the latest results obtained with methods developed at ORNL. These data on coolant density reactivity effects are considered to be the best available. The data in Table 4.4 are plotted in Figs. 5.7, 5.8, and 5.9, and coolant density reactivity coefficients at the operating point, as estimated from these curves for the intermediate and low pressure designs, are listed in Table 5.11. The density reactivity coefficients for the high pressure design were calculated with the diffusion equations described in Section 4.2.

Fig. 5.7. Reactivity Change Versus Coolant Density Ratio for Low Pressure System.

Fig. 5.8. Reactivity Change Versus Coolant Density Ratio for Intermediate Pressure System.

The reactivity additions (given in Table 5.12) that might be produced by the reference loss-of-coolant accident (described in Sect. 5.3.2.2) were estimated from the density reactivity data given above and the depressurization rates presented later in Table 5.13. Table 5.12 lists the average reactivity insertion rates and the total reactivity added as a result of the blowdown at 1 sec and at 5 sec after initiation of blowdown.

These reactivity rates appear to be within the capabilities of reasonably fast scram systems (discussed in Sect. 5.3.1). In the high and

Fig. 5.9. Reactivity Change Versus Coolant Density Ratio for High Pressure System.

Table 5.11. Coolant Density-Reactivity Coefficients

	$(\Delta k/k_0)/(\Delta D/D_0)$	
	At Beginning of Life	At End of Life
Low-pressure system	-0.0094	-0.0034
Intermediate-pressure system	-0.022	-0.0046
High-pressure system	+0.0078	+0.038

Table 5.12. Reactivity Additions During Blowdown

	Initial Reactivity Insertion Rate ($\Delta k/\text{sec}$)	Reactivity Added (Δk)	
		In 1 sec	In 5 sec
Low-pressure system			
At beginning of life	+\$0.37	+\$0.37	+\$1.42
At end of life	+\$0.13	+\$0.14	+\$0.52
Intermediate-pressure system			
At beginning of life	+\$2.3	+\$1.7	+\$3.8
At end of life	+\$0.49	+\$0.49	+\$2.7
High-pressure system			
At beginning of life	-\$0.64	-\$1.0	0
At end of life	-\$3.1	-\$3.6	-\$9.0

low pressure systems, the initiation of a scram within about 1 sec after the start of the blowdown would prevent the reactor from becoming prompt critical. In the intermediate pressure system, it appears that a scram would have to be initiated within approximately 0.4 sec if prompt criticality were to be avoided. (A similar study at Karlsruhe⁶¹ based on their blowdown rates and coolant density reactivity relationships gave a reactivity insertion rate of about $\$4/\text{sec}$ for a similar situation. This result implies that the scram initiation time would be less than approximately 0.2 sec.)

Another coolant density reactivity effect is evident from the data in Table 4.4 and Fig. 5.7. The change in k_{eff} with flooding of the low pressure system core is large and positive. The beginning-of-life Δk of 0.17 and the end-of-life Δk of 0.07 are likely to be greater than the total worth of the control rods. Consequently, the core flooding systems must be provided with poison injection capability to provide the reactivity hold down not afforded by the control rods. It may also be necessary or desirable to add resonance poisons⁶⁵ in the fuel to reduce the reactivity addition on flooding; however, this would probably reduce the breeding performance of the reactor. The core flooding reactivity effect in the intermediate and high pressure systems is still being calculated. Earlier calculations on the intermediate and high pressure systems, although very uncertain, indicated that the maximum value of reactivity may occur at partially flooded conditions.

In summary, the coolant density-reactivity relationships seem to change radically with changes in core composition and calculational techniques. If these relationships can be tailored by changing the core composition somewhat without greatly altering the economic performance of the core, careful consideration should be given to what shape might be desirable. This will probably involve a compromise between control considerations and safety considerations. The Karlsruhe dynamic studies⁶¹ indicate that a small, negative coolant density coefficient is desirable from the control standpoint, since this gives stability under normal operating conditions; however, a negative coefficient is undesirable from the safety standpoint, since this gives a positive reactivity addition

with loss of coolant and eliminates the inherent shutdown mechanism typical of most light water thermal reactors. Of course, the positive reactivity addition with flooding is also undesirable from the safety standpoint.

5.3.2 Thermal Accidents

Loss of flow and loss of coolant are thermal accidents that must be coped with in the SCBR, just as in other types of reactor. Although the effects on reactivity feedback are not so severe as for systems that can undergo coolant phase changes, these accidents place certain physical and reliability requirements on the emergency systems.

5.3.2.1 Loss of Flow. The loss-of-flow accident is not impossible, but highly improbable since there are multiple steam driven main circulators that can be operated with steam from auxiliary boilers. If coolant flow is interrupted, the SCBR, with its high power density and low heat capacity, will have a very short time margin available in which to scram the reactor and to restore forced circulation cooling. If the fuel is uncooled, it will heat up at the average rate of approximately 400°F/sec at full power [as compared with about 92°F/sec in a 1000-Mw(e) BWR]. As an illustration of the magnitude of the problem, a General Electric Company dynamics study⁶² of a 50-Mw(e) SCBR indicates that a flow interruption (or flow notch) of only 0.45 sec will give a 460°F increase in cladding temperature, even with a reactor scram. The loss of forced circulation in the SCBR is a much more difficult situation to cope with than a similar situation in a light water reactor, where the loss-of-flow accident does not become serious until enough water has boiled away to expose a large portion of the core. The time available to add water to the core or to reestablish circulation may be measured in hours for light water reactors, rather than in seconds, as is the case for the SCBR.

In the event of a circulator failure, coolant flow will be reduced rapidly, but not lost entirely, since the remaining circulators are sized to maintain the primary system in service at slightly reduced power. The loss or reduction of coolant flow will tend to add reactivity and give a power increase; however, as discussed in Section 5.3.3.2, these effects are not too severe.

5.3.2.2 Loss of Coolant. A rupture of the primary system leading to a loss of coolant in an SCBR is an accident of great interest because of the high probability of resulting core damage and fission product release. The loss of coolant results in two effects that are important: one is the positive reactivity feedback associated with the coolant density decrease, which is discussed in Section 5.3.1.2, and the other is the loss of heat removal capability. The former is particularly important in determining the reactor power transient, and the latter is important in determining fuel and cladding temperature transients in the ensuing time before the emergency cooling systems become operative. The behavior of each of the three proposed systems following a loss-of-coolant accident is discussed in this section.

Each of the three systems incorporated specific subsystems and features intended to minimize the effects of a primary system rupture. All were equipped with check valves where the pipelines penetrated vessels, and at the components where the lines met other lines, and systems were provided to detect which line had failed and to isolate that line. Failures in the pipelines would produce only small perturbations in the primary coolant system if the failures could be detected and the correct isolation valves closed in approximately 1 sec, and if the pipe failure did not occur between the check valve and the vessel; for example, at the vessel. (This single line isolation philosophy differs from that for boiling water reactors in which all loops are isolated.) In addition to these common features, each design had the following specific characteristics.

In the low-pressure system, the steam-contact boiler and circulators were contained within the primary reactor container to reduce the number and size of possible rupture points. There were four parallel feedwater inlet lines connected to a common internal spray header, and six parallel steam outlet lines. This arrangement reduces the importance of any one line in continuing to supply or remove coolant, and reduces the flow area through which coolant may escape. In addition, there were venturis on the steam lines within the reactor vessel to limit escaping steam flow in the event of an external line rupture. A feedwater recirculation and storage loop was provided external to the reactor vessel. This stored liquid will

act as a limited source of makeup water for the primary system. The rate of makeup is substantially limited by small connecting lines and a small capacity recirculating pump.

In the intermediate pressure system, there were ten parallel steam outlet lines and eight parallel steam inlet lines to reduce the importance of any one line. The larger steam inlet lines, compared with the water inlet in the low pressure design, place a larger escape flow area at the inlet, where a rupture is potentially most hazardous. Two Loeffler boiler-pressurizers were connected on-line to two of the parallel circuits of the primary loop. These boilers will act as a source of makeup steam in the event of a rupture, since they are capable of responding rapidly to changes in system pressure. There were six separate steam generators that produce saturated inlet steam. Each of these has liquid water storage that will provide an extra measure of stored mass and energy to back up and supplement the Loeffler boilers.

In the high pressure system, there were five parallel steam outlet lines and three parallel steam inlet lines to reduce the importance of any one line. Smaller steam lines, due to operation at supercritical pressure and corresponding relatively high steam density, minimize the possible steam escape flow rates if a line ruptures. There were no on-line liquid storage volumes that could act as immediate coolant makeup or pressurizers in the event of a loss of coolant. However, ORNL added a low pressure storage tank that would be closed during normal operation, and would open to force liquid into the reactor when the pressure dropped below 300 psia.

In order to define, for evaluation purposes, a worst credible loss-of-coolant accident for each of the three concepts, consideration was given to the consequences of rupturing any one of the inlet or outlet pipes at a point where the isolation valves on that line would not function. It was tentatively concluded that rupturing of an inlet line will lead to the most serious consequences in each case. This judgment can be verified only by extensive further quantitative analyses for each concept. For evaluation purposes, the following situation was taken as the reference accident.

An inlet pipe was subjected to a double-ended rupture, and the isolation valves on this line fail to close. Steam (or water) instantly began to escape from both ends of the pipe into the containment building. At 1 sec after the rupture occurs, the reactor was instantly scrammed to a β_{10} subcritical reactivity level. It was assumed that no reactivity feedback effects influenced the power before shutdown, and that the fission-product composition corresponded to infinite operation at full power.

The fractional power generation as a function of time after pipe rupture is shown in Fig. 5.10. The accident results depend strongly on whether the reactivity control system will scram the reactor within the 1 sec assumed here. If the scram time is longer, or the positive reactivity feedback, discussed in Section 5.3.1.2, increases the power significantly during this period, it is expected that the consequences would be worse than predicted here.

In order to establish the role that the various subsystems and design features play in each concept, the sequence of events that would follow an inlet line rupture of each system is discussed in the following paragraphs.

Fig. 5.10. Reactor Power Generation Following An Inlet-Line-Rupture Accident.

In the low pressure system, rupture of one feedwater line will provide a short-circuit path through the inlet water spray header for the other three inlet lines and result in a significant reduction of water injection rate into the reactor. The recirculating blowers inside the reactor vessel will continue to move outlet steam back to the inlet of the reactor. Part of this steam will leave the reactor through the turbine supply pipes. As a result, the steam inventory in the reactor vessel will be depleted and there will be a drop in coolant pressure and density. During this process, the recirculated outlet steam will be starved of feedwater in the integral contact boiler and will be returned to the inlet at an elevated temperature. Thus, the loss of feedwater flow and continuation of circulation will result in increasing coolant temperature and decreasing pressure immediately following the rupture.

In the intermediate pressure system, rupture of one of the eight steam inlet lines will cause temporary starvation of flow through the reactor, since all the inlet steam will escape through the hole in the inlet collector. The steam passing through the reactor and steam generator lines will reverse direction. In order to supply the steam being continuously depleted from the primary system, the water stored in the six injection steam generators and two Loeffler boilers will flash. The flashing liquid will derive its heat of vaporization from the remaining liquid and, thus, lower its temperature and corresponding saturation pressure. It is this drop of saturation pressure of the liquid reservoirs that determines the rate of depressurization of the system. Steam will be continuously fed to the point of rupture and out of the system by the Loeffler boilers and the steam generators by separate paths through the circulating blowers and the reactor. The reverse flow through the reactor will enter at a temperature of 1000°F until the flashing steam from the steam generators has reached the core. The steam will then enter at the steam saturation temperature. Cooling of the fuel elements will be determined by the temperature and flow rate of the reversed steam flow.

Rupture of an inlet steam line in the high pressure system will cause starvation of flow into the reactor, since all the inlet steam will escape through the broken line. The drop in pressure in the vicinity of the break will cause a negative pressure gradient, which will cause the coolant steam

flow through the reactor to reverse direction. Steam will be continuously depleted from the primary system, with no source of makeup immediately available as in the other two designs. The rate of depressurization will be determined by the rate of outflow through the broken line. With the circulation pumps continuing to deliver steam to the reactor inlet, the rate of reversed coolant flow through the core will be determined by the difference between the escape rate and the circulation delivery rate. The storage tank will begin to add makeup water when the system pressure drops below 300 psia. Hence there will be a relatively long time delay (about 7 sec) before the makeup system commences operation and stabilizes the system pressure at about 230 psi. Up until that time, fuel element cooling capability will be determined only by the reversed flow through the reactor.

As a result of the inlet pipe rupture accident, each of the designs will experience pressure and steam density decreases and fuel and cladding temperature increases. An elementary analysis of the dynamics of each system was performed that took the various special design features into account. A summary of the important reactor response characteristics derived in the analyses is presented in Table 5.13. The variations in coolant pressure, density, and temperature and in fuel temperature for each concept are also summarized in Figs. 5.11, 5.12, and 5.13.

It is concluded that the average fuel temperature will experience little or no increase for each concept in the short time interval examined here. An earlier study⁶⁶ indicates that gross fuel melting will be prevented if emergency cooling is initiated within about 20 sec. The outlet coolant temperature, which is a good measure of cladding temperature, will experience transients during the first few seconds following rupture that will be well above the range in which cladding failures would be expected; hence, cladding failures are anticipated for each concept.

It is noted that the results presented here for the intermediate pressure design do not include the contribution of the Loeffler boiler-pressurizers, but do include the effect of the liquid in the steam generators. In an effort to be conservative, this concept was penalized compared with the others. Karlsruhe⁶¹ analyzed this accident to include the Loeffler boilers and found that after 1 sec the pressure drops only

Table 5.13. Response to an Inlet-Pipe-Rupture Accident

	Low-Pressure System	Intermediate- Pressure System ^a	High-Pressure System
Average coolant pressure, psia			
Steady state	1,150	2,590	3,560
1 sec after rupture	1,000	1,600	2,500
5 sec after rupture	575	355	580
Coolant flow rate in reactor, lb/sec			
Steady state	11,000	7,000	7,080
1 sec after rupture	9,550	2,800 ^b	6,500 ^b
5 sec after rupture	5,500	730 ^b	1,620 ^b
Average coolant density in reactor, lb/ft ³			
Steady state	2.01	4.5	6.50
1 sec after rupture	1.74	2.9	4.74
5 sec after rupture	1.00	0.58	1.49
Average fuel temperature, °F			
Steady state	4150	3120	3220
1 sec after rupture	4150	3240	3230
5 sec after rupture	3070	2640	2240
Coolant inlet temperature, °F			
Steady state	575	685	750
1 sec after rupture	575	1004 ^b	890 ^b
5 sec after rupture	1440	580 ^b	540 ^b
Coolant outlet temperature, °F			
Steady state	875	1004	1000
1 sec after rupture	915	2040	1240
5 sec after rupture	1900	2160	1570

^aThese results do not include the influence of the Loeffler boilers.

^bFlow has reversed direction through the reactor.

Fig. 5.11. Depressurization Resulting from An Inlet Line Rupture Accident.

Fig. 5.12. Average Coolant Density Decrease Resulting from An Inlet Line Rupture Accident.

Fig. 5.13. Coolant and Fuel Temperature Transients Resulting from An Inlet Line Rupture Accident.

to 90% of its initial value, and the average coolant density drops only to 75% of its initial value. However, they predict that the core coolant flow will drop to 15% of its normal value within 1 sec with no reversal of flow. This would lead to the ORNL conclusion that excessive coolant temperatures and, hence, excessive cladding temperatures, would still be experienced.

5.3.3 Plant Startup and Control Accidents

The sudden and unanticipated criticality of a previously shutdown core presents an accident potential of considerable concern in any hazards evaluation. Also of concern are the requirements on a power control system for maintaining a stable power level and the potential plant

instabilities that could lead to power excursions if the control system failed. The potential for these accidents in an SCBR is discussed below.

5.3.3.1 Plant Startup. A steam-cooled reactor, because of the physical state of its coolant, must be hot and pressurized before it is made critical. The "reference" condition from which the accident potential is assessed is the hot, pressurized condition with all rods inserted. In this condition, the core has sufficient shutdown margin to assure that it remains subcritical upon cooldown and/or depressurization. Therefore, simple cooldown is not considered to have accident potential. After the reactor is critical, or while it is approaching criticality, care must be used in controlling the steam flow, because increases in steam flow will produce increases in reactivity (by means of the coolant density-reactivity effects). Any large rapid increases in steam flow that might occur while switching the auxiliary boilers in or out of the system or while bringing the turbine on line might produce increases in reactor power that would require a scram. Rod withdrawal accidents were discussed in Section 5.3.1.1.

Core flooding appears to present the greatest hazard of unanticipated criticality. As indicated in Section 5.3.1.2, the reactivity increases as the coolant density increases toward flooded conditions. Although the reactivity calculations are not yet complete, it appears that, at least in the case of the low pressure design, the reactivity at fully flooded conditions is greater than the control rod hold down capability. Both the intermediate and high pressure designs flood the reactor for normal shutdown cooling (as discussed in Sect. 6.2.2), so this operation offers the possibility of a reactivity accident. The quantity of water required for flooding will be large, and the flooding will probably occur at a relatively slow rate. A flooding accident can also be brought about by pumping feedwater into the reactor vessel or the Loeffler boiler without providing sufficient heat to convert it into steam, so that eventually the system can become filled with water and the core flooded. If the volume of the primary system below the level of the bottom of the core exceeds the storage capacity of the feedwater system, the probability of this incident occurring can be reduced. Means for positive assurance that criticality with core flooding cannot occur must be provided in any

SCBR system. For example, the poison injection system should have adequate negative reactivity investment capability, regardless of the source of flooding water.

5.3.3.2 Power Control and Stability. Although some SCBR parameters that affect plant stability, in particular the coolant density-reactivity coefficient, are uncertain and are expected to vary over the lifetime of a core, a control system can be designed that would provide stable plant operation.

If the SCBR plants prove to be inherently stable, control system design requirements are minimal. If the plants are marginally stable, with damped oscillations, the "ease" of control is reduced, control action is required more frequently, and more frequent safety actions may also be expected. Thus far no analyses lead to the conclusion that the SCBR's are inherently unstable. However, if future experiments or analyses show them to have an autocatalytic type of unstable behavior, the reliability requirements on the control and safety systems will be greatly increased from the standpoints of both safety and plant availability. In the absence of continuous automatic or manual control of the flux, the unstable reactor will tend to either go on a diverging excursion with a continually increasing rate, or will shut itself down.

The stability of the high pressure system has not been analyzed, and is difficult to estimate. A characteristic of steam-cooled cores is that they may be subject to oscillatory instabilities for certain combinations of Doppler and density coefficients. Figure 5.14 shows the boundary between the stable and unstable regions for the core alone that was calculated by Karlsruhe⁶¹ for the intermediate pressure design; points for the high and low pressure systems have been added. With a density coefficient of $-0.0116 (\Delta k/k_0)/(\Delta D/D_0)$ and a Doppler coefficient of $-7.2 \times 10^{-6} (\Delta k/k)/^{\circ}\text{F}$, the high pressure core should be stable. If the density coefficient is much larger than that given in Table 5.11, core stability may be marginal.

Overall plant stability is considerably more complicated than core stability. It will be influenced by the ability to balance feedwater and steam flows. Because the coolant is maintained in a supercritical state,

Fig. 5.14. Region of Core Stability.

there is no volume of slightly subcooled liquid that can compensate for any steam flow-feedwater flow mismatch. The design and reliability requirements for a suitable control system are very severe.

A dynamic analysis⁶¹ for the intermediate pressure system was performed at Karlsruhe, as mentioned above. At the design point values of the Doppler and density coefficients, the core, by itself, is inherently stable. However, if uncertainty limits of $\pm 25\%$ on the Doppler coefficient and $\pm 40\%$ on the density coefficient are applied, Fig. 5.14 shows that a region of oscillatory core instability is encountered.

Karlsruhe examined the uncontrolled transient response of the plant, including core, coolant loops, and turbine, to several types of perturbations. These included a $\$0.20$ reactivity step and a 10% turbine load increase. The density coefficient was varied as a parameter. With density coefficients that are negative and smaller in magnitude than $0.022 \Delta k / (\Delta D / D_0)$, the plant appears to be inherently stable for reactivity (or flux) perturbations (with the turbine throttle fixed) and to have stable load following characteristics in which an increase in turbine steam flow causes an increase in reactor power. As discussed in Section 5.3.1, present calculations show that the density coefficients will be negative and fall within this desirable range. These results indicate

that stable plant control is attainable, and the plant characteristics will be suitable for a sophisticated control scheme in which the reactor power is made to follow changes in electrical load imposed on the turbine. However, if the density coefficients should turn out to be positive, the Karlsruhe studies indicate that the plant will have an autocatalytic type of unstable behavior for both flux and turbine load perturbations.

The dynamic response to a variety of system failures, including blower failure, loss of feedwater to one (of six) steam generator, a leak in an outlet steam line, and a main steam line rupture, was also analyzed by Karlsruhe. Again, the density coefficient was treated as a parameter. In most of these cases, the core power initially increased, but at rates sufficiently slow that a scram system which responded in 1 sec would be capable of protecting the core.

The dynamics of the low pressure system were not analyzed. B&W is working on an analysis, but the results will not be available in time for inclusion in the present evaluation study. However, the dynamic behavior of the low pressure system may be estimated by analogy to the intermediate pressure system. Two major differences between the low and intermediate pressure systems are the locations of the circulators (external to the pressure vessel in the intermediate pressure system and integral with the pressure vessel in the low pressure system), and the availability of water for rapid flow change compensation (Loeffler boilers in the intermediate pressure system and excess feedwater flow in the low pressure system). Neither of these differences should result in major differences in dynamic behavior. The density coefficient of the low pressure system is smaller than that of the intermediate pressure system. This reduces the influence of coolant density changes on reactivity, and will probably lead to a greater degree of inherent stability in the low pressure system.

5.3.4 Destructive Nuclear Excursion

The question of whether conditions can be reached that would lead to a destructive nuclear excursion have been raised in the past with respect to analyses of sodium-cooled fast reactors. The question arose because of two factors: one is the very short prompt neutron lifetime typical of fast

reactors, and the second is the presence of several critical masses in the core. The conditions for a nuclear excursion depend on very fast reactivity insertions; however, these do not have to be much larger than prompt critical.

A nuclear excursion could lead to violent disassembly of the core and fission product release. This is meant to differentiate a destructive excursion from a lesser excursion that leads to core disassembly by melting, although the total energy releases may be comparable. A destructive excursion must occur in a time so short that fuel and cladding are vaporized before the core can disassemble. To assess the mechanical damage resulting from such an excursion, the quantity of interest is the energy available to stress the core's high pressure containment system. This, too, is the number that may be related to the energy release in the detonation of a high explosive within the high pressure containment vessel.

Since the energy release is roughly proportional to the mass of fissionable material, the direct extrapolation of the energy release calculations for the smaller Fermi and EBR-II reactors to the 1000-Mw(e) fast reactors would lead to quite large energy releases. Fortunately, the presence of a fairly large Doppler effect in the 1000-Mw(e) oxide fueled fast reactors substantially reduces the energy release to values comparable with those of smaller metal fueled fast reactors that do not have a significant Doppler effect.

5.3.4.1 Possible Causes and Magnitudes. Reactivity insertion rates of the order of $\$100/\text{sec}$ are usually required to produce significant destructive energy releases. Several postulated mechanisms, such as rapid depressurization, control-rod ejection, and core compaction, were examined to see whether they might be capable of producing reactivity-insertion rates of this magnitude.

Rapid depressurization does not present a destructive nuclear problem, since, in the case of the large density coefficient listed in Table 5.11, the coolant density would have to decrease at the rate $1666\%/\text{sec}$ to achieve a reactivity insertion rate of $\$100/\text{sec}$. This would correspond to a complete blowdown in 60 msec.

The intermediate pressure design appears to have the greatest potential for high reactivity rates from rod ejection. The average worth of one of 18 rods is approximately $0.0033 \Delta k$ (or about \$1), and the maximum rod worth is estimated to be approximately \$1.33. If it is conservatively assumed that 75% of the rod worth resides in the central 40% (2.0 ft) of travel, an ejection force can be calculated that will be required to produce a reactivity rate of \$100/sec. The result for a 66-lb (30-kg) rod assembly is 1.2×10^4 lb. To eject all rods (as might happen upon reactor scram plus reversal of core pressure drop), a force of only about 130 lb per rod will induce a reactivity rate of \$100/sec. Thus, it seems inconceivable that the large forces required to produce a destructive nuclear excursion from the ejection of a single rod can be produced in the core. On the other hand, the forces required to produce an explosion from the ejection of all the control rods is fairly small. This will, however, require at least two conditions to exist simultaneously. The rods will have to be free to travel (this condition might exist during the first few seconds following a normal scram) and the pressure differential across the core will have to be reversed from its normal operating condition, as in the case of the intermediate pressure system inlet-line rupture examined in Section 5.3.2.2. The reverse differential pressure for an effective control-rod area of 0.75 in.^2 will have to be about 100 psi.

The above discussion is based on the intermediate-pressure concept. The high and low pressure concepts are quite different in that the numbers of rods (108 and 124, respectively) are so much greater. The worth per rod is lower; and the probability of ejecting all rods is less because the rods are not directly exposed to the core differential pressure.

The classical cause of a destructive nuclear energy release is core compaction by gravity collapse following a meltdown. The Karlsruhe design report states that this mechanism may lead to an energy release of 1000 Mwsec. A survey^{67, 68} of meltdown studies for large fast reactors with steam and sodium coolants shows that reactivity rates from gravity compaction range from \$50/sec to \$250/sec, with \$100/sec being typical, and the energy releases range from 400 to 2100 Mwsec. There seems to be a considerable difference in the assumptions and models used in these calculations.

Detailed study is needed for the SCBR concepts to establish an upper limit on the energy release from this mechanism.

Other mechanisms must be considered before the question of the destructive nuclear excursion can be resolved. These might include such things as the core dropping intact into the lower parts of the pressure vessel, or a small local explosion causing rapid compaction of the remainder of the core. Since the coolant is already in the gaseous phase, the SCBR systems will not have two of the problems postulated^{69,70} for sodium-cooled fast reactors; namely, reactivity increase and core compaction caused by violent localized boiling of the coolant.

These discussions indicate that it will be difficult to establish either the credibility of a destructive nuclear excursion or an upper limit on the energy release. For this evaluation, a "typical" upper limit on the reactivity insertion rate may be taken as \$100/sec for the SCBR. A crude extrapolation of Gulf General Atomic calculations⁷¹ for a somewhat similar gas-cooled fast reactor core shows that this reactivity rate will produce an energy yield of approximately 1600 Mwsec for the SCBR conditions of about 4000 kg of fissile material, and a Doppler coefficient of about 0.011 $\Delta k/^\circ K$. This "typical" value of approximately 1600 Mwsec is only illustrative, and more careful study is needed to determine a realistic upper limit.

5.3.4.2 Design Features to Contain Excursion. The capabilities of the primary reactor vessels to withstand explosive energy release are examined in Section 6.1. The low, intermediate, and high pressure system vessels appear to be able to withstand explosions equivalent to 1000-, 2000-, and 2600-Mwsec energy yields, respectively. Thus, the vessels in the intermediate and high pressure designs appear to be able to withstand the postulated "typical" upper limit energy release of 1600 Mwsec corresponding to a reactivity insertion rate of \$100/sec, but the vessel of low pressure design appears to be marginal or inadequate based on these assumptions.

Even if the primary system is not breached by the shockwave of destructive energy release, all the fission products will probably be released from the fuel rods. It will then be necessary to contain and cool the residual fission products dispersed in the vessel. Provisions have not been made

in the designs for this contingency. Removal of the heat generated by the residual fission products may be particularly difficult. It must be assumed that spray rings or other coolant injection equipment will be seriously damaged and incapable of performing as designed. Therefore, in the final designs, consideration should be given to the provision of a protected emergency cooling system.

In summary, the credibility of a nuclear explosion should be determined, since the need for protection in the event of an explosion can lead to considerable differences in containment design. If an explosion is considered credible, an upper limit on the energy release should be established, and provisions should be included in the reactor design for cooling following the explosion. A consistent approach to this problem should be developed and used for the various types of fast reactors. For example, in an effort to prevent fuel compaction in the lower regions of the core, the fuel of the GCFR design is suspended from a thick upper grid, and there is no lower grid. There is also a core divider in the plenum below the reactor.

5.3.5 Problems Associated with a Core Meltdown

The high power densities and low heat capacities of large fast reactor cores make it difficult to defend the approach presently used in large light water thermal reactor designs that core meltdown can be prevented. All the fast reactors have the possibility of a meltdown. It is more difficult to insure core cooling in the steam-cooled and gas-cooled concepts than in the sodium-cooled reactors; however, if cooling is lost, the sodium-cooled reactors will heat up at a much higher rate than the other two types.

A core meltdown presents several potential problems in addition to the destructive nuclear excursion that may have to be dealt with. It may lead to a "containment melt-through" phenomenon that is presently being considered⁷² as a possibility in large water-cooled reactors. The potential problem will be greater in fast reactors than in thermal reactors because the fast reactors have a much higher power density and, also, the molten fast reactor fuel may move into a critical configuration and produce fission

power in addition to that produced by afterheat. The containment melt-through phenomenon should be carefully examined, and if it is considered to be credible, methods should be developed for fast reactors and added to the design for "catching" and cooling the molten fuel. This should be done on a basis consistent with that considered for other types of fast reactors. If a reliable method could be developed for safely containing molten fuel, it would represent a more basic and more satisfactory approach than placing complete dependency on complex emergency core cooling systems for the preservation of containment integrity. The intermediate pressure design is the only one of the three SCBR concepts that includes provisions for catching the core. Depressions in the lower shielding block are intended to spread out the molten fuel and allow it to mix with the layers of steel. It is not clear that this approach will be effective.

Other areas of uncertainty regarding a core meltdown that may require resolution⁷² are steam explosions from large quantities of molten fuel falling into pools of water, metal-water reactions, and hydrogen generation.

5.3.6 Summary and Conclusions

The SCBR concepts appear to be stable and to have characteristics that are suitable for a sophisticated load following control system. Either loss of coolant or core flooding can make large reactivity additions, and a fast scram will be essential to preventing a core meltdown. A reasonably fast scram can cope with fairly large reactivity insertion rates, including those caused by the most rapid loss-of-coolant accident examined. It will be necessary to add poison injection systems for flooded conditions where the reactivity addition exceeds the worth of the rods.

Emergency cooling will also be essential to preventing core meltdown. This will be one of the more difficult problems, since the high power density and low heat capacity lead to rapid core heatup on loss of cooling. This gives a very short time margin, ranging from less than a second to a few seconds, in which to restore cooling to prevent a large amount of cladding failure.

The questions of the credibility of a destructive nuclear excursion and a containment vessel melt-through should be resolved. The emergency equipment provided in the designs to cope with the safety problems is described in Chapter 6, and the adequacy of the engineered safety features is discussed in Section 6.8.

6. ENGINEERING REVIEW OF PLANT DESIGNS

The designs of the plant systems and components were reviewed to evaluate the performance conditions specified, and to make appropriate adjustments in design parameters where such adjustments appeared to be necessary for consistency. The evaluated design conditions were then used as bases for capital cost estimates. Another purpose of the review was to identify potential problems in achieving the performance requirements or in the manufacture of special components and to thereby identify research and development requirements for the plant components.

The reference design for the high pressure plant was given in two reports,^{3,4} the first of which gave details on plant systems for an earlier supercritical SCBR that had a mixture of light and heavy water for spectral-shift control. While this design served as basic information for plant components, there were a number of changes to be made in performance conditions for the plant components to agree with the flow sheet and conditions of the second report, which gave the reactor design requirements for the present evaluation study.

The Karlsruhe report¹⁰ described the entire design for the intermediate pressure system, and relatively minor changes were made as the result of the evaluation study. Information on the reactor characteristics and plant design for the low pressure system was furnished for evaluation during the course of the design study on this reactor, which was performed by Babcock & Wilcox and Sargent & Lundy.⁷

In comparing the three plants, it was apparent that differences in design philosophy accounted for as much or more difference in the three designs than did the difference in steam conditions. The intermediate pressure design **was** more conservative than the other two in the thermal performance of the reactor and in the design of the external heat transfer and steam circulation system. The high pressure design **had** a more compact layout than the other two. Further differences among the three concepts **were** in the containment system designs, the emergency cooling systems designs, and in the turbine-generator selections.

For a truer comparison of economics based on differences due to steam conditions alone, it would be necessary to redesign the concepts to

equivalent criteria for each of the plant systems. A major redesign was beyond the scope of this evaluation study; however, several moderate adjustments were considered. All plants were assumed to have turbine drives for the main circulators; the drives for the high pressure design had been variable-speed electrical motors. A single 1000-Mw(e) (net) tandem compound turbine-generator was assumed for each design, and the units specified for intermediate and high pressure systems were changed accordingly. The layouts were revised to obtain closer normalization; the reactor building for the high pressure system was increased in size, the plant layout for the intermediate pressure system was reduced in size, and a rearrangement of the site layout for the low pressure system was assumed to reduce the length of circulating water lines. An emergency cooling system for the high pressure plant was added because none had been specified. These changes were made as a step toward normalizing the designs, but detailed design normalization studies would probably suggest other more significant changes.

6.1 Reactor Vessels and Internal Structures

The SCBR systems can make use of pressure vessel manufacturing technology developed for light water reactors. The physical dimensions of the three SCBR pressure vessels were not much different than a pressure vessel for a 1000-Mw(e) PWR plant. However, because of the higher operating temperatures and pressures of the intermediate and high pressure SCBR designs, wall thickness requirements were greater than the maximum thicknesses developed for either PWR or BWR reactors. On the other hand, the vessel wall thickness for the low pressure system was well within the range of thicknesses of the vessels currently being manufactured. Another important difference between SCBR and light water reactor vessel requirements is the high intensity fast neutron flux in the SCBR, which requires massive thermal shields inside the vessel to protect the walls from excessive neutron exposure.

The vessels of the low and high pressure designs further departed from conventional light water reactor vessel design by using welded construction rather than bolted flanges for securing the head to the vessel

cylinder. Further, the control rods (discussed in Sect. 6.3) were assembled with the fuel elements in the low and high pressure designs, and were actuated by a steam pressure differential. In PWR or BWR designs, mechanically actuated control rods are used, and the rod drives are installed through penetrations in the top or bottom of the reactor. The vessel and control rod system designs for the intermediate pressure reactor were more conventional, by light water reactor standards, in that the vessel had a bolted head, and the control rods had rack-and-pinion drives, but they, too, varied from light water reactor designs in a number of details.

6.1.1 High-Pressure System

The reactor vessel and internals were arranged so that coolant steam entered and left through nozzles in the lower hemispherical head of the vessel. Coolant steam first flowed upward through the radial blankets and thermal shields in parallel paths into the large steam space above the core. From there the coolant passed downward through the core, where it was heated to average bulk temperatures of 1000°F in the inner two zones and 950°F in the outer zone (see Fig. 6.1).

The lower grid structure contained thermal baffles for separation of the two different temperature streams. These two streams left the reactor in separate piping systems to supply the 1000°F fluid to the high pressure turbine and reheater, and the 950°F fluid to the bypass recirculating system.

The core contained 108 bottom-actuated control rods, each of which has its individual hydraulic command line. Thus, there were 108 penetrations through the vessel wall for these lines, which were approximately 1/2 in. in diameter.

The actuation of control rods from the underside of the reactor affords free access to the top of the reactor core for changing fuel elements. This is especially important in the proposed refueling method in which offset mechanical arms are operated through small ports in the top head of the vessel.

One of the more distinguishing features of the reactor vessel was its lack of a conventional full opening bolted closure. The proposed throughport refueling system does not require head removal for replacing

Fig. 6.1. Cross Section of Vessel for High-Pressure Steam-Cooled Breeder Reactor.

spent fuel elements and control rod drives; hence, head removal would be necessary only for grid-plate removal. Five relatively small nozzles were provided in the top head for refueling access.

Grid plate removal and replacement would be a major operation in that both the reactor vessel head and the containment vessel head would have to be cut and rewelded. Postweld heat treatment would be required for both vessels. It was proposed that the startup boiler be used as a means of heating the reactor vessel to 1000°F for this purpose. This temperature is 100°F less than the usual minimum heat treatment temperature of 1100°F, at which a holding time of 1 hr per inch of thickness is required. The ASME Code⁷³ requires a minimum holding time of 3 hr per inch of thickness at the lower (1000°F) temperature. A more conventional means of heat treatment would have to be used for the containment vessel. The Code is less specific regarding preheating of the metal before re-welding; however, good judgement would probably dictate that preheating be used for both vessels because of the type and thickness of material to be welded, and because of radiation induced embrittlement after prolonged neutron exposure.

For grid plate removal and replacement after the reactor vessel top head had been removed by cutting, the vessel internals, including thermal shields, would be removed to make the grid plate accessible for removal. Removal operations would be performed with the vessel flooded with water for shielding.

The reactor vessel design⁴ included two radial thermal shields of 1 3/4- and 2 1/2-in. thickness. Discussions with B&W personnel after the design report was issued brought out the fact that since radiation induced embrittlement of the vessel cannot be effectively annealed out by use of the startup boiler for supplying high temperature steam, it was decided that a third 2-in.-thick thermal shield should be added in order to keep the fast neutron ($E > 1$ Mev) exposure to less than 1×10^{20} neutrons/cm². ORNL calculations, however, showed a maximum exposure of 1.4×10^{20} neutrons/cm² at the vessel walls. Exposures for top and bottom heads were below 3×10^{19} neutrons/cm². Permissible exposure values are discussed in Section 6.1.4.2.

The inside diameter of the vessel was increased from 158 to 166 in. to accommodate the third thermal shield. This, together with a 4-in. increase in jacket thickness, gave a new outside diameter of 206 in. for the reactor vessel. The outside diameter was previously 190 in.

The reactor vessel was cylindrical with hemispherical heads. The cylindrical portion was made in two layers. The inner cylinder and heads were each 10 in. thick. A thickness of 6 in. was proposed⁴ for the outer cylinder of the jacket. Material for the basic vessel and jacket was not specified. For the specified design pressure of 4100 psig, it was determined that for the given vessel design, Section III of the ASME Boiler and Pressure Vessel Code requires a material having a minimum design stress intensity value of 18,250 psi for the basic vessel material, and 30,400 psi for the jacket material at the design temperature of 800°F. Subsequent discussions with B&W personnel revealed that SA 302, grade B, steel was the intended construction material for both the basic vessel and the jacket. Code Case 1331 (Ref. 74), which covers requirements for nuclear vessels in high temperature service, refers to Section I of the Code⁷⁵ for allowable stresses. Section I gives an allowable stress of 19,100 psi at the vessel design temperature of 800°F. The allowable stress of 19,100 psi satisfies requirements for the basic vessel, but obviously is not adequate for a 6-in.-thick jacket. Accordingly, the jacket thickness was increased to 10 in.

The requirement for a 20-in. wall thickness ruled out the possibility of using a single wall vessel, since present-day fabrication facilities are limited to producing single-wall thicknesses of about 14 in. The two-layer design consisted of a basic vessel with a slight taper machined on the outside that matched a taper machined on the inside of the jacket. The jacket was made up of several cylindrical sections that would be locked together in the final assembly.

Although numerous multilayer vessels have been built for nonnuclear service, experience is very limited in the design and fabrication of such vessels for nuclear service in which the requirements of Section III of the ASME Boiler and Pressure Vessel Code must be met. In most methods of multilayer vessel construction, the usual point of major concern in satisfying Section III requirements is the fact that the welds in the

various layers cannot be satisfactorily radiographed. It appears that in the case of a two layer vessel assembled as proposed, this shortcoming can be avoided by completion of all weld inspection, including radiography, before the jacket is applied over the basic cylinder.

The proposed vessel design is believed to be feasible. However, because of the wall thickness and diameter involved and because of the two layer construction, it is felt that some extrapolation of present-day fabrication techniques would be necessary. B&W acknowledges that detailed manufacturing and assembly procedures for this particular design have not been fully developed. A list of vessel parameters is given in Table 6.1.

6.1.2 Intermediate Pressure System

The reactor vessel for the intermediate pressure system **was** cylindrical and **had** hemispherical heads. It **had** a large bolted closure at the top, unlike the other two designs, and **was** supported by a continuous skirt at the bottom, as shown in Fig. 6.2. Steam **was** supplied to and removed from the reactor through concentric polar pipes at the bottom of the vessel. Nozzles **were** provided in the removable top head for control rod drives, for supplying the internal water spray, and for a flooding vent line. Dimensions and other pertinent data relative to the reactor vessel and internals are given in Table 6.2.

Steam **entered** the vessel through the annulus between the concentric steam lines at the bottom. It then divided into parallel paths and flowed upward outside the core to cool both the radial blanket and the radial thermal shielding. The parallel streams combined at the top of the vessel before flowing downward through the core. The superheated steam left the reactor vessel through the center of the concentric pipes at the bottom.

The fissile reactor core **was** enclosed radially and axially by a fertile blanket region. The vessel wall and heads **were** heavily shielded by multilayered thermal shielding with a total metal thickness of 1.77 ft. According to ORNL calculations, the top of the vessel would receive the greatest neutron exposure, which was found to be 2.45×10^{18} neutrons/cm² for fast ($E > 1$ Mev) neutrons in a 30-year lifetime. The corresponding exposure for sides of the vessel is 1.6×10^{18} neutrons/cm². The

Table 6.1. Parameters of Pressure Vessel and Internals for High-Pressure System

Vessel description	Two-layer cylinder with hemispherical heads
Vessel material	SA 302, grade B, steel
Liner material	Type 308 stainless steel
Liner thickness, in.	0.25
Cylinder wall thickness, in.	20 ^a
Head thickness, in.	10
Vessel height, inside heads, ft-in.	34-3
Vessel inside diameter, ft-in.	13-10
Weight of vessel empty, lb	1,404,000 ^b
Weight of vessel full, lb	1,710,000 ^c
Removable head	No
Insulation location	Outside vessel
Operating pressure at inlet, psia	3700
Operating temperature at outlet, °F	
Outer region	950
Inner region	1000
Operating temperature at inlet, °F	750
Design pressure, psig	4100
Design temperature, °F	800
Core shape	Hexagonal annulus
Active core dimensions, in.	
Equivalent inside diameter	56.6
Equivalent outside diameter	94.2
Equivalent height	57
Inner radial blanket dimensions, in.	
Equivalent inside diameter	19.2
Equivalent outside diameter	56.6
Outer radial blanket dimensions, in.	
Equivalent inside diameter	94.2
Equivalent outside diameter	128.2
Upper axial blanket height, in.	18
Lower axial blanket	None
Composite core and blanket overall size, in.	
Diameter	128.2
Height	74
Thermal shield location	Inside vessel
Thermal shield material	Type 304 stainless steel
Inner thermal shield	
Inside diameter, in.	142.0
Height, in.	~245
Thickness, in.	1.75
Weight, lb	~54,800
Intermediate thermal shield	
Inside diameter, in.	149.5
Height, in.	~245
Thickness, in.	2.5
Weight, lb	~82,700
Outer thermal shield	
Inside diameter, in.	158.5
Height, in.	~245
Thickness, in.	2.0
Weight, lb	~69,900
Vessel penetrations	
Coolant inlet	Three of 16 in. diameter
Coolant outlet	Five of 14 in. diameter
Refueling access	One of about 14 in. diameter, four of about 8 in. diameter
Control-rod drives ^d	108 of about 1/2 in. diameter.

^aIn two layers, each 10 in. thick.^bIncludes approximately 210,000 lb for thermal shields.^cIncludes loaded core.^dIndividual shim-safety rod hydraulic command lines.

Fig. 6.2. Vertical Cross Section of Vessel for Intermediate Pressure Steam-Cooled Breeder Reactor.

Table 6.2. Parameters of Pressure Vessel and Internals of Intermediate-Pressure Reactor

Vessel description	Multilayer cylinder with hemispherical heads
Vessel material	ASTM A508, class 2, steel (SA 302, grade B, steel) ^a
Vessel height, ft	39.37
Cylinder wall thickness, in.	12.2 (15.1) ^b
Head thickness, in.	7.48 (7.6)
Vessel outside diameter, ft	17.78 (18.26)
Type of cladding	Welded
Cladding material	Type 308 stainless steel
Cladding thickness, in.	0.236
Weight of vessel empty, lb	1,191,000 (1,442,000)
Removable head	Yes
Insulation location	Outside vessel
Operating pressure, psia	
Inlet	2675
Outlet	2500
Operating temperature, °F	
Inlet	689
Outlet	1004
Design pressure, psig	3381 (2950)
Design temperature, °F	752
Number of inlet and outlet pipes	One each, coaxial at bottom
Outside diameter of coaxial pipe, ft	5.9
Diameter of outlet pipe, ft	
Inside	3.28
Outside	4.33
Diameter of inner collecting sphere, ft	7.22
Diameter of inner distribution sphere, ft	7.54
Core base plate dimensions, ft	
Diameter	11.61
Height	2.36
Material of core base plate	Type 316 stainless steel
Core shape	Cylinder
Active core dimensions, ft	
Equivalent outside diameter	8.62
Equivalent height	4.95
Outer radial blanket dimensions, ft	
Equivalent inside diameter	8.62
Equivalent outside diameter	11.12
Axial blanket thickness, ft	
Upper	2.09
Lower	1.11
Overall core and blanket size, ft	
Diameter	11.12
Height	8.15
Thermal shield location	Top, bottom, and sides
Thermal shield material	Layers of steel and steam
Thickness of metal layers of radial thermal shield, ft	1.77
Total thermal shield weight, lb	~1,336,000

^aValues and information in parentheses represent changes recommended by ORNL in this evaluation.

^bIn two layers, each 7.55 in. thick.

exposure for the bottom of the vessel was found to be several orders of magnitude less than for the top and sides. These calculated values are less than the reported value of 3×10^{19} neutrons/cm² and are considerably lower than exposures for the B&W reactor vessels.

The weight of core and blanket plus the weight of the radial and lower thermal shielding was transferred to the bottom head of the vessel. The upper axial thermal shielding was supported from and removed with the upper head of the vessel.

The cylindrical wall of the vessel was designed for laminar construction. It was composed of 30 layers, the innermost of which was a 0.79-in. (20-mm)-thick layer of austenitic stainless steel. The remaining 29 layers were composed of a material bearing a DIN designation of "20 NiMoCr 3/6." The chemical composition of the material was given in Table 6.2-1 of the ORNL translation of Ref. 10. It was a vacuum treated (for hydrogen removal) steel that corresponded in chemical composition to that given in the specification for ASTM A 508, class 2, steel, entitled "Quenched and Tempered Vacuum Treated Carbon and Alloy Steel Forgings for Pressure Vessels." Although ASME Code Case 1332 (Ref. 76) rules that this is an acceptable material for use in Class A nuclear vessels, the ORNL evaluation is based on the use of SA 302, grade B, steel for the following reasons:

1. ASTM A 508 is a forging specification, whereas SA 302 is a plate specification. (Forgings are, of course, acceptable for nozzles, etc., but the basic vessel material must be plate.)

2. Code Case 1332 rules that design stress intensity values for ASTM A 508, class 2, steel shall be the same as for SA 302, grade B, steel.

3. Code Case 1236 (Ref. 77), which applies to nonnuclear vessels and Class B and C nuclear vessels, rules that ASTM A 508, class 2, material may be used only for service temperatures not exceeding 650°F, and it is believed that this temperature limitation should also apply to a Class A nuclear vessel. The design temperature for the vessel under consideration is 752°F (400°C) and, thus, exceeds the specified temperature limit.

4. Normalization of costs is improved if the same material (SA 302, grade B) is used for the Karlsruhe reactor as for the two B&W reactors.

The translated report¹⁰ stated that the "design of the reactor pressure vessel was based on German regulations, the AD-Circulars, [and] steam boiler regulations, as well as the DIN specifications. When these regulations and specifications were not sufficient, use was made of the American Boiler and Pressure Vessel Code, Section III, published by the American Society of Mechanical Engineers (ASME)." ORNL made calculations to determine whether the vessel wall thickness of 12.2 in. (310 mm) is adequate by U.S. standards for the stated design conditions. When limited by the design stress intensity values permitted under Section III and Code Case 1331, the wall thickness required to satisfy the stated design conditions of 3380 psig and 752°F is 17.48 in. It thus appears that the reported 12.2 in. is 30% less than required by the ASME Code, Section III. It is noted, however, that Karlsruhe used a very generous design pressure margin.* For instance, the Karlsruhe design pressure margin was 27.1%, whereas B&W used a margin of 10.8% in their high pressure reactor vessel design. Section III of the ASME Code merely states that a "suitable margin" above normal operating pressure is recommended to provide for probable pressure surges up to the setting of pressure relief devices.

In order to put the Karlsruhe and B&W designs on an equal basis, the 10.8% margin used by B&W was applied to the Karlsruhe design. On this basis, the required minimum wall thickness was determined to be 15.07 inches. The estimated cost of the Karlsruhe vessel given in this report is based on this adjusted wall thickness and the use of SA 302, grade B, steel.

The U.S. designer of a nuclear vessel that must operate at a temperature higher than the maximum temperature for which design stress intensity values are given in Section III is penalized in that he cannot avail himself of the higher value of design stress intensity of Section III as compared with the lower allowable stress value of Section VIII (or Section I) for the same material. This penalty is imposed simply because design stress intensity values have not been established for the

$$\text{*Design pressure margin} = \frac{\text{Design pressure} - \text{Operating pressure}}{\text{Operating pressure}} .$$

higher temperatures. At these higher temperatures, Code Case 1331 directs the designer to Section I or Section VIII (Ref. 78), from which the allowable stresses are to be taken and applied to the nuclear vessel design in place of the design stress intensity value of Section III. This is illustrated for the case of SA 302, grade B, material in Fig. 6.3, where the design stress value suddenly drops as the temperature exceeds 700°F.

There is hope that one of the newer steels, ASTM A 542, for example, will soon be approved for nuclear service in the 700 to 900°F temperature range. This material is now under consideration by the Code Committee for Section III use. It is a quenched-and-tempered chrome-moly steel of high strength, with good stress rupture and creep properties. As compared with A 302, grade B, material, A 542 steel has a lower initial nil-ductility transition (NDT) temperature, and a lower rate of increase in NDT with radiation exposure.

Nuclear Technology Corporation investigated⁷⁹ the ability of the pressure vessel to withstand an accidental nuclear excursion. The results

Fig. 6.3. Design Stress Versus Temperature for A 302, Grade B, Steel.

of their investigation show that a vessel, with a wall thickness of 12.2 in., as specified in the Karlsruhe design, is capable of withstanding a nuclear excursion equivalent to the detonation of 1000 lb of TNT. This is predicated, however, on the qualification that ductility of the vessel material is not degraded by irradiation or for other reasons during operation to an ultimate strain (elongation) of less than 7.5%. Data appear to be rather limited on ultimate strain in the material of interest as a function of neutron exposure, especially at operating temperature. Although further investigation should be made into ultimate strain properties of the vessel steel, the reactor vessel design is probably adequate from an excursion containment standpoint.

A portion of the Karlsruhe report was devoted to analysis of transient thermal stresses, called "nonstationary stresses" in the translation, particularly in the heavy section region of the closure flanges. The flange bolting was designed for use of a hydraulic bolt tightening system. The system consisted of 24 hydraulic units, one for each bolt, to which pressure was applied simultaneously from a central hydraulic power unit.

Design of the Karlsruhe reactor vessel appears to be feasible in terms of present-day fabrication capabilities. If such a laminated vessel were proposed for nuclear service in this country, however, there would be concern regarding fulfillment of radiography requirements under Section III of the Code. This uncertainty would prevail despite the fact that the laminated Saxton reactor vessel has received the necessary approvals. In the case of Saxton, it is argued that its design satisfies the intent if not the letter of the Code. For vessels with a wall thickness exceeding 14 in., a more expeditious approach would probably be a two layer design similar to that proposed by B&W for their high pressure reactor vessel; therefore, for the purpose of estimating costs, ORNL assumed two layer vessel walls of SA 302, grade B, material.

6.1.3 Low Pressure System

The reactor vessel for the low pressure reactor was cylindrical and had elliptical heads. The vessel and its internals constituted a unique integral design in which the main steam generator and steam circulator impellers were contained within the reactor vessel, as shown in Fig. 6.4.

Fig. 6.4. Vertical Cross Section of Vessel for Low-Pressure Steam-Cooled Breeder Reactor.

The vessel did not have a removable head. Access for refueling was provided by five nozzles in the top head of the vessel, the largest of these nozzles being the 24-in.-diam. manipulator port. Dimensions and other reactor vessel information are given in Table 6.3.

Feedwater entered the vessel through four nozzles in the bottom head and flowed to a manifold in the steam-generator section beneath the reactor core. The feedwater was sprayed into the direct contact steam generator and mixed with a portion of the superheated steam leaving the down-flow core. The saturated steam thus produced flowed upward between layers of thermal shielding located inside the reactor vessel and outside the core tank. The steam passed through a multiplicity (212) of cyclone separators, arranged in parallel, to reduce the wet steam to dry saturated steam before it reached the inlet to the integral circulators. Steam was discharged from the circulators into a large plenum above the core. The steam then passed downward through the core. The 40,000,000 lb/hr of superheated steam leaving the core was divided at the core outlet so that 10,870,000 lb/hr left the vessel to drive the main high pressure turbine and the circulator drive turbines. The remaining 29,130,000 lb/hr entered the steam-generator section below the core, and supplied heat to the feedwater spray, produced saturated steam which, as described above, was recirculated to the circulator inlets, and cooled the thermal shielding on its way upward inside the vessel.

A core tank enclosed the core and rose to near the top of the reactor vessel. This tank was subjected to positive internal differential pressure of 125 psi, which was provided by the integral steam circulators. Suction to the circulators was taken from outside the core tank, but discharge from the circulator impellers occurred inside the core tank. In this arrangement the impeller was installed just inside the core tank. The core tank had a bolted cover, and access to the cover was gained only by cutting and removing the heads of both the high pressure containment vessel and the reactor vessel. An emergency spray header was provided just inside the top of the core tank.

Seventeen nozzles penetrated the bottom head of the reactor vessel for steam outlet (6), feedwater inlet (4), feedwater drain (2), separator drain (2), vent (2), and control rod drive manifold (1). The sizes

Table 6.3. Parameters of Low Pressure Reactor Vessel and Internals

Vessel description	Cylinder with elliptical heads
Vessel material	SA 302, grade B, steel
Cladding material	Stainless steel ^a
Vessel inside diameter, ft-in.	19-8
Vessel height inside heads, ft-in.	51-0
Cylinder wall thickness, in.	7b
Head thickness, in.	7b
Cladding thickness, in.	0.25
Weight of vessel empty, lb	1,000,000
Removable head	No
Insulation location	Outside vessel
Feedwater inlet pressure, psia	1200
Feedwater inlet temperature, °F	521
Circulator inlet pressure, psia	1125
Circulator inlet temperature, °F	553
Core inlet pressure, psia	1250
Core inlet temperature, °F	585
Core outlet pressure, psia	1050
Core outlet temperature, °F	925
Vessel design pressure, psig	1400
Vessel design temperature, °F	700
Core shape	Cylinder
Active core dimensions, in.	
Equivalent outside diameter	141.1
Height	29
Radial blanket dimensions, in.	
Equivalent inside diameter	141.1
Equivalent outside diameter	170.8
Axial blanket height, in.	
Upper	18
Lower	18
Composite core and blanket overall size, in.	
Diameter	170.8
Height	65
Thermal shields (radial)	
Location	Inside vessel
Material	Type 304 stainless steel
Number required	3
Thickness, each, in.	2.25
Total weight, lb	367,000
Core support grid	
Diameter, ft-in.	16-1
Depth, in.	40
Material	Type 304 stainless steel

^aType not specified.

^bIncludes cladding.

Table 6.3 (continued)

Core tank	
Design pressure, psig	200
Design temperature, °F	950
Maximum inside diameter, ft-in.	16-3
Height, ft-in.	30-3
Weight, lb	261,000
Wall thickness (including cladding, both inside and outside), in.	3
Basic material	SA 302, grade B, steel
Cladding thickness, each side, in.	0.25
Cladding material	Stainless steel ^a
Head outside diameter, ft-in.	15-8
Head depth, inside, in.	29
Number of head penetrations	5
Head thickness, including cladding on both sides, in.	6
Head material	SA 302, grade B, steel
Head cladding thickness, each side, in.	0.25
Head cladding material	Stainless steel ^a
Cyclone separators	
Number required	212
Material	Stainless steel ^a
Steam circulators	
Number required	4
Impeller diameter, in.	24.8
Speed, rpm	10,000
Feedwater manifolds	
Number required	2
Inside diameter, in.	10.5
Material	Type 304 stainless steel
Vessel penetrations	
Steam outlet	Six of 16 in. ID
Feedwater inlet	Four of 7.5 in. ID
Feedwater drain	Two of 3.25 in. ID
Separator drain	Two of 3 in. ID
Vent	Two of 3.25 in. ID
Control rod drive manifold	One of 17 in. ID
Refueling (viewing, etc.)	Four of 10 in. ID
Manipulator port	One of 24 in. ID
Steam circulator	Four of 28 in. ID

of these various nozzles are given in Table 6.3. Inside the steam outlet nozzles were venturi-type flow restrictors for limiting the rate of coolant escape in the event of rupture of a steam line outside the vessel. Refueling ports in the top head had special double sealing closures designed with locking rings to prevent accidental expulsion of the closure plugs even in the event of bolt failure.

The design pressure for the reactor vessel was 1400 psig, and the design temperature was 700°F. The vessel material was SA 302, grade B, steel. The inside diameter was 19 ft 8 in., and the specified wall thickness was 7 in., including 1/4 in. of stainless steel cladding. According to Section III of the ASME Code, for these design conditions a minimum wall thickness of 6.35 in. is required. With no credit taken for the strength of the cladding, the available thickness of 6.75 in. of SA 302, grade B, material is adequate to meet Code requirements.

ORNL calculations of neutron exposure for the reactor vessel show that the maximum exposure occurs in the wall of the vessel. The fast ($E > 1$ Mev) neutron exposure in the vessel wall for a 30-year lifetime is 2.6×10^{20} neutrons/cm². The corresponding maximum exposure for top and bottom heads of the vessel is 4.7×10^{19} neutrons/cm². Although such exposures are higher than the desired maximum of 3×10^{19} neutrons/cm², no attempt was made in this evaluation study to redesign the reactor vessel and internals for increased thermal shielding. Capital costs, therefore, are based on the vessel design as proposed.

Access to internal members of the vessel is extremely limited, except that special provision is made for performing the normal refueling operations, and the design provides for withdrawal of circulator impellers for maintenance. The intent, of course, is that the few other functioning parts, such as feedwater spray heads, located beneath the core be of such design as to insure the utmost reliability. Removal of the core support grid would be a major operation requiring removal, by cutting, of both the high pressure containment vessel head and the reactor vessel head and removal of the entire core tank.

The proposed reactor vessel design is believed to be feasible. There are, however, features of the integral design that would require some developmental effort. For instance, the steam generator and circulators

are certain to require development. Also, seals for penetration of the 7.5-in.-diam. circulator drive shaft through the vessel wall will require development. The steam generator and circulators are discussed further in Section 6.2.

6.1.4 Reactor Vessel Evaluation Summary

6.1.4.1 Vessel Manufacture. The proposed SCBR vessels are considered feasible to build in present day manufacturing facilities. However, for construction of the two layer vessels of the intermediate high pressure designs, some development of fabrication techniques and detailed assembly procedures would be required.

The low and high pressure vessels had welded heads that would require assembly and postweld heat treatment in the field to avoid damage to vessel internals during shipment. Elimination of the bolted flanges reduces the cost of the vessels, but complicates the initial assembly and later access into the vessel if replacement of internals becomes necessary.

6.1.4.2 Fast Neutron Exposures. The maximum integrated fast neutron ($E > 1$ Mev) dose to the three reactor vessels was calculated to be 1.4×10^{20} neutrons/cm² for the high pressure design, 2.5×10^{18} neutrons/cm² for the intermediate pressure design, and 2.6×10^{20} neutrons/cm² for the low pressure design for the 30-year life of the vessels. Present design practice for nuclear pressure vessels of SA 302, grade B, steel (which was assumed for all three concepts) is to limit the fast neutron dose to about 2×10^{19} to 4×10^{19} neutrons/cm² over the life of the reactor vessel. Apparently there is no concern about thermal neutron exposure.

The neutron irradiation criterion for SA 302, grade B, steel is based on an increase in nil ductility transition (NDT) temperature with increasing exposure to fast neutrons. Since the initial NDT temperature of SA 302 is close to room temperature, the neutron exposure is limited to that which increases the NDT temperature by 200 to 300°F.

The present criteria for pressure vessels are largely based on BWR and PWR operating conditions. The higher operating temperatures of the SCBR vessels may permit greater exposures than are permitted with present

criteria; however, analyses and tests of SA 302 steel under SCBR operating conditions would be required before the dose of 10^{20} neutrons/cm² proposed for the high pressure design could be considered acceptable.

It is possible that after further development higher dose rates can be tolerated under SCBR conditions; however, if present day BWR and PWR criteria are to be satisfied, the vessels for the low and high pressure reactors will require additional thermal shielding. The intermediate pressure design is below an acceptable exposure limit, and the thicknesses of the shields could be reduced somewhat. However, the required shield thicknesses for a 2×10^{19} - to 4×10^{19} -neutrons/cm² exposure for the intermediate pressure vessel were not calculated.

6.1.4.3 Destructive Nuclear Excursion Containment Capability. The maximum hypothetical accident for the SCBR could involve a nuclear excursion with a sudden release of energy. The magnitude of this release has not been completely analyzed for these concepts, but as indicated in Section 5.3.4, energy releases of the order of 1000 to 1600 Mwsec (or 500 to 800 lb of TNT) have been postulated. Both the low and high pressure systems had close fitting containment vessels around the reactor vessel to provide additional protection against the consequences of an excursion. The intermediate pressure design depends on the reactor vessel alone.

In the Karlsruhe report on the intermediate pressure system it was claimed that the reactor pressure vessel under operating pressure could absorb the energy from an excursion of 1400 Mwsec (700 lb of TNT). Calculations performed by Nuclear Technology Corporation⁷⁹ with the methods outlined by Wise and Proctor⁸⁰ confirm the Karlsruhe prediction. It appears that the vessel is capable of absorbing the energy from an excursion in excess of 1000 lb of TNT. It should be noted that the ability of the vessel to absorb the energy from an excursion is relatively independent of whether the internal pressure is atmospheric or operating. The bulk of the energy is absorbed by the vessel through plastic deformation, whereas vessel design practice limits operating stresses to the elastic region.

The containment capability of the intermediate pressure system was plotted against the ultimate strain of the vessel material in Fig. 6.5. Two sets of curves are shown: one corresponds to a vessel filled with

Fig. 6.5. Vessel Containment Capability Versus Ultimate Strain for Intermediate Pressure Reactor.

a relatively incompressible fluid, such as water, and the other to an empty vessel or one filled with a compressible fluid, such as air or steam. The latter case is the one pertinent to the Karlsruhe reactor, unless it is assumed that the maximum credible accident occurs while the reactor is flooded — an assumption that appears to be incredible.

The dashed lines on Fig. 6.5 correspond to vessel design conditions as determined by ORNL, whereas the solid lines correspond to the original Karlsruhe vessel design. For an ultimate strain of approximately 7% or more ($\epsilon_u > 0.07$), it may be seen that the empty vessel of either ORNL or

Karlsruhe design is capable of absorbing the energy from more than 2000 Mwsec or 1000 lb of TNT. It is expected that either the Karlsruhe-selected vessel material (20 NiMoCr 3/6) or the ORNL material selection (SA 302, grade B, steel) will exhibit ductility with an ultimate strain in excess of 7% if there is no appreciable degradation in ductility due to irradiation or other effects during operation.

The excursion containment capabilities of the vessels for the high and low pressure reactors were also determined, and curves showing the excursion containment capability of each of the three reactor vessels are plotted in Fig. 6.6. At 7% ultimate strain, the 3600-psi vessel could withstand 2600 Mwsec and the 1200-psi vessel, 1000 Mwsec. The vessels,

Fig. 6.6. Reactor-Vessel Excursion - Containment Capability.

except possibly the 1250-psi vessel, appear satisfactory for preliminary predictions of the energy release of a maximum hypothetical accident, provided tests can show an ultimate strain of about 7% to be acceptable at the neutron exposures of the SCBR vessels. The high pressure containment vessels included in the high and low pressure designs will increase the capabilities of containing an excursion, but the effect of these vessels was not considered in these calculations.

6.2 Primary Heat Transfer and Turbine-Generator Systems

6.2.1 Primary Heat Transfer Systems

Schematic flow diagrams for the three systems are given in Figs. 6.7, 6.8, and 6.9. The three concepts are similar in that they all utilized a large bypass steam flow to convert the feedwater into steam in Loeffler-type boilers before it entered the reactor core. The low-pressure system was described⁷ by the designers as being an "integrally arranged Loeffler cycle." This arrangement was selected in order to obtain good safety characteristics and, thus, provide a system that should have a minimum credible rate of pressure loss. The Loeffler boiler, recycle path, blowers, etc., were contained in the reactor vessel. The system had a direct cycle, with steam flowing from the reactor, through the high pressure turbine, directly to three low pressure turbines and condenser, and then back to the reactor via five stages of feedwater heating. Part of the steam was used to drive the four main circulators and turbine driven feedwater pumps. Approximately 75% of the steam heated in the reactor was recycled through the internal Loeffler boiler to convert the feedwater to steam before it entered the core.

In the intermediate pressure system the turbine steam was again obtained directly from the reactor; the circuit was described¹⁰ as "a direct cycle with simple intermediate superheating for the main turbines and a closed secondary loop for auxiliary facilities." The main steam flow was from the reactor to the high pressure turbine; it was then reheated and went through the intermediate and low pressure turbines to the condenser and back, via eight stages of feedwater heating, to the external Loeffler boiler. Some 60% of the steam heated in the reactor was directed

Fig. 6.7. Steam System of Low Pressure Reactor.

Fig. 6.8. Steam System of Intermediate Pressure Reactor.

through the reheaters to the Loeffler boilers. This design had a secondary steam loop in operation at all times. This closed loop acted as the afterheat removal system during shutdown, as part of the emergency cooling system, and as "ballast" to compensate for pressure fluctuations in the core, and it operated the main steam circulators and auxiliary generators. The original design specified two 500-Mw tandem compound turbine-generators; however, this was changed to one 1000-Mw tandem compound turbine-generator unit for cost normalization. In this design, most of the station auxiliary power was generated by two small generators in the secondary loop; only 5 Mw of auxiliary power was taken from the main generators.

The high pressure system was originally designed³ to use a D_2O-H_2O mixture as the coolant, but later the coolant was changed to ordinary water.⁴ Although the layout of the flow sheet did not change significantly, the various parameters did. The final design report⁴ did not, in all cases, reflect the change in coolant and, as a result, it was necessary to make several assumptions and recalculations to arrive at the flow sheet shown in Fig. 6.9. The primary heat transfer system of the high pressure reactor are basically a parallel loop Loeffler boiler arrangement with bypass reheat loops. The main steam and reheat steam circulators, together with the reheaters and the remainder of the turbine-generator equipment, were external to the secondary containment vessel. For this design, the Loeffler boilers are simply mixing tees in the reheat lines. Subsequent to issuing the final design report,⁴ B&W continued to examine this system and now feels that the design might be safer and more efficient if turbine drives were used instead of electric motors on the circulators. Motors of this size are not readily available, and turbines would be more reliable and efficient. Thus, the flow sheet was modified to include turbine drives. In general, it was not possible to make detailed heat balances for the proposed designs, since sufficient information was not available. However, a heat balance for each flow sheet was evaluated for internal consistency. No inconsistencies were found in the low and intermediate pressure systems. The flow sheet for the high pressure system was incomplete, and it was necessary to calculate some of the values. Design information from other steam plants was

used as a basis for estimating the thermal efficiency of this plant.

Table 6.4 lists the principal heat transfer design data for these plants.

6.2.1.1 Steam Generators. Schematic diagrams of the steam generators for the three proposals are shown in Fig. 6.10. In the low pressure system, the steam generator was integral with the pressure vessel; in the intermediate-pressure system it had a separate vessel; and in the high-pressure system, the feedwater was simply mixed with the recycled super-critical steam in mixing tees.

In the low pressure system, steam flowing directly from the core was mixed with the feedwater at the lower end of the reactor in an annulus approximately 16 ft OD, 8 ft ID, and 5 ft long. Excess moisture was removed, first, at the bottom of the vessel by changing the flow direction of the steam and, again, at the top of the core in centrifugal separators.

Table 6.4. Principal Heat Transfer Design Data

	Low Pressure System	Intermediate Pressure System	High Pressure System
Reactor thermal power, Mw	2900	2519	2326
Coolant temperatures, °F			
Inlet	567	710	750
Outlet	925	1005	1000
Total coolant flow, lb/hr	40×10^6	25.1×10^6	24.2×10^6
Steam flow to high pressure turbine, lb/hr	8.1×10^6	6.61×10^6	7.33×10^6
Total reactor pressure drop, psi	110	172	275
Total electrical output, Mw	1035	1005*	1000
Net station electrical output, Mw	1015	1000	980
Net station efficiency, %	34.9	39.7	42.2

*Five megawatts of plant electrical load is supplied by the main generator; another 22 Mw is generated by two 11-Mw turbine-generators in the secondary cooling system.

INTERMEDIATE PRESSURE SYSTEM

LOW PRESSURE SYSTEM

HIGH PRESSURE SYSTEM

Fig. 6.10. Diagram of Main Steam Generators.

The separated moisture is recycled by returning it with the feedwater. The water is introduced into the steam generator through spray nozzles.

In the intermediate pressure design, steam was generated in a separate Loeffler-type boiler, which is a vessel 6 ft in diameter and 24 ft long, with a 1.5-ft-ID spray column on the top. The steam came from the reactor via the reheater and, as in the low pressure system, excess moisture was removed and recycled. The separation was accomplished by a centrifugal separator, a change in direction, and a set of screens.

A somewhat different problem existed in the high pressure system where, instead of steam, the generator was required to produce supercritical fluid. In principle, this is accomplished by mixing three streams — one from the reheater, one from the reactor directly, and one from the feedwater heaters — in two mixing tees. The exact sequence of mixing apparently has not been established, because different flow sheets within the same report show different arrangements. The tees are basically 15-ft sections of pipe 4 in. larger in diameter than the steam lines (which are approximately 16 in. in diameter).

In attempting to evaluate the low and intermediate pressure designs, the "contact times" for water droplets in the steam flow were calculated and compared with the time necessary to evaporate a droplet. Data reported by Brown⁸¹ were used in calculating the heat transfer coefficients. A maximum time was obtained by assuming that the water remained in the liquid phase as it passed through the generators, and a minimum time was calculated by assuming that the water immediately flashed to vapor upon entering the generator. For the low pressure system, the contact times were 0.222 sec for 100% evaporation and 0.301 sec for no vaporization; the corresponding figures for the intermediate pressure system were 0.07 and 0.1 sec. If it is assumed that the spray nozzles produce droplets 100 μ in diameter (which is on the large side for a common nozzle), both systems are readily able to vaporize the feedwater streams; in fact the calculations showed that 1000- μ -diam (1-mm) particles could be vaporized in the span of the contact times. A synopsis of the calculation for the intermediate pressure system is presented below:

Steam flow	15.8×10^6 lb/hr at 0.28 ft ³ /lb
Water flow	6.6×10^6 lb/hr at 0.02 ft ³ /lb

Based on water and steam being in contact for 12 ft in a 1.475-ft-diam. channel,

<u>No Vaporization</u>	<u>100% Vaporization</u>
V = 123 fps	V = 169 fps
T = 0.1 sec	T = 0.07 sec

For 100- μ particles, $h = 10^5$ Btu/hr·ft²·°F, with h_{fg} in Btu/lb, and $\Delta T = 250^\circ\text{F}$, the contact time is 0.05 sec. The heat required to vaporize is

$$18.5 \times 10^{-6} \frac{\text{ft}^3}{\text{particle}} \times \frac{1}{0.02} \frac{\text{lb}}{\text{ft}^3} \times 1000 \frac{\text{Btu}}{\text{lb}} \cong 1 \frac{\text{Btu}}{\text{particle}},$$

and the heat available is

$$10^5 \frac{\text{Btu}}{\text{hr} \cdot \text{ft}^2 \cdot ^\circ\text{F}} \times 3.4 \times 10^{-3} \frac{\text{ft}^2}{\text{particle}} \times \frac{0.05 \text{ sec}}{3600 \text{ sec/hr}} \cong 1.2 \frac{\text{Btu}}{\text{particle}}.$$

Data on heat transfer between water droplets and supercritical fluid are rather scarce, and it was not possible to evaluate the high pressure design as completely as the others. For this case, it was assumed that the average flow velocity was 200 fps; this agreed with values reported by B&W³, and with independent calculations made at ORNL. On the further assumption that there would be 20 ft of pipe between the last tee and the inlet to the circulator, a contact time of 0.1 sec is obtained. Based on the results of the calculations for the lower pressure systems, this should be more than adequate, provided the feedwater stream is sufficiently "shattered" as it enters the main flow.

In general, the contact steam generators appear to be satisfactory, based on the above analysis. The means for moisture separation are widely used and reliable. The nozzles are also quite common and should present no problems; however, data on spray distribution, droplet size, etc., would be required, and the performance of each steam generator concept needs to be demonstrated. Since there is little information available on supercritical fluid heat transfer, the ability of the mixing tees used in the high pressure design to produce a homogeneous fluid should be investigated.

Another possible problem in connection with the steam generators is that of entrainment and associated scale buildup. This could lead to difficulties in both the reactor core and in the steam generators. At present, there is wide divergence of opinion as to whether scale buildup will be a problem. For instance, the results of a study made in Sweden¹³ in connection with a study of a 1000-Mw(e) steam-cooled fast breeder reactor indicate that direct contact steam generators may not have the same purification effect as surface boilers, and it is speculated that nearly 100% entrainment of the crud into the steam could exist. The crud would probably be in the form of colloidal-size particles that could be deposited on the cladding surface. Since so little is known in this area, it is recommended that any demonstration plant be designed so that a close check can be kept on the scale buildup in the various components. Also it may be necessary to install filters in a demonstration plant to determine whether carryover of radioactive solid particles to the main turbine-generator is a significant problem.

In the low pressure reactor feedwater system, a full flow demineralizer was provided to remove all dissolved solids from the feedwater before it went into the reactor. This system is the same as that used in feedwater systems for once-through steam generators where practically no scale deposits can be tolerated and no blowdown can be used.

As an additional precaution in the low pressure system, the spray nozzles located below the reactor support grid were made accessible for removal and replacement if they became encrusted with scale during evaporation. This is an inconvenience associated with the integral design that may or may not be offset by the advantages of that arrangement.

6.2.1.2 Circulators. The general characteristics of the main steam circulators for the three systems are given in Table 6.5. To reduce the possibility of a loss-of-coolant accident, the circulators were mounted within the primary containment vessel in the low pressure design, and due to space limitations imposed by this arrangement, centrifugal compressors were selected. The preliminary design of these units called for an impeller 24.8 in. in diameter and 6.5 in. long and a 7.5-in.-diam. beam shaft. The mechanical seals and bearing were lubricated with water. The circulators used in the intermediate pressure design are described only

Table 6.5. Main Steam Circulators

	Low Pressure System	Intermediate Pressure System	High Pressure System
Number of circulators	4	6	2
Type	Single flow, radial	Multistage, axial	Single flow, radial
Drive	Turbine	Turbine	Turbine
Capacity, lb/hr			
Each circulator	10×10^6	3.73×10^6	12.8×10^6
Total	40×10^6	22.4×10^6	25.6×10^6
Inlet pressure, psia	1,150	2,425	3,400
Pressure rise, psi	110	265	300
Steam temperature, °F			
Inlet	553	658	731
Outlet	585	685	750
Shaft speed, rpm	10,000	*	3,600
Shaft horsepower			
Each circulator	37,500	15,000	30,500
Total	150,000	90,000	61,000
Efficiency, %	85	*	85
Impeller diameter, in.	24.8	*	31
Shaft diameter, in.	7.5	*	*

*Information not available.

as being "conventional" and utilizing labyrinth shaft seals. The high pressure system circulators had 31-in.-diam impellers, and a combination of several labyrinth seals.

These circulators all represent extensions of present technology. Although gases at these pressures and in these volumes are being handled today, there are few, if any, situations where the required combinations of pressures and volumes exist. As of August 1966, the highest pressure used in a gas-cooled reactor was of the order of 1000 psi.⁸² The shaft seals and bearings are perhaps the most significant problem areas, and the majority of the development work and proof-testing will be needed on

these components. Since the circulators are simply extrapolations of the current state of the art and do not depend on new concepts, the development work does not represent a major effort and should not be a controlling factor in the time required to build a reactor system.

The drives for all the circulators were steam turbines. These are stock items and should present no difficulties.

6.2.1.3 Other Steam Circulators. Two other major steam circulators were used in the intermediate and high pressure systems. The high pressure design had a reheat steam circulator in the primary heat transfer loop, and the intermediate pressure design had a steam circulator in a secondary heat transfer system. The main characteristics of these units are given in Table 6.6. Again, these units were to be turbine driven. Some development work will probably be required for the seals on these circulators

Table 6.6. Other Steam Circulator Characteristics

	Intermediate Pressure System	High Pressure System
Designation	Secondary cooling circuit blowers	Reheat steam circulators
Number of circulators	4	2
Type	Radial	Radial
Drive	Turbine	Turbine
Capacity, lb/hr	1.37×10^6	4.14×10^6
Inlet pressure, psi	2440.2	3350
Pressure rise, psi	250	50
Steam temperature, °F		
Inlet	662	824
Outlet	689	*
Shaft speed, rpm	*	3600
Shaft horsepower	*	2860
Impeller diameter, in.	*	18
Shaft diameter, in.	*	*

*Information not available

also, but since they are much smaller the magnitude of the problem should be less.

6.2.1.4 Other Heat Exchangers. There were three major heat exchangers besides the main steam generator in the intermediate pressure system: a reheater (intermediate superheater) between the high and intermediate pressure turbines and, in the secondary loop, a steam generator and a Loeffler boiler. The reheater was a conventional shell-and-tube heat exchanger. It had adequate provisions for condensate removal during startup, and means for blocking defective tubes. The secondary steam generator was similar in design to the main reheater, being a shell-and-tube heat exchanger with the feedwater to be vaporized flowing through the tubes. The Loeffler boilers were not described in detail, but are shown on the flow sheets as spherical vessels into which steam and feedwater are continuously supplied. Their functions are to provide a source of water for flooding the reactor container, and to act as a reservoir of saturated water to compensate for pressure fluctuations in the core.

The high pressure system had a reheater in the line between the high and intermediate pressure turbines. It was a U-tube U-shell heat exchanger with the reheat system on the shell side. The only design available is the one for the early B&W design,³ details of which are given below:

Material	Type 304 stainless steel
Size of tubes, in.	
Outside diameter	1/2
Wall thickness (min)	0.085
Number of tubes	5000
Effective length of tubes, ft	46.1 (60)*
Heat transfer area, ft ²	30,000 (39,000)*
Weight, lb	199,875 (260,000)*

This reheater was designed for 1050°F inlet steam and minimum D₂O inventory.

ORNL calculations indicated that the reheater described in Ref. 3 could not be used as specified in the system described in Ref. 4. The tube side pressure drop was estimated to be 20% higher than allowed, and

*ORNL modified values.

the overall heat transfer coefficient was found to be 30% less than would be required; thus, a completely new design would be needed. The modifications assumed for the purpose of estimating capital costs are given in parentheses above. The reheater pump pressure rise requirement was also changed from 50 to 80 psi to accommodate the higher pressure drop through the reheater.

6.2.2 Turbine-Generator Systems

6.2.2.1 Turbine-Generator and Condenser Systems. Table 6.7 lists the characteristics of the turbine-generator systems for the three designs. The intermediate pressure design originally had two 500-Mw generators; however, in this review a single 1000-Mw unit was substituted to make possible a more direct cost comparison. In addition, the high pressure design originally had a cross compound turbine, and this was changed to a tandem compound unit.

The turbines, generators, and condensers proposed for these plants are generally within the current state of the art, although the tandem compound turbine-generator units for the intermediate and high pressure systems are about 35% larger than units presently listed.

The condensers for the low and high pressure systems were originally sized on the assumption that 70°F cooling water would be used. The water temperature should have been 57°F according to the ground rules for the study, and the designs were adjusted to take this into account. The intermediate pressure proposal did not have a description of the condenser; therefore, duty, flow, etc., were calculated for this unit.

6.2.2.2 Feedwater Systems. The feedwater systems for these designs were all conventional. The low pressure system had four stages of low pressure heaters, with three parallel sets, and two stages of high pressure heaters in two sets. The intermediate pressure design had three low pressure stages and five high pressure stages in each of two parallel systems. There were four low pressure stages and three high pressure stages, again in two parallel paths, in the high pressure design. The low pressure system had two turbine driven feed pumps; the high pressure design had a single turbine driven feed pump; and the intermediate

Table 6.7. Turbine, Generator, and Condenser Systems

	Low-Pressure System	Intermediate-Pressure System	High-Pressure System
Turbines			
Arrangement	Tandem compound, six flow, 38-in. last-stage blades	Tandem compound, six flow, 33.5-in. last-stage blades ^a	Tandem compound, six flow, 33.5-in. last-stage blades ^a
Throttle conditions			
Pressure, psia	1050	2352	3300
Temperature, °F	925	996.8	994
Flow rate, lb/hr	8.11×10^6	6.61×10^6	7.33×10^6
Reheat conditions			
Pressure, psia	(b)	450	750
Temperature, °F	(b)	950	950
Flow rate, lb/hr	(b)	6.61×10^6	6.70×10^6
Generators			
Rating, total, kva	1.13×10^6	1.12×10^6 (a)	1.13×10^6 (a)
Terminal voltage, kv	18	18 ^a	18 ^a
Power factor	0.9	0.9 ^a	0.9 ^a
Type of excitation	Alternator-excitor driven by main generator rotor	(c)	Shaft driven
Condensing system			
Number of condensers	3 shell; 2 pass	3 shell; 1 pass ^a	1 shell; 1 pass ^a
Tubes			
BWG	18	18 ^a	18
Material	Admiralty	Admiralty ^a	Admiralty
Diameter, in.	1	1 ^a	1
Length, ft	35	35 ^a	40
Design heat transfer surface per condenser, ft ²	214,000 ^a	540,000 ^a	304,000 ^a
Condenser conditions			
Pressure, in. Hg abs	1.5	1.5 ^a	1.5
Cooling-water inlet temperature, °F	57 ^a	57 ^a	57 ^a
Duty, Btu/hr	6.404×10^9	4.2×10^9 (a)	4.47×10^9 (a)
Total steam flow rate, lb/hr	6.748×10^6	4.4×10^6 (a)	4.7×10^6
Total coolant flow rate, gpm	520,000	360,000 ^a	680,000
Overall characteristics			
Net station heating rate, Btu/kwhr	9779	8590	8090 ^a
Net station electrical output, Mw	1012	1000	980
Main generator electrical output, Mw	1035	1005	1000 ^a
Station auxiliary electrical power, Mw	20	27 ^d	20
Reactor thermal power, Mw	2900	2519	2326
Net station efficiency, %	34.9	39.7	42.2 ^a

^aValues assumed for estimating costs.^bDoes not apply.^cInformation not available.^dFive megawatts is supplied by the main generator; the other 22 Mw is generated by two 11-Mw generators driven by turbines in the secondary cooling system.

pressure system had two motor-driven feed pumps. As in other direct cycle plants, full flow demineralization of the feedwater was used.

6.2.3 Decay Heat Removal and Emergency Core-Cooling Systems

Schematic diagrams of the shutdown and emergency core-cooling systems are shown in Figs. 2.11, 2.12, and 2.13 of Chapter 2. The interaction between refueling and decay heat removal forces the methods used to be considered together. The high and intermediate pressure designs were based on wet refueling, and forced circulation of the water that floods the reactors to provide shielding was used for shutdown cooling. The low-pressure design was based on dry refueling and therefore had steam for decay heat removal. The shutdown sequence and decay heat removal methods are described below for each of the three reactor systems.

6.2.3.1 Shutdown Sequence. In the low pressure system, power is reduced until the turbine bypass is shut off. The circulators continue to operate, and heat is removed in the emergency condenser. The condensate is pumped back into the reactor and mixed with internal recirculating steam.

In the intermediate pressure system, power is reduced to 20%, and then the turbines trip off. The remaining heat is removed by secondary steam generators as it is reduced to decay heat level. The secondary steam system is then isolated, and Loeffler boiler water is fed by gravity to flood the fuel elements. The shutdown cooling system is then activated, and feedwater is used to finish filling the vessel.

In the high pressure system, power is reduced until the turbine bypass is shut off. Isolation valves are then closed, except those to main circulators. The isolation condenser is started, and condensate is gravity fed into the reactor inlet. Core flooding is initiated, and the shutdown cooling system is activated when flooding covers the suction line. Use of the isolation condenser is discontinued when flooding of the core is complete.

6.2.3.2 Emergency Core-Cooling Systems. The emergency core-cooling systems were designed primarily to take care of loss-of-coolant accidents. In the event there was a break, either inside the containment vessel or outside of it, means were provided to cool the reactor without damage to

the system. For a simple accident, that is, an accident in which only one component fails and all others remain operable, all the designs had sufficient redundant and/or alternate systems to cope with the situation. A more detailed discussion of the various types of accidents that might be expected to occur is given in Section 5.3. Brief descriptions of the actions taken in the three systems are given below. (The high pressure design did not have an adequate safety system in the original proposal and, consequently, a typical head tank, suppression chamber, spray system, and associated lines, and pumps were added by ORNL for costing purposes.)

In the event of a break outside the containment vessel of the low pressure reactor, isolation valves would close on the damaged line. At least one of the three remaining lines would be connected to the emergency condenser and quench tank for heat removal, as in the shutdown mode. Loss of coolant would be compensated from the feedwater overflow storage. For a break inside the containment vessel, the system would be essentially the same as that for a break outside, with pressure suppression provided by the feedwater overflow storage in the form of a containment vessel spray.

In the event of a break outside the containment vessel of the intermediate pressure system, isolation valves would close on the line that had parted. Secondary steam system circulators inside the containment vessel would operate with steam produced by the decay heat in addition to the auxiliary boiler steam to furnish emergency power. The Loeffler boiler would attempt to compensate for loss of coolant by flashing the water in storage as the pressure dropped. The shutdown cooling system would be used as a long-term backup system. For a break inside the containment vessel, the reactor coolant would equilibrate at a lower pressure (37 psi) through the opening to the containment vessel. The secondary steam generators would continue to operate, with the core spray system adding water to the reactor to enhance steam core cooling at the lower pressure. As above, the shutdown cooling system would be used as a long-term backup system.

In the event of a break outside the containment vessel of the high pressure system, isolation valves would close at the containment vessel on the severed line. The main circulators would continue to operate,

with the isolation condenser in operation for removing the power. The pressurized head tank would compensate for the loss of coolant before the isolation valves sealed off the severed line. The shutdown cooling and flooding systems would serve as long-term backup systems. For a break inside the containment vessel, all isolation valves would close, except those on the main circulators. The main circulators and isolation condenser would provide cooling as discussed above. The suppression chamber and pressurized head tank would be used to restrict the pressure buildup in the containment vessel. The shutdown cooling system would be used in the event of failure of the circulators.

In addition to these accidents, there are many other possible simpler accidents, such as loss of operation of pumps, turbines, and condensers, that would require emergency cooling. It is beyond the scope of this study to go into detail concerning all these possibilities, but these are areas that must be carefully examined in a complete safety analysis.

In the event of a complex accident in any of the three systems in which all other means of cooling were lost, spray cooling systems would be used to prevent melting. A study⁶⁶ of the use of spray cooling indicates there are four possible problems that may be encountered:

1. dryout, or the complete evaporation of the liquid spray at some point above the heated surface due to a water supply smaller than that required by the total heat generation rate;
2. dry patch formation due to hot spots and, possibly, to local differences in surface tension introduced by subcooling of the liquid;
3. sputtering, or breakdown of the film due to physical agitation and ejection of liquid droplets, which could occur because of excessively large temperature differences between the heated surface and the saturated point; and
4. flooding, when the upward steam flow stripped the liquid film from the surface.

The study⁶⁶ examined these possibilities and the means by which they might be overcome, and concluded "that a core spray cooling system that can come into operation within 20 sec after an emergency signal is generated, and which can deliver 2200 gpm of water, should provide adequate

emergency cooling to prevent gross fuel meltdown in the event the normal means of shutdown cooling are unavailable."

However, in a later study (see 5.3.2.2) it was concluded that the initial temperature transients with no emergency cooling will cause cladding failure in each design. These failures will occur in the first few seconds after an accident; that is, in the period before any emergency cooling system can be activated. This point raises two questions that will require further study: (1) Can cladding failures be tolerated in these reactors? (2) If not, is it possible (and feasible) to activate the emergency core-cooling system within some very short (<2 sec) time after an accident? Design factors such as adequate flow distribution from the spray nozzles, reliability and speed of operating valves and pumps, adequate spray pumping head, and sufficient water supply must be included in establishing an adequate system.

If these reactors operate at approximately 3000 Mw(th) or 10^{10} Btu/hr, 10 sec after shutdown the heat removal rate should be down to 10% of the full power value, or 10^9 Btu/hr.⁶⁶ If the heat of vaporization of water is taken to be 1000 Btu/lb, it will require 10^6 lb/hr or 2000 gpm to remove the heat by vaporization alone; after 25 sec, the flow required will be reduced to 1000 gpm. In the low pressure system, two 1015-gpm pumps had provided. The sizes of the pumps are not specified for the intermediate pressure reactor, but based on the Nuclear Technology Corporation analysis⁶⁶ at least 2200-gpm capacity should be provided. ORNL included two 6250-gpm pumps in the high pressure design. All these systems thus had the pumping capacity needed to spray cool the core. A survey of emergency core-cooling problems in water-cooled reactors⁸³ shows that the effectiveness of spray cooling can vary over a wide range; for instance, heat transfer coefficients ranging from 1 to 1000 Btu/hr·ft²·°F are reported. This large variation is due primarily to differences in distribution. In conclusion, it appears that these steam-cooled reactors have the spray cooling capacity required, provided it can be demonstrated that the flow is distributed properly. Judging by the "tightness" of the fuel pin spacing, this could be a significant problem.

6.3 Plant Instrumentation and Control Systems

None of the conceptual designs include information on equipment requirements; for example, such information as numbers, type, and location of primary transducers is lacking. The desirable degree of redundancy and logic sequences remains undetermined. Because of the absence of control studies, this evaluation includes a suggested plant control scheme, which is discussed in the following paragraphs.

In all three designs, reactivity control was effected by insertion or withdrawal of boron-loaded control rods. The control rod drives for the high and low pressure designs were of a new and untested concept. The intermediate pressure design had a rather conventional rack-and-pinion type of drive.

6.3.1 Control Rod Drives

6.3.1.1 High Pressure System. The high pressure design⁴ had 108 shim safety rods that were integral with fuel assemblies. These rods had only two positions, either full-in (minimum reactivity) or full-out (maximum reactivity). Each of the 108 rods was operated independently. The rod drive system proposed was the same as that proposed for the low pressure design.⁷

6.3.1.2 Intermediate Pressure System. The intermediate pressure design¹⁰ had 18 control rods that were suspended from drive units mounted on the pressure vessel head. The rod drives were of the conventional rack-and-pinion type, with spring acceleration on scram. The scram clutch was between the motor drive gear and the pinion gear. Two of the rods were designated control rods, and 16 were shim safety rods. They differed only in operating speed.

The drives are designed so that if a rod extension guide tube was raised, a latch engaged to prevent rod withdrawal. This latch was intended to prevent rod ejection in the event of a failure of a rod drive high pressure thimble. Additionally, the rod drive extension was connected to the rod through a coupling with a rated breaking force. If a rod was stuck or otherwise held down and a withdrawing force existed, the coupling would release. It is questionable whether the 18 control rods are

sufficient. It is believed that the number of independently operable control rods should be increased by at least a factor of 4.

6.3.1.3 Low Pressure System. The low pressure design had 124 shim-safety rods and three regulating rods. The 124 shim-safety rods were hydraulically operated and were identical to those proposed for the high pressure design. The three regulating rods were similar to the shim safety rods, but their drives were designed to provide position control. The hydraulically operated shim-safety rods, which use reactor coolant as a working fluid, were integral with a fuel assembly. When the core was refueled, control rods and their drives were necessarily replaced at the same time. The drive mated with a terminal socket on its individual command line when a fuel control rod element was placed into its core position. The hydraulic control rod and its command system are shown in Fig. 6.11.

When a rod was inserted and the plant was operating, the command line was open to a 1150-psi manifold, and the top of the rod saw a pressure of 1250 psi. The rod was held down by a pressure differential of 100 psi. It was also latched down.

To raise a rod, the command line was momentarily opened to condenser pressure. This exposed the rod to a pressure differential of about 1200 psi and forced the rod downward against a spring-loaded unlatching sleeve. The unlatching sleeve had a delayed return so that the rod remained free to travel for a short period of time. During this time the command line are valved over to a high pressure manifold, and the rod was driven to the uppermost limit of its travel.

The high pressure manifold was maintained at a pressure sufficiently high to drive the rod upward against a restraining pressure of 1250 psi. Manifold pressure was maintained by a shim safety rod drive circulator, which took suction from the low-pressure (1150-psi) side of the core.

The proposed system is simple in concept, and it has some interesting features:

1. If the drive circulator should fail, or if either its inlet or outlet line should open, the rods would be inserted under a pressure differential.

Fig. 6.11. Shim-Safety Rod Arrangement for 1200-psi SCBR.

2. If the core outlet pressure fell by as much as the rod "hold-up" pressure before the core inlet pressure fell during a blowdown, the rods would drop into the core.

3. Because there are so many (124) individually operated rods, it may be possible to test each one completely while the reactor is at power without causing intolerable pressure or power variations.

On the other hand, there are some potential problems:

1. If the high pressure plenum should be vented (such as by failure of a refueling plug), the rods would be prevented from falling in until pressures equalized above and below the core.

2. The rods have only two operating positions, full in or full out. This may not be a problem, however, because of the large number of rods.

3. Since there is no means for filtering the working fluid (reactor coolant), there may be crud buildup in the coolant paths that could adversely affect proper operations.

4. If for any reason a rod should fail to insert as a result of crud buildup or command line blockage, for example, there is no direct positive means for forcing the rods into the core.

5. The rod position readout involves an unusual method of measuring differential pressures in the command line system. The fully inserted indication requires that the full withdrawal pressure be applied against the latched rod. This may present safety problems, since this position indication measurement would eject the rod if it were not latched.

The regulation rods have a hydraulic jack type of drive. Again, rod position indication is complicated by the integral design and involves pressure measurements in the command line system.

It must be recognized, of course, that because the proposed system is conceptual, it is subject to complete design, development, and testing before being put into service.

6.3.2 Suggested Plant Control Scheme

The dynamics of the steam-cooled plant cycle are still being studied, and detailed control system functional specifications will be developed after these studies have been completed. When this information becomes available, the optimum control scheme can be established.

The design reports for the intermediate and low pressure systems gave very brief descriptions of possible plant control schemes. The proposed system for the low pressure reactor **was** the load following type in which the reactor power **was** made to follow large changes in the electrical load on the turbine. The reactor power and recirculating steam flow **were** manipulated to provide steam to the turbine at constant pressure and temperature. As indicated in Section 5.3.3.2, the Karlsruhe dynamics study showed that the plant characteristics will probably be suitable for sophisticated control schemes of this type.

As an alternative, ORNL considered a base load type of control system (in which the turbine output **was** made to follow the reactor power) that may also be workable. The plant operator selected a turbine-generator load demand, and a master controller computed the corresponding blower speed and feedwater pump demand signals. The computation **was** based on known blower and pump characteristics and predetermined optimum parameters for the new demand conditions. Core power **was** adjusted with the regulating rod to set up the core coolant outlet temperature prescribed for the new operating conditions. The turbine inlet valve responded to pressure at the valve inlet. The valve position **was** trimmed and reset by the turbine governor. For example, if the turbine tended to overspeed, the governor adjusted the inlet valve and any excess steam bled off to the main condenser through a dump valve to maintain a constant pressure. Other control loops functioned to maintain the feedwater overflow storage tank level, to balance steam flow and feedwater flow, and to maintain feedwater inlet conditions.

6.3.3 Radiation and Fission-Product Monitoring

The steam-cooled plants **were** of the direct cycle type. Therefore any fission products or other radioactive material in the steam coolant **would** be circulated through the turbines and the condensers. Gaseous radio-nuclides **would** be removed by the condenser air ejectors to the off-gas system through filters, scrubbers, and such holdup equipment as **might** be provided.

The ventilation off-gas system **was** the first, although controlled, source of potential radioactivity release. Radiation monitoring equipment

will be essential to monitor off-gas activity, and to isolate the system if activity levels exceed predetermined limits.

The most probable source of radioactivity is fission product release from fuel pins. In view of the required high cladding temperatures, the probability of localized cladding failures is real. In order to provide for prompt detection of cladding failures, fission product monitoring systems should be installed. These systems should be capable of monitoring the core in at least eight different regions. The Karlsruhe design report¹⁰ on the intermediate pressure reactor discusses a system that is conceptually suitable for all SCBR's. This system serves for the determination of damage in fuel element cladding by measuring the activity of the steam discharged from the fuel elements.

6.4 Fuel Handling Systems

All three concepts employ off line refueling at intervals ranging in frequency from 261 days (intermediate pressure design) to 880 days (low pressure design). Wet refueling is used in the intermediate and high pressure designs, whereas dry refueling is used in the low pressure design. Data on the refueling systems are compared in Table 6.8.

The refueling times reported are compatible with the requirement of a 0.9 reliability factor for a 0.8 plant load factor. Reactor and turbine plant maintenance schedules can be arranged to minimize or negate the effect of refueling time on plant availability. Some development work will be required to check the performance of the semiremote fuel handling mechanisms of the low and high pressure concepts; however, little or no development effort should be required for the intermediate pressure concept because of similarity with BWR and PWR fuel handling systems.

6.4.1 High Pressure System

The high pressure reactor vessel design incorporates several features closely related to the design of the refueling system. For instance, the arrangement of reactor internals is such that an unobstructed volume is provided above the core to accommodate the refueling equipment. The

Table 6.8. Fuel-Handling Data

	Low-Pressure System	Intermediate- Pressure System	High-Pressure System
Refueling method	Off line	Off line	Off line
Refueling interval, full-power days	880	261	460
Refueling time, hr	137	76	<120 ^a
Number of fuel elements replaced	271	54	108
Number of fuel elements shuffled	None	None	None
Number of blanket elements replaced	126	20	72 or 198 ^b
Number of blanket elements shuffled	None	None	None
Storage capacity			
Number of new fuel-element positions	542	(c)	118
Number of new blanket-element positions	252	(c)	213
Number of spent fuel-element positions	542	(c)	108
Number of spent blanket-element positions	252	(c)	198
Storage canal area, ft ²	1896	826	1056
Storage canal volume, ft ³	75,800	33,900	42,240
Maximum spent-fuel cooling time before shipment, days	120	120	120
Method of cooling spent fuel	Water immersion	Water immersion	Water immersion

^aAn arbitrary allotment of 120 hr was made (see text).

^bFor alternate cycles.

^cSpecific areas in storage canal are not designated for new or spent fuel; total capacity is reported as one and one-half core loads.¹⁰

control rod actuator system was located below the core support grid to conform to this basic objective.

A proposal⁸⁴ by AMF Atomics Division of American Machine and Foundry Company was made to the Babcock & Wilcox Company for a refueling system suitable to this reactor. The features of the AMF proposal were incorporated in the reference reactor design. The proposal included, in addition to a description of the refueling system, technical discussions of (1) the refueling port closure system, (2) refueling manipulator, (3) spent fuel transfer system, (4) new fuel cask, and (5) control rod viewing stations. A proposed specification for a seal welder and cutter was also included, and a time estimate for all refueling operations was given.

The refueling system equipment, as depicted in the B&W design report,³ is shown in Fig. 6.12. The refueling equipment was located above the reactor and outside the containment vessel. Access to the reactor for refueling was gained by first removing a concrete shield and then opening the bolted closure in the top of the containment vessel. Five nozzles were thus individually exposed in the top head of the reactor vessel. The central port was for insertion of the manipulator. Separate ports were provided for new fuel addition and spent fuel removal. One port was used for TV camera installation, and one port was used as the operator's control station.

After the reactor had been shut down, the primary system pressure and temperature were reduced in a programmed manner, and the primary system was filled with water in preparation for the wet refueling operation. While this procedure was under way, the secondary containment vessel was opened above the reactor vessel. After the primary system had reached atmospheric pressure, the refueling ports in the reactor vessel were opened. The refueling manipulator was installed in the central port, and the control and viewing assemblies were installed at their respective ports. The new fuel and spent fuel transfer casks were mounted on tracks above the two remaining ports. The spent fuel transfer cask removed spent fuel elements from the containment vessel, transported them to the canal transfer station, and discharged them into the canal. The new fuel transfer cask charged a new element into the vessel and returned to the canal to receive a second new element.

CONTROL STATION

- 1 ATTACH HOUSING TO VALVE
- 2 OPEN VALVE
- 3 LOWER PERISCOPE INTO REACTOR

INDEPENDENT VIEWING STATION HANDLING

- 1 ATTACH HOUSING TO VALVE
- 2 OPEN VALVE
- 3 LOWER PERISCOPE INTO REACTOR

REFUELING MANIPULATOR HANDLING

- 1 LOWER COMPONENT TRANSFER CASK ONTO SHUTTLE VALVE
- 2 OPEN VALVE
- 3 LOWER MANIPULATOR UNTIL MANIPULATOR SEAL FLANGE IS SEATED ON VALVE
- 4 REMOVE CASK TO STORAGE AREA
- 5 ATTACH MANIPULATOR TO VALVE

- 6 RAISE MANIPULATOR CARRIAGE TO POSITION NO 3
- 7 ATTACH SERVICES

Fig. 6.12. Refueling Equipment for High Pressure Reactor.

The refueling manipulator and the viewing station were operated from the control station by an operator who used the periscope and the TV monitor for visual observation. A second TV monitor was provided at the viewing station to furnish visual information to the operators of the spent fuel transfer cask and the new fuel transfer cask. It was proposed that a certain degree of automatic programming be incorporated into the controls of the transfer casks to reduce the number of manual commands necessary to position, load, and unload fuel elements. Through use of interlocks, the programmed system should provide relatively rapid operation with modest backup manual supervision.

The fuel storage canal was divided into two areas to accommodate the spent fuel elements and the recycled fresh elements. Individual canal carts and overhead hoists were provided for each canal area to provide rapid and efficient canal handling of the fuel elements.

It was assumed in the design report⁴ that the refueling operation would be performed during a scheduled annual shutdown period of 120 hr. The AMF time study,⁸⁴ which was completed in January 1964 and was based on the earlier B&W concept,³ indicated that the total refueling operation could be completed in 92 hr. It was thus established that refueling could be completed within the allotted 120 hr.

The AMF time study, however, was based on a different core design and a different fuel management scheme from those proposed in the reference design report.⁴ Removal and replacement of 181 elements and shuffling, or repositioning, of 445 elements was considered. The number of elements to be handled in the latest concept was significantly reduced from the original number. For instance, in the present concept, in alternate fuel cycles, either 181 or 306 elements would be changed and no shuffling would be done. Furthermore, after the AMF study was made, it was decided to change from dry refueling to wet refueling. This change to wet refueling eliminated the cooling gas system and the need for operating the shuttle valves and, thus, further shortened the required refueling time. Another change was the addition of tracks for the new fuel cask so that now both the new fuel and the spent fuel casks were track-mounted. This should afford an added degree of automation to still further reduce the refueling time.

6.4.2 Intermediate Pressure System

The proposed fuel handling system for the intermediate pressure system **was** based on similar systems currently in use on BWR and PWR systems. Fuel elements **were** loaded and replaced under water.

After the reactor **was** shut down and a suitable precooling period **had** elapsed, the reactor vessel **was** flooded with water. The top head of the vessel **was** then removed by a special cover-lifting mechanism and stored at one end of the water filled "loading basin." The track-mounted loading machine **was** then moved into position above the reactor and used to remove spent fuel elements from the core. The spent elements **were** discharged into the inclined fuel transport tube. Gates **were** provided in the transport tube to prevent flow of water from the higher level loading basin to the lower fuel storage canal. The spent elements **emerged** from the transport tube into the canal, where they **were** stored until ready for shipment to the reprocessing facility.

New fuel **was** delivered in casks, which **were** immersed in the loading basin. The new fuel **was** transferred, under water, from the casks into the reactor. Drawings of the refueling operation sequences are given in the design report,¹⁰ and one is shown in Fig. 6.13.

As indicated in Table 6.8, the planned refueling interval **was** 261 days. This is one-third the anticipated life of the core. Hence, one-third of the fuel elements **was** changed each cycle. Likewise, one-third of the control elements **was** changed each cycle. However, only one-sixth of the radial blanket elements **was** changed each cycle, so their life **was** twice that of the fuel elements. No shuffling of fuel or blanket elements **was** planned.

The reported capacity of the fuel storage canal **was** one and one-half core loads. Storage space should normally be reserved in the canal for a contingency requiring complete unloading of the core. Since not more than one-third of the core is removed during a refueling operation, and since the required spent fuel cooling time is less than the normal operating period between refueling operations, a total storage capacity of one and one-half cores appears to be adequate.

Fig. 6.13. Intermediate Pressure Reactor Refueling System.

It is noted that the storage canal has no provision for segregating new fuel and spent fuel areas. It is generally desirable that these areas be separate to prevent the possible spread of contamination from spent fuel into the new fuel area. The provision for segregated areas would not have a significant effect on capital costs.

The reported refueling time, measured from the time the reactor was shut down until it was back at power, was only 76 hr. This time was broken down as follows:

Cooling and flooding	16.5 hr
Fuel replacement	44.0
Draining and reheating	15.5
	<hr/>
Total	76.0 hr

When compared with the refueling times reported by ORNL for advanced converter reactors,²⁴ 76 hr appears to be an exceptionally short time for refueling. For instance, for the five off-line-refueled reactors discussed in the ORNL report, the refueling shutdown periods ranged from 10 to 25 days.

Removal and replacement of the bolted vessel head was expedited by use of the hydraulic bolt tightener, which had an individual hydraulic unit for each of the 24 flange bolts. With this device, all bolts could be loosened or tightened simultaneously, with power being supplied from a common hydraulic power unit. This undoubtedly contributes to the short refueling time.

Although a greater number of fuel elements must be handled during each refueling interval in the B&W reactors than in the Karlsruhe reactor, head removal was not required in the B&W designs, and separate machines were provided by B&W for handling new fuel and spent fuel simultaneously. On this basis, it is difficult to envision the Karlsruhe reactor having a refueling time advantage over the B&W reactors.

6.4.3 Low Pressure System

The refueling system for the low pressure reactor was essentially the same as the one for the high pressure reactor. Both systems were based on the proposal by AMF.⁸⁴ The total refueling time, including cooldown and

startup, was estimated to be 137 hr, or about 50% longer than the estimate for the high-pressure reactor.

For this concept, access and viewing were accomplished through five relatively small nozzles in the top heads of both the high pressure containment vessel and the reactor vessel. Sleeves extended from these nozzles through the array of cyclone separators inside the reactor vessel. Offset mechanical arms were used to reach all fuel element positions beneath the cyclone separators. Refueling operators work high pressure containment vessel but inside the low pressure containment vessel.

During scheduled annual shutdown periods, some fuel handling would be necessary in order to change the orificing of radial flow distribution through the reactor core to adjust for changes in radial power distribution during the fuel lifetime.

The more complete description and evaluation given above for the high pressure reactor refueling system applies to the low pressure reactor system, since both are based on the proposed AMF refueling concept.

6.5 Reactor Containment Structures

The containment schemes differed for each of the three designs. The high pressure design had a high pressure containment vessel that encloses only the reactor vessel. The remaining equipment was in a controlled leakage building surrounding the reactor. The intermediate-pressure design had a lower pressure containment structure that enclosed the reactor, refueling system, and secondary and emergency cooling systems. The low pressure design had both high pressure and lower pressure containment structures.

There appear to be no basic reasons inherent to the concepts for having the three different types of containment schemes. The high pressure containment immediately around the reactor vessel has an advantage in that a high steam pressure could be maintained in the event of a break of a reactor vessel nozzle or a line within the containment vessel. Part of this advantage is nullified, however, by having the main circulators and their lines outside the containment vessel. The lower pressure

containment vessel backing up the high pressure containment vessel in the low pressure reactor design provides for possible releases through refueling penetrations or through the circulator drive removal penetrations. The intermediate pressure design does not have the protection of the high pressure containment vessel to maintain steam pressure in the event of a break; however, inside the containment vessel is the secondary cooling system, which operates continually and has the capacity to remove decay heat in the event of loss of coolant in the primary loops (if the break can be isolated) or loss of flow. All designs require fast acting isolation valves on the steam lines penetrating the containment vessels.

It appears that a common, optimized containment scheme could be used for the three concepts, and it would permit a better normalized comparison of containment costs. However, further safety studies would be required to arrive at specific features of a common design, and extensive changes of the overall designs might be required. For these reasons, a study of this type was not attempted in this evaluation study; instead, the containment vessels proposed by the sponsors were checked for adequacy and modified, as required, to satisfy the proposed design criteria. Descriptions and evaluations of the characteristics of the containment systems are given below.

6.5.1 High Pressure Design

The containment vessel for the high pressure reactor was designed to fit closely (about a 12-in. annular space) around the reactor vessel. It had a bolted closure at the top that must be opened to gain access for refueling the reactor. Concrete biological shielding, 8 ft thick, surrounded the containment vessel.

Details of the design of the containment vessel were not specified in the design report;⁴ however, the design requirements for the vessel were discussed with B&W representatives. The basic design criteria were that (1) the vessel must withstand a maximum energy release that produced a pressure surge of 1000 psia (this included a sudden core energy release equivalent to the discharge of 1000 lb of TNT), and (2) the maximum equilibrium pressure with pressure suppression would be 300 psia. Pertinent features of the containment vessel are given in Table 6.9 and Fig. 6.14.

Table 6.9. High Pressure Reactor Containment
Vessel Design Characteristics

Design basis	Contain primary coolant and complete core meltdown
Type of construction	Cylindrical steel vessel with hemispherical heads
Vessel material	SA 302, grade B, steel
Inside diameter, ft-in.	19-2
Total height, ft	40
Wall thickness, in.	5.25
Head thickness, in.	2.625
Maximum internal pressure, psia	1000
Equilibrium pressure, psia	300
Pressure suppression	
Type	Wet well
Number of tanks	Two
Tank inside diameter, ft	6.5
Tank inside height, ft	40
Tank wall thickness, in.	2
Tank weight, each, lb	62,000
Total* volume, ft ³	2500

*For the two pressure-suppression tanks.

The following assumptions were made in arriving at portions of the information presented in the table: (1) the containment vessel material was SA 302, grade B, steel, and (2) the concrete biological shield surrounding the containment vessel contributed no strength as a pressure vessel. That is, the steel wall of the containment vessel must be of adequate thickness to withstand the maximum energy release without taking credit for any reinforcement afforded by the surrounding concrete. The tentative thicknesses for shell and heads given in Table 6.9 were determined with the formulas of Article I-1, Appendix I, Section III, ASME Boiler and Pressure Vessel Code.⁷³

Fig. 6.14. High Pressure Reactor Containment System.

Pressure suppression was provided by means of two water filled tanks that serve as wet wells and were connected by a single large diameter pipe to the containment vessel.

6.5.2 Intermediate Pressure Design

The containment vessel for the intermediate pressure reactor, Fig. 6.15, was similar to a conventional containment vessel for a PWR; that is, it was essentially a large building that contained a number of items of equipment. Design characteristics of the vessel are summarized in Table 6.10. In addition to the reactor, the vessel enclosed the auxiliary

Fig. 6.15. Intermediate Pressure Reactor Containment System.

Loeffler boilers, the secondary steam generators, the recirculating steam blowers, the refueling equipment, a water-spray emergency cooling system, and an air-conditioning system. Separate air locks for equipment and for personnel were provided.

The containment vessel was completely enclosed by concrete shielding. The cylindrical section was protected from damage by the interior concrete

Table 6.10. Intermediate Pressure Reactor Containment
Vessel Design Characteristics

Design basis	Contain primary coolant and complete core meltdown
Type of construction	Cylindrical steel vessel with one hemispherical head and one elliptical head
Vessel material	(SA 212, grade B, steel) *
Inside diameter, ft	98.4
Total height, ft	236
Wall thickness, in.	(1.5)
Head thickness, in.	
Hemispherical head	(0.75)
Elliptical head	(1.5)
Maximum internal pressure, psia	36.8 (62)
Net vessel volume, ft ³	(1,204,000)
Weight of vessel, lb	(4,170,000)
Type of pressure suppression	Wet well and water spray

*Values and information in parentheses represent changes of sponsor's design recommended by ORNL in this evaluation.

construction of the reactor building. The interior concrete structure rose to the height of the building crane rails, which rested thereon. This arrangement had the advantage of not subjecting the wall of the containment vessel to the loading of the building crane.

Nuclear Technology Corporation evaluated⁶⁶ the maximum credible accident condition for this reactor. A curve showing the containment pressure as a function of volume is given in Fig. 6.16. The predicted pressure is 47 psig, based on the ORNL estimate of a free volume of 1,200,000 ft³ in the containment vessel. This pressure is conservative, however, in that no credit is taken for wet well or spray-type pressure suppression, which are provided in the design.

In calculating the required wall thickness, SA 212, grade B, steel was assumed for the vessel material. For the predicted accident pressure,

Fig. 6.16. Steam Overpressure in Primary Containment Vessel Resulting from a Loss of Coolant.

the required cylinder wall thickness to satisfy the ASME Code, Section III, is 1.5 in. This thickness is adequate also for the elliptical head. The hemispherical head, however, requires a thickness of only half this amount, or 0.75 in.

The main circulators, steam generators, reheaters, and turbine-generators were housed in a conventional controlled leakage building maintained under slight negative internal pressure. The condensers, feed-water heaters, startup boiler, and plant turbine generator were also in the controlled leakage building.

6.5.3 Low Pressure Design

The low pressure reactor had two containment vessels: a close fitting high pressure containment vessel enclosing the reactor vessel, similar to the high pressure reactor containment vessel, and a larger lower pressure containment vessel enclosing the high pressure containment vessel, circulator drives, and refueling system. The design characteristics of these containment vessels are summarized in Table 6.11 and shown schematically in Fig. 6.17.

The high pressure containment vessel was designed to accommodate the full reactor operating pressure in the event of a pipe rupture inside it. As in the high pressure reactor design, SA 302, grade B, steel was assumed

Table 6.11. Containment Vessels for Low Pressure Reactor

	Low Pressure Vessel	High Pressure Vessel
Design basis	Contain coolant and complete core meltdown	
Type of construction	Cylindrical vessel with torispherical heads	Cylindrical vessel with one elliptical head and one hemispherical head
Vessel material	SA 212, grade B, steel	SA 302, grade B, steel
Inside diameter, ft-in.	70-0	24-10
Height, ft	122	60
Wall thickness, in.	1.5	7
Head thickness, in.	1.5	7
Design pressure, psig	62*	1200
Design temperature, °F	500	700
Weight of vessel, lb	1,819,000	1,284,000
Type of pressure suppression	Water spray	None

*90% of accident pressure, as permitted by Section III of the ASME Code.

to be the material of construction. The vessel design differed from that for the high pressure reactor in that it does not have a bolted closure for the head and, therefore, would require field assembly and postweld heat treatment of the head. Penetrations for refueling corresponded to the refueling ports in the reactor vessel. Penetrations in the side of the vessel were provided for removing the circulators.

Entry into the low pressure containment vessel was accomplished through two personnel air locks. There was an equipment hatch at the operating floor level for removal of the spent and new fuel transfer casks.

The low pressure containment vessel was sized by Sargent & Lundy to accommodate the maximum pressure resulting from a loss-of-coolant accident (that is, the maximum credible accident) and to use 1 1/2-in.-thick plate to avoid the necessity of postweld heat treatment. ORNL calculations

Fig. 6.17. Low Pressure Reactor Containment System.

for the proposed vessel show that it is capable of containing a maximum accident pressure of 69 psig as a class B (containment) vessel under Section III of the ASME Code. An accident analysis similar to the one⁶⁶ made for the intermediate pressure reactor was used to determine the developed accident pressure. From this analysis, it was found that a minimum free volume of 320,000 ft³ is required in the low pressure containment vessel in order for the design pressure to not be exceeded under conditions of the maximum credible accident. It is thus required that 77% of the gross volume of the vessel be available as free volume.

Detail drawings are not available from which an accurate calculation of the free volume may be made. The large amount of concrete shown inside the containment vessel indicates that the design may be marginal in terms of available expansion volume; however, considering its preliminary

nature, the design is reasonable. Accordingly, estimated costs for the vessel were based on the design as proposed.

6.6 Reactor Auxiliary Systems and Plant Services

The auxiliary systems and plant services for the steam-cooled plants were essentially the same as for a BWR plant, except for some differences in capacity requirements. Principal auxiliary and plant service systems not discussed elsewhere in this report included: (1) electrical system, (2) plant compressed-air system, (3) radioactive waste disposal systems (liquid and gaseous), (4) chemical cleaning and decontamination systems, (5) service and domestic water systems, (6) fire protection system, (7) communication system, and (8) emergency steam and electrical systems. Other auxiliary systems are the emergency core cooling system described in Section 6.2, the circulating water system, and the feedwater purification system; the latter two systems were considered in the turbine-plant requirements.

Since there are no special problems involved in the design of these systems, this review was concerned only with each system being adequately accounted for in the plant layouts and with determining that capacity requirements were consistent with the requirements of the different designs.

6.7 Site and Buildings

The ground rules for the evaluation studies specified the AEC hypothetical Middletown site as the reference site for the plants. Site conditions are described in the USAEC "Guide to Nuclear Power Cost Evaluation."¹⁵ Modifications to the plant layout designs of this study were assumed as required to conform with the site conditions and normalized layout considerations.

Takeoffs of building areas and volumes from the proposed layouts were made for the purpose of capital cost estimates. Some of the areas, particularly for the turbine-generator and auxiliary systems, were adjusted as a step toward normalizing the designs. It was necessary to prepare a new layout of the reactor building for the high pressure system

to account for differences in the designs and layouts given in the early design report³ and the revised reference design report.⁴ An increase in reactor building size was required for this concept to provide for the emergency core cooling system equipment added by ORNL to the design. The turbine buildings of the intermediate and high pressure systems were sized to accommodate a single 1000-Mw(e) (net) tandem compound turbine-generator unit rather than the two 500-Mw(e) tandem compound and 1000-Mw(e) cross compound units, respectively, of the original designs of these concepts. Moderate reductions in some of the reactor building volumes were assumed for the intermediate pressure design to provide consistency with the other designs. However, potential reductions in the building volume for possible reductions in the number of steam circulation loops were not assumed, since normalization of reactor systems was not carried to that extent in this study. The building sizes were used as reported for the low pressure design. A summary of the evaluated building sizes is given in Table 6.12.

The reactor buildings housed areas under containment and areas where a slight negative pressure is maintained to prevent outleakage except through the filter-adsorber systems and stack. These latter areas are designated as confinement areas in Table 6.12. Since these were direct-cycle plants, the turbine buildings were also maintained at a slight negative pressure to prevent outleakage and, as in BWR plants, had shielding around the turbines and other equipment exposed to the primary steam. The dose rate due to ^{16}N activity at the outer surfaces of the pipes leading to the turbines was estimated in the Karlsruhe study to be about 100 rem/hr. However, calculations in a Nuclear Technology Corporation study⁸⁵ of this system indicate the dose rate at these surfaces to be 1 to 2 rem/hr. The two dose rates correspond, respectively, to equilibrium ^{16}N activities of 1.7 and 0.01 mc/cm³.

The arrangement of buildings on the site differed among the concepts. For the purpose of normalization, it was assumed that the buildings would be arranged with respect to the river to minimize the lengths of water circulating lines. A typical plant arrangement is shown in Ref. 15. A rearrangement of the low pressure reactor layout was assumed, since the proposed design provided for an addition of a second plant, and the length

Table 6.12. Summary of Data on Structures

	Low-Pressure System			Intermediate-Pressure System			High-Pressure System		
	Plan (ft × ft)	Area (ft ²)	Volume (ft ³)	Plan (ft × ft)	Area (ft ²)	Volume (ft ³)	Plan (ft × ft)	Area (ft ²)	Volume (ft ³)
Turbine-Generator Building	115 × 290	33,350	4.26 × 10 ⁶	100 × 300	30,000	3.45 × 10 ⁶	100 × 300	30,000	3.45 × 10 ⁶
Auxiliary Building	70 × 335	23,450	1.87 × 10 ⁶				50 × 100	5,000	0.40 × 10 ⁶
Reactor Building		16,800	2.90 × 10 ⁶		29,400	3.62 × 10 ⁶		16,200	1.77 × 10 ⁶
Includes confinement areas	120 × 140	16,800	2.50 × 10 ⁶	164 × 143	21,500	1.66 × 10 ⁶	130 × 135	16,200	1.76 × 10 ⁶
containment cylinder	70 diam, 122 high		0.40 × 10 ⁶	100 diam, 250 high	7,900	1.96 × 10 ⁶	19 diam, 40 high		0.01 × 10 ⁶
Control Room	In Auxiliary Building			In Service Building			42 × 60	2,520	0.12 × 10 ⁶
Administration Building	In Service Building			55 × 132	6,860	0.17 × 10 ⁶	110 × 70 ^a	7,700	0.5 × 10 ⁶
Fuel-Handling Facilities	In Reactor Building			In Reactor Building			In Reactor Building		
Radioactive Waste Facilities	69 × 103	7,000	0.35 × 10 ⁶	66 × 144	9,500	0.25 × 10 ⁶	54 × 116	6,260	0.24 × 10 ⁶
Service Building	46 × 262 ^b	12,050	0.96 × 10 ⁶	132 × 394 ^c	52,000	2.34 × 10 ⁶	64 × 96 ^d	6,140	0.12 × 10 ⁶
Crib House	40 × 56	2,240	0.14 × 10 ⁶	34 × 100	3,400	0.17 × 10 ⁶	60 × 60	3,600	0.14 × 10 ⁶
Miscellaneous Buildings (Guard House, Well House, etc.)	Negligible			Negligible			Negligible		
Stack (free standing)	(300 high) ^e			(300 high) ^e			(300 high) ^e		

^aIncludes laboratories, lockers, and storage.^bIncludes laboratories, shops, and offices.^cIncludes control room, shops, fuel storage, decontamination facilities, and offices.^dIncludes shops.^eAdded during evaluation.

of the water circulating lines was longer than necessary for a single plant at the site.

6.8 Plant Safety Equipment Reliability Requirements

The reliability aspects of systems whose correct operation is essential to the protection of the plant or the public were identified and evaluated. The potential accidents that a steam-cooled reactor might be subject to are discussed in Section 5.3. The emergency systems provided in the designs are described earlier in this chapter.

6.8.1 Systems Essential to Containment

The systems essential to containment are associated with preserving the integrity of the containment system. This group includes systems for the following essential functions: shutting the reactor down, emergency cooling of the core, poisoning of water that might flood the core, isolating coolant lines leaving the outer containment buildings, reducing the pressure in the outer containment buildings with water sprays, and collecting, filtering, and releasing from a stack the radioactive leakage from the pressure containment building. In developing this list it was assumed that preventing major core damage is essential to containment integrity because the designs do not include provisions for handling a **destructive nuclear excursion or containment melt-through**.

The essential systems must be designed to meet safety system standards.^{86,87} All the components in the essential systems should have sufficient redundancy and diversity in pumps, valves, prime movers, piping, emergency power supplies, and instrumentation that a single failure cannot prevent the successful operation of the system.

6.8.1.1 Reactor Safety System and Scram Mechanism. Except for the power limiting effect of the Doppler coefficient associated with the fuel, the steam-cooled reactor did not have an inherent shutdown mechanism, so an applied reactivity shutdown will be required to prevent core melt-down. The small plutonium beta fractions, short neutron lifetime, and minimal reserve heat storage capacity inherent in steam-cooled fast breeder

cores require prompt safety action. The scram must be initiated within times less than 0.2 sec, as discussed in Section 5.3.1.

The design reports did not include safety system designs. The rod drives are discussed in Section 6.3, along with several potential problem areas. The drives are probably capable of the fast scram response times required. Scram insertion force was provided by a spring in the intermediate pressure system drives and by a 1200-psi differential pressure in the low and high pressure system drives. The designs should be carefully reviewed to determine whether forced insertion of reactivity is essential and whether the force provided by the rod drive designs is sufficient. The rod insertion might be opposed by distorted core geometries or possibly by reverse pressure differential forces across the core (as discussed in Sections 5.3.2.2 and 5.3.4.1).

A highly reliable applied shutdown requires the use of two complete and independent sets of safety systems and fast scram mechanisms.^{70, 88} The reference designs **did** not include any form of reserve (or secondary) shutdown system and, therefore, a second automatic safety system and a fast scram mechanism are recommended. In order to minimize common failure modes (systematic failures), the two systems must be of different design and construction, and both must have adequate time response.

6.8.1.2 Emergency Core Cooling Systems. Emergency core cooling systems are required to function if normal shutdown cooling is not available or if a loss-of-coolant accident has occurred in which the leak cannot be isolated. The emergency core cooling problem is more difficult in the steam-cooled reactors than in large light water reactors because the fuel heats up four to five times faster if it is not cooled, and the time margin for starting emergency cooling would be much shorter. (At full power, the fuel of a steam-cooled fast breeder reactor would heat up at the average rate of approximately 400°F/sec if forced cooling was interrupted.) The discussion of the loss-of-coolant accident in Section 5.3.2.2 indicates that the cladding temperature will reach the failure point within 1 to 5 sec if emergency cooling has not started. An earlier study⁶⁶ indicated that fuel melting would occur within 1 to 4 min without emergency cooling. However, since cladding failure might allow the fuel to move into a more reactive configuration or obstruct cooling, it is probably

more significant than fuel melting in determining the allowable time margins.

The reference reactor designs did not provide the degree of redundancy in emergency core cooling systems that is employed in current light-water reactor designs.⁸³ The primary pressure vessel designs prevented the use of rapid flooding systems (unless all the coolant lines at the bottom of the vessels could be isolated following a break in one of these lines).

Direct core spray (or steam fog from a spray, as is possibly the case in the low pressure design) was the main form of low pressure emergency cooling. This is in contrast to the emergency core cooling systems of gas-cooled and sodium-cooled reactors, which depend on forced circulation of the coolant for this function. The spray systems provided in the steam-cooled reactor designs, required capacities, and the problems of flow distribution are discussed in Section 6.2.2. In general, the designs provided one spray system with redundant pumps. Since these are essential systems, it is recommended that, as a minimum, two independent, full capacity spray systems be provided, each with a separate spray ring and source of water. The auxiliary boilers might be used to inject steam for fog cooling to serve as a diverse form of low pressure emergency cooling. The time margins mentioned above are too short for starting pumps and emergency electric power sources, and measures such as using pressurized storage tanks (as in recent PWR designs) or running the spray pumps continuously against closed valves may be needed.

It will probably be necessary to add some form of high pressure emergency core-cooling systems for cases such as loss of forced circulation or slow depressurization. In the intermediate pressure reactor design, the Loeffler boilers in the secondary steam systems might partially serve for this purpose. In the high pressure reactor design, the pressure of the pressurized head tank might be increased so that it could partially serve for this purpose. Current BWR designs have steam driven pumps, and PWR designs include pressurized storage tanks for injection of water for high pressure emergency cooling.⁸³ These systems can probably be adapted for use in the steam-cooled plants.

Several recommendations can be made that would contribute to minimizing the consequences of the loss-of-coolant accident during the first

few seconds and, thereby, to reducing the requirements on the emergency core cooling systems. The heated water supplies riding on the primary system could be made more effective in sustaining core coolant flow by making their connecting lines to the primary system as short as possible and their diameters as large as possible. Venturis could be added to the steam inlet and outlet lines (where not already provided) to limit the sonic flow and extend the blowdown time. Continuation of feedwater flow would help to extend the blowdown time. The intermediate pressure reactor design is better than the other two in this regard, since two parallel feedwater systems are provided with separate piping, feedwater heaters, pumps, etc. The two B&W designs provided only a single feedwater chain. The reliability could be improved by splitting the system into parallel chains (or by using an emergency feedwater system, as in the Gulf General Atomic design for the gas-cooled fast reactor) so that the failure of a single pipe would not cut off all the feedwater going to the reactor. In the low pressure reactor design, the use of separate headers and spray nozzles for each feedwater inlet line would prevent the short circuiting of most of the feedwater by a failure in a single feedwater line.

6.8.1.3 Poisoning Systems. Although flooding of the core may not be feasible in the loss-of-coolant accident, it is possible under some conditions discussed in Section 5.3.3.1 that the core could be flooded. As discussed in Section 5.3.1.2, the steam-cooled cores may be most reactive in the partially or fully flooded condition. Under these conditions, the core may be critical even with all of the rods inserted. This situation will require the addition of some form of poison-injection system that could add sufficient negative reactivity to make the core subcritical, regardless of the source of flooding water.

6.8.1.4 Containment Isolation Systems. The coolant lines penetrating the outer containment vessel must be isolated if there are enough fuel cladding failures to release significant amounts of fission products to the coolant. The penetration-isolation techniques have not been designed in detail. The high pressure reactor design had two valves in series at the boundary of the high pressure containment vessel. (This

design did not have a low pressure containment vessel.) The intermediate-pressure reactor design had one isolation valve just outside the reactor vessel, and one outside the low pressure containment vessel for each coolant line. It would be necessary to use two valves in series⁸⁹ at the boundary of the low pressure containment vessel (as is done in BWR's). The low pressure reactor design was unusual in that it had both high and low pressure containment vessels. One valve was used between the reactor vessel and the high pressure containment vessel, and there was another outside the low pressure containment vessel for each coolant line. Again, it is thought that it will be necessary to use two valves in series at the boundary of the low pressure containment vessel. Isolation of coolant lines at the high pressure containment boundary will serve to hold the reactor pressure up for breaks inside of this vessel; however, isolation at this point does not appear to be essential to the protection of the public. (It is not clear that there is room for actuators for the isolation valves shown between the reactor vessel and high pressure containment vessel, or how these valves could be maintained.)

It will be necessary to isolate all the coolant lines penetrating the outer containment vessels upon the detection of high radiation in the steam. The system for doing this will have severe reliability requirements since, in addition to the containment considerations, inadvertent isolation of all the coolant lines would eliminate the sources of on-line water that flash and provide some initial core cooling during a loss-of-coolant accident. Provision should be made for on-line testing and exercising of isolation valves (as is done in conventional steam plants for the main stop valves that protect the turbines from overspeeding).

6.8.1.5 Containment Pressure Reduction Systems. Spray systems were provided in the low and intermediate pressure reactor designs to reduce the pressure buildup in the low pressure containment buildings. These systems are essential to the protection of the public, since they are needed to prevent the failure of the containment vessel from overpressure due to afterheat and, also to reduce the building pressure to near atmospheric to limit the leakage of radioactivity. The low pressure reactor design had a dousing tank in the top of the containment building similar to that employed in Canadian designs. This has the attractive reliability

feature that the sprays can operate for a considerable length of time without pumps. The intermediate pressure reactor design had a more conventional system that depended on pumps. It is recommended that two completely independent, full capacity systems⁸⁹ be provided as a minimum.

6.8.1.6 Emergency Electrical Power. The low pressure reactor design provided two 2500-kw Diesel generators for on-site emergency power sources. The intermediate pressure reactor design provided two house generators (of unspecified size) that were in continuous operation and were driven by steam turbines in the two secondary coolant loops. These could also be driven by steam from the auxiliary boiler. These power sources will become significant items if the emergency core cooling capabilities are increased to something like those of a BWR (with essential electrical loads of approximately 6000 to 7000 hp).

It is recommended that two sources⁸⁹ of on-site power be available. Each unit should be sized to individually provide power for essential electrical loads (one-half of load where essential loads are redundant). These systems will have a very short time margin of a couple of seconds or less for startup, and this will probably require the use of continuously fired auxiliary boilers for electrical power sources rather than Diesel generators.

6.8.1.7 Dynamic Containment Systems. All three designs included dynamic containment systems that consisted of negative pressure buildings around pressure containment buildings, exhaust blowers, radioactive filters, and stack release. These systems are not developed in any detail in the reference designs, and it is not clear that they are essential systems for reactor siting. A Karlsruhe study⁶¹ completed after the reference design report was issued indicates that these systems may be considered essential. For comparison, dynamic containment is an essential element in the siting of current large BWR's, and filtering and elevated release give a significant reduction in the dose at site boundaries. However, the large PWR designs do not employ dynamic containment, and can tolerate ground level release with their lower leakage from the pressure containment vessel.

The Karlsruhe report¹⁰ gives the most detail on these systems. Three exhaust blowers are provided, with one being a 50% spare. These systems

can be made to be highly reliable, since they are in continuous operation; however, if they are essential systems, it is recommended that 100% standby capacity in exhaust blowers, ducts, and filters be provided.

6.8.2 Systems for Maintaining Forced Coolant Flow

The forced circulation of coolant by the main steam circulators is not essential to core integrity. However, it would be desirable to continue forced circulation during accident conditions for the economic considerations of reducing the damage to the plant, and for possible safety considerations of reducing the number of times the emergency core cooling system would be called on to act. In this light, it may be desirable to provide the following systems with reliability features comparable with those needed for the essential systems.

6.8.2.1 Circulator Motive Power Systems. The main circulators in the high and low pressure designs were driven directly by reactor steam. In the event of closure of the turbine stop valves, the steam exhausting from the circulator drive turbines would be bypassed to either the main or auxiliary condensers to provide continuity of flow. The reliability of this system could be improved by providing some redundancy in auxiliary (emergency) condensers. For example, the Swedish steam-cooled reactor design¹³ provided a separate emergency condenser for each circulator for this purpose. The Karlsruhe intermediate pressure design had the additional feature of two secondary steam coolant loops that drove the main circulators. To provide redundancy in motive power, each of the two secondary loops had its own condenser that was separate from the condensers for the main turbines. This reactor had the additional feature that these secondary loops provided shutdown cooling when the main coolant loops were isolated. One loop with one of two circulators operating was sufficient if the reactor was pressurized.

With the reactor shutdown, the power required to drive the circulator turbines was approximately 1.5% of that required at full power. The afterheat power was instantly available after shutdown, and it was sufficient to provide the power needed for the circulators; however, the steam conversion efficiency might be so low that fuel temperatures could be excessive. For this reason, reliance was placed on the auxiliary boilers

as the main steam supply, and the reactor generated steam was considered a supplemental supply after the initial switchover interval. All three designs provided two redundant auxiliary boilers. The time margin for restoring circulation was very short, as discussed in Section 5.3.2.1, so the auxiliary boilers must be kept in a no-load standby condition to effect a rapid changeover upon demand. (These boilers could also serve as sources of emergency electrical power.)

6.8.2.2 Circulator Bearing Lubrication Systems. The lubrication systems for the circulator bearings are not detailed in the design reports; however, they are generally very complex systems and should be redundant. For example, the Swedish steam-cooled reactor design¹³ provided separate bearing-lubrication water systems for each circulator.

6.8.2.3 Steam Carryover. It will be necessary to provide reliable control systems to prevent the carryover, following a scram, of wet steam from the contact boilers in quantities that might deblade the circulators or to design the circulators to withstand the maximum carryover.

6.8.2.4 Isolation of Coolant Loops. It appears that all three designs would provide systems for detecting and isolating individual coolant loops that have failed. This single-line-isolation philosophy differs from that for boiling water reactors, where all loops are isolated, and it will present a more difficult detection problem. The failed loop must be isolated within a few seconds to prevent a major blowdown. For this fast closure, it may be necessary to use a fast acting valve at each containment penetration that is in addition to the two (slower acting) leak-tight valves needed for containment purposes. These isolation valves, or check valves, will be needed to prevent backflow and possibly coolant bypassing of the core when individual circulators are shut down.

6.8.2.5 Feedwater System. The reliability of the feedwater systems was discussed earlier in this section.

7. ECONOMIC EVALUATION

7.1 Capital Costs

Capital cost estimates for the three 1000-Mw(e) SCBR power stations are listed under five principal accounts in Table 7.1, and a more detailed breakdown is given in Table 7.2. The methods employed in arriving at these estimates were similar to those used and described in other ORNL reactor evaluation studies^{24,90}, and consisted essentially of the following steps:

1. The design parameters evaluated and discussed in Chapter 6, which present an engineering review of the plant designs, were used as the bases for capital cost estimates.
2. Capital cost data presented by the designer were evaluated and compared with cost data on analogous systems or components of other reactor concepts being evaluated.
3. The designer's cost estimates were adjusted, or new cost estimates were made, when it appeared necessary to do so for cost normalization.
4. Independent estimates of major components of the plant were made from unit cost data developed for the ORNL evaluation studies.
5. Cost estimates of systems not clearly defined (such as auxiliary and service systems) were determined by analogy with other reactor plant systems that serve similar functions and for which costs have been developed.
6. Direct construction costs were arranged and displayed in accordance with the AEC classification of accounts given in Table 106-2 of the USAEC "Guide to Nuclear Power Plant Evaluation."¹⁵
7. Indirect costs were based on methods outlined in Ref. 15; however, percentages applied in determining these costs were changed. The percentages used, which were applied to cumulative totals, are listed in Table 7.1.

The cost data supplied by the designers varied widely. Costs for the high pressure concept described in Ref. 3 were furnished by B&W, but were not revised to account for changes in the reference design described

Table 7. . Estimated Total Capital Costs for 1000-Mw(e) Steam-Cooled Breeder Reactor Power Plants

	Basis of Indirect Cost (% of accumulated cost)	Low-Pressure System	Basis of Indirect Cost (% of accumulated cost)	Intermediate- Pressure System	Basis of Indirect Cost (% of accumulated cost)	High-Pressure System
Direct construction cost						
Account						
21 - Structures and improvements		\$ 19,063,000		\$ 15,391,000		\$ 15,200,000
22 - Reactor plant equipment		34,909,000		47,230,000		39,240,000
23 - Turbine-generator units		32,853,000		25,213,000		24,307,000
24 - Accessory electric equipment		4,254,000		4,000,000		3,800,000
25 - Miscellaneous power plant equipment		1,250,000		1,250,000		1,250,000
Total direct construction cost		\$ 92,269,000		\$ 93,084,000		\$ 83,797,000
Indirect construction cost						
General and administrative	6	5,536,000	6	5,585,000	6.2	5,195,000
Subtotal		\$ 97,805,000		\$ 98,669,000		\$ 88,992,000
Miscellaneous		978,000		987,000	1	889,000
Subtotal		\$ 98,783,000		\$ 99,656,000		\$ 89,881,000
Engineering design and inspection						
Architectural and engineering services	5	4,933,000	5	4,983,000	5.1	4,584,000
Subtotal		\$103,722,000		\$104,639,000		\$ 94,465,000
Nuclear engineering	2.0	2,074,000	2.0	2,093,000	2.1	1,984,000
Subtotal		\$105,796,000		\$106,732,000		\$ 96,449,000
Startup		786,000		783,000		781,000
Subtotal		\$106,582,000		\$107,515,000		\$ 97,230,000
Contingency		10,658,000		10,752,000	10	9,723,000
TOTAL DIRECT AND INDIRECT CONSTRUCTION COST		\$117,240,000		\$118,267,000		\$106,953,000
Customer cost						
Investor-owned plant						
Interest during construction		12,662,000		12,773,000	10.8	11,551,000
TOTAL DEPRECIABLE CAPITAL COST		\$129,902,000		\$131,040,000		\$118,504,000
20 - Land and land rights		360,000		360,000		360,000
TOTAL INVESTOR-OWNED CAPITAL COST		\$130,262,000		\$131,400,000		\$118,864,000
Publicly owned plant						
Interest during construction		8,441,000		8,515,000	7.2	7,701,000
TOTAL DEPRECIABLE CAPITAL COST		\$125,681,000		\$126,782,000		\$114,654,000
20 - Land and land rights		360,000		360,000		360,000
TOTAL PUBLICLY OWNED CAPITAL COST		\$126,041,000		\$127,142,000		\$115,010,000

Table 7.2. Details of Direct Construction Costs

	Low-Pressure System	Intermediate- Pressure System	High-Pressure System
Account			
21 - Structures and improvements			
211 Ground improvements	\$ 1,000,000	\$ 1,000,000	\$ 1,000,000
212 Buildings			
A-Turbine-generator plant	4,400,000	3,840,000	3,800,000
B-Reactor auxiliaries	420,000	2,133,000	360,000
C-Administration	1,250,000	330,000	330,000
D-Miscellaneous	50,000	50,000	50,000
E-General service	In 212C	618,000	550,000
F-River-front structures	850,000	700,000	650,000
Total cost, item 212	\$ 6,970,000	\$ 7,671,000	\$ 5,740,000
218 Stack	\$ 100,000	\$ 100,000	\$ 100,000
219 Reactor building			
A-Containment vessels	6,983,000	4,620,000	5,420,000
B-Confinement areas	3,950,000	2,000,000	2,940,000
Total cost, item 219	\$10,933,000	\$ 6,620,000	\$ 8,360,000
Total cost, account 21	\$19,003,000	\$15,391,000	\$15,200,000
22 - Reactor plant equipment			
221 Reactor equipment			
.1 Pressure vessel	\$ 7,600,000	\$ 9,700,000	\$ 8,700,000
.2 Reactor controls	1,200,000	1,000,000	1,000,000
.3 Shielding (cooling)	120,000	120,000	100,000
.4 Auxiliary heating and cooling	2,075,000	1,650,000	2,500,000
.5 Cranes and hoists	150,000	150,000	150,000
Total cost, item 221	\$11,145,000	\$12,620,000	\$12,470,000
222 Heat transfer systems			
.1 Reactor coolant system	\$ 5,464,000	\$12,300,000	\$ 3,370,000
.2 Reheaters (live steam)		3,360,000	3,500,000
.3 Primary coolant treatment	200,000	200,000	200,000
.4 Auxiliary steam supply (oil-fired)	600,000	300,000	600,000
Total cost, item 222	\$ 6,264,000	\$16,160,000	\$ 7,670,000
223 Nuclear fuel handling and storage	\$ 2,500,000	\$ 2,000,000	\$ 2,500,000
225 Radioactive waste treatment and disposal	400,000	400,000	400,000
226 Instrumentation and control	3,100,000	3,900,000	3,000,000
227 Feedwater supply and treatment	5,600,000	5,150,000	6,300,000
228 Steam, condensate, and feedwater piping	5,100,000	6,200,000	6,100,000
229 Other reactor plant equipment	800,000	800,000	800,000
Total cost, account 22	\$34,909,000	\$47,230,000	\$39,240,000
23 - Turbine-generator unit			
231 Turbine-generator	\$26,075,000	\$19,976,000	\$19,174,000
232 Circulating water systems	2,070,000	1,575,000	1,500,000
233 Condensers and auxiliaries	3,253,000	2,429,000	2,300,000
234 Central lubricating system	80,000	80,000	80,000
235 Instruments and controls	600,000	400,000	400,000
236 Piping	575,000	553,000	653,000
237 Generator auxiliaries	75,000	75,000	75,000
238 Other	125,000	125,000	125,000
Total cost, account 23	\$32,853,000	\$25,213,000	\$24,307,000
24 - Accessory electric equipment	\$ 4,254,000	\$ 4,000,000	\$ 3,800,000
25 - Miscellaneous power plant equipment	\$ 1,250,000	\$ 1,250,000	\$ 1,250,000

in Ref. 4. Furthermore, several other changes were made as the result of the engineering review and discussions with B&W representatives. Some of these changes, such as replacing motor drives with turbine drives and assuming the use of a tandem compound turbine-generator instead of a cross compound unit, are discussed in Chapter 6. Additional changes, not accounted for in Chapter 6, were assumed at the suggestion of B&W to make the containment and safety features of the high pressure concept more equivalent to those of the low pressure concept, and the cost estimates given in Tables 7.1 and 7.2 include the costs of these changes. The changes made to the reference high pressure design described in Chapter 6 consisted essentially of the following: (1) modifications to the reactor vessel to accept integral circulators and mixers, (2) an increase in the size of the 1000-psi secondary containment vessel, (3) elimination of the recirculating loop piping and valves, (4) revision of the reheat system to use a condensing live steam reheater that gives a slightly lower thermal efficiency, (5) addition of a tertiary (low pressure) containment vessel, and (6) additions to the isolation condenser system. Based on the new cost estimates made by ORNL, the effect of these changes was to increase the estimated cost of the reference high pressure concept from about \$114/kw to the present value of \$121.7/kw for an investor-owned plant. The B&W estimates for several components of the high pressure concept are referred to in the following discussions, but an itemized comparison between the designer's estimates and ORNL's was not attempted.

The intermediate pressure system costs were presented in Deutsche marks and with cost accounting that was not readily useful for cost normalization. Therefore, new cost estimates were prepared.

The low pressure concept cost analysis⁹¹ was prepared in some detail by Sargent & Lundy Engineers. These costs were adjusted to account for differences in indirect costs and other judgments to place all concepts on a common basis. Areas of important differences are described later in this chapter.

A discussion, by accounts, of bases and assumptions used in arriving at the estimates reported in Tables 7.1 and 7.2 is presented below. The estimates are intended to represent costs as of June 1967, with no allowance for possible future escalation of costs.

7.1.1 Direct Construction Costs

Account 21 - Structures and Improvements. Identical improvement costs were assumed for each concept, since the ground rules specified locating each plant at the AEC hypothetical site described in Ref. 15. The costs included clearing and grading of the site and improvements such as railroad spurs, roads, fencing, lighting, and fire protection, sanitary sewer, and storm systems, which were assumed to be alike for each plant. A cost of \$1,000,000 was estimated for these items.

Structures (accounts 212, 218, and 219) were examined for the two higher pressure systems on the bases of what appeared necessary for the proposed equipment arrangement, since the designers did not present sufficient detail to permit close evaluation. The low pressure design was given in sufficient detail to permit direct estimates to be made. Area and volume requirements for the principal buildings are given in Section 6.7 of this report.

The costs presented for the turbine-generator buildings were based on the use of a single 1000-Mw(e) (net) tandem compound unit in all cases. Only the low pressure plant designers considered this arrangement. The high pressure reactor was designed for a cross compound unit, while the intermediate pressure design proposed two 500-Mw(e) (net) tandem compound units. Building costs were assessed on cost per gross cubic feet for the various areas; that is, areas above the operating floor, below the operating floor, and below grade. Unit costs average about 80 cents per gross cubic foot for the total volume for a direct cycle plant.

Buildings other than the reactor building consisted of facilities for service and auxiliary equipment, radioactive waste disposal, control room, offices, and laboratories. Unit costs for these buildings were taken as comparable with those of similar facilities for other reactors, such as light water reactors, although space requirements for the specific plants may differ. For these facilities, unit costs varied from about 40 cents to over \$2.00 per gross cubic foot.

Reactor structure costs, including shielded areas, but exclusive of secondary high pressure containment and pressure-suppression systems, were estimated by zones. Unit costs ranged from \$1.35 to over \$2.00 per cubic

foot. High pressure containment vessels were estimated in a manner similar to that used for estimating reactor pressure vessels. Table 7.3 outlines the basic features selected for estimating the costs of the containment and reactor vessels. In addition to the features shown in Table 7.3, other factors, such as internals, special features, and requirements of fabrication and erection, were considered in arriving at the total installed cost.

Account 22 - Reactor Plant Equipment. Reactor equipment costs (account 221) are made up principally of the cost of the pressure vessel and its internals. A breakdown of these costs is listed in Table 7.3, and a summary of pressure vessel cost details is given in Table 7.4.

Because of the manner of construction of the intermediate and high pressure system vessels, the cylindrical sleeve was estimated as though it was a separate vessel. Within limits of 14 in. maximum wall thickness, unit costs were reduced for increased wall thicknesses. The penalty

Table 7.3. Summary of Pressure Vessel Characteristics and Costs

	Low-Pressure System	Intermediate- Pressure System	High-Pressure System ^a
Reactor pressure vessel			
Construction	Welded	Flanged	Welded
Material	Type 302, grade B, steel	Type 302, grade B, steel	Type 302, grade B, steel
Wall thickness, in.	7	15	20
Shielding material	Stainless steel	Stainless steel	Stainless steel
Shielding weight, lb	367,000	1,336,460	210,000 (285,000)
Vessel weight, lb	1,000,000	1,442,000	1,194,000 (1,600,000)
Unit cost, \$/lb			
Stainless steel-clad vessel	3.50	3.50	3.75
Shields	2.00	2.00	2.00
Low-pressure containment vessel			
Wall thickness, in.	1.5	Not applicable	
Weight, lb	1,819,000	Not applicable	
Unit cost, \$/lb	1.20	Not applicable	
High-pressure containment vessel			
Wall thickness, in.	7		5.25
Weight, lb	1,284,000		404,000 (530,000)
Unit cost, \$/lb	3.50		3.50

^aValues in parentheses are for modified design.

Table 7.4. Pressure Vessel Costs

	Low Pressure System	Intermediate Pressure System	High Pressure System*
Basic vessel	\$4,300,000	\$5,100,000	\$4,480,000 (6,000,000)
Penetrations	300,000	600,000	300,000 (430,000)
Shielding	700,000	2,600,000	420,000 (570,000)
Internals	1,800,000	400,000	500,000 (1,200,000)
Flange penalty		500,000	
Transportation	500,000	500,000	500,000
Total	\$7,600,000	\$9,700,000	\$6,200,000 (8,700,000)

*Values in parentheses are for modified design.

for cylindrical sleeve fabrication is not well known. It was assumed that the techniques are feasible and that costs would be competitive with those of thinner vessels (up to about 14 in.).

Shop fabrication of all major vessel components was considered. However, the high pressure containment vessels and possibly the reactor vessels for the low and high pressure concepts would require field welding of the heads to the vessels and subsequent postweld heat treatment.

In the low and high pressure designs, the use of internal control rods (also a part of account 221) actuated by steam pressure resulted in a large cost saving compared with the cost of the more conventional hydraulic jack or rack-and-pinion drives. The designer obtained a shop estimate of \$5000 per control rod unit, and the installed cost of the associated steam actuator systems was estimated to be \$3500 per unit. The ORNL estimate of the control rod contribution to the fuel element assembly cost agrees fairly well with the designer's estimate.

Estimates of the high pressure plant costs for emergency cooling, decay heat removal, and other auxiliary heating and cooling systems were based on the equipment requirements of a BWR system adjusted for power

level, as described in Chapter 6. The intermediate pressure plant systems were indicated but not very well detailed, and, thus, some assumptions were required regarding size and duty. The low pressure plant systems also lacked clear definition of equipment, although costs were supplied and appeared to be satisfactory.

Costs of heat transfer systems (account 222) were evaluated by checking the major cost items, which are steam circulators and heat exchangers.

ORNL verified costs reported by the designers for the low and high pressure steam circulators by comparison with ORNL unit cost data. The turbine drive costs reported for the low pressure concept checked closely with the ORNL estimate. New estimates were made for the intermediate pressure plant circulators and drives and for turbine drives for the high pressure design, which were substituted for the motor drives originally specified. Selected compressor design parameters and costs are given in Table 7.5.

Only the intermediate and high pressure systems had live steam reheaters. The sizes of the reheaters are given in Chapter 6. From ORNL cost data on heat exchangers, a unit cost of \$66/ft² was estimated for the high pressure system and \$41/ft² for the intermediate pressure system. In comparison, the designer estimated a cost of $\$4.4 \times 10^6$ for the high pressure system or about \$74/ft² for one-third less area.

Table 7.5. Main Steam Circulating Blower
Characteristics and Cost Bases

	Low Pressure System	Intermediate Pressure System	High Pressure System
Type	Single stage centrifugal	Multistage axial	Single stage centrifugal
Number	4	6	2
Drive power, each, hp	42,500	14,900	33,500
Total cost, \$			
Drive and associated equipment	3.60×10^6 (*)	2.67×10^6	2.07×10^6
Circulator	1.70×10^6	2.32×10^6	1.31×10^6

*Condenser and associated equipment not required.

The fuel handling machine (a portion of account 223) was reported to cost in the order of $\$1 \times 10^6$ for the high pressure plant. In addition to the refueling manipulator, this account covered the refueling port closure system, control rod drive replacement system, spent fuel transfer system, fuel casks, and related hardware. A seal welder and cutter is included in account 229, Other Reactor Plant Equipment. The intermediate pressure plant had the simpler requirements and a lower cost.

The contribution of the other components of the fuel handling system to the overall account cost was not closely examined. In the absence of specific designs, casks, viewing equipment, and transfer systems were evaluated on rather broad bases.

Radioactive waste treatment and disposal system (account 223) costs were assumed to be \$400,000 for all cases; this is an amount that is typical for light water reactor plants. Specific requirements were not specified, but they would probably not be peculiar to these plants.

Instrumentation and controls (account 226) estimates are shown in Table 7.6. The use of computer control was assumed for all cases.

A burst slug detection system specified by the designer was priced for only the intermediate pressure reactor. This feature was not included in the other designs.

The costs of feedwater supply and treatment systems (account 227) constituted about 5% of total direct costs. The individual items were considered near-standard designs that paralleled those of present direct cycle boiling water reactor systems. These costs (shown in Table 7.7) were not based on optimized designs but represent conservative equipment selections. The designers for the low pressure plant provided system details from which cost estimates could be made directly. Stainless steel tubes for the feedwater heaters were considered for all cases, since these are direct cycle plants.

Steam, condensate, and feedwater piping (account 228) also followed conventional practice. The high pressure plant designers specified type 316 stainless steel for high temperature piping (1000°F). High alloy steel (P-22) is an alternative; however, a detailed study would be necessary to determine the choice. For steam, the estimates were based on

Table 7.6. Instrumentation and Control Cost

	Low Pressure System	Intermediate Pressure System	High Pressure System
Fixed costs			
Reactor and associated controls	\$1,000,000	\$1,000,000	\$1,000,000
Burst slug detection system		400,000	
Computer	800,000	800,000	800,000
Radioactive waste disposal system	250,000	250,000	250,000
Total fixed cost	\$2,050,000	\$2,450,000	\$2,050,000
Variable costs			
Heat transfer systems	\$ 350,000	\$ 650,000	\$ 250,000
House turbine-generators		100,000	
Fuel handling system	200,000	250,000	300,000
Piping, tubing, and wiring	500,000	450,000	400,000
Total variable cost	\$1,050,000	\$1,450,000	\$ 950,000
Total	\$3,100,000	\$3,900,000	\$3,000,000

Table 7.7. Feedwater Supply and Treatment System Costs

	Low Pressure System	Intermediate Pressure System	High Pressure System
Makeup supply, treatment, and storage	\$ 400,000	\$ 400,000	\$ 400,000
Condensate purification	800,000	800,000	800,000
Feedwater heaters	3,400,000	2,400,000	3,000,000
Feedwater pumps and drives	1,000,000	1,550,000	2,100,000
Total	\$5,600,000	\$5,150,000	\$6,300,000

stainless steel for 1000°F piping, and alloy steel for temperatures under 1000°F. Unclad carbon steel was assumed for feedwater piping. Major piping system costs are given in Table 7.8.

The cost of other reactor equipment (account 229), principally decontamination and remote handling equipment, was estimated at \$0.8 million for all cases.

Account 23 - Turbine-Generator Systems. The turbine-generator installed costs (account 231) were based on a single tandem compound unit for all concepts. The designers proposed a single cross compound unit for the high pressure plant and two tandem compound units for the intermediate pressure plant. Substitution, as indicated, gives costs favorable to both plants over those proposed, and also conforms to the current trend in selection of single tandem compound units when practical. Pricing was developed from list prices,⁹² with 7% discount. Bases for the costs are listed in Table 7.9. Recent information⁹³ indicates that units in the 650- to 800-Mw(e) range may be uprated to 1000 Mw(e) without serious loss of performance, with considerable capital savings over nominal rating prices for 1000-Mw(e) units. The final selection would need to be optimized, but for normalization, the costs were based on uprating the units.

Water circulating systems and condenser costs were estimated for two-pass stainless steel tubes with 27°F temperature rise and 57°F inlet water. Intake and discharge structure costs are included in account 21,

Table 7.8. Principal Piping System Costs

	Low Pressure System	Intermediate Pressure System	High Pressure System
Main and auxiliary piping	\$2,300,000	\$2,800,000	\$2,400,000
Condensate and feedwater piping	1,800,000	2,200,000	2,500,000
Other piping	1,000,000	1,200,000	1,200,000
Total	\$5,100,000	\$6,200,000	\$6,100,000

Table 7.9. Turbine-Generator Installed Costs and Design Bases

	Low-Pressure System	Intermediate-Pressure System	High-Pressure System
Type of unit	Tandem compound, six flow, 38-in. last-stage blades	Tandem compound, six flow, 33.5-in. last-stage blades	Tandem compound, six flow, 33.5-in. last-stage blades
Speed, rpm	3600	3600	3600
Rating			
Mw(e)/Mva	965 ^a /1130	955 ^b /1117	950 ^b /1111
psi/°F/°F	1050/925/565 ^c	2400/1000/950	3500/1000/1000
Back pressure, in. Hg abs	1.5	1.5	1.5
Base unit rating, Mw(e)/Mva	600/720	750/900	750/900
Base cost	\$20,700,000	\$16,400,000	\$16,400,000
Adjustments			
Turbine rating, Mw(e)	980	940	927
Turbine cost	\$+2,190,000	\$ +922,000	\$ +797,000
Generator cost	+2,040,000	+1,085,000	+1,055,000
Pressure	0	+810,000	0
Superheat	0	0	0
Reheat	0	0	0
Power factor (0.9)	0	0	0
Subtotal	\$24,930,000	\$18,407,000	\$18,252,000
Discount (7%)	-1,745,000	-1,288,000	-1,278,000
Subtotal	\$23,185,000	\$17,119,000	\$16,974,000
Bypass (dump) at 100%	+1,590,000	+1,557,000 ^d	+1,050,000 ^d
Foundations	+400,000	+400,000	+350,000
Erection	+900,000	+900,000	+800,000
Total cost, account 231	\$26,075,000	\$19,976,000	\$19,174,000

^aOverpressure not applicable.^bWith 5% overpressure allowance.^cSaturation at 380°F; not true reheat; exhaust from drive turbine.^dDesuperheating station and piping in account 236.

Structures and Improvements. Common ground rules were applied to each concept and, in so doing, the design of the low pressure concept was revised in two areas: (1) the layout was altered to reduce the length of water circulating lines; and (2) 100% instead of the reported 150% water circulating capacity was used.

Account 24 - Accessory Electric Equipment. Cost estimates were made comparable with those for water-cooled reactors, with allowance for auxiliary power differences that resulted from the use of turbine drives for system equipment.

Account 25 - Miscellaneous Power Plant Equipment. This account includes cranes, hoists, compressor and vacuum systems, service water systems, communications, and general office, laboratory, and shop equipment. The requirements were considered equivalent for these concepts and similar to those of water-cooled reactors. A cost of \$1,250,000 was estimated.

7.1.2 Unit Capital Costs

Total construction costs, net electrical output, and the corresponding unit costs for the three concepts are given in Table 7.10. The unit costs were adjusted to a normalized base for 1000-Mw(e) plants by dividing the total construction costs by $10^6(\text{rating}/1000)^{0.75}$.

Table 7.10. Construction and Unit Power Costs

	Low-Pressure System	Intermediate- Pressure System	High-Pressure System
Total construction cost, $\$10^6$			
Investor owned	130.3	131.4	118.9
Publicly owned	126.0	127.1	115.0
New electrical output, Mw	1012	1000	970
Total unit cost, $\$/\text{kw(e)}$			
Investor owned	129.1	131.4	121.7
Publicly owned	126.5	127.1	117.7
Capital contribution to power- production cost, ^{a,b} mills/kwhr(e)	2.53	2.57	2.38

^aBased on 0.8 load factor and 13.7% per year charge against the total capitalization.

^bExclusive of fuel inventory.

7.1.3 Capital Cost Uncertainties

The capital cost evaluation indicates that the unit costs of the low and intermediate pressure concepts are nearly the same, whereas the unit cost of the high pressure concept is \$7 to \$9/kw(e) lower. While a comparison of relative costs among the three concepts shows some of the cost differences that can be attributed to design features associated with the particular steam conditions, other differences are the result of preferences of different designers in layout, design of components, or safety features of the plants. Further development work and analyses are required to determine a design of a safety system that will best satisfy requirements of a steam-cooled plant. Layouts could be optimized in more detailed studies to reduce cost differences in the Buildings and structures account (212), and standardizing the design of reactor vessels, reactor internal structures, and thermal shields would be expected to affect cost differences significantly. Although it is beyond the scope of this study to perform the necessary design analyses and optimization studies to identify relative costs solely attributable to steam conditions, some of the cost trends can be indicated.

Earlier in this section, the effect on capital cost of modifying the high pressure design to make its safety features equivalent to those of the low pressure design was discussed. The net effect was to add about \$7.5/kw to the cost estimated originally for the high pressure concept. No attempt was made, however, to modify features of the intermediate pressure design in a similar manner. Further optimization of the design of safety systems could be expected to have a significant effect on relative costs.

Buildings and Structures, account 212, is another area in which cost differences cannot be strictly accounted for by differences in steam conditions. Buildings for the low pressure concept are estimated to cost \$5 to \$6/kw higher (including indirect costs) than for the other two concepts after the cost of tertiary containment is added to the cost of the high pressure concept, as was done above. Part of this cost difference is due to the requirement of larger confinement space for the low pressure reactor to accommodate about twice as much area for fuel storage as is

required in the higher pressure concepts. This feature may account for about \$1 to \$2/kw(e) of the difference, and another \$1/kw(e) may be accounted for in the increased space requirements for the larger turbine-generator plant components, but the remaining \$3/kw difference probably could be reduced if optimized plant layout studies were made for all three concepts.

The differences in costs of reactor vessels might also be reduced if a design more common for the three reactors were developed. Thermal shields alone are some \$3/kw higher for the intermediate pressure concept than for the other two concepts. Most of this difference is due to the lower neutron exposure limitation assumed for the intermediate pressure concept. If further research and development shows the higher exposures assumed for the low and high pressure concepts to be acceptable, the cost of the intermediate pressure reactor vessel may be reduced by this amount, or possibly more. Vessel costs would be affected further if core performance characteristics of the three designs were normalized.

The adjustments described above are only a few that would be expected from a design normalization study of the three reactor plants. Although a small amount of design normalization (i.e., using single tandem compound turbine-generator units and turbine drives for steam circulators) was performed, extensive optimizations or normalizations of the designs were not attempted in this evaluation study and, therefore, the cost estimates reported in Table 7.1 were determined basically on an evaluation of the designs as submitted or modified by the sponsors.

7.2 Operation and Maintenance Costs

Annual operation and maintenance costs for the steam-cooled plants assumed to be essentially the same as for PWR or BWR plants, except for small differences in insurance costs, and were based on data given in the draft of the NUS Cost Guide.⁹⁴ A permanent staff of 78 people assumed, and the payroll costs included an allowance for fringe benefits. Insurance premiums include commercial third-party liability coverage at \$240,000 per year, and federal indemnity at \$30 per thermal megawatt per year. Administrative and general costs were based on 14% of the total

of payroll, repair and maintenance materials, and contract services costs.

The breakdown of annual operation and maintenance costs is shown in Table 7.11. Based on a 0.8 load factor, the operation and maintenance contribution to the power production cost was 0.28 mill/kwhr for each concept.

Table 7.11. Annual Operation and Maintenance Costs

	Low Pressure System	Intermediate Pressure System	High Pressure System
Total payroll	\$ 726,000	\$ 726,000	\$ 726,000
Repair and maintenance materials and contract services	730,000	730,000	730,000
Administrative and general	204,000	204,000	204,000
Insurance	327,000	316,000	311,000
Total	\$1,987,000	\$1,976,000	\$1,971,000

7.3 Fuel Fabrication Costs

ORNL performed a preliminary analysis of fuel fabrication costs for the three steam-cooled reactors. The pertinent features of each reactor design are listed in Table 7.12, and features of the fuel element designs for each reactor are compared in Table 7.13. It should be noted, however, that there are certain discrepancies and/or omissions in each design report, and some of these parameters might be significantly altered in a final design.

A simplified fabrication flowsheet for breeder reactor fuel is shown in Fig. 7.1. A single fabrication plant produced both radial blanket and core fuel elements for a given reactor type. The plant was physically divided into two areas; one was suitable for remote fabrication of equilibrium cycle mixed oxides ($\text{UO}_2\text{-PuO}_2$), and the adjacent area for fabricating

Table 7.12. Reactor Parameters Pertinent to Fuel-Fabrication Cost Analyses

	Low-Pressure System	Intermediate- Pressure System	High-Pressure System
Reactor power, Mw			
Electrical	1,000	1,000	1,000
Thermal	2,900	2,519	2,326
On-line factor, %	80	80	80
Full-power days per cycle	876	854	460
Maximum core fuel exposure, Mwd/MT heavy metal	100,000	100,000	100,000
Power distribution, %			
Core	90	93	81
Axial blanket	8	3	2
Radial blanket	2	4	17
Reactor loading, kg heavy metal			
Core	33,045	33,000	14,493
Axial blanket	34,904	16,000	4,600
Radial blanket	20,862	37,000	36,200
Total	88,811	86,000	55,293
Refueling schedule, kg heavy metal per cycle			
Core	33,045	33,000	14,493
Axial blanket	34,904	16,000	4,570
Radial blanket	20,862	18,500 ^a	27,870 ^b
Total	88,811	67,500	46,933
Average fuel exposure, ^c Mwd/MT heavy metal			
Core	70,000	57,500	58,000
Axial blanket	5,800	4,100	5,400
Radial blanket	2,000	4,000	7,600
Refueling rate, ^d kg heavy metal per year			
Core	11,015	12,290	9,200
Axial blanket	11,635	5,959	2,900
Radial blanket	6,954	6,890	17,691
Total	29,604	25,139	29,791

^aAverage radial blanket refueled per cycle; one-half of total radial blanket elements in reactor.

^bAverage radial blanket refueled per cycle; one-half of radial blanket refueled at end of odd-numbered cycles; all of radial blanket refueled at end of even-numbered cycles.

$$^c \text{Average fuel exposure (Mwd/MT)} = \frac{(\text{Full-power days/cycle}) \times \text{Mw(th)} \times (\% \text{ power in region})}{\text{Refueling schedule (MT heavy metal/cycle)}}$$

$$^d \text{Refueling rate (kg/yr)} = \frac{\text{Refueling schedule (kg/cycle)}}{(\text{Full-power days/cycle})/[(\text{on-line factor}) \times (365 \text{ days/yr})]}$$

Table 7.13. Fuel Element Design Parameters

	Low Pressure System	Intermediate Pressure System	High Pressure System
Core			
Fuel diameter, in.	0.322	0.250	0.210
Fuel length, in.	29.0	59.4	57.0
Fuel loadings			
kg/ft	0.14	0.08	0.06
kg per fuel rod	0.33	0.40	0.26
Axial blanket			
Fuel diameter, in.	0.301	0.250	0.210
Fuel length, in.	36.0	28.0	18.0
Fuel loadings			
kg/ft	0.12	0.08	0.06
kg per fuel rod	0.36	0.19	0.09
Radial blanket			
Fuel diameter, in.	0.433	0.456	0.425
Fuel length, in.	36.0	74.0	57.0
Fuel loadings			
kg/ft	0.25	0.28	0.24
kg per fuel rod	0.75	1.72	1.49
Fuel ratios, kg/kg			
Axial blanket/core	1.06	0.48	0.32
Radial blanket/core	0.63	0.56	1.92
Total blanket/core	1.69	1.05	2.24
Axial/total blanket	0.63	0.46	0.14
Radial/total blanket	0.37	0.54	0.86
Core/total blanket	0.59	0.96	0.45

radial blanket fuel was comparable to existing plants that produced light water reactor fuel. Axial blanket fuel was sized and classified along with the radial blanket material (both are depleted UO_2); the lower region of the element was then loaded into the cladding tube and transferred to the remote fabrication section of the plant for loading core fuel and the upper axial blanket.

One of the ground rules in these economic studies affects the radial blanket fabrication costs; that is, complete radial blanket assemblies

Fig. 7.1. Simplified Fabrication Flowsheet for Vibration Compacted Breeder Reactor Fuel.

for each reactor type are purchased from a central plant. It was assumed that this plant operated at a capacity that can accommodate the maximum demand for any single reactor and, thus, yield lower unit costs for other reactors. Appropriate penalties were applied to compensate for plant alterations pertaining to a single fuel element design. This is in contrast to the single purpose fabrication plants assumed in previous evaluations. The validity of using such a central fabrication plant has been discussed.

The throughput for the radial blanket plant was assumed to be 2000 kg/day. This capacity would handle the largest single demand with little or no overproduction. Use of the central plant results in savings up to 35% in radial blanket fabrication costs over those of single purpose plants.

Tables 7.14 and 7.15 summarize the fuel fabrication costs. Table 7.14 gives the costs in terms of contribution to the power production cost [mills/kwhr(e)], and Table 7.15 lists the costs per unit weight of heavy metal (\$/kg). More detailed costs are given in Table 7.16 for a maximum fuel exposure of 100,000 Mwd/MT, and in Table 7.17 for a 50,000-Mwd/MT maximum exposure. In terms of power production costs, the fabrication costs are lowest for the high pressure system because of the high percentage of power produced in the radial blanket and a large ratio (1.92) of radial blanket (cheap fuel) to core. The next highest cost is for the intermediate pressure system, which also has a low ratio (1.05) of total blanket (cheap fuel) to core. The costs for the low pressure system are the highest. Although the larger fuel diameter is conducive to lower fabrication costs, this is somewhat offset by the shorter fuel length (pancake design). The use of a small quantity of radial blanket and low burnup of the radial blanket fuel also penalized this design.

Table 7.14. Contribution of Fuel Fabrication Costs
to Power Production Costs as a Function of
Utility Size and Fuel Exposure

Utility Size [Mw(e)]	Core Fuel Exposure (Mwd/MT)	Fuel Fabrication Cost [mills/kwhr(e)]		
		Low Pressure System	Intermediate Pressure System	High- Pressure System
Capital charge rate: 30% per year				
30,000	100,000	0.38	0.35	0.34
15,000	100,000	0.43	0.39	0.38
30,000	50,000	0.65	0.60	0.51
15,000	50,000	0.72	0.67	0.58
Capital charge rate: 22% per year				
30,000	100,000	0.36	0.33	0.31
15,000	100,000	0.40	0.37	0.35
30,000	50,000	0.59	0.53	0.40
15,000	50,000	0.63	0.58	0.44

Table 7.15. Contribution of Fuel Fabrication Costs to Costs per Unit Weight of Heavy Metal as a Function of Utility Size and Fuel Exposure

Utility Size [Mw(e)]	Core Fuel Exposure (Mwd/MT)	Fuel Fabrication Cost (\$/kg heavy metal)		
		Low Pressure System	Intermediate Pressure System	High Pressure System
Capital charge rate: 30% per year				
30,000	100,000	91	98	80
15,000	100,000	101	110	90
30,000	50,000	88	98	86
15,000	50,000	96	108	97
Capital charge rate: 22% per year				
30,000	100,000	85	92	74
15,000	100,000	94	103	83
30,000	50,000	79	85	67
15,000	50,000	85	93	74

In each of these cost estimates, the fabrication plant was assumed to operate 260 days per year, and the capital charges were calculated for rates of 22 and 30% per year. Costs included were capital charges, operating expenses, and hardware costs. Specifically excluded were use or inventory charges, scrap recovery, fuel losses, shipping, and chemical conversion of fuel to the oxide forms.

In the low and high pressure designs, the control rods were discarded at the end of each cycle, consequently, their cost should be included in fuel cycle costs. The control rods were purchased completely fabricated. The additional cost might, however, be normalized with fuel element costs to yield a more meaningful unit fabrication cost. Table 7.18 summarizes these additional costs. The control rod fabrication costs are not included in the fuel fabrication costs shown in Tables 7.14 through 7.17.

Table 7.16. Details of Fuel-Fabrication Requirements and Costs for a Core Fuel Exposure of 100,000 Mwd/MT

System and Utility Size [Mw(e)]	Fuel-Fabrication Requirements (kg heavy metal/day)				Fuel-Fabrication Costs (\$/kg heavy metal)			
	Core	Core Plus Axial Blanket	Radial Blanket	Reactor Refueling	Core	Core Plus Axial Blanket	Radial ^a Blanket	Reactor Refueling
Capital charge rate: 30% per year								
Low pressure								
30,000	1272	2614	802	3416	210	103	52	91
15,000	636	1307	401	1708	237	116	52	101
Intermediate pressure								
30,000	1420	2108	796	2904	177	120	42	98
15,000	710	1054	398	1452	200	136	42	110
High pressure								
30,000	1062	1398	2040	3438	182	139	40	80
15,000	531	699	1020	1719	215	164	42	90
Capital charge rate: 22% per year								
Low pressure								
30,000	1272	2614	802	3416	197	96	48	85
15,000	636	1307	401	1708	222	109	48	94
Intermediate pressure								
30,000	1420	2108	796	2904	166	112	39	92
15,000	710	1054	398	1452	187	127	39	103
High pressure								
30,000	1062	1398	2040	3438	167	127	37	74
15,000	531	699	1020	1719	196	149	37	83

^aRadial blanket fabrication cost is based on optimum radial blanket plant operating at 2000 kg/day.

Table 7.17. Details of Fuel-Fabrication Requirements and Costs for a Core Fuel Exposure of 50,000 Mwd/MT

System and Utility Size [Mw(e)]	Fuel-Fabrication Requirements (kg heavy metal/day)				Fuel-Fabrication Costs (\$/kg heavy metal)			
	Core	Core Plus Axial Blanket	Radial Blanket	Reactor Refueling	Core	Core Plus Axial Blanket	Radial Blanket	Reactor Refueling
Capital charge rate: 30% per year								
Low pressure								
30,000	2544	5228	802	6030	190	93	52	88
15,000	1272	2614	401	3015	210	103	52	96
Intermediate pressure								
30,000	2840	4216	796	5012	160	108	42	98
15,000	1420	2108	398	2506	177	120	42	108
High pressure								
30,000	2124	2796	2040	4836	158	120	40	86
15,000	1062	1398	1020	2418	182	139	40	97
Capital charge rate: 22% per year								
Low pressure								
30,000	2544	5228	802	6030	180	88	48	79
15,000	1272	2614	401	3015	197	97	48	85
Intermediate pressure								
30,000	2840	4216	796	5012	152	103	39	85
15,000	1420	2108	398	2506	166	112	39	93
High pressure								
30,000	2124	2796	2040	4836	146	111	37	67
15,000	1062	1398	1020	2418	167	127	37	74

Table 7.18. Control Rod Fabrication Cost Based on \$5000 per Rod

System	Number of Control Rods	Additional Cost per Core (\$)	Refueling Schedule (kg heavy metal/cycle)	Additional Fabrication Cost (\$/kg heavy metal)
Low pressure	127	635,000	88,811	7.15
High pressure	108	540,000	46,933	11.51

7.4 Fuel Preparation (Conversion) Costs and Spent Fuel Reprocessing Costs

7.4.1 Fuel Preparation (Conversion) Costs

The uncertainties in fuel preparation cost estimates may be substantial, perhaps $\pm 50\%$, since they were extrapolated to large-scale production from a minimum of experience with preparation of fast reactor core fuels. Fuel preparation includes those operations necessary to convert makeup and recycle fuel material to the proper chemical and physical form for feed to the fabrication process. In this case, the assumed fuel-preparation steps were

The fuel preparation plant was assumed to be an integral part of either the fuel fabrication plant or the spent fuel reprocessing plant, or both, in that no site costs were included, and sharing of supporting services and service personnel was assumed. The costs would be somewhat higher (possibly $>10\%$), and there would be one or two additional shipping charges if the preparation plant were independent.

Table 7.19 gives fuel preparation cost estimates for 15,000- and 30,000-Mw(e)-equivalent industry sizes and a 22% per year fixed charge

Table 7.19. Fuel Preparation (Conversion) Costs for 15,000- and 30,000-Mw(e)-Equivalent Industry Size

- Basis: 1. Cost of preparation of UO_2 and $\text{UO}_2\text{-PuO}_2$ for fabrication, starting from depleted UF_6 and $\text{Pu}(\text{NO}_3)_4$
 2. Preparation plant size assumed to match the amount and type of reactor indicated
 3. 260 production days per year
 4. 22% per year fixed-charge rate on capital investment

	Low Pressure System		Intermediate Pressure System		High Pressure System	
Industry size, Mw(e)	15,000	30,000	15,000	30,000	15,000	30,000
Throughput rates						
$\text{UF}_6 \rightarrow \text{UO}_3$, MT(U)/year	421	843	350	699	419	838
$\text{UO}_3 + \text{Pu}(\text{NO}_3)_4 \rightarrow \text{UO}_2\text{-PuO}_2$, MT(U + Pu)/year	165	331	184	367	138	276
$\text{UO}_3 \rightarrow \text{UO}_2$, MT(U)/year	279	558	193	386	309	618
Overall, MT(U + Pu)/day	1.71	3.42	1.45	2.90	1.72	3.44
Unit costs						
$\text{UF}_6 \rightarrow \text{UO}_3$, \$/kg(U)	3.57	2.43	4.00	2.70	3.55	2.44
$\text{UO}_3 + \text{Pu}(\text{NO}_3)_4 \rightarrow \text{UO}_2\text{-PuO}_2$, \$/kg(U + Pu)	18.1	13.9	17.5	13.5	18.5	14.8
$\text{UO}_3 \rightarrow \text{UO}_2$, \$/kg(U)	2.32	1.62	2.80	1.95	2.17	1.53
Overall average, \$/kg(U + Pu)	11.6	8.50	13.9	10.3	10.5	7.91
mill/kwhr(e)	0.049	0.036	0.049	0.036	0.045	0.034

rate on capital investment. Similar estimates are presented later in Table 7.21 for plant sizes up to an assumed upper limit of about 10 MT/day. This corresponds to about 90,000 or 105,000 Mw(e), depending on reactor type. The data in the tables can be used for other throughput rates by interpolation on a log-log plot of cost versus throughput rate, such as Fig. 7.2.

7.4.2 Spent Fuel Reprocessing Costs

The reprocessing cost estimates were based on central, aqueous process (solvent extraction) plants. The possibility of on-site pyrochemical processing was not investigated for this study. The basic estimates were made for single purpose plants to reprocess 1 and 10 MT/day of standard BWR-PWR fuel. The 1-MT/day estimate was revised to reflect the differences between BWR-PWR and fast breeder reactor fuels, and the revised estimate was scaled to larger plant sizes with the empirical cost-scaling

Fig. 7.2. Fuel-Preparation and Spent-Fuel Reprocessing Costs.

laws developed for BWR-PWR fuels. Thus, capital investment and annual operating costs were calculated for sizes required to service 15,000, 30,000, 60,000 90,000, or 105,000 Mw(e) of a given fast breeder reactor type from the following equations:

$$\text{Capital investment (\$)} = 39.5 \times 10^6 \left(\frac{\text{MT/year}}{260} \right)^{0.35},$$

$$\begin{aligned} \text{Annual operating cost (\$/year)} &= 0.084 \times \text{capital investment} \\ &+ (0.26 \times 10^6) \left(\frac{\text{MT/year}}{260} \right). \end{aligned}$$

The capital investment included site and startup costs and working capital. The capital investment was annualized by multiplying by the fixed-charge rate to cover the costs of return on investment (cost of money), recovery of investment (depreciation), interim replacements, income taxes, property taxes, and property insurance. All other costs were included in the annual operating cost, except the high level waste charge and the depleted uranium storage charge.

An annual high level waste disposal charge was calculated from the following empirical cost-scaling equation, which was obtained by fitting TASCO computer code⁹⁵ results for perpetual acidic waste storage in tanks:

$$\begin{aligned} \text{High level waste disposal charges (\$/year)} &= \\ 1.46 \times 10^6 &\left[\frac{(\text{MT/year})(\text{Mwd/MT})}{10^7} \right]^{0.8444} \end{aligned}$$

The numerator in the brackets is approximately equal to the grams of fission products handled per year, and the denominator is a normalizing factor. This cost is also believed to be sufficient to cover reduction to solids and ultimate disposal in a salt mine. This estimate was based on government (state or federal) ownership of the waste disposal facilities, with the capital, operating, and perpetual maintenance costs charged to the reprocessing plant on an amortized annual payment basis (something like the Nuclear Fuel Services arrangement with New York State). In a similar manner, an annual charge for storage of the depleted uranium recovered in reprocessing was assessed with the following equation:

Depleted-uranium waste storage charges (\$/year) =

$$1.53 \times 10^6 \left(\frac{\text{MT/year}}{10^3} \right)^{0.844}$$

The four types of costs (capital, operating, high level waste, and depleted uranium storage) are reported separately in Table 7.20, and then added together to arrive at the total unit cost, \$/kg or mills/kwhr(e), given in the table and shown on Fig. 7.2.

In addition to the costs for the 15,000- and 30,000-Mw(e)-equivalent industry sizes listed in Table 7.20, costs for plant sizes up to an assumed upper limit of about 10 MT/day are summarized in Table 7.21, as discussed above. The data in the tables can be used for other throughput rates by interpolation on a log-log plot of cost versus throughput rate, such as Fig. 7.2.

The cost estimates were based on processing mixed core and blanket fuels by mechanical shearing of the fuel, dissolution with nitric acid, feed adjustment to desired conditions for solvent extraction, separation of the uranium and plutonium from each other and from the fission products by solvent extraction, and final cleanup of the plutonium by ion exchange and of the uranium by additional solvent extraction. Capital and operating costs in addition to those in the estimates for light water-cooled reactor fuels were included to allow for the higher plutonium content and for the shorter preprocessing decay time (45 days), which implies more severe off-gas handling problems and other problems associated with handling short lived fission products not normally of concern after 120 to 180 days of cooling.

These reprocessing cost estimates cannot be claimed to have an absolute accuracy of any better than $\pm 30\%$, and perhaps not that good. (The much more detailed du Pont estimates⁹⁶ claimed only $\pm 20\%$.) The technology assumed involved more of an extrapolation of present day aqueous processing experience than that assumed for the advanced converter reactors, although the extrapolation seems to be reasonable. The relative accuracy, for purposes of comparison with other reactor types, in the current evaluation study should be better than the $\pm 30\%$ figure may suggest, although not necessarily as low as $\pm 10\%$.

Table 7.20. Spent Fuel Reprocessing Costs for 15,000- and 30,000-Mw(e)-Equivalent Industry Size

- Basis: 1. Single purpose, central, aqueous processing plants sized to match the assumed amount and type of reactor indicated
 2. 260 production days per year
 3. 22% per year fixed-charge rate on capital investment

	Low Pressure System		Intermediate Pressure System		High Pressure System	
Industry size, Mw(e)	15,000	30,000	15,000	30,000	15,000	30,000
Throughput rate, U + Pu + FP						
MT/year	444	888	377	754	447	894
MT/day	1.71	3.42	1.45	2.90	1.72	3.44
Capital investment, 10 ⁶ \$	47.6	60.7	45.0	57.4	47.8	60.8
Annual operating cost, 10 ⁶ \$	4.45	5.99	4.16	5.57	4.46	6.01
Annual waste charge, 10 ⁶ \$	1.78	3.20	1.58	2.84	1.48	2.65
Annual charge for depleted uranium storage, 10 ⁶ \$	0.74	1.28	0.63	1.13	0.73	1.32
Unit total cost						
\$/kg	39.3	26.8	43.1	29.4	36.8	24.7
mills/kwhr(e)	0.166	0.113	0.155	0.105	0.163	0.111

Table 7.21. Summary of Fresh Fuel Preparation (Conversion) and Spent Fuel Reprocessing Costs for 60,000-, 90,000-, and 105,000-Mw(e)-Equivalent Industry Size

- Basis: 1. Preparation and processing plant sizes assumed to match the amount and type of reactor indicated
 2. 260 production days per year
 3. 22% per year fixed-charge rate on capital investment

	Low Pressure System		Intermediate Pressure System		High Pressure System	
Industry size, Mw(e)	60,000	90,000	60,000	105,000	60,000	90,000
Throughput rate, U + Pu + FP						
MT/year	1776	2664	1508	2640	1789	2683
MT/day	6.83	10.25	5.80	10.15	6.88	10.32
Preparation cost						
mills/kwhr(e)	0.026	0.022	0.026	0.021	0.025	0.021
\$/kg	6.24	5.19	7.50	5.89	5.80	4.83
Reprocessing cost						
mills/kwhr(e)	0.079	0.065	0.077	0.056	0.077	0.063
\$/kg	18.80	15.50	20.50	15.50	18.20	14.90

7.5 Shipping Costs

The costs of shipping irradiated fuel elements were computed with a special computer code designated MYRA. Cooling times of 30 days for core and 90 days for blanket elements were selected as compromises between shorter cooling times, which would be more economical, and longer times, which would reduce the heat and activity of the elements. The costs included: (1) loading, unloading, rigging, testing, and decontamination; (2) insurance; (3) railroad freight charges; and (4) annual fixed charges on cask costs. Other assumptions and cost bases were: (1) 1000 miles shipment each way; (2) compliance with AEC dose limits of 10 mr/hr at 6 ft from outside of the vehicle; (3) maximum weight of cask of 120 short tons; (4) cask cost of \$1.25 per lb of cask weight; and (5) casks shared among plants sufficiently to reach a maximum allowable utilization of 80%. Costs were also estimated for shipping recycled fresh fuel of moderate gamma activity and virgin fuel of zero gamma activity where applicable.

The results of these analyses, which are summarized in Table 7.22, gave unit shipping costs that were combined with mass balances throughout the lifetime of the reactors to obtain levelized values for the effect of shipping costs on the unit fuel cycle costs.

7.6 Fuel Cycle Costs

Fuel cycle costs were calculated for an industry size of 15,000 Mw(e) of the reactor type being considered, as in the case of the fabrication and processing costs. The "Economics" portion of the code TONG⁹⁷ was used for this calculation. The technique used consisted of discounting future costs and income to an equivalent present value. The amount of energy and the total value of the energy were considered to be proportional. The fuel cycle cost was then based on the value of fuel materials used and the amount of power produced. A detailed description of the numerical procedure involved has been given by Kasten and his associates.⁹⁰

The basic values used in the calculations are summarized in Table 7.23. The unit costs used in calculating the fuel cycle costs are shown

Table 7.22. Fuel Shipping Costs

System	Fuel Assembly Type	Year	Cooling Time (days)	Unit Shipping Cost (\$/kg heavy metal)		
				Spent Fuel	Fresh Fuel	Recycled Fuel
Low pressure	Core and axial blanket	1980	30	12.00	1.70	2.40
		1990	30	11.00	1.50	2.10
	Radial blanket	1980	90	2.50	0.55	
		1990	90	2.10	0.45	
Intermediate pressure	Core and axial blanket	1980	30	10.00	1.20	2.00
		1990	30	9.00	1.00	1.80
	Radial blanket	1980	90	2.20	0.50	
		1990	90	1.80	0.40	
High pressure	Core and axial blanket	1980	30	18.00	2.00	2.70
		1990	30	16.00	1.70	2.40
	Radial blanket	1980	90	3.00	0.60	
		1990	90	2.50	0.50	

Table 7.23. Values Used in Fuel Cycle Cost Calculations

Load factor	0.8
Industry size, Mw(e)	15,000
Value of UF ₆ of 0.3% enrichment, \$/kg of uranium	3.00
Charge rate on processing and fabrication plants, %/year	22
Maximum fuel exposure, Mwd/MT	100,000
Material losses during fabrication, %/cycle	0.2
Material losses during processing, %/cycle	1.0
Value of fissile plutonium, \$/g	9.32
Interest rates, % per year	
On plutonium	13.2
On fabrication charges	12.8
On processing and shipping costs	7.2

in Table 7.24. The fabrication and processing data in this table constitute a summary of more detailed data, which are presented in Sections 7.3 and 7.4. The shipping cost data are a summary of data presented in Section 7.5. Fuel cycle costs were also calculated for each cycle and for the 30-year history of operation from the data of Tables 7.23 and 7.24 and mass balances described in Chapter 4. The costs for the equilibrium cycle and for the levelized 30-year history are given in Tables 7.25 and 7.26.

For the intermediate and high pressure systems, the equilibrium condition consisted of more than one cycle, depending upon the particular combination of core and blanket regions refueled at a given time. The values quoted for the equilibrium cycle were for an average taken over the sets of cycles that constituted the equilibrium condition.

In Tables 7.25 and 7.26 the credits are shown as negative costs. The negative "burnup and loss" entries resulted from the production of

Table 7.24. Unit Fuel Cycle Costs

	Unit Costs (\$/kg of heavy metal)		
	Low Pressure System	Intermediate Pressure System	High Pressure System
Shipping*			
Postirradiation	9.768	6.440	7.770
Preirradiation	1.965	1.315	1.268
Fabrication*			
Core and axial blanket	116.3	136.0	153.7
Radial blanket	47.2	49.0	42.3
Reprocessing	39.3	43.1	36.8
Preparation			
$\text{UF}_6 \rightarrow \text{UF}_3$	3.57	4.0	3.55
$\text{UO}_3 + \text{Pu}(\text{NO}_3)_4 \rightarrow \text{UO}_2 \cdot \text{PuO}_2$	18.10	17.5	18.5
$\text{UO}_3 \rightarrow \text{UO}_2$	2.32	2.80	2.17

*These costs were derived by mass weighting of data in Table 7.22 for shipping and in Table 7.16 for fabrication.

Table 7.25. Equilibrium Fuel Cycle Costs

	Costs (mills/kwhr)		
	Low Pressure System	Intermediate Pressure System	High Pressure System
Burnup and loss	-0.392	-0.099	-0.060
Fabrication	0.480	0.412	0.413
Processing	0.158	0.136	0.145
Shipping	0.047	0.025	0.037
Inventory charges	0.885	0.881	0.657
Fabrication interest	0.125	0.101	0.066
Processing interest	-0.023	-0.017	-0.011
Total	1.280	1.439	1.244

Table 7.26. Levelized Thirty Year Fuel Cycle Costs

	Costs (mills/kwhr)		
	Low Pressure System	Intermediate Pressure System	High Pressure System
Burnup and loss	-0.391	-0.106	-0.053
Fabrication	0.503	0.428	0.411
Processing	0.165	0.145	0.145
Shipping	0.049	0.026	0.038
Inventory charges	0.872	0.886	0.677
Fabrication interest	0.177	0.157	0.063
Processing interest	-0.011	-0.013	-0.018
Total	1.364	1.523	1.263

more fissile material than was consumed. The negative "processing interest" entries resulted from the payment of processing costs after revenue was received. These items were assumed to accrue interest during the interval between receipt of the revenue and payment of the cost.

Table 7.27. Power Cost Components

Fixed charge rates, % per year	
Depreciating capital	13.7
Nondepreciating capital	12.8
Private ownership of fuel inventory charge, % per year	13.2
Plant load factor	0.8
Present worth discount factor, %	6
U ₃ O ₈ cost, \$/lb	8
Separative work cost, \$/kg	26
Plutonium value, \$/g	9.32

7.7 Power Generation Costs

The basic values for estimating the power costs are given in Table 7.27. Capital, operating and maintenance, and fuel cycle costs were combined in Table 7.28 to give comparative unit power generation costs for the three concepts.

Table 7.28. Power Costs

	Costs (mills/kwhr)		
	Low Pressure System	Intermediate Pressure System	High Pressure System
Capital cost	2.53	2.57	2.38
Operating and maintenance cost	0.28	0.28	0.28
Fuel cycle cost	1.36	1.52	1.26
Total	4.17	4.37	3.92

8. RESEARCH AND DEVELOPMENT PROGRAM

8.1 Background Information

The research and development requirements for steam-cooled breeder reactors were established by B&W for the purpose of this evaluation.⁹⁸ Three possible programs were considered that lead, respectively, to the commercial introduction by 1982 of a low pressure (1250-psi) SCBR and/or a high pressure (3700-psi) SCBR. The intermediate pressure design, which was not sponsored by B&W, is not discussed because its research and development requirements would presumably be comparable to those for the other two concepts. Also, the simultaneous development of all three steam-cooled designs does not appear to be warranted.

The proposed programs are based on a detailed analysis of the requirements for developing the low pressure design. These were then adjusted as necessary to establish costs for the high pressure designs. Costs for developing both concepts at the same time were essentially the sum of costs for the individual programs, with the exception of minor economies to be gained from the use of the same critical experiments and fuel handling equipment, similar cladding materials, similar core design methods, and related studies of steam chemistry problems.

All three programs centered around the design, construction, and operation of a 50-Mw(e) reactor experiment, a 500-Mw(e) prototype plant, and a 1000-Mw(e) commercial demonstration plant. Thus, the development of a single steam pressure design involved the construction of three reactors, and the combined program required construction of six reactors. The research and development program for supporting this design and construction effort was divided into the following 11 specific development tasks:

Task 1	Materials research and development (including fuel)
Task 2	Fuel assembly development
Task 3	Component development
Task 4	Development of control rods and drives
Task 5	Fuel handling system development

- Task 6 Plant instrumentation and controls development
- Task 7 Thermal and hydraulic performance optimization
- Task 8 Reactor safety evaluation
- Task 9 Physics research and development
- Task 10 Coolant chemistry and technology development
- Task 11 Research and development management

The programs associated with these tasks were designed to achieve full commercial status of the high and low pressure concepts in the shortest possible time. Other assumptions used as bases for establishing the required programs are listed below:

1. The program would begin with a coordinated study to develop conceptual designs of all three reactors. This is considered necessary to provide the required degree of guidance and coherence to the program.

2. Design and construction of the reactor experiment would continue at an accelerated pace so that the greatest possible amount of information on fuel performance, system operation, and turbine maintenance problems, would be available to be factored into the design of the 500-Mw(e) demonstration plant.

3. Critical components, as typified by the turbine-driven steam circulators and direct contact boilers, for each of the three reactors would be thoroughly prooftested through extended operating periods in suitable facilities prior to installation in the reactors.

4. Modular construction of direct contact boilers would be used to reduce boiler design costs to a minimum. In other words, a single module would be used in varying numbers in all three reactors to provide the necessary capacity. Similarly, six circulators would be used in the 500-Mw(e) plant, and eight circulators of the same size in the 1000-Mw(e) plant. The units in the 500-Mw(e) plant would normally be operated four or six at a time at reduced rating. This approach would minimize the rather heavy first-of-a-kind engineering and test cost of these units, and insure the greatest possible reliability.

5. Relatively straightforward component development was assumed to permit start of 500-Mw(e) plant design and construction prior to operation of the 50-Mw(e) plant. A similar approach was taken in scheduling the 1000-Mw(e) plant relative to the 500-Mw(e) plant.

6. Most difficult development items ~~were~~ considered to be the fuel and cladding. Scheduling ~~provided~~ four years to observe 50-Mw(e) operation of the plant and to fabricate the core for the 500-Mw(e) plant. The same period ~~was~~ allowed for the core of the 1000-Mw(e) plant relative to that of the 500-Mw(e) plant. Scheduling of the experimental physics program ~~was~~ intended to provide time to effect minor adjustments in core design, as required to meet control, core lifetime, and safety requirements.

7. It ~~was~~ assumed that a direct superheated steam power cycle would be satisfactory from a maintenance point of view. The research and development program ~~permitted design of~~ a saturated steam cycle, but reactor cost estimates and scheduling ~~did not~~.

8. The program would be unimpaired by shortages of funds or plutonium, or by unusually long lead times for pressure vessels, turbines, generators, etc.

9. These reactor programs would possibly be paralleled by steam-cooled reactor development in Europe. However, the reactor designs in European programs might be for different pressures and different safety criteria and construction codes. In view of this, input from these other programs would be considered as complementary only and would not be relied upon. No work scope or cost reductions would be made in any of the three development programs described as the result of the European programs.

8.2 Costs and Schedules

The estimated costs and schedules of the proposed programs ~~were~~ based on the following guidelines and the general philosophy described above:

1. All design, construction, and operating costs for the 50-Mw(e) experiment ~~were~~ considered chargeable to the program.

2. Design, construction, and operating cost of the 500-Mw(e) plant for the first two years (prior to purchase by utility) ~~were~~ reduced by sale of power at 5.0 mills/kwhr at a 0.6 load factor. The sale price ~~was~~ based on 7% present worth of net income (3 mills/kwhr) expected during the remaining 28-year life of the plant operating at a 0.8 load factor.

3. AEC support for first 1000-Mw(e) plant was confined to first-of-a-kind design and construction assistance amounting to 20% of direct construction costs.

4. Possible effect of longer lead time required for procurement of pressure vessels for high pressure system was not factored into the development program schedule. Material procurement problems currently associated with heavier material needed for the 500- and 1000-Mw(e) reactor pressure vessels would add 18 months to the lead time for each of these vessels.

The costs of the individual and combined programs are summarized in Tables 8.1, 8.2, and 8.3.

It may be seen that more than 80% of the costs of the programs are associated with the reactor experiments, the prototype plants, and

Table 8.1. Low Pressure SCBR Development Costs

Fiscal Year	Costs (thousands of dollars)				
	50-Mw(e) Reactor Experiment	500-Mw(e) Prototype Plant	1000-Mw(e) First Commercial Plant*	Supporting Research and Development	Total
1969	1,250	200	200	6,270	7,920
1970	4,500	100	100	7,420	12,120
1971	6,700	100	100	7,120	14,020
1972	6,700	200	100	6,430	13,430
1973	6,700	3,900	100	5,040	15,740
1974	3,000	19,000	100	3,010	25,110
1975	3,000	26,400	200	2,080	31,680
1976	3,000	28,100	900	1,270	33,270
1977	3,000	23,200	2,000	900	29,100
1978		12,070	3,200	1,200	16,470
1979		11,270	4,500	900	16,670
1980			4,500	900	5,400
1981			3,000		3,000
Totals	37,850	124,540	19,000	42,540	223,930

*Costs include only first-of-a-kind engineering and construction assistance.

Table 8.2. High Pressure SCBR Development Costs

Fiscal Year	Costs (thousands of dollars)				
	50-Mw(e) Reactor Experiment	500-Mw(e) Prototype Plant	1000-Mw(e) First Commercial Plant*	Supporting Research and Development	Total
1969	1,250	200	200	7,280	8,930
1970	4,500	100	100	8,020	12,720
1971	6,700	100	100	7,350	14,250
1972	6,700	200	100	6,600	13,660
1973	6,700	3,900	100	5,200	15,900
1974	3,000	18,000	100	3,010	24,110
1975	3,000	22,600	200	2,080	27,880
1976	3,000	28,100	900	1,270	33,720
1977	3,000	19,500	2,000	900	25,400
1978		8,560	3,000	1,200	12,760
1979		8,560	3,900	900	13,360
1980			4,000	900	4,900
1981			3,000		3,000
Totals	37,850	109,820	17,700	44,770	210,140

*Costs include only first-of-a-kind engineering and construction assistance.

assistance on the first 1000-Mw(e) plants. The sale of the 500-Mw(e) reactors in January 1979 under the ground rules given above would reduce the costs of the individual programs by \$127.7 million and the combined program by \$255.4 million.

Relatively small amounts of money for design studies and economic evaluations should be available from utilities in the near future. More definitive knowledge as to the degree of support they are willing to offer a major steam-cooled fast breeder reactor development program must await solicitation of these organizations. The only assumption made herein regarding utility support is that it would be possible to sell the 500-Mw(e) power demonstration reactor to a utility at a fair price after its operating and economic characteristics had been satisfactorily proven.

Table 8.3. Annual and Total Costs for Developing Both Low Pressure and High Pressure Steam-Cooled Breeder Reactors Simultaneously

Fiscal Year	Costs (thousands of dollars)				
	50-Mw(e) Experiment (2 reactors)	500-Mw(e) Prototype Plant (2 reactors)	1000-Mw(e) Commercial Plant* (2 reactors)	Supporting Research and Development	Total
1969	2,500	400	400	11,760	15,060
1970	9,000	200	200	13,780	23,180
1971	13,400	200	200	12,590	26,390
1972	13,400	400	200	11,540	25,540
1973	13,400	7,800	200	8,830	30,230
1974	6,000	37,000	200	5,040	48,240
1975	6,000	49,000	400	3,840	59,240
1976	6,000	56,200	1,800	2,360	66,360
1977	6,000	42,700	4,000	1,800	54,500
1978		20,630	6,200	2,400	29,230
1979		19,830	8,400	1,800	30,030
1980			8,500	1,800	10,300
1981			6,000		6,000
Totals	75,700	234,360	36,700	77,540	424,300

*Costs include only first-of-a-kind engineering and construction assistance.

As an alternate, a utility would design, construct, and operate this reactor while provided with adequate first-of-a-kind assistance through to the end of a two-year precommercial operating stage. This would not change the program costs.

The schedule for developing steam-cooled breeder reactors shown in Table 8.4 is applicable for developing a single pressure concept or two concepts simultaneously.

8.3 Performance Characteristics of Low and High Pressure Systems

The research and development program for the high pressure system was based on the following modifications to the reference design:^{4,5}

Table 8.4. SCBR Development Program Schedule

	FY-1969	1970	1971	1972	1973	1974	1975	1976	1977	1978	1979	1980	1981	1982
Reactor design, construction, and operation														
50-Mw(e) Experiment														
Design														
Construction														
Testing and operation														
500-Mw(e) Demonstration Plant														
Preliminary design and Title I														
Completion of design														
Construction														
Precommercial operation														
Commercial operation														
1000-Mw(e) Commercial Plant														
Preliminary design and Title I														
Completion of design														
Construction and operation														
Associated research and development programs ^a														
Task 1 - Materials (including fuel)														
Task 2 - Fuel assembly development														
Task 3 - Component development														
Task 4 - Control rods and drives development														
Task 5 - Fuel-handling systems development														
Task 6 - Plant instrumentation and controls development														
Task 7 - Thermal and hydraulic performance optimization														
Task 8 - Reactor safety evaluation														
Task 9 - Reactor physics research and development														
Task 10 - Coolant chemistry and technology development														
Task 11 - Research and development management														

^aThe schedules are identical for the three programs.

1. An integral arrangement would be provided that would differ from that for the low pressure design in that steam separators would not be required and the direct contact boiler would become a mixing chamber. Penetrations for circulator drives would probably be in the upper head of the vessel and would be smaller. A close fitting containment vessel would incorporate vapor suppression, and be designed to operate at a pressure significantly lower than 3600 psi.

2. The reheater would be eliminated to allow the simplification required to advantageously utilize the integral arrangement of a condensing reheat cycle, with a slight reduction in plant efficiency to 42%.

The principal design parameters of the reference designs (as given by B&W) are listed in Table 8.5. Since relatively little effort has gone into SCBR design studies, B&W believes that the performance characteristics listed can be substantially improved, especially for the low pressure design, after results of planned thermal and hydraulic analyses of the fuel assembly are available.

8.4 Research and Development Tasks for the Low Pressure System

The costs of specific development tasks and subtasks associated with the 50-Mw(e) reactor experiment and 500-Mw(e) prototype plant are listed in Tables 8.6 and 8.7, respectively.

8.4.1 Task 1 - Cladding, Fuel, and Structural Materials Development

The objectives of task 1 would be to: (1) determine the behavior of Incoloy 800, and modifications of the alloy for cladding and structural use as an alternate to the Inconel 625 specified for the reference designs; (2) assess the potential of high-strength nickel alloys and other candidates for cladding and structural use; (3) assess the performance of fuel, cladding, and structural items used in the 50-Mw(e) experiment in order to make materials selections for the 500-Mw(e) demonstration plant and the 1000-Mw(e) commercial plant; and (4) make materials selections for the reactor vessel, piping, auxiliary components, and control equipment.

Table 8.5. Performance Data for B&W Reference Designs^a

	Low-Pressure System	High-Pressure System
Reactor core inlet, coolant pressure, psia	1250	3700
Fuel type	Mixed oxide	Mixed oxide
Coolant	Steam	Steam
Thermal power, Mw	2900	2326
Electric power, Mw	1012	975
Station efficiency, %	34.9	42.0
Steam conditions		
Temperature, °C	496	538
Pressure, atm	71.4	228
Fuel configuration		
Rods per bundle	391	504
Diameter, cm	0.889	0.584
Cladding material	Inconel 625	Inconel 625
Coolant pressure, atm		
Inlet	85.0	251.8
Outlet	78.3	233.0
Coolant temperature, °C		
Inlet	300	399
Outlet	496	538
Average core power density, kw/liter	390	559
Average specific power, Mw/kg fissile	0.817	0.890
Maximum $\int k \, d\theta$, w/cm	43.0	39.4
Average burnup, Mwd/kg	70.0	73.16
Average fissile inventory, kg heavy metal	3637.1	2615.5
Fertile inventory, MT	29.30	13.28
Fuel life, years at 0.8 power factor	3.14	1.714
Initial enrichment, % fissile (equilibrium cycle)	10.75	15.93
Pu in spent fuel, g fissile/kg U (in-core)	117.8	145.8
Breeding ratio	1.41	1.00
System doubling time, years	9.8	Not applicable
Total power to steam circulators, Mw	100	55.0
Maximum cladding surface temperature, including hot-spot correction, °F	1300	1335
Maximum cladding surface temperature, with no hot-spot correction, °F	1175	1210

^aData from Ref. 98.

Table 8.6. Costs of Research and Development in Support
of 50-Mw(e) Reactor Experiment

	FY Period	Costs (thousands of dollars)
Task 1 - Materials Development		
1.1 Cladding and structural alloy selection	1969-71	147
1.2 Oxidation-corrosion tests	1969-71	1,042
1.3 Mechanical property tests	1969-71	509
1.4 Fuel characterization	1969-71	218
1.5 Thermal irradiations in BAWTR	1969-73	1,330
1.6 Postirradiation tests	1975-78	2,700
	Subtotal	5,946
Task 2 - Fuel Assembly Development		
2.1 Fuel assembly development	1969-71	1,220
2.2 Cladding development	1969-71	876
2.3 Fuel assembly tests	1969-72	566
	Subtotal	2,662
Task 3 - Component Development		
3.1 Steam test facility	1969-70	880
3.2 Direct contact boiler	1970-71	138
3.3 Steam separators	1970-71	98
3.4 Seals, valves, port closures	1970-71	93
3.5 Circulator test facility	1969-70	3,000
3.6 Circulator procurement and tests	1970-72	600
	Subtotal	4,809
Task 4 - Development of Control Rods and Drives		
4.1 Component test facility	1969	136
4.2 Tests of components of rods and drives	1969-71	1,354
4.3 Control rod test facility	1969-70	197
4.4 Prototype rod tests	1971-72	842
	Subtotal	2,529
Task 5 - Fuel Handling System Development		
5.1 Component test facilities	1969	500
5.2 Development and testing of components	1969-71	1,170
5.3 Integrated system demonstration	1973	200
	Subtotal	1,870

Table 8.6 (continued)

	FY Period	Costs (thousands of dollars)
Task 6 - Instrumentation and Controls Development		
6.1 Simulation studies	1969-71	458
6.2 Instrumentation evaluation	1970-73	562
	Subtotal	1,020
Task 7 - Thermal and Hydraulic Performance Optimization		
7.1 Facility construction	1969	500
7.2 Roughened surface optimization	1969-70	120
7.3 Fuel assembly heat transfer	1969-71	639
7.4 Hydraulic flow evaluation	1969-71	380
	Subtotal	1,639
Task 8 - Reactor Safety Evaluation		
8.1 Accident analysis	1969-71	680
8.2 Reactor safety features analysis	1969-71	210
8.3 Special studies	1969-74	415
	Subtotal	1,305
Task 9 - Reactor Physics Research and Development		
9.1 Critical experiments	1970-73	980
9.2 Methods development	1970-72	190
	Subtotal	1,170
Task 10 - Coolant Chemistry and Technology Development		
10.1 Plant environmental technology evaluation	1969-70	160
10.2 Purification and treatment development	1970-74	205
10.3 Chemical cleaning and decontamination	1970-74	565
10.4 Radiolytic dissociation	1969-73	860
	Subtotal	1,790
Task 11 - Research and Development Management	1969-72	525
	Total	25,265

Table 8.7. Costs of Research and Development in Support of 500-Mw(e) Prototype Plant

	FY Period	Costs (thousands of dollars)
Task 1 - Materials Development		
1.1 Cladding and structural alloy selection	1972-73	64
1.2 Oxidation-corrosion tests	1972-73	375
1.3 Mechanical property tests	1972-74	421
1.4 Irradiation tests	1978-80	2,600
	Subtotal	3,460
Task 2 - Fuel Assembly Development		
2.1 Fuel assembly development	1972-74	880
2.2 Cladding development	1972-73	496
2.3 Fuel assembly tests	1972-75	440
	Subtotal	1,816
Task 3 - Component Development		
3.1 Direct-contact boiler	1972-74	340
3.2 Steam separators	1972-74	273
3.3 Seals, valves, port closures	1972	50
3.4 Shaft seals and bearings	1970-71	500
3.5 Circulator procurement and tests	1971-73	1,550
	Subtotal	2,713
Task 4 - Development of Control Rods and Drives		
4.1 Develop and test components	1970-71	600
4.2 Test prototype rods and drives	1972-73	535
	Subtotal	1,135
Task 5 - Fuel-Handling System Development		
5.1 Development and testing of components	1972-74	1,170
5.2 Integrated system demonstration	1975	300
	Subtotal	1,470
Task 6 - Instrumentation and Controls Development		
6.1 Simulation studies	1973-75	815
6.2 Instrumentation evaluation	1974-75	500
	Subtotal	1,315
Task 7 - Thermal Hydraulic Performance Optimization		
7.1 Facility construction	1971	250
7.2 Fuel assembly thermal performance evaluation	1972	132
7.3 Hydraulic flow evaluation	1972-73	407
	Subtotal	789
Task 8 - Reactor Safety Evaluation		
8.1 Accident analysis	1971-76	1,315
8.2 Reactor safety features analysis	1971-76	650
8.3 Special studies	1971-76	440
	Subtotal	2,405
Task 9 - Reactor Physics Research and Development		
9.1 Critical experiments	1971-74	1,495
9.2 Methods development	1973-75	400
	Subtotal	1,895
Task 10 - Coolant Chemistry and Technology Development		
		0
Task 11 - Research and Development Management		
	1972-75	475
	Total	17,473

It was anticipated that the "pancake" core configuration to be utilized in the 500- and 1000-Mw(e) plants would use fuel assemblies of nearly identical design. Thus, successful operation of the 500-Mw(e) plant would provide a proven design of cladding, fuel, and fuel assembly for the 1000-Mw(e) plant.

The selection of a reference alloy for cladding was aided by previous AEC research programs on superheat reactors. These programs also indicated that Incoloy 800 was the logical choice if the cladding temperature could be maintained below 1300°F. Although most of the potential cladding alloys have been tested sufficiently out of pile in the AEC superheat and LMFBFR programs to provide qualitative information, additional tests are needed for fast reactor design. Corrosion tests in the superheat program were conducted at less than 200,000 Btu/hr.ft², and at a temperature controlled to approximately ±100°F. B&W has had experience with corrosion tests operated at 600,000 Btu/hr.ft², and confidently expects to operate at 700,000 Btu/hr.ft². Control of the experimental temperature to +25°F is anticipated. Such tests would provide adequate comparison for true evaluation of any irradiation effects on corrosion during the 50-Mw(e) experiment. A few additional mechanical properties tests would be necessary to provide designers with adequate data. These out-of-pile tests would be conventional and would present no problem in execution. In-pile and post-irradiation property data would be provided for the major part by the AEC-LMFBFR programs. Final selection of cladding and structural alloys for the 50-Mw(e) experiment and the 500-Mw(e) demonstration plant would be influenced by the continuing AEC fast reactor programs. Supplementary information would be gained from tests made on materials irradiated in the 50-Mw(e) experiment.

If UO₂-PuO₂ pellets were used for fuel, no research would be necessary; however, evaluation and characterization of pellets made by competing methods would be necessary in test reactors and the 50-Mw(e) experiment. This would include postirradiation evaluation of materials from these tests.

Most materials selections for piping, auxiliary controls, and components would be based on experience with water-cooled nuclear plants and

with conventional steam generating plants. A few unusual materials requirements are anticipated, however. Materials engineers would participate in the design of the associated equipment in order to assure the inclusion of appropriate materials in corrosion and mechanical property evaluations. In the event that lower cladding temperatures (1100°F) were reached as a result of investigations of surface roughening of the cladding, a greater choice of materials would be available.

8.4.2 Task 2 - Fuel Assembly Development

The objectives of task 2 would be to: (1) optimize two fuel bundle designs derived from previous B&W research and development; (2) produce bundles of each design for mechanical, hydraulic, and thermal tests; (3) determine the fuel bundle design to be used in the 50-Mw(e) experiment; (4) develop a fuel pin from B&W concepts that complements the bundle design and performance requirements; and (5) optimize the fuel pin and fuel bundle designs for the 500-Mw(e) demonstration plant.

The reference design required prepressurization of the fuel pin, and development work was required to choose the end-welding method and means of securing prepressurization. The method chosen would have to be tested. Prior B&W research and development has indicated the directions to be taken.

Ribbed tubing would have to be produced to satisfy the requirements of one of the two bundle designs. B&W already has a sample tube that appears to meet the requirements. Further development would proceed in cooperation with tubing suppliers.

Production methods for enclosure cans would have to be affirmed. The cans would be fabricated and tested in B&W's fuel element development shop.

A grid would have to be produced for use in one of the bundle designs. This subtask would be accomplished by personnel who produce similar grids for thermal reactors. Methods for bundle assembly and joining would have to be developed. This work also would be performed by experienced people.

Analytical design methods for evaluating the fuel pin and the fuel bundle would have to be developed. Much of the information required would

be obtained from the B&W IMFBR effort. Creep-collapse, stress-rupture, vibration, and other conventional mechanical tests would be performed to support the analytical work.

Tolerances for bundle fabrication would have to be established on the basis of thermal, hydraulic, and other considerations. These would be found through performance studies and bundle flow tests.

8.4.3 Task 3 - Component Development

The objective of task 3 would be to develop out-of-core nuclear steam supply system component technology for the 50-, 500-, and 1000-Mw(e) plants. As previously mentioned, component development for the 500- and 1000-Mw(e) plants would be consolidated by using the same size circulators on both plants. Similarly, steam generators and separators would consist of modular arrays of identical units in appropriate numbers for each plant.

This program is organized and funded on the basis that turbine maintenance problems would be comparable with those experienced in BWR plants. They can be made so by adding additional steam generator modules, and passing all core steam flow through these units for washing by the recirculated overspray provided.

The most expensive subtask in the component development program would be to design, build, and test turbine driven steam circulators of proven reliability for use on the 50-, 500-, and 1000-Mw(e) plants. It was considered expedient to provide respective test facilities to operate the 50-Mw(e) and the 500/1000-Mw(e) circulators for a period of two years. Each facility would be equipped to provide the necessary fossil fired steam boilers, condensers, feedwater train, auxiliary power, etc.

A high degree of reliability would also have to be demonstrated for steam generators, steam separators, isolation valves, steam and feedwater line seals, and the pressure vessel. This work would require relatively small expenditures compared with that needed for the circulators.

The passage of all core steam flow through direct contact boilers, as mentioned above, might not be the best measure (from an economic standpoint) to reduce steam contamination. For instance, steam filters

may be adequate, and their use would have little affect upon plant efficiency. Another approach that might be investigated is to design for remote turbine maintenance. Thus, there are at least three approaches to reduce steam contamination in a direct cycle SCBR.

A steam test facility would be designed and constructed for performance and evaluation testing of selected vessel internal components under simulated SCBR environmental conditions. The components to be evaluated in the steam test facility would include full size fuel assemblies (flow tests), a direct contact boiler module, steam separators, control rods and drives, and a steam circulator.

Circulator test facilities would be required for long-term proof testing of turbine driven circulators prior to their use in the 50-, 500-, and 1000-Mw(e) plants. The large power requirements required for considerable periods of time tend to make this phase of the program more expensive than might be required if proven reliability of this equipment were not so important.

8.4.4 Task 4 - Development of Control Rods and Drives

The objective of task 4 would be to develop safe and reliable shim safety rod and regulating rod systems for the near-term 50-Mw(e) SCBR experiment and the 500- and 1000-Mw(e) SCBR plants.

This task would consist of design and engineering studies to establish the functional, operational, and design criteria for the prototype shim safety rod and the regulating rod jack drive. Preliminary tolerances and fabrication procedures for the production of the components of the drive systems would be established in this task. Also included in this task would be a state-of-the-art literature search to provide the basis for initial selection of materials to be used in the prototype shim safety rod and regulating rod jack drives. Research programs and test requirements for evaluation of the drive components would be determined, and preliminary design of necessary test apparatus would be performed.

The design incorporated hydraulically actuated shim safety rods and regulating rods. In both drive systems, the control rods were located

integrally in fuel element subassemblies to eliminate above-core drive mechanisms and components, and to remove the requirements for bottom penetrations in the reactor vessel.

The facilities for testing control rods and drives prior to their use in the 50- and 500-Mw(e) plants would consist principally of suitably sized autoclaves built into the steam test facility previously described. The cost of these facilities to be added to the steam test facility is given in Table 8.6. Rods and drives for both reactors would be designed, fabricated, and tested as soon as possible.

Prior to the autoclave tests of prototype rods for each reactor, there would be a period of rod component tests. These would be essentially shop tests, but some special facilities would be required.

8.4.5 Task 5 - Fuel Handling Systems Development

The objective of task 5 would be to develop necessary fuel handling techniques and equipment for the near-term 50-Mw(e) SCBR experiment and the 500-Mw(e) demonstration plant. The 1000-Mw(e) plant would utilize equipment developed for the 500-Mw(e) plant.

Experience with existing gas-cooled reactor fuel handling systems has demonstrated the feasibility of such systems for application to the near-term 50-Mw(e) SCBR experiment and the 500-Mw(e) demonstration plant. The development of the fuel handling system would depend heavily on the existing technology, but some effort would be required to adapt the present technology to the SCBR, and demonstrate the necessary reliability and safety.

The equipment would be designed for dry refueling. Components include a cooling system for the fuel being handled, a fuel assembly gripper, remote viewing equipment, refueling port closures and seals, electrical devices, and materials of construction.

Environmental testing of fuel handling equipment would require a large vessel capable of operating at steam pressures to 25 psig. This vessel would be supplied with steam by its own boiler, or would use steam from another facility (such as the steam in circulator test facilities). In order to permit personnel training and fuel handling procedure

development, this facility would also include a simulated fuel storage pool, tracks, and casks.

Preoperational proof testing of the integrated fuel handling system would be performed during plant preoperational and startup testing.

8.4.6 Task 6 - Plant Instrumentation and Controls Development

The expedient method of developing instrumentation for the SCBR would be to adapt LMFBR reactor instrumentation to permit operation in a 1250-psi steam environment, and determine by plant simulation studies what types of controls would be required and what demands would be placed on them. Since the kinetic behavior of the reference design has not yet been investigated, it is not possible to accurately assess instrumentation development requirements. In any event, it is assumed that the required instrumentation would be within the present state of the art, with only minor modification and proof testing required. Further planning must await results of a safety and kinetics analysis in task 8.

Autoclaves constructed for other purposes, such as control rod drive proof testing or materials evaluation, would be adapted for environmental testing of in-pile instrumentation. Models and methods developed in task 8 would be used to evaluate plant instrumentation and control.

The analytical models developed for simulation studies would be evaluated by comparing the results of the simulation studies with actual plant operating data from the 50-Mw(e) SCBR experiment. The analytical simulation models would be modified so that all characteristics would be adequately represented. This improved simulation-model would then be available for studies involving the 500-Mw(e) demonstration plant.

8.4.7 Task 7 - Thermal and Hydraulic Performance Optimization

The objective of task 7 would be to: (1) obtain thermal and hydraulic performance information on the SCBR reactor core and internals in order to specify detailed design and confirm performance capability of the near-term 50-Mw(e) SCBR experiment and the 500- and 1000-Mw(e) plants; and (2) further optimize thermal and hydraulic performance of fuel assemblies

by pursuing improvements in surface-roughness technology and performing any required methods development.

An inherent problem in this area is the possibility of flow induced vibration of fuel pins and/or spacers of sufficient magnitude to threaten integrity of the fuel assembly. Past experience with superheat reactor fuel elements has been singularly free of this problem; however, flow velocities in the SCBR are somewhat higher. It appears quite possible that selection of the reference type of fuel assembly (spacer grid versus integrally spaced) may be influenced by tendencies for flow induced vibration.

An important feature of the SCBR design, which should aid in the solution of this problem, is the passage of steam through the core in a downward direction. This puts fuel assembly walls under a collapsing pressure drop, which could be utilized to take up tolerances between pins in the integral design without recourse to spring mechanisms. At least two assembly designs would be tested.

Test facilities required for thermal and hydraulic development would be the following: (1) an existing heat transfer loop in the B&W Research Center at Alliance, Ohio, for surface roughness optimization; (2) an existing PWR fuel assembly test facility at the Alliance Research Center modified to test seven-pin assemblies of prototype SCBR core lattices; (3) a hydraulic flow test facility that would use cold water to test flow distribution in the various components comprising the integral reactor system (core, core tank, direct contact boiler, thermal shields, circulators, and separators), and would be located at the Alliance Research Center of B&W; (4) a fuel assembly flow test facility for initial cold water flow testing (also at Alliance Research Center); and (5) a fuel assembly flow test facility for isothermal flow testing with steam (also at Alliance Research Center).

8.4.8 Task 8 - Reactor Safety Evaluation

The safety program for the SCBR would have to: (1) evaluate potential abnormalities in SCBR operation and their consequences; (2) assess the design-basis accident for the SCBR and the adequacy of the engineered

safety features; (3) develop improved analytical tools, design criteria, and standards; and (4) establish specifications for safety instrumentation and systems.

Safety is an integral aspect in every phase of plant design and operation. When the SCBR is compared with thermal reactors, several significant factors are apparent; namely, the short neutron lifetime, the use of plutonium fuel with its small delayed-neutron fraction, the possible assembly of the core during or following meltdown, and the steam density coefficient. Of these, only the latter is unique to steam-cooled fast reactors.

The design of the SCBR, as the designs of other reactors, depends on the development of reliable engineered safety features. In order to develop these engineered safety features, it would be necessary to study the SCBR concept and its dynamic performance to ascertain the credible accidents. Only after an understanding of the behavior of the SCBR was obtained would it be possible to design the various safety features required and to analyze their effects.

The LMFBFR safety program would contribute significantly in the area of assembly type accidents. The models available for analysis more accurately describe the behavior of a steam-cooled system than the liquid metal systems. There is also a significant quantity of technical information and methods available from conventional steam plants, PWR's, BWR's, and the EVESR, that could be applied to the SCBR safety program. The effects of site conditions and waste disposal are common to other reactor types. The safety program for water reactors offers a basis for comparison, and much data would be useful in the program for the SCBR.

The program required for determining methods of preventing, mitigating, terminating, and controlling accidents have been grouped into three work areas: (1) accident analysis and associated technology; (2) reactor safeguards; and (3) specialized studies. The SCBR program would have to include both the efforts to fulfill the short-term needs associated with the safe design of steam-cooled test facilities, and the longer range responsibility of assuring the safety of the public as the commercial SCBR's were built.

8.4.9 Task 9 - Reactor Physics Research and Development

The objectives of task 9 would be to provide the necessary experimental data and calculation methods required for nuclear design of the 50-, 500-, and 1000-Mw(e) SCBR plants. An important factor of uncertainty in the physics analysis of all fast breeders with plutonium fuel is the value of the ratio of capture-to-fission cross section. It is assumed here that a precise value for this ratio will be determined in other programs, so that measurement is not included in this program.

Critical experiments would be needed to check calculational techniques and verify or determine reactivity coefficients in cores of the fast steam-cooled 50- and 500-Mw(e) reactors. The critical assemblies would be designed to simulate the expected reactor cores as closely as possible. The following measurements would be made: (1) fissile loading requirements; (2) Doppler flooding and voiding coefficients; (3) control rod worths; (4) reactivity effects of fuel movement, e.g., bowing, expansion, etc.; (5) danger coefficient measurements of various clad materials; and (6) integral cross section data. The critical assembly program would be scheduled so that information could be fed into the detailed design two years before completion of construction. Thus, the detailed design of the critical assembly would be started as soon as possible.

The work in this area would also consist of surveillance and utilization of fast reactor physics information and methods made available by the IMFBR and other programs. In general, physics methods would be applicable to all types of fast reactors. Improvements are needed in the following areas: (1) burnup physics; (2) voiding and flooding; (3) fuel management techniques; (4) reactivity coefficient calculations; (5) control rod worth calculation techniques; (6) multidimensional kinetics; (7) multidimensional transport; (8) cross-section updating; and (9) shielding. Of these, only flooding calculation method requirements are peculiar to the SCBR. The voiding problem in a steam reactor is one of uniform density changes rather than local voiding.

8.4.10 Task 10 - Coolant Chemistry and Technology Development

The objective of task 10 would be to develop new water-steam coolant chemistry technology for engineering application to the SCBR plant cycle and system in the following areas: (1) purification, impurity measurement, and additive development; (2) chemical cleaning and decontamination; and (3) radiolytic dissociation control.

Considerable water-steam chemistry technology was obtained during the development of such systems as the Universal Pressure Boiler, PWR's, BWR's, and the superheat reactors (EVESR, etc.), some of which is applicable to the SCBR. However, there appear to be two principal problem areas.

The first is development of methods for chemical cleaning and decontamination. In the event that turbine contamination would be more severe than in BWR's, considerably more effort might be required for decontamination. This might be alleviated by steam microfiltering equipment, or the passage of turbine steam through direct contact boilers. The backup solution could be an indirect cycle utilizing a condenser boiler, or a surface-type boiler heated by superheated steam circulated by the steam circulator.

The second problem is the control of radiolytic dissociation, with the main object being to hold constituents affecting cladding integrity to acceptable values. Reduction of maximum cladding surface temperatures would obviously lessen the severity of this problem. Proper choice of cladding material would also be a factor. The tentative selection of Incoloy 800 and reduction of the maximum cladding surface temperature to 1200°F were largely directed toward this problem.

It was assumed for this program that the direct cycle SCBR conceptual design would prove to be satisfactory from a system contamination and safety point of view, with a backup position being passage of turbine steam through additional direct contact boilers. The development of water chemistry for an indirect cycle or a direct cycle in combination with steam filters was not included. Also not included was any work in conjunction with the use of D₂O in either a direct or indirect cycle.

Facilities for this work would be the water chemistry laboratory and the Alliance Research Center of B&W and the 50-Mw(e) reactor experiment.

8.4.11 Task 11 - Research and Development Management

This task would provide direction for the overall development program, and maintain effective communication between research and development and reactor design, construction, and operation activities.

Overall direction of a program as large as this would be essential to avoid duplication of effort, keep all phases of the work objectively focused on developing the product, and make decisions associated with unforeseen problems.

8.5 Development Assistance from Other Programs

8.5.1 Liquid Metal-Cooled Fast Breeder Reactor Program

The LMFBR program would assist the steam-cooled fast breeder reactor development in the following categories: (1) improved fast reactor methods of nuclear analysis; (2) experimental cross section data; (3) critical experiments; (4) development of fast reactor safety criteria; (5) fast reactor fuel and cladding development; (6) fuel, cladding, and core structure irradiation data; (7) fast reactor instrumentation development; and (8) development of plutonium fuels reprocessing and fabrication technology.

The total contribution from LMFBR fuel and cladding development is estimated to be worth \$7.0 million. The LMFBR program is emphasizing stainless steel for structural use, and the SCBR would use Incoloy 800, a stainless steel, for fuel cladding. ORNL is investigating means of strengthening Incoloy 800, with a concomitant increase in ductility. Specimens are presently being tested in EBR II. The estimate of research and development requirements for the SCBR is based on successful completion of the ORNL work.

The LMFBR program would also contribute on-line computer technology estimated to be worth \$5.0 million. Contributions in other areas have been neglected that would include work in plutonium fuel technology, safety and physics methods, and experimental physics data.

8.5.2 Gas-Cooled Fast Reactor Program

The gas-cooled fast reactor, if developed, would contribute useful technology in fuel handling, emergency cooling, and fuel management. The low pressure SCBR (and also the high pressure SCBR) programs take no credit for possible contributions in these areas.

8.5.3 Other Programs

Significant contributions from the sodium- and steam-cooled fast reactor programs abroad would be possible. However, it is difficult to evaluate such contributions, since timing might not be favorable, and the reactor designs might be quite different. In view of this, it was considered appropriate to neglect any reduction in program costs resulting from these programs.

Since the steam-cooled breeder reactor incorporates a direct cycle operating at a pressure (1250 psi) similar to that of a BWR, a considerable amount of BWR technology would be directly applicable, including the following: (1) auxiliary systems for radioactive waste disposal; (2) turbine maintenance and decontamination procedures; and (3) applicable designs of pressure vessels, containment vessels, turbines, condensers, condensate systems, isolation valves, and emergency core-cooling systems.

In short, the available technology and the current development programs, combined with other phases of conventional steam power plant technology, result in relatively inexpensive component development requirements associated with varying degrees of first-of-a-kind engineering and tests, rather than feasibility determinations, especially for the low pressure system.

8.6 Analysis of the Proposed Program

With the prescribed format for the analysis of research and development programs associated with the various reactor concepts as a basis, the following paragraphs present ORNL's evaluation of B&W's proposed SCBR development program.

8.6.1 Performance Characteristics

The important performance characteristics of the two steam-cooled fast reactors used as a basis for the development program are summarized in Table 8.5. Wherever the numbers differ from those in the design reports prepared by B&W, they are the results of the ORNL evaluation.

As stated in Chapter 3, the concept that produced power at the lowest cost was the supercritical high pressure design developed by B&W, and the concept that yielded the highest breeding ratio and the shortest doubling time was the low pressure B&W concept. The maximum spread in power cost was 10%, which may be within the range of accuracy of the evaluation, and is partially due to different degrees of conservatism in the designs presented.

8.6.2 Problems Associated with Each Design Alternative

The development program for the higher pressure concepts would be essentially the same as that given for the 1250-psi design, except that if the higher pressure designs were to be developed, more effort should be concentrated on reducing the coolant volume fraction, increasing the fuel volume fraction, and selecting structural materials with lower neutron absorption properties.

Much more information must be developed on the physics of steam-cooled reactors before optimum configurations and nuclear characteristics can be specified with accuracy. Heat transfer from the fuel rods to the steam must also be developed more thoroughly before the thermal and hydraulic characteristics of the core can be accurately determined. In particular, the safety of these reactors must be more thoroughly investigated and adequate safety features developed, not only for the maximum accident, but also for the more probable accidents of lesser magnitude that might occur.

The effect of center melting of the fuel is not well known, although the calculations indicated that some center melting could be expected in the two concepts considered. Development of fuel elements with improved heat transfer characteristics or higher surface-to-volume ratios may be desirable if center melting is found to be detrimental and unsafe.

Development of reliable large-scale steam circulators represents some extension of existing technology and would require the construction and testing of prototypes in order to determine that the extrapolation was successful.

In common with all advanced concepts, the safety of the steam-cooled fast reactor is paramount to the success of the concept. The high pressure containment concept proposed by B&W adds to the cost of the low pressure plant and may be responsible in part for its high power cost. However, the provision of that containment vessel and satisfactory emergency cooling systems is considered essential.

8.6.3 Important Milestones

Important milestones in the SCFR development program are listed in Table 8.8. The most important of these milestones are the dates of availability of various experimental facilities. The first of these is the 50-Mw(e) steam-cooled fast reactor experiment (SCFRE), which would be expected to start operation by December 1973. Four years is allowed for operation of the experiment before startup of the 500-Mw power demonstration plant. The schedule for construction and operation of the SCFRE allows five years for design and construction, plus the four years of operation, for a total of nine years. This makes no allowance for possible setbacks that might occur, but setbacks could be provided for by backup efforts on those problems that appear most likely to cause trouble. However, as suggested for the gas-cooled reactor program, another year before startup would be more suitably conservative.

8.6.4 Tasks 1 and 2 - Cladding, Fuel, and Structural Materials Development and Fuel Assembly Development

The proposed program is centered on Incoloy 800 for cladding material on the assumption that surface temperatures can be reduced to 1200°F or below. This selection is inconsistent with the materials specified in the reference design tabulation given in the summary of the program. Achievement of the lower surface temperature would require redesign of the core, a new physics evaluation, and a new thermal hydraulic analysis. Changes resulting from the analyses would have an effect on the overall

Table 8.8. Summary of Important Milestone Dates in Steam-Cooled Fast Breeder Reactor Development Programs*

Start of program	7-1-68
50-Mw(e) experiment design start	7-1-68
500-Mw(e) plant design start	7-1-72
50-Mw(e) experiment construction complete	7-1-73
Development of control rods and drives complete	7-1-79
Test and operation of 50-Mw(e) experiment	12-1-73 to 7-1-77
Component development complete	7-1-74
Physics research and development complete	7-1-79
Coolant chemistry technology development complete	7-1-74
Fuel assembly development complete	7-1-79
Instrumentation and controls development complete	7-1-79
1000-Mw(e) plant design start	1-1-76
Reactor safety evaluation complete	7-1-76
500-Mw(e) plant in operation	12-1-77
500-Mw(e) plant precommissioning operation	12-1-77 to 7-1-79
Contract let for four 1000-Mw(e) plants	1-1-79
Operation of a 1000-Mw(e) plant	1-1-81
Commercial availability of four plants	12-31-82

*Same for all pressures or combined program involving two different pressures.

evaluation, because reduction of the surface temperature would require either a change in linear fuel rating, which would change the core size, or a reduction in the steam temperature. From the physics standpoint, however, Incoloy 800 with no columbium, tantalum, or molybdenum would result in better neutron economy and, thus, would improve the breeding ratio and doubling time. The materials program should include a broader survey of alternate materials for cladding and structural use in the core.

The program is based on development of pelletized fuel, whereas the reference design and the evaluation included in this report were based on the use of vibration compacted oxide fuels. Heat transfer characteristics would be improved by the use of pellets. It is not obvious, however, that the fuel swelling problem is less serious for pellets than it is for vibration compacted fuels. The part of the program that deals with the fuel body properties and performance is common to all fast breeder reactors regardless of the coolant used, particularly if burnups of the order of 100,000 Mwd/MT maximum are postulated.

Prepressurization of the fuel rod is a problem that is of particular importance to the steam-cooled reactor. The large number of rods required for one fuel loading (about 130,000 in the low pressure design) makes the probability of a leak very high. Since it is desirable to attain absolute leaktightness to maintain the prepressurization and to prevent leakage when fission gas pressure builds up inside the rods, a program to develop reliable and consistent methods of sealing after prepressurization and of testing for leaktightness would be required. A very high degree of quality control would also be required in manufacturing the rods to meet these requirements.

The use of a direct steam cycle increases the importance of leaktightness of the fuel rods throughout their lives. Alternate approaches to this problem should be investigated to determine whether a pressure equalization system could be devised, or whether the cladding could be made sufficiently strong to be free standing without prepressurization.

The amounts of money shown in the program for materials development and fuel assembly testing appear to be low by a factor of about two, and the fuel assembly development program period should be continued to 1979.

8.6.5 Task 3 - Component Development

The component development program included steam generators, separators, and circulators. It did not include any work on the reactor vessels or development of control rod drives and refueling equipment, which are covered by separate tasks.

The vessels proposed for the low and intermediate pressure designs presented no problems beyond those associated with vessels of current water-

cooled reactors so long as the only steam in contact with the vessel walls was at saturation temperature. Both designs were based on reactor vessels with welded heads; however, it was indicated in the development program that the vessels would have removable heads. Capital cost evaluations given in this report are based on the use of welded heads for both B&W concepts, and the use of flanged head construction would have some effect, both on the capital cost and on the development program, for the fuel handling and shutdown cooling systems. Thermal shielding, although inadequate in the proposed design for the low pressure reactor, can be increased without imposing any additional development work.

The major component problem would be the development of steam circulators for all designs. The proposed program envisions construction of prototype machines to be tested under actual operating conditions, so it should be possible to uncover and solve any problems that developed. It might be necessary to build and test more than one prototype before a completely satisfactory circulator was achieved.

8.6.6 Task 4 - Development of Control Rods and Drives

B&W proposed a unique control rod and drive combination in which the rod would be an integral part of the fuel bundle, and be driven by hydraulically actuated pistons that would also be built into the fuel assembly. Since this is a new concept in control rod operation, its design and development would require extensive effort. The first set of these rod and drive systems was estimated to require four years of development effort at a total cost of \$2,529,000 culminating in the drives for the 50-Mw reactor experiment. Additional testing was proposed for the larger scale reactors at an additional cost of \$1,135,000, or a total of \$3,664,000 for development of the rods and drives for the 50- and 500-Mw(e) reactors. This amount may be adequate if the concept proves to be successful, but if it should turn out to be unsuccessful, some development work would be required to provide conventional rods and drive mechanisms for the steam-cooled reactor. It is probable that some development on these systems would continue until the large-scale reactors were operating.

8.6.7 Task 5 - Fuel Handling Systems Development

The development of special equipment that is useful only on the 50-Mw(e) reactor experiment should be held to a minimum consistent with the requirements for making that experiment operable and successful. It was proposed to develop fuel handling equipment for the 50-Mw(e) experiment, and also to develop larger scale equipment suitable for both the 500- and 1000-Mw(e) reactors.

Cooling of the core must be maintained throughout the shutdown period while the reactor is being refueled. For this reason, it is desirable to design and develop remote devices so that the large-scale reactor can be refueled while the cooling system continues to operate. This may not be necessary for the reactor experiment if it can be designed to allow flooding of the core and removal of the top head. It is recommended that either this latter course be followed, or that refueling equipment be developed for the reactor experiment that can be scaled up to the size required for the larger reactors.

8.6.8 Task 6 - Instrumentation and Controls Development

The total cost estimated for development of instruments and controls up through the developments for the 500-Mw(e) reactor was \$2,335,000. The program did not include any development for the extrapolation of the instrument systems to those required for the 1000-Mw(e) reactor, which would probably include some further simulation studies and some studies of the effects of size of the core on in-core instrumentation and on the safety of the system.

This program is intimately related to the thermal, hydraulics, physics, and safety programs in that the simulation studies must incorporate systems analyses that relate all those characteristics and also evaluate the accidents against which the safety program development must be planned.

Most instrumentation was expected to be available from existing applications, so the development and design work in this area consisted primarily of systems analyses and studies. The amount of money provided is probably adequate for the range of coverage it provides, but funds should be available to support this work until the large reactors are in operation.

8.6.9 Task 7 - Thermal and Hydraulic Performance Optimization

Two problems in the thermal-hydraulic area that have not been discussed in the research and development program are the thermal conductivity of the fuel and the heat transfer correlation for transfer of heat from rods to steam. In the evaluation of these reactors, it was found that these questions introduced the greatest uncertainties.

Out-of-reactor work with electrically heated rods could be performed to establish more reliable heat transfer data between the rods and the steam. Determination of the internal performance of the fuel, especially after long exposure in the core, should be done in radiation facilities. The funds indicated for this work are probably not adequate to perform in-reactor experiments, but since this problem is common to all fast reactors with the same fuel forms, the steam-cooled breeder can benefit from the results of work performed under the LMFBR program.

8.6.10 Task 8 - Reactor Safety Evaluation

The portion of the safety program on reactor safety features should include a study of the functional adequacy of the spray systems to start fast enough to prevent melting and to distribute coolant throughout the core. This is a major factor of uncertainty in evaluating the adequacy of these safety features for use in steam-cooled fast reactors. Although the design studies and the evaluation were based on the use of core spray systems for emergency cooling under conditions where the main coolant flow ~~was~~ not available, it has not been determined whether other means of providing emergency cooling would or would not be better for this purpose. A review of alternates, such as steam or compressed air, should probably be a part of the program. In any event, it is important that fast action on the part of these systems be demonstrated because of the need for immediate availability of emergency cooling under accident or loss-of-flow conditions.

The task should also include some investigation of the consequences of meltdown of the core, resultant criticality problems, such as nuclear excursions, and methods for dealing with them to provide safe containment of the radioactive materials in the fuel.

8.6.11 Task 9 - Reactor Physics Research and Development

Although additional experimental and analytical work is needed to establish the capture-to-fission cross-section ratio for ^{239}Pu , and this quantity is a very important factor in determining the potential of the steam-cooled fast reactor, it ~~was~~ assumed the results of work now in progress ~~would~~ make this data available. However, once the ratio ~~was~~ determined, a reevaluation of the physics performance of steam-cooled breeder reactors should be performed.

The proposal that the critical experiment should be made with fuel pins in the form of an almost exact mockup of the actual core is subject to some question. However, it is true that critical experiments should be performed with core compositions the same as those proposed for the reactors, and it would be desirable to vary these compositions in the critical experiments to study their ~~affects~~ on core performance. Extensive work is required on the physics of these reactors, and it would not stop when the reactor experiment had operated. It would be required for the larger scale reactors until they were operating at full power.

The assumption that government-owned facilities and staff would be used in performing these critical experiments may be accurate, but the cost of that use should be included in the program.

8.6.12 Task 10 - Coolant Chemistry and Technology Development

The program on coolant chemistry ~~proposed~~ studies of the problems of chemical cleaning and decontamination and of radiolytic dissociation, but it ~~did~~ not include consideration of stress corrosion. It is not known whether stress corrosion would be a significant problem in the steam-cooled reactor, particularly if the water purification program was successful in removing the constituents that contribute most to this type of attack. The austenitic stainless steels are most sensitive to stress corrosion in the presence of chlorides, but it appears that the core materials for steam-cooled reactors would be operated at temperatures above those at which the austenitic stainless steels could be used. In spite of this, however, some exploratory work should be included in

the program to determine whether stress corrosion may be a problem with the proposed materials.

8.6.13 Task 11 - Research and Development Management

It may be that five engineers and scientists would be adequate to manage the proposed program, but it appears more likely that a full-time director would be required for the overall program, and that each task would also require a specialist in its management, so as many as 11 or 12 management people might be required to adequately supervise the entire program. This would increase the total cost of this task from \$1 million to perhaps \$2.5 million.

8.6.14 Summary and Additional Comments

This evaluation of the proposed research and development program is based on the program proposed by B&W for the low pressure reactor, with the general assumption that these comments would also apply generally to programs for the other reactors, with slight modifications. The problems that were uncovered during the evaluation of the proposed designs were compared with the programs proposed, and those that were not included are discussed in the foregoing paragraphs.

In summary, it was found that the proposed research and development program was based on some conceptual features that were different from the features of the reference designs; for example, pelletized instead of vibration compacted fuel, removable pressure vessel head instead of welded head, and eight circulators instead of four for the 1000-Mw(e) design. These differences are probably improvements and acceptable. The program included very little radiation testing, and depended on separate governmental assistance for critical experiments for which no costs were given.

No development programs were proposed to cover fuel recycle, demonstration safety systems, or plant design studies. The addition of these items might cause an increase in the estimated cost of the program. It is recognized that the planning, scheduling, and estimating of development costs is subject to considerable uncertainties and limitations due to the unknown developments that can occur in the course of the work.

The uncertainties in estimating costs and schedules for research and development are reflected in the differences between those estimates for the steam-cooled fast reactor and the gas-cooled fast reactor programs. In these two programs, the items reflecting the greatest differences in estimated costs were: (1) core and fuel assembly development (\$16.3 million for SCBR versus \$93.7 million for GCFR); and (2) physics development (\$3.1 million for SCBR versus \$20.5 million for GCFR). The estimates for component development were very close, \$16.8 versus \$16.7 million. Both programs depend heavily for support from results of the LMFBFR program, but the gas-cooled reactor depends most heavily at \$209 million compared with \$12 million for the SCBR.

The estimated cost of supporting research and development for the low pressure steam cooled reactor was \$42,738,000, spread over a period of 11 years, with the maximum in any one year being \$7,422,000. Even though a large body of steam technology exists, the program areas applying to the reactor core would be expected to be nearly as broad as the corresponding program areas for other fast reactor concepts. Thus, the component task (No. 3) could be expected to benefit greatly from existing technology, but all other tasks would require extensive effort. The total program cost would probably be of the order of \$90 million to \$100 million, instead of the \$42.5 million given in the proposal. Table 8.9 shows a revision of Tables 8.6 and 8.7 in which costs have been increased to yield a total of \$91.8 million for the research and development portion of the program, excluding reactor design and construction.

Table 8.9. Revised Development Program Cost Summary for Low-Pressure Steam-Cooled Breeder Reactor

Research and Development	Costs (thousands of dollars)												Total
	FY-1969	1970	1971	1972	1973	1974	1975	1976	1977	1978	1979	1980	
Task 1 - Materials Development (including fuel)	2,000	2,000	2,000	2,000	1,500	1,500	1,500	1,500	1,500	2,000	1,500	1,500	20,500
Task 2 - Fuel Assembly Development	1,600	1,800	1,600	1,600	1,400	3,000	1,400	1,000	1,000	1,000	1,000	1,000	17,400
Task 3 - Component Development	2,000	2,400	1,100	1,200	900	200							7,800
Task 4 - Development of Control Rods and Drives	400	1,000	1,300	900	400	400	400	400	400	400	200	200	6,400
Task 5 - Fuel-Handling System Development	800	1,000	1,000	1,000	1,000	800	600	600	600	600	600	600	9,200
Task 6 - Instrumentation and Controls Development	200	300	300	500	300	1,000	1,000	500	500	500	500	500	6,100
Task 7 - Thermal and Hydraulic Performance Optimization	700	800	800	800	800								3,900
Task 8 - Reactor Safety Evaluation	300	500	700	700	500	800	500	400					4,400
Task 9 - Physics Research and Development	200	1,000	1,000	1,000	1,000	500	500	500	500	500	500	500	7,700
Task 10 - Coolant Chemistry and Technology Development	200	500	500	500	400	200							2,300
Task 11 - Research and Development Management	400	400	400	400	400	400	400	300	200	100	100	100	3,600
Task 12 - Plant Design Studies	200	300	400	400	400	400	400						2,500
Totals	9,000	12,000	11,100	11,000	9,000	9,200	6,700	5,200	4,700	5,100	4,400	4,400	91,800

9. PARAMETRIC DESIGN STUDIES OF STEAM-COOLED FAST BREEDER POWER REACTORS

The Pacific Northwest Laboratory of Battelle Memorial Institute was asked to perform a parametric study of steam-cooled fast breeder power reactors over the range of steam conditions of current interest. The purpose of this study was to identify the major effects of the steam pressure and the importance of the choice of pressure on reactor performance based on consistent methods and assumptions. Interest in this subject arose from the wide range of steam pressures chosen in four recent design studies: 1200, 1500, 1800, and 2500 psi. The study was intended to supplement the ORNL evaluation by helping to separate the effects of steam-pressure choice from the effects of design approach and assumptions. Quantitatively, it was not normalized to any of the ORNL specific evaluations, and the results for a given reactor differ somewhat from the ORNL results, since different methods of calculation were used where necessary for expediency.

This chapter is a condensation of the Battelle-Northwest report on the parametric studies.¹⁴ It contains the basic assumptions made for that study and some of the significant results and conclusions presented in that report. The ORNL staff did not critically review these results or the methods by which they were obtained before presenting them here, but it is believed that the parametric study gives a good perspective of the effects of some of the variables and, thus, can be used to guide future efforts on steam-cooled fast breeder reactor design, provided the assumptions and methods used in the study are carefully considered along with the results.

The parametric studies provided estimates of the **affect** on breeding ratio, doubling time, and fuel cycle cost of the following design and operating parameters in reactors designed to produce 1000 Mw of electric power: (1) steam conditions and turbine inlet pressure and temperature; and (2) core design, volume, and shape (height-to-diameter ratio). The sensitivity of reactor performance to parameters that could be varied to reflect changes in design philosophy was also examined.

The following ranges of parameters were investigated:

Steam pressure, psi	1000-3000
Steam temperature, °F	900-1100
Height-to-diameter ratio	0.2-0.6
Core volume, liters	5,000-10,000
Core material volume fractions, %	
Coolant	25-60
Fuel	60-25
Structural material	15

The general approach taken was to set up a matrix of nine cases for core physics calculations to cover the range of steam inventories and core material volume fractions of interest. Results of these calculations would then be plotted in a form suitable for input to the parametric engineering core design and fuel cycle cost computations. These were made with an expanded version of a digital analog model that had been developed at Battelle-Northwest for FFTF core design tradeoff studies. This model designs cores to specified limits (e.g., cladding temperature, fuel center-line temperature, core pressure drop) and allows design parameters to be varied or fixed according to the cases being investigated. The primary results presented here are for cases in which core volume, core shape, power distribution, out-of-core pressure drop, and steam temperature were held fixed over the full range of steam pressure, and material volume fraction and fuel rod size were varied. At one selected pressure, the effects of varying the other design parameters are presented. The effect of varying these parameters at other pressures was not studied.

9.1 Summary of Results

The nuclear survey was based on a set of nine combinations of steam inventory and core material volume fractions, designated as cases OA through 2C. Figure 9.1 shows the characteristics of these cases and their relationship to the three specific designs evaluated by ORNL. The geometric model for the physics calculation was based on the Karlsruhe intermediate pressure design. In most calculations, 0.276-in. pins, spherical geometry, an 18-in. depleted uranium blanket, and a Karlsruhe-type reflector were used. In all cases 15% cladding structure of

Fig. 9.1. Reference Designs Relative to Parametric Nuclear Analysis.

Inconel 625 was assumed. An equilibrium plutonium isotopic composition of 76:21:2.3:0.7 for ^{239}Pu : ^{249}Pu : ^{241}Pu : ^{242}Pu was used for all cases. The methods used in the physics parametric study were chosen to be as close as possible to those used by ORNL so that the two studies would compare reasonably well. The ORNL cross section set was used to help eliminate discrepancies in the nuclear analysis.

Table 9.1 is a summary of the physics results that were incorporated as input into the Battelle-Northwest design and economics simulation. In comparing the results from Table 9.1 with the gross design parameters of Fig. 9.1, it may be seen that the cases with lower steam inventory and higher fuel fraction have the best breeding ratio, as expected. Blanket breeding gains are modest in value, even for the "A" cases, which have low steam inventory. This indicates that the blanket model used may not be optimum.

The potential doubling time* of a steam-cooled breeder reactor is not, however, obvious from the physics results alone, since the fissile

*The definition of doubling time used here is

$$t_d = \frac{0.693}{\text{Fraction of annual increase}} \cdot$$

The out-of-core inventory for each design is based on process times for a 15,000-Mw(e) industry.

Table 9.1. Results of Battelle-Northwest Physics Parameter Survey of Steam-Cooled Fast Breeder Reactors

Case	Steam Inventory (lb of steam per ft ³ of core volume)	Core Volume Fractions		k_0	Core Volume (liters)	Core Enrichment (%)	Initial Breeding Ratio		Reactivity Effects		Plutonium Inventory (kg)
		Steam	Fuel				Core	Total	k_{void}	Δk	
OA	0.367	0.25	0.60	1.01	1,000	15.15	0.890	1.420	0.999	-0.011	752
					3,000	12.55	1.067	1.390	1.000	-0.010	1867
					6,000	11.70	1.170	1.385	1.003	-0.007	3479
					8,000	11.40	1.195	1.380	1.001	-0.009	4519
					10,000	11.25	1.215	1.385	1.001	-0.009	5574
OB	1.18	0.25	0.60	1.01	1,000	14.90	0.835	1.280	0.991	-0.019	739
					3,000	12.50	0.980	1.240	0.999	-0.011	1859
					6,000	11.67	1.071	1.255	1.001	-0.009	3470
					8,000	11.30	1.11	1.258	0.995	-0.015	4479
					10,000	11.20	1.115	1.245	0.999	-0.011	5549
OC	2.83	0.25	0.60	1.024	1,000	15.15	0.755	1.080	0.999	-0.025	752
					3,000	12.75	0.855	1.043	1.009	-0.015	1896
					6,000	12.00	0.920	1.045	1.018	-0.006	3569
					8,000	11.65	0.947	1.050	1.014	-0.010	4619
					10,000	11.55	0.9485	1.0425	1.018	-0.006	5723
1A	0.367	0.40	0.45	1.010	1,000	19.10	0.71	1.35			712
					6,000	13.40	1.015	1.307	1.003	-0.007	2991
					8,000	12.95	1.055	1.292	1.003	-0.007	3853
					10,000	12.90	1.050	1.262	1.014	+0.004	4797
1B	1.18	0.40	0.45	1.022	1,000	19.4	0.64	1.12			724
					6,000	13.80	0.900	1.115	1.020	-0.002	3080
					8,000	13.40	0.926	1.105	1.023	+0.001	3987
					10,000	13.10	0.945	1.11	1.023	+0.001	4872
1C	2.83	0.40	0.45	1.063	1,000	21.1	0.55	0.89			788
					6,000	15.10	0.741	0.889	1.071	+0.008	3373
					8,000	15.00	0.748	0.870	1.088	+0.025	4467
					10,000	14.50	0.770	0.775	1.082	+0.019	5396
2A	0.367	0.55	0.30	1.053	1,000	30.00	0.45	0.93			749
					6,000	18.80	0.700	1.019	1.060	+0.007	2804
					8,000	17.85	0.745	1.018	1.059	+0.006	3548
					10,000	17.30	0.7695	1.019	1.062	+0.009	4297
2B	1.18	0.55	0.30	1.076	1,000	31.2	0.39	0.80			780
					6,000	19.90	0.611	0.805	1.093	+0.017	2969
					8,000	18.90	0.645	0.840	1.093	+0.017	3759
					10,000	18.30	0.678	0.825	1.096	+0.020	4548
2C	2.83	0.55	0.30	1.092	1,000	32.5	0.35	0.67			813
					6,000	21.10	0.532	0.7015	1.122	+0.030	3150
					8,000	20.30	0.565	0.695	1.1375	+0.046	4039
					10,000	19.70	0.578	0.700	1.141	+0.049	4899

inventory depends on the engineering design of the core and on safety constraints. Although the void coefficients given in Table 9.1 decrease for higher fuel fractions and lower steam inventory, there is considerable uncertainty in their magnitude that depends on the cross section data used. Because of the uncertainty in specifying a limit on the void coefficient, and the uncertainties in its calculated values, it was not used as a constraint on the fuel volume fraction in these parametric studies. Instead the limit on the fuel volume fraction was arbitrarily set at 60%. This represents a minimum allowable pitch-to-diameter ratio of about 1.05, which is somewhat lower than has normally been taken as the minimum practical value.

In formulating the input for the parametric calculations, it was necessary to use linear approximations over discrete ranges to describe the coolant properties and physics data and to employ linear interpolation for intermediate points. The results show some perturbations or inflections in curves that reflect the input and calculational methods rather than system characteristics. This should be borne in mind when interpreting the results presented.

Appendix A of the Battelle-Northwest report¹⁴ discusses the mathematical model and calculational procedures used to perform the parametric engineering and economics studies. The results are summarized in Figs. 9.2, 9.3, and 9.4, which show the variation in breeding performance and fuel cycle costs over a range of pressures for three general design approaches selected from the sensitivity studies. By adjusting the number and size of the fuel pins, a maximum design cladding temperature of 1374°F and a maximum fuel center-line temperature of 4850°F was maintained for each design over the pressure range shown. Design 1 in these figures is similar in most respects to the Karlsruhe D-1 design, design 2 is similar to the B&W low pressure design, and design 3 is similar to design 2, with the exception of hydraulic performance. The hydraulic performance assumed for each design is indicated by the out-of-core pressure drop and the friction-factor multiplier, which represents different in-core structure designs. The streamlining of designs 2 and 3 was necessary for the smaller volume cores, particularly at low system pressure, to obtain reasonable pumping powers.

Fig. 9.2. Breeding Ratio Versus Reactor Outlet Pressure.

Fig. 9.3. Doubling Time Versus Reactor Outlet Pressure.

Fig. 9.4. Fuel Cycle Costs Versus Reactor Outlet Pressure.

The improved breeding ratio of design 2 compared at a constant pressure with design 1 (Fig. 9.2) is due to the higher fuel volume fraction in design 2, as well as the lower steam inventory. The gain in breeding ratio at constant pressure is comparable with the gain in breeding ratio from varying the pressure over the entire range. The effect of redesigning at constant pressure is even more evident in the doubling time shown in Fig. 9.3. The gain in doubling time is due partially to the improved breeding ratio, but the lower fissile inventory of the smaller core volume is also important. Designs 2 and 3 are not optimized, but they do show the gains that can be made by altering the thermal and hydraulic assumptions, so more compact cores with higher fuel fractions can be considered.

The doubling time curve for design 1 in Fig. 9.3 is distorted by an error in the integral control circuit that maintains the fuel center-line temperature at 4850°F. Between 1950 and 2200 psia, the actual fuel center-line temperature changes from 4849 to 4854°F. The temperature

remains at 4854°F from 2200 to 2400 psia, and then returns to 4850°F above 2400 psia. This small error in fuel center-line temperature is the primary cause of small inflections in the fuel volume fraction and steam inventory. These inflections, in turn, cause small errors in both breeding ratio and fissile inventory. These errors compound, however, to cause a considerable distortion of the doubling time curve. The curve for design 1 should have the same shape as for designs 2 and 3. The controller error at 2850 psia is negligible; hence, the doubling time shown for that point is correct. These errors could have been eliminated by proper selection of controller gains and phases; however, the resulting distortions do not, in general, affect the conclusions drawn from the data, so the studies were not repeated.

The fuel cycle costs for the three designs are shown in Fig. 9.4. These appear to change so little between designs or over the pressure range that, as a first approximation, fuel cycle costs could be considered to be independent of steam pressure. At lower system pressure, the higher fabrication costs associated with smaller fuel pins offset the gain in plutonium revenue from better breeder performance. The trends for fuel cycle costs over the pressure range could be affected by more refined unit fabrication cost information.

From the results of this parametric study, the following conclusions can be drawn:

1. Fuel cycle costs are very weakly affected by choice of steam pressure.
2. The choice of steam pressure depends on whether the chief objective in a given design is to minimize capital cost or to emphasize breeding ratios and doubling time.
3. Performance, in terms of breeding ratio, doubling time, and fuel cycle cost, is affected as much by design approach and assumed limitations as by choice of steam pressure.
4. Designs that reduce hydraulic resistance are important to realizing the full benefits of the breeding ratio potential in low steam pressure designs.

This study also shows some key areas where additional information is needed at an early date to make a more conclusive assessment of the

potential for the steam-cooled fast breeder reactor, and to help resolve the choice of steam pressure. One of the most important aspects of future work on steam-cooled fast reactors is to carry out some critical experiments with a representative neutron spectrum. Such experiments would put steam-cooled fast reactor evaluations on a much firmer footing by calibrating cross section sets and methods. In addition, dynamic system studies should be performed to define the limits on safety coefficients that must be obtained in the reactor design. Of particular importance is defining the necessary void coefficient. Design studies could then be performed over a suitable range of core volumes, height-to-diameter ratios, steam inventories, thermal limits, and hydraulic designs to define better the limiting design conditions with the required void coefficient as a criterion. Because of the economic attraction of employing high fuel volume fractions in the core, data should be obtained on hydraulics and heat transfer at low pitch-to-diameter ratios for pin arrays to define practical limits as a function of steam conditions. With this information more rigorously evaluated, the potential performance of the steam-cooled fast breeder reactor can be determined with much greater confidence.

9.2 Assumptions Made in Study

The equilibrium plutonium composition was held constant for all cases and was taken from Kiefhaber and Ott.⁹⁹ It is in close agreement with specific cycle analyses reported by others.^{5,8} The depleted uranium used in the core and blanket was a composition suggested by Eschbach¹⁰⁰ as typical of that available in 1980. The fuel pin diameter, as well as many other design specifics, was taken from the Karlsruhe D-1 design.¹⁰ Spherical geometry was assumed for ease of calculation; cylindrical cores with various height-to-diameter ratios were represented by relating them to spheres of equivalent buckling. All the nonfuel material, both cladding and structure, was assumed to be Inconel 625, which has the following nominal composition (in wt %):

Fe	3
Ni	61
Cr	22
Mo	9
Ta	4

The blanket was assumed to be geometrically identical to the core (in some specific designs it is more dense than the core and in others less dense). An 18-in. shell of blanket material was used for all base calculations, although the results are not very sensitive to this dimension. Most calculations were for the beginning of life for the blanket, and it was assumed that no plutonium was present. The reflector was patterned directly after that of the D-1 design.

10. COMMENTS OF REVIEWERS

1. The work of the Alternate Coolant Task Force shows a strong possibility that the low pressure SCBR is potentially an attractive candidate for development into an economical breeder. The SCBR appears to offer near-term development potential while achieving an adequately high breeding ratio in the low pressure (1200-psi) concept. Even though the capital cost of the high pressure (3700-psi) SCBR appears attractive, its breeding potential is too low for serious consideration toward achievement of the goals of the breeder reactors. It should be classified as an advanced converter reactor concept.

2. None of the designs in the report are well optimized, and many of the problem areas in the SCBR are not defined in detailed fashion. To illustrate the inadequacy of the current designs, it is interesting to realize that less than \$300,000 was spent for the design of the low pressure SCBR. The earlier B&W work on the high pressure SCBR design had educational and background value, but it supplied little directly applicable engineering data.

3. In many aspects the designs of this report have severe deficiencies. The steam-cooled reactors lack adequate operational and safety information, and **there are no defined systems for cooling the debris** of the plant after a destructive incident. Although these features could be designed into the steam-cooled reactors, they could have a significant impact on the normalized evaluation and lead to substantial economic penalties. Surely questions of this type cannot be answered at this time due to the limited engineering and development of this concept. Many of these same types of questions have not been answered for the LMFBR.

4. The report refers to one of the disadvantages of the steam-cooled fast reactor as the reduction in breeding ratio due to the relatively soft neutron spectrum. Generally this is true for the higher pressure systems; however, the low pressure design **had** a mean fission energy above 160 kev, which is higher than that of some interesting sodium reactor designs. The report also refers to the handicap of larger inventory and low breeding ratio of the steam-cooled fast reactor. Even with these

handicaps and lack of optimization, as indicated in the same paragraph, the low pressure steam reactor achieved a respectable doubling time of 12 years.

5. A discussion is presented on the effects of the possible higher capture-to-fission ratio for ^{239}Pu . It is indicated that the greatest effect would be the lengthening of the doubling time, particularly in those systems with low breeding ratios. This is true; however, the economic impact is about equivalent for systems with high or low breeding ratios. The most severe and probably significant effect would occur in those reactors designed for near minimum leakage geometry with an in-core breeding ratio near unity and small control rod requirements. In this type of system, major design changes would be required from a resultant lower breeding ratio that could result in substantial economic effect.

6. The maximum-to-average radial power factors for the steam-cooled reactors are quoted as 1.48, 1.44, and 1.12 for the low, intermediate, and high pressure cores, respectively. It is believed that lower power peaking factors could be accomplished in the low and intermediate pressure designs. Time and funding have not allowed optimization and achievement of this desirable and possibly illusive goal. Substantial importance should be placed on this feature in further studies and in appropriate normalizations among the various steam-cooled reactors, between the steam-cooled reactors and the gas-cooled reactors, and between the alternate coolant concepts and the reference LMFBR concept.

7. There are instances where a deficiency of technology has been attributed solely to the SCBR rather than being described as a problem common to all types of fast breeder reactors. Since the commonality of many problems may not be well known to the reader, this would help to insure that the reports would not be misconstrued.

8. The report points out that two different fast shutdown systems should be provided, and that they must be of different design. It seems unwise to establish arbitrarily the absolute need for two fast shutdown systems based on different principles. Before such a statement is made, it should be shown that they are needed and that they are technologically available. Water-cooled reactors do not have two fast shutdown systems.

Perhaps fast reactors should have two fast and independent systems, as we are showing in the reference design for the Combustion Engineering follow-on study, but it may be impossible to have these of entirely different principles. It may be desirable, but not necessary.

9. Materials selections for components (SA 302, grade B, steel) and cladding (Inconel 625, 19-9DL) should include other potential materials. A modified SA 302, grade B, steel containing 1/2 to 3/4% nickel, designated ASTM A 533, grade B, class 2, has superior materials properties and is currently being used in the fabrication of pressure vessels. As indicated in the evaluation, 19-9DL is not a logical fuel pin cladding material, since it is susceptible to stress corrosion cracking in the steam environment.

10. There is a question of the practicality of fabricating core quantities of highly prepressurized fuel pins with adequate assurance of no "leakers" or potential "leakers."

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APPENDICES

Appendix A

SUMMARY REPORT ON KARLSRUHE CONCEPT OF 1000-Mw(e) INTERMEDIATE
PRESSURE STEAM-COOLED FAST BREEDER REACTOR

At the end of 1964, the Karlsruhe group completed a relatively detailed design study of a 1000-Mw(e) sodium-cooled fast breeder reactor called Na 1 (Refs. 1 and 2*). This design was used as the basis of a subsequent system analysis³ which showed the dynamic behavior and safety features of sodium-cooled breeders, as well as the **affect** on costs and safety of a number of design variations. A second design study is currently under way that will give the basic specifications for a 300-Mw(e) prototype reactor (Na 2) to be constructed as a joint effort of the German government and industry.

Similarly, a design study of a 1000-Mw(e) steam-cooled reactor (D 1) has now been completed.⁴ This was previously described in May 1966.⁵ Important features of the design are summarized in this paper. The present D 1 design is not claimed to be the optimum of all possible steam-cooled fast reactors; however, the Karlsruhe group thought it valuable to study one reference design in detail in order to uncover the physics and engineering problems typical of steam cooling. A more detailed system analysis will be made subsequently to investigate dynamic behavior, safety problems, cost optimization, and design of a prototype reactor.

Shortly after starting the investigation of steam-cooled breeder reactors, it became apparent that many more configurations and alternatives are **possible** than for a sodium-cooled reactor. Thus, the choice of the main parameters for a reference design was quite difficult. The problem was further complicated by the fact that no steam-cooled breeder reactor had yet been built, and only a few studies of this approach had been published.⁶⁻⁹

General Features of the D 1 Concept

Starting from a review of published information, the Karlsruhe point of interest was the hope for lower capital costs relative to those of a

*These references appear at the end of this appendix.

sodium-cooled reactor, and consequently lower power costs. This goal seemed to be attainable by the use of approved components from modern conventional steam power plants, the application of the direct cycle proved in BWR's, and the possible evolution from light water reactor technology. Besides, it seemed to be of general value to have an alternative solution in the case of unforeseen difficulties with sodium.

Although the steam-cooled fast reactor may represent an improvement over light water-cooled reactors, it should be pointed out that its breeding potential is lower and its fuel costs higher than in the case of sodium cooling. This is shown in detail in the following analyses.

Breeding Ratio

The breeding ratio of a light water steam-cooled fast reactor in the range of 150- to 180-atm pressure will be smaller than that of a fast reactor with sodium cooling. This is due to two specific reasons:

1. The energy spectrum is softer, as shown in Fig. A.1. This results in somewhat smaller η values and amplifies the effect of fission product poisoning.

Fig. A.1. Neutron Flux Spectrum.

2. The temperatures of the structural material in general will be higher. This is especially true of the cladding. Therefore, more structural material or a special high temperature material must be used to withstand the loads (e.g., Inconel 625). Also, with respect to corrosion, Inconel 625 is required. The content of 9% molybdenum and 4% niobium or tantalum in this material is unfavorable for breeding.

However, if an attempt is made to compensate for the influence of the softer energy spectrum by making the coolant fraction in the core as small as possible with respect to the design and tolerance problems (e.g., 32% on the average), the total breeding ratio of a 1000-Mw(e) steam-cooled fast reactor of medium burnup is still approximately 0.1 lower than in a sodium-cooled reactor of the same burnup and a coolant fraction of 50%. For sodium cooling, breeding can even be raised by the use of carbide fuel. In that case, the difference amounts to about 0.25. It should be kept in mind that the use of other steam pressures or of heavy water would change these numbers.

Fuel Rod Power and Rating

For the sodium-cooled reactor, the fuel rod power is limited by the melting point and the thermal conductivity of the fuel material. For steam cooling, the cladding temperature and the heat transfer to the coolant are also limiting values. This results in a larger influence of the hot-channel factors for steam cooling. The deviation of a temperature difference from the design value is proportional to this difference and to the hot-channel factor. The larger temperature difference from cladding to coolant results in a higher cladding temperature at the hot spot. In addition, two hot channel factors are higher than for sodium:

1. The relative deviations in coolant channel dimensions are larger because of the smaller coolant fraction.
2. The heat transfer number depends more strongly on the coolant flow and, therefore, on the channel diameter.

The maximum cladding temperature is defined by mechanical considerations. Here, the fresh condition is important where the cladding is still under external pressure. Detailed studies of creep buckling under these conditions were made, and it was found to be the dominant effect. For

Inconel 625 and a typical structural material content in the core, a maximum temperature of approximately 760°C at the cladding inner surface and at the hot spot seems to be a limiting value. It was necessary, even for this, to assume a remarkable internal inert gas pressure.

Figure A.2 shows the life of a cylindrical tube versus temperature for different external pressures. The strong dependence on temperature is evident. The results of a number of thermodynamic calculations* based on the hot-spot temperature of 760°C on the cladding inner surface are given in Table A.1. In these calculations the following parameters were assumed:

Coolant pressure, atm	170
Coolant temperature, °C	
Inlet	365
Outlet	540
Pressure drop, atm	6.6
Power shape factors	
γ radial	0.86
γ axial	0.74

Accordingly, the plant efficiency was constant. Cores 1 to 3 (Table A.1) show the maximum rod power for different rod diameters, and the attainable rating for 1000 Mw(e), a coolant fraction of 32%, and a core diameter of 260 cm.

The rod power increases linearly with the diameter because the heat flux stays nearly constant. By contrast, the rating decreases because a larger critical mass is required with a larger core volume. These rod powers and ratings are relatively low compared with those of a sodium-cooled reactor. However, these figures could be raised to a certain extent by the choice of a smaller value for the outlet temperature, but this would decrease efficiency. A system analysis would be required to determine whether the power generation costs could be lowered by decreasing the outlet temperature.

*The relations for heat transfer and pressure drop are based on measurements of R. Moeller, Institute for Reactor Components, Nuclear Research Center, Karlsruhe, Germany.

Fig. A.2. Time to Collapse Versus Temperature for Cylindrical Tubes and Various External Overpressures.

Table A.1. Thermodynamic Data

	Core 1	Core 2	Core 3	Core 4	Core 5
Fuel rod diameter, mm	6.4	7	8	7	7
Core height, cm	132	144	167	75	75
Core diameter, cm	260	260	260	320	247
Reactor power, Mw(e)	1000	1000	1000	1000	600
Maximum rod power,* w/cm	403	443	497	483	483
Average power rating, Mw/kg Pu	0.78	0.73	0.63	0.77	0.74
Pressure drop, atm	6.6	6.6	6.7	6.0	6.0
Coolant fraction, α , %	32	32	32	22	22

*Hot channel.

Reactivity Coefficient of Coolant Density

The dependence of k_{eff} on the coolant density for different values of burnup and a typical cylindrical 1000-Mw(e) core is shown in Fig. A.3. Owing to the affect of fission products, the power coefficient of coolant

Fig. A.3. K_{eff} Versus Coolant Density for Various Burnup Values.

density becomes more and more positive. This is of small importance for reactivity disturbances because of the very large negative Doppler coefficient of steam-cooled reactors. Nevertheless, the power coefficient stays relatively constant over a longer period of operation because, practically, only the shim absorbers will be replaced by fission products.

The total loss of coolant will result in a relatively high positive reactivity. Under the assumption that even for large leakage the total loss of coolant will require some time (seconds), the reactivity ramp produced will amount to less than \$10/sec. Therefore, the total energy production during an excursion of this type will not be excessive.

Economy

The lower breeding ratio of the light water steam-cooled reactor gives a larger doubling time. Therefore, the sodium-cooled system leads to a better strategy in optimum use of fuel reserves.

Table A.2 gives a preliminary comparison of the power generation costs. These figures should be interpreted as a trend that must be proved by later detailed evaluations. The costs for the light water

Table A.2. Comparison of Power Costs

	Costs (mills/kwhr)		
	LWR	D 1	Na 1
Fuel cycle	1.9	1.2	0.875
Capital	2.56	2.90	3.13
Operation	0.24	0.24	0.24
Total	4.7	4.34	4.25

reactor (LWR) are based on evaluations of German industry. In comparison, the steam-cooled D 1 has higher capital costs due to some additional circuit components and the higher coolant pressure. On the other hand, the turbine will be less expensive. By contrast, the fuel cycle costs are definitely lower for the fast reactor, and the savings result in more attractive total power generation costs. Even a certain increase in fuel cycle costs would be tolerable so that, for example, the burnup could be reduced from 100,000 Mwd/t to some 55,000 Mwd/t. This would improve the breeding ratio and the coolant density coefficient, and simplify the design of the fuel elements to a certain extent. It is a fact, however, that the very attractive fuel costs of the sodium-cooled breeder reactor could not be reached with a steam-cooled system.

As shown in Table A.1, the power rating increases with a decrease in rod diameter. Some improvements of the fuel cycle costs for the steam-cooled reactor seem possible in this way if the difficulties combined with small rod diameters and a close lattice can be solved, and the fabrication costs of the fuel elements do not increase significantly.

All considerations for cores 1 to 3 are strongly influenced by the core diameter and the coolant fraction in the core. If these limitations could be neglected, a more favorable rating would also be possible with a smaller core height, as shown in core 4. In this example, also, the pressure drop is some 10% lower and, correspondingly, the efficiency is improved. Unfortunately, the diameter of core 4 is very large. In order to avoid difficulty with the pressure vessel, almost no radial blanket

could be used. Therefore, the power output must be decreased in this case to perhaps 600 Mw(e), as shown by core 5.

The coolant fractions of cores 4 and 5 are extremely small and lead to tolerance problems. An improvement would be possible perhaps by using turbulence promoters on the cladding surface, because, in general, this requires a larger channel diameter. However, this would again increase the core volume.

Detailed optimization of breeding, rating, reactor costs, and efficiency will require a system analysis. Preliminary results show, however, that light water steam cooling becomes somewhat more favorable for smaller power stations. This tendency seems to be true for the fuel costs, as well as for the capital costs, compared with those of sodium-cooled systems. At present, the power level that will give a break-even point for the power costs cannot be stated. Also, another break-even point is expected in capital costs, since a decrease of specific capital costs is a bit more pronounced for sodium cooling than for steam cooling because of the low pressure. Anyway, this information could be drawn only from optimized detailed designs for both coolants, based on the same safety requirements and on the same technological development standards. Moreover, the construction and operation of both systems under similar conditions would probably be required.

It is still believed that steam cooling may be attractive and may find a good market. Hence, neither system will exclude the other since they appear to be supplementary in important features.

Reference Design D 1 - Basic Decisions

The characteristics of the reference design are listed in Table A.3. The values given were based on the foregoing analyses and other considerations discussed briefly in the following paragraphs.

Fuel and Fuel Element

A UO_2 - PuO_2 mixture was chosen as the fuel, and an external pin diameter of 7 mm was selected as a compromise between high rating and good mechanical stability. The cladding material was Inconel 625 because

Table A.3. Characteristics of the Reference Design D 1

Overall plant

Thermal power, Mw	2519
Net electrical power, Mw	1000
Overall efficiency, %	39.7
Fuel	Mixed UO_2 - PuO_2
Fertile material	UO_2
Total breeding ratio	1.15
Doubling time, years	37
Coolant	H_2O steam
Steam pressure at turbine inlet, atm	160
Steam temperature at turbine inlet, °C	536

Reactor

Core form	Cylinder
Core diameter, m	3.389
Core height, m	2.485
Fuel region diameter, m	2.628
Fuel region height, m	1.510
Blanket axial/radial thickness, cm	35/38
Number of radial zones	3
Number of fuel cells	181
Number of blanket cells	120
Cell can configuration	Hexagonal
Cell can wall thickness, mm	3

	Zone 1	Zone 2	Zone 3
Outer diameter, cm	183.2	262.8	338.9
Number of fuel cells	79	84	
Number of blanket cells			120
Number of control cells	9	9	
Coolant fraction, fuel and upper blanket, %	32.0	32	
Coolant fraction, lower blanket, %	40.9	40.8	
Coolant fraction, radial blanket, %			24.1
Structure fraction, fuel and upper blanket, %	7.9	7.9	
Structure fraction, lower blanket, %	7.6	7.6	
Structure fraction, radial blanket, %			18.3

Heat removal data

Thermal output of fuel region, Mw	2343
Thermal output of axial blanket, Mw	73
Thermal output of radial blanket, Mw	101
Coolant inlet temperature, °C	375
Coolant flow rate, tons/hr	11,394

Table A.3 (continued)

Heat removal data (continued)

Maximum coolant outlet temperature (hot channel), °C	648	
Average coolant outlet temperature, °C	540	
Maximum cladding temperature (hot channel), °C	754	
Maximum fuel temperature (hot channel), °C	2170	
Maximum nominal fuel temperature, °C	1832	
Maximum coolant velocity, m/sec	55	
Pressure drop across core, atm	9.27	
Cell width, cm	18.6	
Total length of fuel element, m	3.160	
Weight of fuel material, kg	~200,000	
Weight of blanket material, kg	~275,000	
Fuel cladding material	Inconel 625	
Blanket cladding material	Incoloy 800	
Fuel rod diameter, mm	7	
Blanket rod diameter, mm	12.5	
Fuel cladding thickness, mm	0.37	
Blanket rod cladding thickness, mm	0.55	
Number of fuel rods per fuel element	469	
Number of blanket rods per fuel element	133	
Number of blanket rods per radial blanket element	169	
Height of void above fuel, mm	250	
Maximum fission-gas pressure in fuel, atm	260	
Coolant fraction in core, %	32.2	
	<u>Zone 1</u>	<u>Zone 2</u>
Maximum nominal rod power, w/cm	326	326
Maximum power density, Mw/liter	0.462	0.464
Average power density, Mw/liter	0.321	0.258
Breeding ratio	0.536	0.351
Fuel composition, %		
²³⁹ Pu	9.73	12.03
²⁴⁰ Pu	2.99	3.69
²⁴¹ Pu	0.30	0.37
²⁴² Pu	0.13	0.16
²³⁸ U	86.85	83.75
Fissile Pu in Pu + U, %	10.03	12.40
Fuel loading, kg		
Plutonium	1661	2050
Uranium	14,827	14,401

Table A.3 (continued)

Heat removal data (continued)

	Axial Blanket	Radial Blanket
Breeding ratio	0.084	0.179
Fuel composition, %		
^{235}U	0.39	0.39
^{239}Pu	1.85	1.85
^{238}U	97.76	97.76
Fuel loading, kg		
Plutonium	299	695
Uranium	15,677	36,556

of its good high temperature strength and corrosion behavior. Spiral fins that are integral parts of the cladding were used as fuel rod spacers. To prevent creep deformation, the rods were filled with noble gas at a pressure of 120 atm. Fission gases were stored in a plenum, since venting to the coolant must be excluded for steam-cooled reactors, especially those with a direct cycle steam system.

Based on the foregoing considerations, mainly the creep deformation problems, the maximum rod power was set at approximately 415 w/cm. With this figure, a power rating of approximately 0.73 Mw/kg of fissionable material will be reached.

Shape of Reactor Core

For the core, a cylindrical shape with two enrichment zones of equal volume was chosen. In order to have a reasonable pressure vessel size, the core outer diameter was set at 260 cm. This led to a height-to-diameter ratio of nearly 0.6.

Coolant and Coolant Pressure

Light water steam was used as the coolant. Heavy water or a mixture of heavy and light water was excluded because the better breeding ratio does not appear to compensate for the greater difficulties during re-fueling and the increased tritium problems. An average coolant pressure

of 170 atm and a suitable steam outlet temperature of 540°C were chosen. These conditions give a relatively good heat transfer and overall efficiency, and allow the use of a conventional modern steam turbine. Besides, extrapolation to higher and lower pressures during system analysis will be possible.

Direction of Flow in the Core

The cooling steam flowed in an upward direction through the radial blanket and inner thermal shield, and then downward through the core. In this way, any residual moisture will be evaporated before reaching the core, and no special hold-down device for the fuel subassembly is required.

Control Rods

The control rods were on subassembly positions. This allowed for a smaller number of rods, and uniform subassembly cross sections for the whole core. The control rod drives were mounted on top of the vessel head, and were connected to a grid plate above the core to prevent a control rod blowout in case of a nozzle break. Therefore, the driving shafts did not disturb the lower outlet plenum for the hot steam, and the drives did not occupy the space below the pressure vessel. This allows a simple and novel arrangement of the reactor internals and the coolant pipe connections.

Refueling System

Refueling would be done under water. This allows for quick operation, gives high performance, and is one of the important advantages of the light water reactors that should be retained. For this purpose, the design will make provisions for easy flooding of the reactor. This flooding possibility is also an advantage with respect to decay heat removal after some accidents.

Coolant System and Power Plant

The reactor coolant system used the Loeffler cycle, with spray-type steam generators. The plant would operate in direct cycle. This allows for lower capital costs. With respect to component size and safety

considerations, six independent Loeffler circuits **were** used. For decay heat removal and some other duties, two subsidiary coolant circuits with surface-type steam generators **were** provided. To further reduce capital costs and to provide maximum access to the plant during operation and after an accident, only those components **would** be installed in the containment vessel that are necessary to fulfill the safety requirements.

Containment

The containment system was designed in agreement with the following design criteria: all vessels, containers, or rooms that could contain fission products or plutonium aerosols above atmospheric pressures must be enclosed by a second gastight envelope, and protected by an inner blast shield against any damage. To lower the maximum containment pressure, a pressure suppression system was used.

Reference Design D 1 - Outline Description

A simplified flow diagram and the containment system are shown in Fig. A.4. Six main Loeffler circuits equipped with spray-type steam generators and two smaller subsidiary cooling loops with surface-type steam generators **were** connected to the reactor. Two relatively small standard Loeffler boilers **were** also interconnected to these loops. In addition, two steam lines lead from the reactor to the two main turbine-generators feeding in the working steam at 170-atm pressure and 540°C. After expansion in the high pressure cylinders of the turbines, this steam **was** reheated in surface-type reheaters, which are part of the main Loeffler circuits. With this system, 1000-Mw(e) power **was** produced from 2517-Mw heat generated in the reactor. The overall efficiency amounted to 39.7%.

During reactor operation, the two subsidiary loops **were** in continuous operation. The nonradioactive secondary steam generated in these systems **was** used to feed the driving turbines of the steam blowers and other auxiliary equipment of the plant. Moreover, it **was** used as a nonradioactive steam barrier in the labyrinth-type seals of the machines run on radioactive steam.

Fig. A.4. Simplified Flow Diagram and Containment System for Reference Design D 1.

The two Loeffler boilers generated only a small quantity of steam during normal operation; their main purpose was to act as a buffer and storage in case of power transients, equipment failures, or leakage. Also, the volume of water in the two Loeffler boilers, which were located at a higher level than the reactor, was used to flood the reactor for the refueling procedure. After shutdown of the reactor and an initial steam-cooling period, this process of flooding had to be carried out at a relatively high pressure because of the decay heat generation. The flooded reactor was then cooled by the use of a pressurized water system, which is not shown on Fig. A.4. Simultaneously, the pressure and temperature were reduced to the conditions of the refueling process in conformity with the temperature changes admissible for the reactor components. At the same time, it was possible to connect a primary water purification and degasification system. The whole depressurization and flooding process is shown graphically in Fig. A.5. Draining the flooded reactor followed roughly the same sequence, but in opposite direction. The rapid increase in reactivity (see Fig. A.3) during this process required some safety precautions.

Fig. A.5. Sequence During Depressurization and Flooding Process.

The layout of the plant components and the protective containment walls was dictated by safety considerations. The core **was** enclosed in a heavy steel pressure vessel; the neutron and gamma shield in the vessel **acted as** a first blast shield. The pressure vessel **was** located in a reinforced concrete reactor cell designed to withstand the pressure that would develop in the event of the maximum credible accident. The walls of this cell **also acted as** biological shielding. The upper part of this cell **was** filled with water that **constituted** a refueling pool. Final protection in this sense **was** provided by a heavy movable cover bridge.

All coolant pipes, with their valves, the pressurized water cooling system, and the purification circuits, as well as the equipment of the two subsidiary coolant loops, including the two Loeffler boilers, **were** located below and around the reactor cell in a dry well formed by concrete walls. Any increase in pressure in this volume caused by a reactor accident or any leakage would be dissipated through ventpipes into the refueling pool and, ultimately, to the pressure resistant first containment vessel. This first container, the six main Loeffler circuits, and the two main turbine-generators **were** enclosed in a second gastight envelope. Here, a small negative pressure **was** maintained. It **could be** expected that this system **would** control all credible accident conditions, as well as handle and discharge, in a controlled manner, any potential release of radioactivity.

Under shutdown conditions, all penetrations between the first and the second containment vessels **that might contain** radioactivity **were** closed. For this purpose, one quick acting check or overload stop valve (adjacent to the reactor pressure vessel) and two independent normal stop valves **were installed** in each pipe line. In this case, the decay heat **was** removed by the two subsidiary coolant loops or, under flooded conditions, by the water cooling system. Also the penetrations **remained closed during** the startup procedure until the reactor power **exceeded** about 10%. The plant **operated** during that period as a dual cycle system with the two auxiliary turbines and their condensers.

Full power production **occured** in a direct cycle similar to that of the BWR's. Nearly 10,000 tons/hr of saturated steam **was** generated at maximum load in the six spray-type steam generators and pumped to the reactor.

Here this steam was superheated to 540°C . The steam flow coming from the reactor was then divided into three paths: approximately 3000 tons/hr going to the two main turbines, approximately 7000 tons/hr going to the six Loeffler circuits, and the rest going to the two subsidiary systems.

Any sudden positive power step by the two main turbine-generators of an order permissible in modern conventional power stations would not result in an unacceptable decrease in steam pressure or in an undue increase in power. The water reservoir in the bottom sections of the main steam generators, where the water separated out of the saturated steam was collected, had a stabilizing effect in this case. It kept the steam pressure nearly constant at this point of the circuit so that any pressure change at the reactor outlet would strongly influence the relatively small pressure difference to this point. A decrease in this pressure difference, however, would also decrease the amount of steam supplied to the steam generators. Consequently, more steam would be available for the main turbine-generators. Additional steam required during this transient would be generated by the water reservoir of the steam generators. A sudden decrease in power could easily be controlled by a bypass system to the main turbines.

A preliminary dynamic analysis was made for several disturbances in the coolant circuit. In general, the Doppler effect will stabilize the conditions after some overswing. As an example, Fig. A.6 shows the reactor power as function of time for various burnups after a relatively large, sudden coolant density decrease of 0.006 g/cm^3 . This value corresponds nearly to a pressure decrease of 10 atm or a temperature increase of 25°C . The steam-cooled fast breeder reactor would probably not give extremely difficult control problems. However, it is possible that a more detailed analysis of the dynamic behavior of the plant will show that the water volume in the main steam generators is not a necessary prerequisite. In that case, it would be possible also to use cheaper types of steam generators, perhaps those in which the steam produced is still slightly superheated. Moreover, operation with supercritical steam pressure would be possible.

In a similar way, the two Loeffler boilers stabilized the pressure on the reactor inlet side. This is important if a leak develops at this

Fig. A.6. Power Versus Time for Different Burnup Rates After a Sudden Coolant Density Decrease of 0.006 g/cm^3 .

section of the cooling system. The Loeffler boilers then prevent an unduly rapid decrease of the steam flow passing the core, and of the pressure in the cooling system, so that special safety measures can be taken. Leaks located in the cooling systems may be isolated from the rest of the reactor system by closing the proper valves. Leaks of the reactor vessel, however, could not be isolated; they would ultimately lead to a depressurized reactor. In this case, a water spray system would go into operation by which the coolant steam flowing into the core could be moisturized to increase its cooling efficiency substantially and avoid meltdown of the core. In addition, emergency flooding of the reactor would be carried out as early as possible. If the leak was below core level, additional water would be fed into the cooling system, and the whole dry well would be flooded with cooling water from the main condensers until the core was below water level. This latter method of flooding would also be carried out after a maximum credible accident. The flooding would provide for dissipating the decay heat for unlimited periods of time, even from a completely destroyed reactor.

Another abnormal operating condition or accident to be considered is an increase in radioactivity in the turbine steam as a result of fuel element leakage, cladding failure, or fuel damage. If the increase is relatively slow, the defective subassembly can be identified by a fission product detection system that is in continuous operation. When a certain activity level is reached, operational course and power level of the plant will then be dictated by the dose rate measured at the turbines and the off-gas lines until the defective subassembly is replaced.

Any heavy and sudden increase in radioactivity, however, would require immediate safety measures. The reactor would be scrammed, and all penetrations in the first containment vessel that carried radioactive steam would be closed. The check valves and the overload stop valves are, therefore, designed for a short closing time. However, operation of these valves alone would not be sufficient to guarantee complete leak-tightness. Therefore, the two independent normal stop valves in each duct would also be closed. A system analysis may show, perhaps, that a controlled fast power setback would also be adequate in this case. This would avoid excessive thermal stresses in the cooling circuit.

During and following the transient from "open cycle power production" to the "closed cycle shutdown condition", the two subsidiary cooling circuits would not be affected. Hence, no additional steps would be necessary to protect the systems for removal of the decay heat. The behavior of the cooling system during the transient is shown qualitatively on Fig. A.7. The remarkable mass flow increase in the subsidiary coolant loops as a result of the decrease in reactor pressure drop is interesting. Only one subsidiary loop with one steam blower operating was adequate for decay heat removal as long as the steam pressure remained close to the operating level. In this connection, it must be mentioned that the four steam circulators within the first containment vessel (each loop had two blowers) on which the decay heat removal essentially depended were of a particularly simple design, so a high degree of reliability might be expected. All other important equipment for the decay heat removal system (e.g., feedwater pumps, condensers, etc.) was located outside the first containment vessel and was not radioactive, so it remained accessible in any case. Leakages from the radioactive reactor cooling circuit to the

Fig. A.7. Transition from "Open Cycle Power Production" to "Closed Cycle Shutdown Condition."

nonradioactive secondary system were prevented because the secondary system pressure was greater than the reactor coolant pressure by about 20 atm. A conventionally fired startup boiler was also available as a standby energy surge to drive the steam blowers.

Figure A.8 shows a longitudinal section through the reactor. The cylindrical core and blanket arrangement consisted of 163 fuel subassemblies, 18 control subassemblies, and 120 blanket subassemblies of identical hexagonal cross section. The central core region contained the fuel in two enrichment zones; the outer region contained the fertile material. In each fuel subassembly, 469 fuel elements were installed. The whole core structure was supported by a bottom grid plate, and held together by an external shroud. The cooling steam entered the reactor vessel from below through the annular channel of the big concentric stud and flowed from the bottom to the top through the radial blanket and the inner radiation shielding established between the reactor core and the pressure vessel wall. In this way it reached the upper plenum chamber and flowed down again into the core of the reactor. When it had been heated, it left

Fig. A.8. The Reactor.

the reactor pressure vessel through the inner channel of the concentric stud at a temperature of 540°C. A surge pipe **was** attached to the top of the pressure vessel that led to the steam volume of the Loeffler boilers. Normally, the inner opening **was** closed by a special device; only during the flooding process it **was** opened. Through a central hole in this device, the water **was** delivered to the water sprinkler.

A remarkable feature shown in this drawing is the high expenditure for inner radiation shields necessary for a steam-cooled fast reactor in order to keep exposure of the walls of the pressure vessel low. In this design, the only shielding material used was steel. The thickness was selected to keep the exposure of the wall of the vessel over its 30-year life from exceeding approximately 2×10^{19} neutrons/cm², which is a permissible value for ferritic material. Moreover, this shielding **kept** the activation of the top flange low so that **work could be performed** on it with the proper safety precautions.

The pressure vessel **had** relatively large dimensions. The inner diameter **was** 4.8 m, the height **was** about 13 m, and the wall thickness of the cylindrical portion built in multilayer construction **was** about 300 mm. The bulk weight of the vessel without internals **would be** some 540 tons. It **would be** necessary to weld the vessel on the site to combine several prefabricated sections. However, there are no fundamental obstacles in the way of this type of construction.

A longitudinal section through the reactor building is shown in Fig. A.9. The reactor pressure vessel **extended** into the dry well and forms a spherical header in this part. In this arrangement, the stud connections of the pipes and the penetration of the hot steam line through the pressure vessel wall, which **was** maintained at lower temperatures, **remained accessible**. Moreover, it **was possible to arrange** the fast acting valves near the pressure vessel and install the pipes within the reactor building in a sufficiently flexible way to prevent damage by an excursion of the reactor.

For the refueling process, the pressure vessel head **was** lifted off under water and laterally disposed in the refueling pool. The transport **was carried out** by means of a special, movable, heavy lifting bridge. Refueling **was** carried out in direct view by a second, lighter, movable

Fig. A.9. Reactor Building.

bridge that **carried** all necessary equipment. The spent fuel elements **were discharged** into a fuel element storage bay outside the containment vessel through a shielded, inclined transfer tube. The first containment vessel **was** surrounded by an additional gastight concrete shield, which **was** the second containment vessel.

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Appendix B

GROUND RULES AND COST BASES

1. General Considerations1.1 Definition of Criteria for Evaluating Technical Feasibility

The technical evaluation of the alternate coolant fast breeder reactors covers five general areas: (1) physics, (2) heat transfer, (3) materials, (4) systems and components, and (5) safety. Calculations were made by ORNL personnel in these areas for verifying the designers' results, and, also, for providing an evaluation of all concepts on a comparable basis.

The heat transfer calculations consisted of a check of the surface and center maximum temperatures and the total heat removal based on the core configuration provided by the designer, and on the power distributions determined by the ORNL physics analysis. These temperatures were then used by the materials engineers in evaluating the ability of the materials to serve at the indicated temperatures. The pressure drops and pumping power required were also calculated, as were the emergency cooling capability and the general heat balance of each system.

The physics evaluation included calculation of neutron balance, breeding ratio, and plutonium yield, all based on core composition as given by the designer, and initial loading with plutonium from PWR discharge fuel. Power distributions were calculated on the basis of the specified fuel management program provided by the designer. Coefficients of reactivity due to fuel Doppler effect, coolant void, and flooding effects and overall coolant density were calculated. Based on initial fuel and blanket compositions and the proposed fuel and blanket management programs, complete histories of the mass balances throughout the life of the reactor were calculated. The analysis also included a determination of a fuel cycle cost including fuel fabrication, shipping, and reprocessing costs.

The design characteristics of some of the principal components of the reactor primary system were calculated, and their requirements were

compared with current technology of corresponding equipment for which some experience is available. Particular attention was paid to equipment when new requirements were involved, such as those for the steam or gas circulators. The reactor vessels and their internals were analyzed and compared with code requirements for the specified design and operating conditions. Refueling and fuel handling equipment were reviewed for adequacy and for consistency with the requirements of the other reactor systems with which comparisons were made.

1.2 Considerations in Safety Review

The safety evaluation consisted of a review of several types of accidents that might occur, their **affects** on the integrity of the reactor, and the resulting potential hazards that might develop. The accidents considered included: (1) reactivity accidents, (2) thermal accidents, (3) **plant startup and control accidents**, and (4) **nuclear destructive excursions**, and there was some discussion of containment of core meltdown. For all these conditions, safety features were examined to determine whether they could successfully cope with the postulated accidents.

There were no firm ground rules established for the design of the safety features, and the AEC General Design Criteria changed during the course of this study. Also, the concept sponsors used considerably different approaches to the design of safety features and emergency equipment. ORNL attempted to normalize the emergency equipment to a reasonable degree of reliability, but it was not normalized to give the various types of **reactors** "equivalent" degrees of safety for a similar siting situation. Such a normalization would be reflected in the capital costs of the different reactors; however, this would be difficult to do on a consistent basis because of the conceptual nature of these designs, and the lack of safety feature analyses and licensing experience with these advanced designs.

1.3 Timing (Date of Introduction)

The time period under consideration in the studies was from 1970 to 2020. For each reactor design, an estimate was made as to the year in which a prototype reactor would be in operation, and the first 1000-Mw(e)

commercial reactor would be started up. This would be followed by the introduction of four such reactors two years later, which would set the date of introduction of "commercially available" plants. The schedule for these reactors was based on the assumption of successful completion of the proposed development programs. This criterion implies that although engineering development may be required to make the design practicable, a technological breakthrough is not needed.

1.4. Ground Rules on Trends

Capital costs were estimated for each reactor plant as a part of the economic evaluation based on costs as of June 1967, with no allowance for escalation.

1.5 Plant Size

The electrical capacity of each reactor plant was nominally sized at 1000 Mw(e) (net); however, the fuel fabrication and processing plants were sized to handle fuel from about 15 reactors of each type.

2. Financing Conventions

The financing conventions used to estimate total power costs are summarized in Table B.1.

3. Capital Cost Bases

The bases for the capital cost estimates are listed below:

1. The equipment and systems of each plant were assumed to have been completely developed, and each plant was one of a number of the same type to be built. No discount credit for quantity orders of equipment was assumed.

2. Indirect costs were based on the breakdown used in Ref. 15, and amounted to about 41.8% of direct costs.

3. The plants were located at the AEC hypothetical Middletown site described in Ref. 15, and river water cooling was assumed. No thermal pollution problem was considered.

Table B.1. Financing Conventions for Alternate-Coolant
Breeder Reactor Evaluation

Type of ownership: private investor
Plant capacity factor: 0.80
Salvage value of plant: zero

	Fixed Charge Rates (% per year)		
	Depreciating Investment	Land and O & M Working Capital	Fuel-Cycle Working Capital
Return on investment ^a	7.20	7.20	7.20
Depreciation (30-year sinking fund)	1.02		
Interim replacements	0.35		
Federal income taxes ^b	2.04	4.80	4.80
State and local taxes	2.84	0.80	0.80
Property insurance	0.25		0.40
Total	13.7	12.8	13.2

^aBased on 52% bonds at 4.615% per year and 48% equity at 10% per year.

^b50% of gross taxable income.

4. A four-year construction schedule with a 40-hr work week and no overtime was assumed.

5. Direct costs were based on June 1967 U.S. list prices, with no allowance for escalation.

6. Turbine-generator costs were based on U.S. manufacturers' June 1967 list prices, with a 7% discount factor applied.

7. Reported capital costs do not include costs of transformer and switchyard, and do not allow for a state sales tax.

8. Land costs were assumed to be \$360,000 for all concepts.

9. Interest during construction was based on a 6% annual cost of money.

10. Differences in net electrical ratings of various concepts (970 to 1000 Mw) were taken into account by dividing the estimated total construction cost by the quantity $10^6(\text{rating}/1000)^{0.75}$.

4. Value of Materials

The unit costs of the materials specified are summarized below:

<u>Item</u>	<u>Unit Cost</u>
1. Natural uranium as U_3O_8	\$8.00/lb
2. Unirradiated thorium as ThO_2	\$5.00/lb
3. Separative work cost	\$26/kg
4. Diffusion plant tails concentration	0.002 weight fraction
5. Fully enriched ^{235}U	\$11.18/g
6. Depleted uranium	\$3/kg
7. Fissile plutonium	Five-sixths of value of fully enriched ^{235}U ; i.e., \$9.32/g

5. Fuel Cycle Cost Bases

The bases for the fuel cycle cost estimates are given below:

1. The reactors were assumed to be loaded initially with plutonium or enriched uranium and refuelled with discharged plutonium until equilibrium was reached. Excess plutonium was assumed to be sold.

2. "Levelized" fuel cycle costs were based on the present worth of all expenditures and receipts during a reactor lifetime of 30 years, with a discount factor of 6%. The effective average lifetime load factor was assumed to be 0.8, and constant over plant life. Unit fuel costs were also assumed to be constant based on a 15,000-Mw(e) industry.

3. Private ownership of fissile and fertile materials during fabrication and processing, as well as on-site at the reactor plant, was assumed to be vested in the reactor plant owner.

4. Annual charges on fissile and fertile inventories and on fabrication costs of fuel elements were computed at 13.2%.

5. The reprocessing plant and fabrication plants were assumed to be located at the same site, and to be 1000 miles from the reactor plant. These plants were assumed to be designed to process a single type of fuel element, and to be capable of serving an electrical industry of 15,000- or 30,000-Mw(e) capacity of the concept being considered. A

reference value of 22% per year was used as the fixed charge rate on these plants.

6. Losses during fabrication were assumed to be 0.2% per pass, and losses during processing were assumed to be 1% per pass.

7. Based on the spent fuel being shipped to a plant located at a distance of 1000 miles from the reactor site, it was assumed that: (a) the maximum allowable cask weight was 120 tons fully loaded; (b) shipments of spent fuel would be made by rail, and all sites would have railroad sidings and facilities for handling 120-ton casks; (c) rail freight rates would be \$0.0204/lb for a full cask and \$0.0187/lb for an empty cask; (d) round-trip time would be 20 days; (e) casks would be purchased at a cost of \$1.25 per pound of cask weight, and fixed charges on casks would be 15% per year, including recovery of investment, return on investment, taxes, and maintenance; (f) shipments would be designed to comply with 10 CFR 71 and with ICC Order 70, and the cask would have the exclusive use of the vehicle; (g) casks could be shared among reactors of the same type **and**, when casks were shared, a maximum limit of 0.8 would be placed on the cask utilization factor (time in use divided by total time); (h) the cost of property insurance against damage to or loss of cask and contents would be 0.0005 times the value of the shipment; (i) it would not be necessary for a courier to accompany the shipment; (j) individual fuel assemblies would be canned before shipment; and (k) fuel assemblies would not be disassembled before shipping.

8. Holdup times were established separately for each concept, depending on the characteristics of the fuel concerned.

6. Operation and Maintenance Cost Bases

Estimates of operation and maintenance costs were based on methods described in the NUS economic evaluation guide (Ref. 95). Items in the operation and maintenance category **were** plant staffing, consumable supplies and equipment, outside support services, administrative and general expenses, coolant makeup, and nuclear liability insurance. Plant staffing costs **consisted of the** direct payroll plus 10% fringe benefits. A reference staff of 78 people was assumed for a 1000-Mw(e) BWR or PWR single-unit

station and ~~was~~ based on five-shift operation. Five more people were assumed for the GCFR designs because of coolant handling requirements. Consumable supplies and equipment costs were based on the estimates for BWR and PWR plants, with adjustments made for evaluated differences in requirements for other concepts. The annual cost of outside support services was assumed to be \$130,000 for all concepts. Administrative and general support functions were supplied through the utilities central office, and the cost of these functions was estimated at 14% of the sum of the costs for plant staffing, consumables, and outside support services. The initial charges for coolant and moderator inventory were included in capital cost charges; the operation and maintenance component of this cost **covered** the cost of makeup quantities of these fluids. Nuclear liability insurance cost **included** commercial coverage at an estimated cost of \$240,000 per year for a 1000-Mw(e) station, and government coverage to a total of \$560 million at an annual premium of \$30 per thermal megawatt of reactor rating.

Appendix C
PHYSICS TABLES

Table C.1. Reactor Refueling Schedule for Core Region of High-Pressure System

	Cycle 1 Loaded					Cycle 2 Loaded				
	Zone 1	Zone 2	Zone 3	Zone 4	Total	Zone 1	Zone 2	Zone 3	Zone 4	Total
Time, fpy ^a	0.0					1.33				
Nuclide, kg										
²³⁵ U	8.21	8.56	8.74	9.10	34.61	8.21	8.56	8.74	9.10	34.61
²³⁸ U	2,727.74	2,845.67	2,904.52	3,022.44	11,500.37	2,727.74	2,845.67	2,904.52	3,022.44	11,500.37
²³⁹ Pu	524.79	454.81	419.84	349.65	1,749.09	506.32	438.80	405.06	337.34	1,687.52
²⁴⁰ Pu	222.36	192.71	177.88	148.24	741.19	214.53	185.93	171.62	143.02	715.10
²⁴¹ Pu	139.83	121.19	111.87	93.23	466.12	134.90	116.93	107.93	89.94	449.70
²⁴² Pu	49.64	43.02	39.71	33.09	165.46	47.89	41.50	38.31	31.92	159.62
Total	3,672.57	3,665.96	3,662.56	3,655.75	14,656.84	3,639.59	3,637.39	3,636.18	3,633.76	14,546.92
	Cycle 1 Discharged					Cycle 2 Discharged				
	Zone 1	Zone 2	Zone 3	Zone 4	Total	Zone 1	Zone 2	Zone 3	Zone 4	Total
Time, fpy	1.33					2.588				
Nuclide, kg										
²³⁵ U	4.59	4.01	4.84	6.79	20.23	4.76	4.20	5.01	6.92	20.88
²³⁸ U	2,564.85	2,627.24	2,731.96	2,936.64	10,860.69	2,575.86	2,641.55	2,743.58	2,942.80	10,903.79
²³⁹ Pu	411.73	357.56	355.57	330.37	1,455.23	405.48	353.01	349.26	321.49	1,429.24
²⁴⁰ Pu	229.96	201.75	187.73	155.40	744.84	222.70	195.62	181.59	149.86	749.78
²⁴¹ Pu	93.46	75.49	75.24	73.10	317.29	92.24	74.68	74.20	71.54	312.66
²⁴² Pu	47.99	40.73	38.47	32.95	160.14	46.53	39.59	37.30	31.83	155.26
Total	3,352.57	3,306.78	3,393.81	3,535.25	13,588.42	3,347.57	3,308.65	3,390.94	3,524.44	13,571.61

^aFull-power years.

Table C.1 (continued)

	Cycle 6 Loaded					Cycle 7 ^b Loaded				
	Zone 1	Zone 2	Zone 3	Zone 4	Total	Zone 1	Zone 2	Zone 3	Zone 4	Total
Time, fpy	6.629					7.865				
Nuclide, kg										
²³⁵ U	8.08	8.45	8.64	9.01	34.18	7.85	8.25	8.46	8.86	33.42
²³⁸ U	2,685.63	2,809.15	2,870.92	2,994.38	11,360.08	2,609.59	2,743.34	2,810.20	2,943.61	11,106.74
²³⁹ Pu	575.15	498.44	460.11	383.42	1,917.12	566.16	490.69	452.91	377.44	1,887.20
²⁴⁰ Pu	228.96	198.44	183.17	152.65	763.21	295.53	256.13	236.42	197.02	985.11
²⁴¹ Pu	75.83	65.72	60.67	50.56	252.78	87.88	76.17	70.31	58.59	292.94
²⁴² Pu	36.36	31.51	29.09	24.24	121.21	46.57	40.36	37.26	31.05	155.24
Total	3,610.01	3,611.71	3,612.60	3,614.26	14,448.58	3,613.58	3,614.94	3,615.56	3,616.57	14,460.65
	Cycle 6 Discharged					Cycle 7 Discharged				
	Zone 1	Zone 2	Zone 3	Zone 4	Total	Zone 1	Zone 2	Zone 3	Zone 4	Total
Time, fpy	7.865					9.161				
Nuclide, kg										
²³⁵ U	4.70	4.16	4.97	6.87	20.70	4.42	3.89	4.71	6.65	19.66
²³⁸ U	2,537.67	2,609.31	2,713.43	2,916.56	10,776.96	2,455.60	2,534.79	2,645.75	2,862.11	10,498.25
²³⁹ Pu	446.38	382.85	381.43	356.73	1,567.40	432.44	371.38	371.88	350.23	1,525.94
²⁴⁰ Pu	240.58	211.41	196.04	161.22	809.25	291.04	251.65	236.64	200.32	979.64
²⁴¹ Pu	65.31	56.15	52.88	45.71	220.05	77.33	66.50	62.68	53.88	260.38
²⁴² Pu	33.92	28.85	27.18	23.43	113.38	42.51	35.98	34.07	29.65	142.21
Total	3,328.56	3,292.73	3,375.93	3,510.52	13,507.74	3,303.34	3,264.19	3,355.73	3,502.84	13,426.08

^bCycles 6 and 7 alternate for the remainder of reactor history.

Table C.2. Reactor Refueling Schedule for Blanket Regions of High-Pressure System

	Cycle 1 Loaded				Cycle 2 Loaded			
	Axial Blanket	Radial Blanket		Total	Axial Blanket	Radial Blanket		Total
		New Material	Remaining from Previous Cycle			New Material	Remaining from Previous Cycle	
Time, fpy	0.0				1.33			
Nuclide, kg								
²³⁵ U	13.56	108.66	0.0	122.22	13.56	39.50	49.26	102.32
²³⁸ U	4,562.89	36,102.34	0.0	40,665.23	4,562.89	13,124.46	22,684.82	40,372.17
²³⁹ Pu	0.0	0.0	0.0	0.0	0.0	0.0	194.51	194.51
²⁴⁰ Pu	0.0	0.0	0.0	0.0	0.0	0.0	18.57	18.57
²⁴¹ Pu	0.0	0.0	0.0	0.0	0.0	0.0	7.99	7.99
²⁴² Pu	0.0	0.0	0.0	0.0	0.0	0.0	0.56	0.56
Total	4,576.45	36,211.00	0.0	40,787.45	4,576.45	13,163.96	22,955.71	40,696.12
	Cycle 1 Discharged				Cycle 1 Discharged			
	Axial Blanket	Radial Blanket		Total	Axial Blanket	Radial Blanket		Total
		Removed	Remaining For Next Cycle			Removed	Remaining for Next Cycle	
Time, fpy	1.33				2.588			
Nuclide, kg								
²³⁵ U	8.74	20.92	49.26	78.92	8.93	60.73	0.0	69.66
²³⁸ U	4,475.72	12,676.29	22,684.82	39,836.83	4,479.72	35,101.45	0.0	39,581.17
²³⁹ Pu	51.15	266.22	194.51	511.88	48.68	584.25	0.0	632.93
²⁴⁰ Pu	5.68	28.57	18.57	52.82	5.28	65.18	0.0	70.46
²⁴¹ Pu	2.25	13.62	7.99	23.86	2.01	34.91	0.0	36.92
²⁴² Pu	0.12	0.95	0.56	1.63	0.10	3.21	0.0	3.31
Total	4,543.66	13,006.57	22,955.71	40,505.94	4,544.72	35,849.73	0.0	40,394.45

Table C.2 (continued)

	Cycle 6 Loaded				Cycle 7 ^a Loaded			
	Axial Blanket	Radial Blanket		Total	Axial Blanket	Radial Blanket		Total
		New Material	Remaining from Previous Cycle			New Material	Remaining from Previous Cycle	
Time, fpy	6.629				7.865			
Nuclide, kg								
²³⁵ U	13.56	39.50	49.56	102.62	13.56	108.66	0.0	122.22
²³⁸ U	4,562.89	13,124.46	22,691.04	40,378.39	4,562.89	36,102.34	0.0	40,665.23
²³⁹ Pu	0.0	0.0	191.01	191.01	0.0	0.0	0.0	0.0
²⁴⁰ Pu	0.0	0.0	18.14	18.14	0.0	0.0	0.0	0.0
²⁴¹ Pu	0.0	0.0	7.73	7.73	0.0	0.0	0.0	0.0
²⁴² Pu	0.0	0.0	0.54	0.54	0.0	0.0	0.0	0.0
Total	4,576.45	13,163.96	22,958.02	40,698.43	4,576.45	36,211.00	0.0	40,787.45
	Cycle 6 Discharged				Cycle 7 Discharged			
	Axial Blanket	Radial Blanket		Total	Axial Blanket	Radial Blanket		Total
		Removed	Remaining for Next Cycle			Removed	Remaining for Next Cycle	
Time, fpy	7.865				9.161			
Nuclide, kg								
²³⁵ U	8.98	61.20	0.0	70.18	9.03	21.10	49.54	79.67
²³⁸ U	4,480.94	35,119.34	0.0	39,600.28	4,476.04	12,683.60	22,690.75	39,850.39
²³⁹ Pu	48.11	576.65	0.0	624.76	50.55	262.82	191.22	504.59
²⁴⁰ Pu	5.18	63.99	0.0	69.17	5.58	28.03	18.16	51.77
²⁴¹ Pu	1.96	34.13	0.0	36.09	2.19	13.31	7.74	23.24
²⁴² Pu	0.10	3.11	0.0	3.21	0.12	0.92	0.54	1.58
Total	4,545.27	35,858.42	0.0	40,403.69	4,543.51	13,009.78	22,957.95	40,511.24

^aCycles 6 and 7 alternate for the remainder of reactor history.

Table C.3. Refueling Schedule of Core and Axial Blanket Regions of Intermediate-Pressure System

		Cycle 1 ^a Loaded		Cycle 2 ^b Loaded				
		Total		Recycle	Makeup	Remaining from Cycle 1	Total	
Time, fpy	0			0.712				
Nuclide, kg								
²³⁵ U		124.34			41.25	69.22	110.47	
²³⁸ U		41,321.40			13,701.62	27,013.18	40,714.80	
²³⁹ Pu		2,532.90			938.02	1,784.26	2,722.28	
²⁴⁰ Pu		1,073.20			373.47	729.52	1,102.99	
²⁴¹ Pu		674.94			249.92	372.65	622.57	
²⁴² Pu		239.59			83.19	160.66	243.85	
Total		45,966.37			15,387.47	30,129.49	45,516.96	
		Cycle 1 Discharged		Cycle 2 Discharged				
		Removed	Remaining for Cycle 2	Total	Recycle	Removed	Remaining for Cycle 3	Total
Time, fpy	0.712				1.45			
Nuclide, kg								
²³⁵ U		34.62	69.22	103.84		29.22	63.57	92.79
²³⁸ U		13,506.86	27,013.18	40,520.04		13,242.20	26,672.00	39,914.20
²³⁹ Pu		892.16	1,784.26	2,676.42		928.34	1,896.46	2,824.80
²⁴⁰ Pu		365.18	729.52	1,094.70		373.24	755.73	1,128.97
²⁴¹ Pu		186.32	372.65	558.97		159.22	363.79	523.01
²⁴² Pu		80.34	160.66	241.00		79.08	163.39	242.47
Total		15,065.48	30,129.49	45,194.97		14,811.30	29,914.94	44,726.24

^aThe fabricated fuel is of two enrichments, given by $\epsilon_i \bar{E}$, where $\bar{E}$ = average enrichment:

Zone	Fraction of Fuel	ϵ_i
1	0.486	0.9205
2	0.514	1.0751

Of the initial loading specified, the following is loaded into the axial blanket: ²³⁵U, 42.48 kg; ²³⁸U, 14,119.17 kg.

^bFor cycle 2 and later cycles of the loading specified the following is loaded into the axial blanket: ²³⁵U, 14.16 kg; ²³⁸U, 4,706.39 kg.

Table C.3 (continued)

	Cycle 12 ^c Loaded				Cycle 13 Loaded			
	Recycle	Makeup	Remaining from Previous Cycle	Total	Recycle	Makeup	Remaining from Previous Cycle	Total
Time, fpy	8.13				8.89			
Nuclide, kg								
²³⁵ U		41.19	58.36	99.55		39.33	62.24	101.57
²³⁸ U		13,690.87	26,223.66	39,914.53		13,423.74	26,423.93	39,847.67
²³⁹ U	1,083.11		2,085.36	3,168.47	1,042.14		2,101.22	3,177.03
²⁴⁰ Pu	376.62		865.99	1,242.61	444.44		828.30	1,287.02
²⁴¹ Pu	118.79		256.68	375.47	102.83		246.11	357.92
²⁴² Pu	67.00		154.66	221.66	81.17		139.97	224.33
Total	1,645.52	13,732.06	29,644.71	45,022.29	1,670.58	13,523.19	29,801.77	44,995.54
	Cycle 12 Discharged				Cycle 13 Discharged			
	Recycle	Removed	Remaining for Next Cycle	Total	Recycle	Removed	Remaining for Next Cycle	Total
Time, fpy	8.89				9.63			
Nuclide, kg								
²³⁵ U		20.83	62.64	83.07		26.71	58.36	85.07
²³⁸ U		12,663.07	26,423.93	39,087.00		12,808.67	26,223.66	39,032.33
²³⁹ Pu		1,078.74	2,101.22	3,179.96		1,054.80	2,085.36	3,140.16
²⁴⁰ Pu		444.54	828.30	1,272.84		461.58	865.99	1,327.57
²⁴¹ Pu		119.31	246.11	365.42		104.18	256.68	360.86
²⁴² Pu		73.04	139.97	213.01		67.64	154.66	222.30
Total		14,399.53	29,801.77	44,201.30		14,523.58	29,644.71	44,168.29

^cCycles 12 and 13 alternate for the remainder of the reactor history.

Table C.4. Refueling Schedule for Radial Blanket Region of Intermediate-Pressure System

Cycle 1 Loaded			Cycle 2 Loaded		
	New Material	Total	New Material	Remaining from Cycle 1	Total
Time, fpy	0		0.712		
Nuclide, kg					
235U	115.79	115.79		109.98	109.98
238U	38,479.00	38,479.00		38,358.80	38,358.80
239Pu	0.00	0.00		108.09	108.09
240Pu	0.00	0.00		1.36	1.36
241Pu	0.00	0.00		0.17	0.17
242Pu	0.00	0.00		0.00	0.00
Total	38,594.79	38,594.79		38,578.40	38,578.40
Cycle 1 Discharged			Cycle 2 Discharged		
	Removed	Remaining for Cycle 2	Removed	Remaining for Cycle 3	Total
Time, fpy	0.712		1.45		
Nuclide, kg					
235U		109.98	32.50	71.90	104.40
238U		38,358.80	12,678.80	25,552.20	38,231.00
239Pu		108.09	122.75	91.47	214.22
240Pu		1.36	3.31	1.50	4.81
241Pu		0.17	0.81	0.31	1.12
242Pu		0.00	0.02	0.00	0.02
Total		38,578.40	12,838.19	25,717.38	38,555.57
Cycle 12 ^a Loaded			Cycle 13 Loaded		
	New Material	Remaining from Previous Cycle	New Material	Remaining from Previous Cycle	Total
Time, fpy	8.13		8.89		
Nuclide, kg					
235U		100.37	39.58	65.88	105.46
238U		38,110.64	12,844.76	25,385.37	38,230.13
239Pu		298.68		203.03	203.03
240Pu		9.87		6.45	6.45
241Pu		3.66		2.21	2.21
242Pu		0.10		0.06	0.06
Total		38,523.32	12,884.34	25,663.00	38,547.34
Cycle 12 Discharged			Cycle 13 Discharged		
	Removed	Remaining for Next Cycle	Removed	Remaining for Next Cycle	Total
Time, fpy	8.89		9.63		
Nuclide, kg					
235U	28.83	65.88		100.37	100.37
238U	12,564.23	25,385.37		38,110.64	38,110.64
239Pu	208.62	203.03		298.68	298.68
240Pu	10.04	6.45		9.87	9.87
241Pu	5.21	2.21		3.66	3.66
242Pu	0.22	0.06		0.10	0.10
Total	12,817.15	25,663.00		38,523.32	38,523.32

^aCycles 12 and 13 alternate for remainder of reactor history.

Table C.5. Refueling Schedule for Core Region of Low-Pressure System

	Cycle 1 Loaded				Cycle 10 ^a Loaded			
	Zone 1	Zone 2	Zone 3	Total	Zone 1	Zone 2	Zone 3	Total
Time, fpy	0.0				20.703			
Nuclide, kg								
²³⁵ U	29.08	24.57	31.48	85.13	29.81	24.66	31.61	85.45
²³⁸ U	9,663.80	8,164.80	10,461.80	28,290.40	9,696.80	8,196.00	10,507.40	28,400.20
²³⁹ Pu	780.00	730.52	1,082.96	2,593.48	1,030.20	972.78	1,430.42	3,433.40
²⁴⁰ Pu	330.50	312.04	458.84	1,101.38	255.86	241.58	355.24	852.68
²⁴¹ Pu	207.84	196.24	288.58	692.66	49.47	46.71	68.68	164.86
²⁴² Pu	73.78	69.67	102.45	245.90	22.92	21.64	31.82	76.37
Total	11,085.00	9,497.84	12,426.11	33,008.95	11,084.43	9,503.37	12,425.17	33,012.97
	Cycle 1 Discharged				Cycle 10 Discharged			
	Zone 1	Zone 2	Zone 3	Total	Zone 1	Zone 2	Zone 3	Total
Time, fpy	1.737				23.116			
Nuclide, kg								
²³⁵ U	13.75	12.77	21.22	47.74	9.94	10.00	18.62	38.56
²³⁸ U	8,836.00	7,544.20	9,970.56	26,350.76	8,526.00	7,351.40	9,851.00	25,728.40
²³⁹ Pu	932.22	819.48	1,128.53	2,880.23	1,048.50	935.64	1,338.40	3,322.54
²⁴⁰ Pu	346.62	320.29	466.48	1,133.39	326.52	292.20	399.88	1,018.60
²⁴¹ Pu	122.14	119.39	203.14	444.67	61.53	54.28	71.81	187.62
²⁴² Pu	72.34	69.06	103.91	245.31	23.15	21.53	31.29	75.97
Total	10,323.07	8,885.19	11,893.84	31,102.10	9,995.64	8,665.05	11,711.00	30,371.69

^aCycle 10 repeats for the remainder of the reactor history.

Table C.6. Refueling Schedule for Blanket Regions of Low Pressure System

	Cycle 1 Loaded			Cycle 10* Loaded		
	Axial Blanket	Radial Blanket	Total	Axial Blanket	Radial Blanket	Total
Time, fpy	0.08			20.703		
Nuclide, kg						
²³⁵ U	104.65	62.55	167.20	104.65	62.55	167.20
²³⁸ U	34,796.00	20,798.00	55,594.00	34,796.00	20,798.00	55,594.00
Total	34,900.65	20,860.55	55,761.20	34,900.65	20,860.55	55,761.20
	Cycle 1 Discharged			Cycle 10 Discharged		
	Axial Blanket	Radial Blanket	Total	Axial Blanket	Radial Blanket	Total
Time, fpy	1.737			23.116		
Nuclide, kg						
²³⁵ U	78.52	53.64	132.16	70.74	51.12	121.86
²³⁸ U	34,046.00	20,596.20	54,642.20	33,742.00	20,494.00	54,236.00
²³⁹ Pu	595.95	198.17	794.12	788.38	256.18	1,044.56
²⁴⁰ Pu	25.49	4.81	30.30	43.06	7.81	50.87
²⁴¹ Pu	6.33	0.69	7.02	12.83	1.39	14.22
²⁴² Pu	0.18	0.01	0.19	0.50	0.02	0.52
Total	34,752.47	20,853.52	55,605.99	34,657.51	20,810.52	55,468.03

*Cycle 10 repeats for remainder of reactor history.

Table C.7. Neutron Balance for
High Pressure System

Nuclide	Startup	Start of Cycle 6	End of Cycle 6
Core			
Hydrogen	0.00006	0.00007	0.00008
Boron	0.10480	0.07943	0.00000
Carbon	0.00000	0.00000	0.00000
Oxygen	0.00254	0.00239	0.00216
Structure	0.01364	0.01374	0.01507
²³⁵ U	0.00508	0.00505	0.00316
²³⁶ U	0.00000	0.00000	0.00019
²³⁸ U	0.18009	0.17496	0.16857
²³⁹ Pu	0.24325	0.26776	0.22629
²⁴⁰ Pu	0.04811	0.04942	0.05413
²⁴¹ Pu	0.08335	0.04526	0.04078
²⁴² Pu	0.01353	0.01011	0.01019
Fission products	0.00000	0.00000	0.03085
Blanket			
Hydrogen	0.00188	0.00182	0.00174
Oxygen	0.00102	0.00120	0.00152
Structure	0.04626	0.04182	0.03826
²³⁵ U	0.02380	0.01753	0.00807
²³⁶ U	0.00000	0.00022	0.00073
²³⁸ U	0.21776	0.22514	0.22918
²³⁹ Pu	0.00000	0.03409	0.10073
²⁴⁰ Pu	0.00000	0.00607	0.01943
²⁴¹ Pu	0.00000	0.00191	0.00755
²⁴² Pu	0.00000	0.00010	0.00054
Fission products	0.00000	0.00405	0.01490
Leakage	0.01483	0.01786	0.02588
	1.00000	1.00000	1.00000

Table C.8. Neutron Balance for
Intermediate Pressure System

Nuclide	Startup	Start of Cycle 11	End of Cycle 11
Core and axial blanket			
Hydrogen	0.00006	0.00006	0.00006
Boron	0.03003	0.02292	0.00000
Carbon	0.00000	0.00000	0.00000
Oxygen	0.00290	0.00282	0.00277
Structure	0.09932	0.09689	0.09889
^{235}U	0.01083	0.00812	0.00644
^{236}U	0.00000	0.00015	0.00028
^{238}U	0.37530	0.35078	0.34596
^{239}Pu	0.24885	0.29801	0.30032
^{240}Pu	0.05199	0.05969	0.06221
^{241}Pu	0.09042	0.04467	0.04398
^{242}Pu	0.01344	0.01112	0.01093
Uranium fission products	0.00000	0.00166	0.00327
Plutonium fission products	0.00000	0.01413	0.02823
Radial blanket			
Hydrogen	0.00002	0.00002	0.00002
Oxygen	0.00018	0.00022	0.00023
Structure	0.00774	0.00792	0.00806
^{235}U	0.00284	0.00250	0.00232
^{236}U	0.00000	0.00004	0.00006
^{238}U	0.05518	0.05768	0.05927
^{239}Pu	0.00000	0.00685	0.01050
^{240}Pu	0.00000	0.00070	0.00128
^{241}Pu	0.00000	0.00006	0.00018
^{242}Pu	0.00000	0.00000	0.00000
Uranium fission products	0.00000	0.00011	0.00017
Plutonium fission products	0.00000	0.00004	0.00021
Leakage	0.01090	0.01284	0.01436
	1.00000	1.00000	1.00000

Table C.9. Neutron Balance for Low-Pressure System

Nuclide	Absorptions		
	Startup	Start of Equilibrium Cycle	End of Equilibrium Cycle
Core			
Hydrogen	0.00001	0.00001	0.00001
Boron	0.03192	0.00703	0.00000
Carbon	0.00000	0.00000	0.00000
Oxygen	0.00322	0.00323	0.00287
Structure	0.03454	0.03665	0.03395
²³⁵ U	0.00846	0.00903	0.00341
²³⁶ U	0.00000	0.00000	0.00025
²³⁸ U	0.33501	0.35242	0.29428
²³⁹ Pu	0.22202	0.30375	0.27185
²⁴⁰ Pu	0.04639	0.03712	0.04130
²⁴¹ Pu	0.08582	0.02107	0.02273
²⁴² Pu	0.01131	0.00370	0.00335
Uranium fission products	0.00000	0.00000	0.00470
Plutonium fission products	0.00000	0.00000	0.03617
Axial blanket			
Hydrogen	0.00003	0.00003	0.00002
Oxygen	0.00038	0.00038	0.00062
Structure	0.02250	0.02312	0.02241
²³⁵ U	0.00536	0.00553	0.00311
²³⁶ U	0.00000	0.00000	0.00016
²³⁸ U	0.12153	0.12470	0.13058
²³⁹ Pu	0.00000	0.00000	0.04141
²⁴⁰ Pu	0.00000	0.00000	0.00438
²⁴¹ Pu	0.00000	0.00000	0.00104
²⁴² Pu	0.00000	0.00000	0.00002
Uranium fission products	0.00000	0.00000	0.00070
Plutonium fission products	0.00000	0.00000	0.00144
Radial blanket			
Hydrogen	0.00004	0.00004	0.00003
Oxygen	0.00013	0.00013	0.00013
Structure	0.00163	0.00165	0.00127
²³⁵ U	0.00182	0.00185	0.00108
²³⁶ U	0.00000	0.00000	0.00003
²³⁸ U	0.04127	0.04174	0.03342
²³⁹ Pu	0.00000	0.00000	0.00594
²⁴⁰ Pu	0.00000	0.00000	0.00043
²⁴¹ Pu	0.00000	0.00000	0.00004
²⁴² Pu	0.00000	0.00000	0.00000
Uranium fission products	0.00000	0.00000	0.00008
Plutonium fission products	0.00000	0.00000	0.00009
Leakage	0.02661	0.02682	0.03670
	1.00000	1.00000	1.00000

Appendix D

THERMAL-HYDRAULIC ANALYSES

Comparison of Correlations for Convective Heat
Transfer Coefficients of Reactor Cores

In calculating the cladding surface temperatures for the low pressure system, a comparison was made between values developed by B&W and those determined in ORNL calculations. From this comparison it was apparent that the heat transfer coefficients predicted in the respective calculations were significantly different.

The correlation used by B&W was one presented by Heineman,^{34*} although the correlation was not applied as he recommended. Heineman's equations are shown below:

1. For $6 < L/D_e \leq 60$,

$$\frac{hD_e}{k_f} = 0.0157 \left(\frac{GD_e}{\mu_f} \right)^{0.8} \left(\frac{C_p \mu}{k} \right)_f^{1/3} \left(\frac{L}{D_e} \right)^{-0.04} \quad (1)$$

2. For $L/D_e > 60$,

$$\frac{hD_e}{k_f} = 0.0133 \left(\frac{GD_e}{\mu_f} \right)^{0.8} \left(\frac{C_p \mu}{k} \right)_f^{1/3} \quad (2)$$

B&W used Eq. (1) and omitted the L/D_e term, which tends to decrease the value of h as the distance from the entrance increases. To justify this approach, B&W stated that the combination of the honeycomb spacers, which separate fuel pins, and varying pin diameters **would** produce an enhancement of the heat transfer coefficient over the entire pin length equivalent to that produced by an abrupt entrance.

The subject of heat transfer to fuel pins separated by spacers was investigated experimentally at ORNL for the EGCR fuel bundles.¹⁰¹ These bundles had spacers that would be expected to produce considerably more heat transfer enhancement than those used in the B&W steam-cooled design.

*References are those given at end of text of this report.

The ORNL results indicate that the local Nusselt number is increased by the spacer a maximum of 57% above the upstream value. This enhancement is less than one-half the amount produced by the entrance to the bundle (see Fig. D.1). Proceeding downstream from the spacer the Nusselt number diminishes rapidly and becomes asymptotic to the undisturbed value at 20 hydraulic diameters. After only 13 diameters, 90% of the effect of the spacer has been lost. In the case of the B&W design, 13 hydraulic diameters represent only 1.1 in. from the end of the spacer grid. This means that 18 in. or 62% of the active fuel-pin length has a heat transfer coefficient that is not significantly enhanced by the presence of the honeycomb spacer grid.

The fuel pins **were** slightly reduced in diameter along a 2-in. axial length, which **was** centered at the spacer region. According to the design report, the purpose of this change in diameter **was** to avoid the additional pressure loss that the change in flow area at the spacers would otherwise introduce. A radial reduction of less than 10 mils will maintain the flow area constant. Because of the development of stress in the cladding, the diameter change is not likely to be abrupt. Yet, unless it is abrupt, the surface resistance to the transfer of heat due to the laminar sublayer will not be materially affected. The principal turbulence promotion caused by the spacers will be due to the form drag caused

Fig. D.1. Local Nusselt Modulus as a Function of Axial Position for Tube 4 Series 4.3. (From Ref. 101)

by the grid. For the most part, this turbulence will be at the boundary-layer interface, and penetration of turbulent eddies to the surface will be small. Limited boundary-layer disruption will be caused by the dimples and spring clips.

It was found that the "hot cladding temperature" exists over a pin length approximately equal to the axial distance between spacers in the low pressure design. Because of this, and in view of the conclusions of the two previous paragraphs, it is felt that a correlation based on fully developed flow is appropriate for prediction of the heat transfer coefficients. The use of Eq. (1), with the L/D_e term omitted, which gives an 18% increase in heat transfer coefficient compared with Eq. (2), does not appear to be justified.

In addition to Heineman,³⁴ Sutherland³³ has published experimental results for heat transfer to superheated steam. His equation is

$$\frac{hD_e}{k_b} = 0.021 \left(\frac{GD_e}{\mu_b} \right)^{0.8} \left(\frac{C_p \mu}{k} \right)_b^{0.6} \left(\frac{T_b}{T_w} \right)^{0.575} \quad (3)$$

In order to compare the heat transfer coefficients predicted by Eqs. (2) and (3), a program was written for the IBM 360 facility. Calculations were performed with the values of heat flux, mass flux, pressure, and temperature that occur in the hot bundle of the low pressure system. (Input data for the computer codes are given in Table D.1.) The Sutherland correlation predicted coefficients which, at a heat flux equal to the hot-bundle maximum, were higher than those from the Heineman equation under pressure and temperature conditions representative of those at the core entrance. For the conditions at the core midplane, however, it underpredicted the Heineman coefficient by about 25%. Table D.2 shows that these extrapolated conditions represent a heat flux that is far in excess of that used in the Sutherland experiment.

Calculations were also made with 60% of the maximum heat flux in the steam-cooled design. This approximates the heat flux at the core exit or entrance. The same general observations as noted above apply under these reduced heat flux conditions, except that under these conditions

Table D.1. Input Data for Computer Codes

High-pressure reactor

D_e , equivalent diameter, ft	
Between spacer grids	0.01215
At spacer grids	0.00488
Flow area, ft ²	
Between spacer grids	0.113230
At spacer grids	0.089286
Power distribution	(a)
Flux peaking factors at beginning of life	
Radial	(a)
Axial	1.7268
Number of fuel bundles	
Inner region	66
Outer region	42
Flow, lb/hr	
Inner region	15.127×10^6
Outer region	9.171×10^6
Total thermal power, Mw	2326
Inlet temperature, °F	760

Intermediate-pressure reactor

D_e , equivalent diameter, in.	
In core	0.110064
In lower blanket	0.2738749
Flow area, in. ²	
In core	13.6045076
In lower blanket	16.6867043
Power distribution, Mw(th)	
Inner region	
Upper axial blanket	12.1
Core	1301
Lower axial blanket	18.9
Total	1332
Outer region	
Upper axial blanket	8.31
Core	1046
Lower axial blanket	12.5
Total	1067
Radial blanket	120
Total	2519
Flux peaking factors	
Radial	1.18
Axial	1.345
Equivalent number of bundles in outer region	89.75
Total reactor flow, lb/hr	2.517×10^7
Flow to outer region, lb/hr	1.12×10^7
Flow to hot bundle, lb/hr	
Beginning of life	1.666×10^5
End of life	1.8137×10^5

^aTotal from sponsor's report⁷ for beginning of life.

Table D.1 (continued)

Intermediate-pressure reactor (continued)

Power to hot bundle, Mw(th)	
Beginning of life	15.56
End of life	17.0
Engineering hot-channel factors	
On fuel element power at reloading	1.09
On Δt_f at reloading	1.07
On q/A at reloading	1.05
Enthalpy rise in radial and upper axial blankets	
Power, Mw(th)	124.15
Enthalpy rise, Btu/lb	17.1
Inlet enthalpy, Btu/lb	1116.4
Inlet temperature, °F	688
Pressure at bundle inlet, ^b psia	2665

Low-pressure reactor

D_e , equivalent diameter, in.	
In core	0.1321357
Out of core	0.1636403
Flow area, in. ²	
In core	17.401689
Out of core	21.338532
Spacer frontal area, in. ²	2.2909248
Power distribution at end of life ^c	
Core	
%	0.852
Mw(th)	2470
Axial blanket	
%	0.127
Mw(th)	368.3
Radial blanket	
%	0.222
Mw(th)	63.8
Flux peaking factors ^c	
Radial	1.41
Axial	1.18
Number of fuel bundles	271
Core flow, lb/hr	34.22×10^6
Total reactor power, Mw(th)	2900
Data for calculating change in inlet temperature due to heat input of upper axial blanket	
Heat input, Mw(th)	184.1
Btu/lb	18.37
Inlet enthalpy, Btu/lb	1203.4
Inlet temperature, °F	589.0

^bFrom Ref. 66.^cORNL Calculation.

Table D.2. Comparison of Heat Transfer Conditions

	Heineman Correlation	Sutherland Correlation	Low Pressure Design Predictions ^a	Intermediate Pressure Design Predictions ^a
Heat flux, Btu/hr·ft ²	50,000 to 287,000	25,000	673,000 (max) 418,000 (av) ^b	480,000 (max)
Mass flux, lb _m /hr·ft ²	140,000 to 790,000	28,000 to 129,000	1,306,000	2,010,000
Reynolds number	20,000 to 370,000	64,000 to 328,000	194,000 to 274,000	275,000 (max)
Bulk temperature, °F	550 to 900	556 to 729	575 to 1020	720 to 1198
Pressure, psia	300 to 1500	1000	1154 to 1250	2555 to 2666

^aNumbers are approximate.^bBeginning of life.

the Sutherland correlation is closer to that of Heineman over the higher temperature range.

The Sutherland correlation was developed under low heat flux, temperature, and mass flow conditions. Hence it cannot be extrapolated with confidence to reactor core conditions. Even though the Sutherland correlation presents a better theoretical argument, Heineman's Eq. (2) correlates data much closer to reactor hot-bundle conditions, particularly with respect to heat flux. While the Sutherland correlation is probably more accurate in the range of conditions near saturation, the Heineman correlation, as given in Eq. (2), appears to be supported by recent experimental evidence for conditions similar to those encountered at the core midplane where the maximum flux occurs.

A literature survey through December 1967 indicated that nothing significant has been published since the Sutherland paper on the subject of heat transfer to superheated steam. Thus, since a choice must be made, the preferred equation at present is the Heineman equation for fully developed flow, Eq. (2). This equation is applicable to both the low and intermediate pressure designs for the steam-cooled fast breeder reactor.

Thermal Conductivity of the Vibration Compacted Mixed Oxides of Uranium and Plutonium

A rather extensive review of the literature was made to determine appropriate values for the thermal conductivity of the vibration compacted UO_2 - PuO_2 mixture used as fuel in the three steam-cooled reference designs. An evaluation of the data collected is presented, along with comments from direct conversation with experimenters in the field, in Ref. 40.

Little information was found on experimental determinations of the thermal conductivity of these mixed oxides, particularly at the proposed temperatures and linear heat ratings for the low and high pressure systems. The maximum linear heat rating for the intermediate pressure system was significantly lower than the heat ratings for the other two designs. None of the experimental fuel elements have had backfill helium pressures similar to those proposed for the steam-cooled fuel elements. Only one

experiment approximated the operating temperatures and heat ratings proposed, and there was disagreement by different persons who evaluated this experiment as to whether center melting occurred in the fuel.

In the absence of directly applicable data, it appeared that the best procedure would be to approximate the thermal conductivity of the powder as a function of temperature, and, after allowing for a self-sintering effect, integrate the function of thermal conductivity as a function of temperature over the temperature range of interest to obtain values comparable with the experimentally determined values of the $\int k(T) dT$.

The temperature range of interest is from surface temperatures around 1400°F (760°C) to center-line temperatures near the melting point of the mixed oxides (about 4960°F or 2738°C). In the reactor, the centers of the fuel rods would be at such a high temperature that they would essentially sinter to the same form as a presintered pellet. Around this sintered core, there would be a region of columnar grains that are partially sintered. Around the perimeter of the fuel would be the vibration compacted powder in essentially the same form as that originally loaded in the rod.

The interface between these regions was estimated on the basis of the performance of UO₂ pellets when they are sintered outside the core. This sintering process begins with "green" UO₂ that is considerably less dense (less than 40% theoretical density) and much more "active" than the powder granules loaded in the rods. Therefore, it would be expected that the vibration compacted powder would sinter much less easily and at higher temperatures than the "green" UO₂. Discussions with persons familiar with the sintering operation indicated that the material undergoes sintering very slowly (about one year required to achieve complete sintering) at temperatures around 1200°C. At 1800°C, the sintering operation is complete in a matter of hours. Since the vibration compacted powder is less "active," it was assumed that no sintering would be achieved at temperatures below 1400°C, and it was assumed that sintering would be complete at temperatures of 1800°C and above.

For an estimate of the thermal conductivity of the mixed oxides with high helium backfill pressures, an examination was made of data for the thermal conductivity of powders in general. It was found that the thermal conductivity of the powders was relatively insensitive to the nature of the heavy metal oxide, but was quite sensitive to the thermal conductivity of the backfill gas. It appeared that as long as the mean free path of the gas molecule was greater than the interstitial distance between particles, a low thermal conductivity rate was obtained. The thermal conductivity increased with increasing pressure until the mean free path became less than the interstitial distances; then the effect of pressure was "saturated," and no further increase was obtained. Some of the best data available were for 83% theoretical-density ThO_2 , for which thermal conductivities in the temperature range of interest were about $0.0125 \text{ w/cm}\cdot^\circ\text{C}$ ($0.723 \text{ Btu/hr}\cdot\text{ft}\cdot^\circ\text{F}$). For the backfill pressures investigated, the pressure effect had not been "saturated" at these temperatures. It was estimated that the increase in thermal conductivity due to higher pressures would, in part, be counteracted by the decrease in thermal conductivity of the solid in changing from ThO_2 to the mixed $\text{UO}_2\text{-PuO}_2$.

For the fully sintered powder, it was assumed that the thermal conductivity was the same as the thermal conductivity of the sintered pellets. Values for the thermal conductivity of the sintered UO_2 were obtained from Ref. 41, which indicate that the experimental data for k as a function of T can be expressed by

$$k(T) = \frac{38.24}{T + 129.4} + (4.788 \times 10^{-13})T^3, \quad (4)$$

where T is in degrees Kelvin. This correlation is for sintered material of 95% theoretical density, and the $\int k(T) dT$ from 0°C to the melting point is 90 w/cm .

The mixed oxides of uranium and plutonium will have a slightly lower thermal conductivity than the values indicated above due to the fact that the thermal conductivity of PuO_2 is lower than that of UO_2 . However, this effect will be small, and the decrease is not included in this calculation. A correction that is included is the decrease in thermal

conductivity due to the lower density. It is recommended that the correction be of the form

$$k(T) = k_0(T) \frac{1 - P}{1 + \frac{1}{2} P}, \quad (5)$$

where P is the porosity (void fraction) and $k_0(T)$ is the thermal conductivity at 100% theoretical density.

The relationship in Eq. (4) is corrected for 85%-dense oxide by multiplying by 0.791/0.927 or 0.853. The value of thermal conductivity at 1800°C was found to be 0.0194 w/cm.°C or 1.12 Btu/hr.ft.°F. It was then assumed that the thermal conductivity of the partially sintered powder in the range of 1400 to 1800°C varied linearly with temperature from a value of 0.0125 to 0.0194 w/cm.°C. The expression for this relationship is thus

$$k(T) = -0.0164 + (1.725 \times 10^{-5})T, \quad (6)$$

where T is in degrees Kelvin.

The total integral from the surface temperature of the powder, T_s , to the center temperature, T_m , is found by the summation of the integrals

$$\begin{aligned} \int_{T_s}^{T_m} k(T) dT &= \int_{T_s}^{1673} 0.0125 dT \\ &+ \int_{1673}^{2073} [-0.0164 + (1.725 \times 10^{-5})T] dT \\ &+ \int_{2073}^{T_m} \left[\frac{32.62}{T + 129.4} + (4.085 \times 10^{-13})T^3 \right] dT, \end{aligned} \quad (7)$$

which gives

$$\begin{aligned} \int_{T_s}^{T_m} k(T) dT &= 27.28 - 0.0125T_s \\ &+ 32.62 \ln \frac{T_m + 129.4}{2202.4} + (1.0213 \times 10^{-13})T_m^4. \end{aligned} \quad (8)$$

For a surface temperature of 1532°F (833.3°C or 1106.3°K) and a center-line temperature equal to the melting temperature of 4960°F (2737°C or 3010.8°K), the value of the integral is 33.41 w/cm.

If this value of the integral is equated to $q/4\pi$, where q is the linear heat rating of the fuel, it is found that the maximum linear heat rating that can be obtained with a center temperature below the melting point is 13.78 kw/ft. Of course, if the surface temperature of the fuel is lowered significantly, the value of the integral will be increased, and the maximum linear heat rating may be increased without reaching the melting temperature at the center of the fuel rod. It appears, however, that the value of 1532°F is representative of the concepts being examined. In addition, the accumulation of fission products within the oxide tends to lower the melting point so that after long-term irradiation the melting point may be reduced to 4800°F or less.

For the uniform generation of heat in an infinite cylinder, it can be shown that

$$\int_{T_s}^{T_m} k(T) dT = \frac{1}{4\pi} q, \quad (9)$$

where q is the linear heat rating. Therefore, a combination of this equation with Eq. (8) can be used to determine the center-line temperature when the surface temperature and the linear heat rating are known. Since Eq. (8) cannot be solved explicitly in terms of T_m for this calculation, an approximation was used. The nature of this approximation can best be visualized from Fig. D.2, which presents the thermal conductivity as just described as a function of temperature. The cross-hatched area under the curve is an indication of the value of the integral.

The value of the integral is 33.41 w/cm over the temperature range from 1106.3°K to 3010.8°K, as noted above. Dividing the value of the integral over this range by the temperature difference (the length of the abscissa on the plot) produces a mean effective value of k for the interval (or average ordinate for the area). This value is 0.0175 w/cm·°C or 1.01 Btu/hr·ft·°F. If it can be assumed that the value of the mean k remains relatively constant for minor variations in the limits

Fig. D.2. Thermal Conductivity of Vibration-Compacted UO₂ Powder.

of the integral (which is certainly true within the accuracy that the thermal conductivity is defined), the center temperature may be calculated from the relatively simple relationship

$$\int_{T_s}^{T_m} k dT \cong \bar{k}(T_m - T_s) = \frac{q}{4\pi}, \quad (10)$$

$$T_m = T_s + \frac{q}{4\pi\bar{k}}. \quad (11)$$

An examination of Fig. D.2 indicates that if the surface temperature of the fuel is less than 1532°F, the proportionate area increase of the integral will be less than 1.01 Btu/hr.ft.°F multiplied by the temperature integral, so the estimation of the center-line temperature will be slightly conservative. On the other hand, the estimation of a temperature at the center that is less than the melting temperature will be slightly high. It is also possible, due to this method of calculation, to predict center temperatures greater than the melting temperature. When this is done, the temperatures should be considered "fictitious," since the thermal conductivity of the melted oxide will be different from the thermal conductivity of the solid.

To represent the situation that exists in the fuel rod when center-line melting occurs, it was assumed that the melted fuel was essentially at constant temperature (the melting temperature), and the volume of this melted core was calculated from the relationship

$$\int_{T_s}^{T_m} k(T) dt = \frac{q}{4\pi} \left(1 - \frac{2b^2}{a^2 - b^2} \ln \frac{a}{b} \right), \quad (12)$$

where a is the outer radius of the fuel and b is the radius of the melted center.

It is also easy to visualize the advantage of fuel pellets rather than vibration compacted powdered fuel by examining Fig. D.2. In this case, the area between the two solid curves (the upper curve for solid pellets and the lower curve for the powder) between the temperature limits represents the increase in the value of the integral and, therefore, the increase in the linear heat rating that may be obtained from sintered pellets.

It has been postulated that center voids will form in the powder, since it is quite possible that voids will form during the self-sintering, and that these voids will migrate to the center of the rod; however, this will be a relatively slow process. All the experimental data found that indicated the rapid (within minutes) formation of a center hole were from experiments that were accompanied by center melting. The hole, of course, was found when the fuel slug was examined at room temperature and the melted center had solidified on the surrounding fuel. However, due to the relatively large volumetric expansion of UO_2 upon heating and also the volumetric expansion on melting, it is possible that the center hole did not exist at reactor temperatures.

In conjunction with this evaluation of the thermal conductivity of the vibration compacted powders, it is necessary to evaluate the appropriate corresponding contact coefficient. While it is admitted that the powder will be forced against the cladding during the packing operation, the area of contact will be limited to the "edges" of the particles. In addition, the force that the sintered pellets will exert on the cladding due to thermal expansion will not be exhibited by the vibration compacted

powder. The contact coefficient will, at best, equal that of low density pellets. This value is generally estimated⁴¹ to be about 2000 Btu/hr·ft²·°F.

Maximum Linear Heat Rating as a Function of Surface Temperature

The maximum linear heat rating compatible with the requirement that no center melting occur was calculated as a function of the outer cladding surface temperature. This calculation was performed with the most probable value based on present experimental data for the thermal conductivity of the fuel as a function of temperature. The two types of fuel considered were vibration compacted mixed oxides or uranium and plutonium with a theoretical density of 85%, and sintered pellets of the mixed oxides with a theoretical density of 95%. The first ~~was~~ the fuel proposed for use with the designs in this study; the second is representative of the fuels proposed for use in liquid metal-cooled fast breeder reactors.

The results of these calculations are given in Tables D.3 and D.4. To provide a basis for comparison in these calculations, it was assumed that the outside diameter of the cladding was 0.350 in., and that the cladding thickness was 0.010 in. The cladding was assumed to be Inconel 625 with a thermal conductivity of 138 Btu/in.·hr·ft²·°F or 11.5 Btu/hr·ft·°F. The contact coefficients which were used in these studies were 2000 Btu/hr·ft²·°F for the vibration compacted oxide and 3500 Btu/hr·ft²·°F for the sintered pellets.

In using Tables D.3 and D.4 to compare the surface temperatures and the linear heat ratings for the steam-cooled and liquid metal-cooled reactors, there is one important point which should be kept in mind that the proponents of the steam-cooled reactor have not emphasized in their comparisons with the liquid metal-cooled designs. The point is that the linear heat ratings and the surface temperatures given in Tables D.3 and D.4 must occur at the same location in the reactor. It is not meaningful to indicate a maximum linear heat rating that occurs at one axial position and a maximum surface temperature which occurs at another axial position. This point can be better visualized by referring to Figs. D.3 and D.4.

Table D.3. Heat Ratings and Temperatures for
Sintered UO_2 - PuO_2 Pellets

Maximum center-line temperature: 4900°F

Heat Rating (kw/ft)	Outer Cladding Surface Temperature ($^\circ\text{F}$)	Inner Cladding Surface Temperature ($^\circ\text{F}$)	Pellet Surface Temperature ($^\circ\text{F}$)
21.9853	592.646	652.006	900
21.5303	649.007	707.138	950
21.0886	705.181	762.121	1000
20.6594	761.181	816.962	1050
20.242	817.017	871.67	1100
19.8356	872.698	926.254	1150
19.4397	928.232	980.72	1200
19.0537	983.629	1035.07	1250
18.677	1038.9	1089.32	1300
18.3091	1094.04	1143.47	1350
17.9495	1149.07	1197.53	1400
17.5979	1203.98	1251.5	1450
17.2537	1258.79	1305.38	1500
16.9167	1313.5	1359.18	1550
16.5864	1368.12	1412.91	1600
16.2625	1422.65	1466.56	1650
15.9447	1477.09	1520.14	1700
15.6327	1531.45	1573.66	1750
15.3262	1585.74	1627.12	1800

Figures D.3 and D.4 are not presented as detailed plots but, rather, as sketches to illustrate the points discussed below. Figure D.4 is based on the calculations for the low pressure steam-cooled design, and Fig. D.3 is based on rather limited values given for a sodium-cooled design.¹⁰² It is assumed that both reactors have the same axial thermal flux distribution, which is shown as the dashed curve with the ordinate at the right.

The bulk coolant and surface cladding temperatures are presented as functions of axial position over the length of the core. The curves for the surface cladding temperature for both reactor concepts are drawn so that the maximum temperature is 1300°F . Since the sodium-cooled reactor has a much higher heat transfer coefficient ($\sim 23,000 \text{ Btu/hr.ft}^2.\text{F}$) than the steam-cooled reactor, the surface temperature follows the

Table D.4. Heat Ratings and Temperatures for
Vibration Compacted UO_2 - PuO_2

Maximum center-line temperature: 4900°F

Heat Rating (kw/ft)	Outer Cladding Surface Temperature ($^\circ\text{F}$)	Inner Cladding Surface Temperature ($^\circ\text{F}$)	Fuel Surface Temperature ($^\circ\text{F}$)
14.2063	581.211	619.568	900
14.0733	634.195	672.193	950
13.9403	687.18	724.819	1000
13.8073	740.164	777.444	1050
13.6743	793.148	830.069	1100
13.5413	846.133	882.694	1150
13.4083	899.117	935.32	1200
13.2753	952.102	987.945	1250
13.1423	1005.09	1040.57	1300
13.0093	1058.07	1093.2	1350
12.8763	1111.05	1145.82	1400
12.7434	1164.04	1198.45	1450
12.6104	1217.02	1251.07	1500
12.4774	1270.01	1303.7	1550
12.3444	1322.99	1356.32	1600
12.2114	1375.98	1408.95	1650
12.0784	1428.96	1461.57	1700
11.9454	1481.95	1514.2	1750
11.8124	1534.93	1566.82	1800

Fig. D.3. Relative Temperature and Flux Conditions over Length of Core for Sodium-Cooled Reactor.

Fig. D.4. Relative Temperature and Flux Conditions over Length of Core for Steam-Cooled Reactor.

development of the bulk coolant temperature more closely than the surface temperature in the steam-cooled reactor, which has a coefficient of about 1500 Btu/hr.ft².°F at the hot spot. Therefore, the maximum surface temperature for the sodium-cooled reactor falls closer to the reactor outlet, where the relative flux is substantially less than the maximum. On the other hand, the maximum surface temperature in the steam-cooled reactor occurs relatively near the midplane, where the relative flux is near its maximum.

Therefore, if the reactors have the same maximum linear heat rating of 18 kw/ft, the heat rating in the sodium-cooled reactor, where the surface temperature is a maximum of 1300°F, is about $18 \times 0.9/1.2$ or 13.5 kw/ft. In the steam-cooled reactor for the same maximums, the linear heat rating at the same linear position as the maximum surface cladding temperature is about $18 \times 1.15/1.2$ or 17.25 kw/ft. It should be noted that a maximum linear heat rating for a reactor and a maximum surface cladding temperature should be identified as corresponding values at the same axial position.

Care should be taken in using values from Tables D.3 and D.4, since attempts to compare the limitations of the steam-cooled and sodium-cooled reactors when only the maximum linear heat rating or the maximum cladding

temperature are considered can be misleading. It is much better to evaluate each design individually than to draw comparisons that may not be applicable.